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Preface

This 61st volume of the TRANSACTIONS of the American Institute of Electrical Engineers marks the end of the transition to the improved publication procedure adopted in August 1940. In accordance with the provisions of that procedure, this volume contains all approved technical-program papers and related discussions presented during the calendar year. Pages 1–312 and 763–892 comprise the papers published in the Transactions Sections of the monthly issues of ELECTRICAL ENGINEERING, and pages 313–762 and 893–1076 comprise papers and discussions that appeared in advance in the June and December 1942 "Supplements to Electrical Engineering—Transactions Section," respectively.

The technical papers and discussions in this volume were presented at these AIEE conventions and District meetings:

1. 1942 winter convention, New York, N. Y., January 26–30, 1942.
2. North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942.
3. 1942 summer convention, Chicago, Ill., June 22–26, 1942.
4. 1942 Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942.

In addition, this volume contains:

1. "Bibliography on Circuit-Interrupting Devices, 1928–1940," prepared under the sponsorship of the AIEE committee on protective devices.
2. "Bibliography on Electrical Safety 1930–1941," prepared by the AIEE committee on safety.
3. "Bibliography on Automatic Stations 1930–1941," prepared by the AIEE committee on automatic stations.
4. Report, "Simplified Calculation of Fault Currents," prepared by a working group under the sponsorship of the AIEE committee on protective devices.
5. 1942 annual report of the AIEE board of directors.
6. A complete listing of AIEE officers and committees for 1942–43.

Full correlation of all material in this volume has been accomplished by means of the multientry index beginning on page 1157. Reference to any of the several subject entries for a technical paper will lead directly to the paper and to any published discussion on that paper.

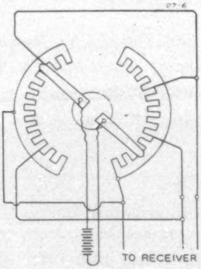
Statements and opinions given in papers and discussions published in TRANSACTIONS are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

Errata

1. Page 139, column 3, paragraph 1, line 1. "*C*" should read "*B*".

2. Page 315, Figure 4. There should be no connection on the lower lead marked "To Receiver."

3. Page 315, Figure 6. One interconnecting lead was omitted from the illustration; it is included in the revised figure below:



4. Page 315, Figure 7. The word "Power" should read "Pointer."

5. Page 554, Figure 2. Curve 5 is for Helena and curve 6 for Bismarck.

6. Page 1026, column 1, paragraph 6, lines 14 and 15. The statement, "...the procedure described by Boehne and Linde..." should read "...the procedure described in a previous paper..." Added to the reference list should be:

4. A New Principle of Cable Design, **William A. Del Mar.** AIEE Transactions, volume 60, 1941, May section, pages 206–10.

7. Page 1038, column 1, last paragraph, line 12. The word "voltage" should be deleted.

Field Tests and Performance of a High-Speed 138-Kv Air-Blast Circuit Breaker

PHILIP SPORN
FELLOW AIEE

H. E. STRANG
MEMBER AIEE

Synopsis: Since 1926 many significant field tests on high-voltage circuit breakers have been made on the American Gas and Electric Company's central system, which have served as an important aid in high-voltage circuit breaker development. Such tests have also served as a means for studying and checking the behavior of this large power system under short-circuit conditions.

A 138-kv air-blast breaker of novel design has recently been given a series of field tests both for normal interrupting duty and for ultrahigh-speed reclosing service. Circuit interrupting ability at least equal to that expected of any modern oil breaker of conventional design was obtained and, in addition, an unusual superspeed reclosing performance was obtained which points the way to a possible liberalization of existing derating factors for this kind of service.

While still somewhat of an innovation in the high-voltage field, the air-blast circuit breaker undoubtedly has certain basic advantages. It is believed that these tests are an important step forward in developing the possibilities of the air-blast breaker for high-voltage service and that they have brought the entire electric power industry closer to the possibility of benefit from an application of this interrupting principle.

Description of Breaker

At the 1941 winter convention there was presented before the AIEE, a paper[1] describing a new high-voltage air-blast circuit breaker, known as the conserved-pressure type. As built for 138 kv, this breaker has two interrupting units of the axial or longitudinal blast type in series per pole. Each of these units, as shown in Figure 1, consists of an enclosing thick-walled tube of insulating material, containing a stationary and a movable contact, an orifice into which the arc is drawn, and a piston and cylinder for actuating the moving contact. This assembly is housed in a vertical porcelain shell for weather protection. Two such columns plus a disconnecting member comprise a pole unit of the 138-kv breaker. Each pole has a storage tank in which sufficient air for two complete close-open operations is stored at 350 pounds per square inch. The three tanks are coupled together by a common header and connected to a central air compressor plant through a double-acting check valve. Each pole has its own electrically operated blast valve to control the flow of air to the contacts. One pneumatic cylinder with electrically actuated control valves operates the three isolating switches through an enclosed system of push-pull rods between phase units.

The interrupting action of this breaker is unique in that the arc is drawn into a space deliberately maintained at high pressure, instead of into the free air as has been common for other types of air breakers. This back pressure, which is maintained by regulating the size of the vent from the arcing chamber, provides a medium having a dielectric strength several times that of air at atmospheric pressure in which the interrupting contacts are separated.

Referring to Figure 2, the action of the breaker in interrupting a circuit is as follows:

1. The protective relay energizes the coils of the three blast valves, causing them to open, admitting air to the passage leading to the interrupting units.
2. Contacts are separated by action of the pistons in each unit.
3. The arc is drawn into the insulating orifice through which air is passing, where it is extinguished; the moving contact continues on into the area of high pressure and high dielectric strength which prevents the arc from restriking.
4. A definite time after the blast valves have been energized (they are interlocked pneumatically to require action of all three valves) air is admitted to the disconnect actuating cylinder.
5. After the disconnect has started to open,

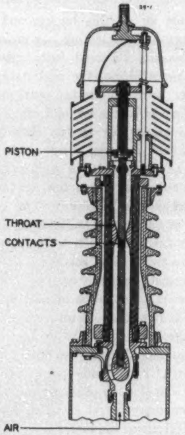


Figure 1. Cross section of interrupting unit

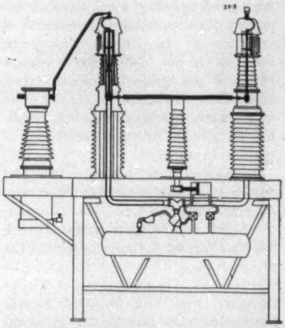


Figure 2. Single-pole unit of 138-kv breaker

Paper 42-9, recommended by the AIEE committee on protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26-30, 1942. Manuscript submitted July 9, 1941; made available for printing November 3, 1941.

PHILIP SPORN is vice-president in charge of engineering of the American Gas and Electric Service Corporation, New York, N. Y. H. E. STRANG is engineer of the switchgear department of General Electric Company in Philadelphia, Pa.

blast air is cut off, allowing the interrupting contacts to return to the normal closed position after the isolating switch has opened.

The closing action is performed entirely by the isolating switch, its action being fast and controlled by a positive driving force, so that it is capable of closing repeatedly against currents as high as 6,000 to 10,000 rms amperes at 132 kv without harmful effects.

Background of Field-Test Experience

Inasmuch as the facilities of the American Gas and Electric Company's systems, and especially its central system have been lent for the making of interrupting tests on several previous occasions and at the cost of certain extra operating expense and some disturbances to service, it is pertinent at this time, before describing the present series of tests, to review briefly what this background of testing has produced in the way of results and benefits.

While the earliest tests on 138-kv breakers, made in 1926 and 1927[2] produced some real benefits in proving and strengthening the interrupting devices of that time, it remained for later tests, first made in 1930 and later in 1937 and 1938, to bring about important developments in speed. The 1930[3] tests brought to completion the successful development of the 8-cycle breaker, which since has become standard, replacing the 15- to 18-cycle breaker in use up to that time. This improvement in speed was particularly valuable at that time as it permitted taking full advantage of improved high-speed relaying, such as the carrier systems, and in materially reducing the clearing time of faulted transmission circuits. These developments in turn made possible the first attempts at ultrahigh-speed reclosing[4] which has since become a most important tool in the art of transmission. In addition, these tests, made at the full 1,500-megavolt ampere rating of the breaker, disclosed certain weaknesses in design or construction which were thereupon remedied, resulting in greatly increased reliability of breakers in service.

A series of tests, begun in 1937 and completed in 1938[5] resulted in the successful development of a still faster breaker, the 5-cycle multibreak interrupter, which has since formed the basis for most of the ultrahigh-speed reclosing installations on this system. The extra speed of this interrupter over the standard 8-cycle breaker has made possible the overall reclosing cycle, including the time from initial short circuit to final reestablishment of the circuit, of 18 to 20 cycles.[6] This has been accomplished both by the installation of new breakers, as well as by rebuilding existing breakers with the new interrupters.

The 1937–38 tests were further noteworthy in that satisfactory performance was obtained at a duty of 2,000 megavolt amperes, or 33 per cent above the design rating of the breaker. This result has been of real economic value in more ways than one. First, it has formed the basis for a saving in physical size, and consequently in the cost of large 138-kv circuit breakers. Where the 2,500-megavolt ampere breaker was formerly made with a 72-inch tank, with the new interrupter it is now made with a 66-inch tank. Second, it has made possible a rebuilding of a considerable number of existing 66-kv breakers at Windsor for an increased rating well above that for which they were originally designed and at a very substantial saving below the cost of buying new breakers. In this instance, where the normal modernization could offer a rating of not more than 500 megavolt amperes, according to existing standards, a single breaker was rebuilt and successfully withstood a test in the field considerably above this value, and fully adequate for the requirements on these breakers. It is fairly safe to say that without the background of experience gathered in these tests, as well as in the 1930 tests at Philo, the rebuilding and testing of the Windsor breakers for an interrupting duty so much above the maximum previously considered available would never have been initiated.

While all of this background of experience in field testing has proved that the benefits received fully justify the cost, nevertheless, the undertaking of a further series of tests, particularly at this time of increased load requirements, was a matter for serious consideration. For several reasons, however, these tests were believed to be of sufficient importance to justify the actual cost and the inconveniences involved. The development of an outdoor, oilless, high-voltage circuit breaker, which to some may appear to be of only academic interest at the present time, may emerge as a timely, significant, and much needed undertaking. It is not at all inconceivable that restrictions of one form or another may be encountered in the use of oil for future breakers. This leads pertinently to a discussion of some of the advantages and disadvantages of oil as used in conventional oil circuit breakers.

It cannot be denied that oil has proven to be an excellent medium for circuit breakers, and that it has helped to bring them to the present high state of development. Some of the advantages of oil may be listed as follows:

1. Oil possesses high insulating value, or high dielectric strength and uniformity.
2. Oil is a good arc quenching or cooling medium, particularly effective when properly controlled or directed.
3. Behind the use of oil in circuit breakers exists a background of decades of experience and development, culminating in the present successful types of modern breakers.

The disadvantages of oil as a medium for breakers are likewise quite real, and in the course of time, by comparison with air, may appear even greater. Some of these drawbacks are as follows:

1. Oil, being inflammable, constitutes a possible fire hazard. For outdoor breakers, however, this hazard is not regarded as very serious, and experience so far has borne this out.
2. The use of oil presents a maintenance problem of sizable proportions. The problem consists briefly of the following:

(*a*). The conditioning of the oil itself, including filtering and drying out, and the equipment required for that purpose.

(*b*). The handling of oil, both for conditioning and for maintenance of the circuit breaker, and the pumping, piping, and storage facilities required.

(*c*). The longer time and increased cost for breaker maintenance resulting from above oil-handling problems and requirements.

Viewing the comparison from the standpoint of possible future war conditions, oil circuit breakers may present other serious disadvantages. Also, the normal fire hazard from oil circuit breakers may be substantially increased by greater duties accompanying the more rapid growth of systems, and possibly by the necessity for larger physical concentrations or greater crowding of oil circuit breakers.

Object of Tests

While the principal burden of carrying out circuit-breaker development work must of necessity fall on factory high-power testing facilities, nevertheless it is a fact that the best and most satisfying proof of performance of a high-capacity interrupting device comes from tests made on a system large enough to supply at least full rated interrupting current under actual operating conditions.

The value of high-speed reclosing of high-voltage circuit breakers is becoming more widely recognized and the demand is increasing for circuit breakers capable of

Table I. Three-Phase 138-Kv Interrupting Tests

Test No.	Operation	RMS Current (Amperes) Closing	RMS Current (Amperes) Opening	Megavolt-Amperes at 138 Kv	Arc Length (Cycles)	Arc Length (Inches)	Breaker Operating Time (Cycles)
1	O		960	230	0.8	0.6	5.1
2	O		650	155	1.2	1.4	5.1
3	CO	1,040	650	155	2.0	2.5	6.0
4	O		1,480	350	1.6	1.9	5.8
5	CO	2,100	1,270	300	2.3	2.3	6.3
6	O		3,100	740	1.7	1.7	5.3
7	CO	4,300	3,000	720	1.6	1.5	5.3
8	O		5,600	1,340	1.7	1.8	5.6
9	CO	6,900	5,300	1,270	1.7	2.0	6.0
*10	CO	6,200	5,200	1,240	1.5	1.7	5.6
*11	CO	6,000	5,100	1,220	1.3	1.5	4.6
12	O		6,100	1,460	1.6	1.8	5.3
13	CO	8,100	6,100	1,460	1.9	2.1	6.2
14	O		7,700	1,840	1.7	2.1	6.2
15	CO	9,300	7,500	1,800	2.2	2.7	6.0
*16	CO	10,000	7,400	1,770	1.7	2.2	5.4
*17	CO	10,300	7,800	1,860	1.0	1.2	5.2

* Indicates 15-second duty cycle.

performing this duty. In order to find wide application, therefore, any new type of circuit breaker must be capable of clearing a fault promptly, reclosing preferably in less than 20 cycles, and then clearing again in the event the fault on the line still persists.

The circuit breaker field test program, therefore, consisted of two parts:

1. Interrupting tests.
2. Reclosing tests.

The test breaker was rated 138 kv, 1,500 megavolt amperes, 8 cycles with a reclosing time of 20 cycles. All tests were made at approximately 138 kv.

As in the case of both the 1930 tests, and the 1937–1938 tests, these tests were conducted at the Philo plant on the system of The Ohio Power Company. The system setup was substantially the same as that shown in Figure 4 of the reference paper describing the 1937–1938 tests,[5] and for that reason will not be repeated here. As in the case of previous tests, the system connections in so far as possible were so arranged that service areas near the Philo plant would not be directly connected to the short-circuited bus, except on the final, full-capacity shots. Service disturbances were thereby kept to a minimum. A view of the breaker setup for test is shown in Figure 3.

Results of Tests—Breaker

The results of the first series are shown in Table I. Except for the first preliminary test, they were all three phase-to-ground tests, the tabulated values of current and operating time being maximums of the three phases. All tests were made as originally scheduled in a total elapsed time of eight hours, during which there was no inspection of the breaker. Tests 10–11 and 16–17 were made on the standard duty cycle with a 15-second interval. During none of the tests was there any visible fire from vents of the interrupting units. On the closing-opening tests there was a moderate flash from the disconnecting switch contacts, but after ten such operations, of which seven were practically at or above full rating, the blades showed only moderate burning, and so located as not to interfere in any way with their current carrying ability or other normal function.

Following this series of seventeen tests, an inspection of the contacts and interrupting chambers showed them to be in such condition that they were all put back in for the reclosing tests without dressing up or replacement of any parts.

During this first group of tests covering a range of duty from 10 per cent to 125 per cent of the interrupting rating of the breaker, the operating time, measured from energizing the trip coil till the arc was interrupted ranged from 4.6 to 6.3 cycles, including a dead time (to contact separation) of approximately 4 cycles.

It has been shown that in service the minimum reclosing time, without increasing the probability of restrike, is a function of the duration of the original fault, hence of breaker operating time as well as relay time. This gives added impetus to the desire for a fast clearing breaker. Prior to the reclosing tests, it was found possible to make a substantial

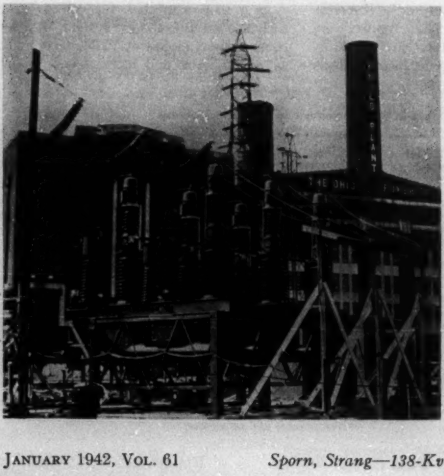


Figure 3 (left). Test setup of 138-kv breaker at the Philo plant

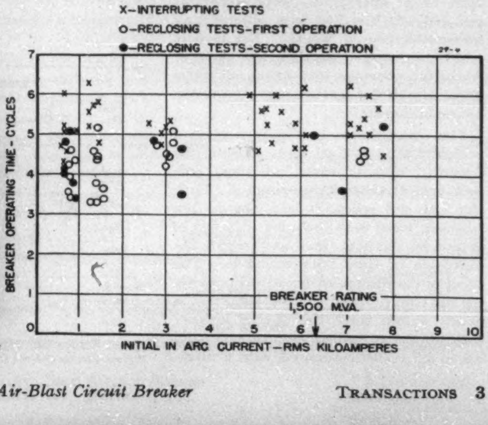


Figure 4 (below). Diagram of breaker performance for both interrupting and reclosing tests reduction in the breaker dead time by some simple changes and adjustments, thereby reducing the maximum breaker time to approximately five cycles.

The results of the reclosing tests are shown in Table II. During tests 18 to 22 inclusive, difficulties with the temporary control system prevented obtaining complete operation, but did give some interrupting operations showing the effect of the improvement in contact parting time. These control difficulties were remedied, and tests 23 to 25 were completed.[8] In the first operation in each of these tests marked "*a*" in the Table, the fault was initiated by a backup breaker and cleared by the test-breaker interrupting units. The reclosing control was so arranged that instead of the normal sequence of opening the isolating switch next, the blast valve was promptly deenergized, thus cutting off the interrupting air, and allowing the arcing contacts to reclose. This reestablished the fault, causing operation of the protective relays again, opening the blast valves a second time, this time followed by the conventional operation of the isolating switch. After final adjustments, the reclosing time (defined as that measured from energizing the trip coil until the breaker arcing tips contact on reclosing) ranged from 17.7 to 20 cycles. Test 23 represented interruption of light current followed by reclosure on what corresponds practically to normal load current, whereas test 25 demonstrated that the breaker could interrupt a current appreciably beyond its rating, reclose into that fault in less than 20 cycles, and interrupt again with the same operating time for the second operation as for the first.

Following these reclosing tests, the contacts, which had been subjected to a total of 25 operations, were somewhat rounded off, but had not suffered any measurable loss of length. The throats had been enlarged in diameter $^1/_{16}$ inch. As in the previous inspection, all parts were suitable for further operation without replacement.

The chart shown in Figure 4 gives a complete record of all tests showing the operating time of each individual pole unit against current interrupted. Both the first and second operations on the reclosing tests were included in order to determine if there was any significant difference in time between them. The effect of the change in dead time between the interrupting and the reclosing test is clearly shown since the overall breaker time for the reclosing tests ranged from 3.8 to 5.2 cycles compared with a maximum of 6.3 for the previous 17 interrupting tests.

Figure 5 is an oscillogram from closing-opening test 15, interrupting 1,800 megavolt amperes; this may be considered typical of the performance on the first series. Figure 6 shows the results of test 25 in the reclosing series, interrupting 1,770 megavolt amperes, reclosing in 17.7 cycles and then interrupting 1,870 megavolt amperes.

Results of Tests—System

By comparison of the results obtained on these tests, as shown in Tables I and II, with the results obtained on the 1937–1938 tests,[5] it will be seen that the total operating time of the air-blast breaker, particularly in the series of straight interrupting tests, was somewhat longer than the corresponding breaker time of the multiple-break breaker in 1938. For example, on the heavier shots on the oil circuit breaker, breaker time was on the order of 4 cycles, while the duration of short circuit, including relay time, was approximately 5 to $5^1/_2$ cycles. In the air blast tests, however, the actual breaker time for the heavy shots varied between 5 and 6.2 cycles; after adding relay time of approximately 1 cycle, this represented a maximum duration of more than 7 cycles.

As a result of the longer duration of short circuit on these tests, it was to be expected that the disturbing effects on the system would be somewhat greater than those experienced in 1937 and 1938. That this was actually the case was evidenced by a number of reports from nearby areas which would be most affected by the final tests involving a complete system short circuit. These reports were concerned almost entirely with the shutting down of motors having instantaneous undervoltage protection. Certain clay products plants, glass plants and other industrial plants were affected in this manner.

For the reclosing tests, which were begun on June 8 and completed on June 22, the operation of the breaker, as well as the tripping relays themselves, was somewhat speeded up so that the duration of short circuit was more or less comparable to that obtained in the 1937–1938 tests. For this reason, system disturbances were expected to be materially reduced and, in fact, were intended to be practically eliminated by stopping the tests short of the full system capacity and keeping the nearby connected areas entirely isolated from direct connection to the short-circuited bus. This plan, however, was frustrated by a most unusual mechanical failure in the operating arrangements of a three-pole disconnecting switch which inadvertently left the main and reserve busses tied together, thereby subjecting the test breaker and the system to a full short circuit. Since the mechanical failure in the control of this disconnecting switch occurred beyond the motor-operated control mechanism, both the switchboard control light indication and the motor-mechanism check indication showed the switch to be open, whereas the blades of the switch were actually closed and making contact, although not completely driven home. While the test as planned would have subjected the breaker to an ultrahigh-speed reclosing duty at approximately its full rating of 1,500 megavolt amperes, this incident resulted in giving the breaker a reclosing shot at nearly 2,000 megavolt amperes instead.

As is to be expected, the voltage disturbance on the 138-kv system, at the time of short circuit at Philo, tapers off as points more and more remote from

Table II. Three-Phase 138-Kv Reclosing Tests

Test No.	RMS Current (Amperes)		Megavolt-Amperes at 138 Kv.	Arc Length		Breaker Operating Time (Cycles)	Reclosing Time (Cycles)
	Closing	Opening		(Cycles)	(Inches)		
18*a*		1,600	380	0.6	0.1	3.8	
b	2,600	*					21.0
19*a*		1,430	340	1.8	2.0	4.6	
b	2,700	*					21.2
20*a*		1,470	350	2.0	2.6	5.2	
b	2,300	*					23.0
21*a*		790	190	1.9	2.5	4.6	
b	1,160	650	155	1.7	1.9	4.7	21.2
22*a*		3,200	770	2.0	2.4	4.8	
b	**						
23*a*		900	220	1.3	1.2	4.3	
b	1,200	920	220	1.8	2.2	5.1	20.0
24*a*		3,100	740	2.0	2.2	5.1	
b	4,100	3,400	810	1.0	1.1	4.8	18.9
25*a*		7,400	1,770	1.5	2.1	4.6	
b	10,200	7,800	1,870	1.7	2.2	5.2	17.7

* Test breaker did not trip. ** Test breaker did not reclose.

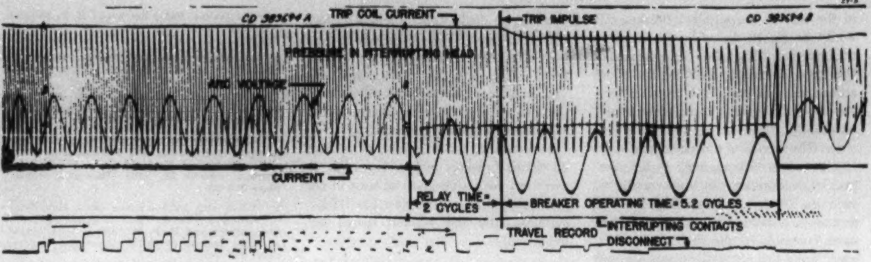


Figure 5. Oscillogram from test 17

Open-close-open operation interrupting 1,860,000 kva

Philo are considered. This was seen from the records taken on the permanently installed automatic oscillographs at Lima, Howard, and Torrey substations. An indication of the relative severity of voltage dips was also obtained from recording voltmeter charts taken at various locations, although the accuracy of these comparisons is questionable because of the variable damping characteristics of the different instruments.

Discussion of Results

As compared with the time which has been spent in developing oil circuit breakers to their present state of satisfactory performance, it is remarkable that in such a comparatively brief time an entirely new principle of arc interruption has been developed and incorporated in a successful breaker which gives substantially five-cycle performance on its first real system test. In other words, while it took thirty years to get the present five-cycle oil circuit breaker, this has been practically realized with air blast in some two years of development work.

This comparison may appear even more striking when it is pointed out that in all previous field tests on oil circuit breakers on this system, it has never been possible to complete an initial series of tests on a new design without disclosing some difficulty in the breaker itself, not of basic importance, it is true, but still sufficient to prevent the completion of the tests until such difficulties were remedied. These tests on the air-blast breaker were, therefore, the first series of tests which was ever undertaken on the American Gas and Electric Company systems in which the breaker in its original condition successfully completed an entire scheduled series without any adjustments, difficulties, or inspections throughout. It is true that on the reclosing tests it was not possible to complete the program as first planned, but this was due entirely to external relay characteristics which had nothing to do with the breaker itself. With proper adjustments in the relay setup, tests were carried through without incident. This is believed in itself to be an unusual performance in the light of past experience with oil circuit breakers and in view of the radically new design of circuit breaker being tested.

Conclusions

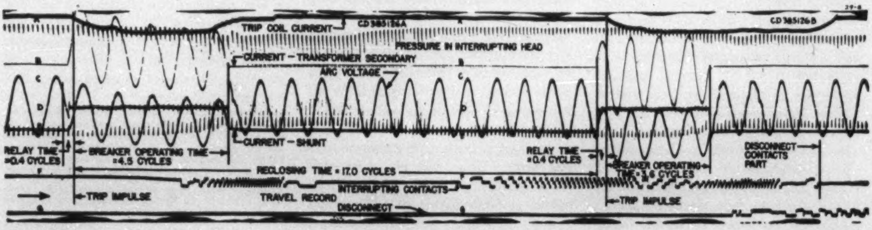
1. The high-voltage air-blast breaker tested has demonstrated an interrupting performance at least the equivalent of that expected from any modern oil breaker of conventional design. It may reasonably be expected that present ideas entering into mechanical design and construction will undergo changes during the next few years, but a successful interrupting principle has again been established beyond doubt.

2. With the extension of ultrarapid reclosing applications on the American Gas and Electric Company systems, the adaptability of any high speed breaker to reclosing is obviously an indispensable qualification. The present air-blast breaker has this inherent adaptability, as demonstrated by tests at reclosing speeds at least equal to, and at short-circuit duty far beyond, any similar tests ever made on oil breakers. Based on prevailing standards, the interrupting ratings of 138-kv oil breakers are subject to a reduction of 15 to 25 per cent when applied on 20-cycle reclosing service. Although it may be too early to form definite conclusions, even a conservative interpretation of these test results points the way to a probable downward revision of such derating factors as applied to air-blast breakers in the future.

3. The current-transformer problem is decidedly more complex for the air-blast breaker than for the conventional oil circuit breaker since the relatively simple and economical procedure of applying bushing current transformers to oil circuit breakers cannot be carried out

Figure 6. Oscillogram from test 25

Interrupting 1,770,000 kva, reclosing in 17 cycles, and interrupting 1,870,000 kva



in the case of the air-blast breaker. It would be highly desirable, therefore, in the interests of the future development of this type of circuit breaker, if a more economical solution of this problem than the separate current transformer could be found.

4. The success of this development on 138 kv points encouragingly to the prospect of developments at higher voltages, such as 230 kv, or even higher. The economic picture here might be even more favorable to the air-blast breaker, considering the physical dimensions and large quantities of oil required in the conventional breakers for such voltages.

5. As regards the possible difficulties in what may be considered an experimental period in the use of air and the equipment required for handling the air, there is no doubt that practical experience is needed. The only way to get this, of course, is through trial installations. This will be done with this breaker and, it is hoped, with others, with the view of acquiring the necessary field experience to handle air successfully.

Fortunately, the existence of reliable oil circuit breakers makes it possible to carry out such a program at least in the moderately high-voltage class like 138 kv, systematically and without delay, but unhurriedly.

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The Acceleration-Oscillogram Method of Motor-Torque Measurement

C. R. ATKINSON
ASSOCIATE AIEE

E. G. DOWNIE
ASSOCIATE AIEE

Synopsis: The need for an improved method of measuring starting torque of large induction starting synchronous motors as compared to the power input and dynamometer methods, has led to the use of the acceleration oscillogram. A low ripple, permanent magnet type d-c generator is driven by the motor, and traces a time-speed oscillogram, from which acceleration, and the torque producing it, may be calculated.

Comparative torque tests, at full and reduced voltage, made by this and the other methods on the dynamometer coupled motor, showed the definite advantage of the acceleration tests in combined convenience and accuracy. Interesting facts about synchronous motor starting torques, not accurately shown by previous tests, have been brought to light. The acceleration or deceleration oscillogram is also applied to the measurement of torques or losses of very small high-speed motors by using other methods of rotation indication, involving no loading of the motor.

THERE has been need in our motor testing departments for a simpler and more accurate means of obtaining speed-torque curves of various types of motors. Particularly this need has been felt in the a-c synchronous motor section, building synchronous motors (induction starting) of from approximately 25 to 1,000 horsepower rating.

Two methods have been used, one based on measurement of power input, and the other on the dynamometer. The first is rather inaccurate because several variable factors can be accounted for only in an arbitrary manner, and the second involves a rather elaborate test setup, including a dynamometer of sufficient torque to load the motor at all speeds.

The method we will describe gives results of accuracy comparable to the dynamometer test. It is taken at full voltage with no corrections, and requires only a simple test setup. It is based on the simple principle that the torque developed in a motor while accelerating from standstill to full speed is absorbed by friction and windage and acceleration of the rotating parts. If only useful output torque is to be determined, friction and windage are not considered, and the required results are obtained from acceleration figures only.

The Tachometer Generator

Speed-acceleration data may be obtained graphically from a time-speed curve, or directly from the proper electric circuit. The first requirement in producing an accurate time-speed curve is a tachometer generator which produces a d-c voltage, free from commutator and rotational ripple, exactly proportional at all times to the rotational speed of the motor. Any ripple in the voltage becomes confused with pulsations from actual speed surges in the starting motor, and makes the resulting curve difficult to analyze.

The use of a satisfactory d-c generator has largely made possible the accurate results we have obtained. The generator is a permanent magnet field type, specially built with a large number of commutator segments for minimum commutator ripple and the best possible armature construction for low rotational ripple. With a 100-microfarad filter condenser, no ripple is visible in the oscillographic trace of the output voltage. The coupling used to connect the generator to the motor must be rigid in torsion, and must not exert any axial or radial force on the generator shaft extension. Any deflection of the generator shaft introduces rotational ripple. The coupling being used is of the common leather ring type, with provision for mounting the motor half by three tapped holes and cap screws on the truly faced end of the motor shaft. Best results were obtained by not clamping the generator too rigidly in position, thus allowing any slight radial deflection to be absorbed by the whole unit, rather than the shaft alone.

The oscillogram method of torque test is especially convenient for the synchronous motor, for other starting data may be obtained on the film. Line current and field discharge current are recorded on the film as well as the speed indicating voltage from the generator.

Torque Calculations From Oscillograms

Calculations from the time-speed oscillogram begin with speed and time calibrations, in terms of revolutions per second per millimeter and seconds per millimeter. The quotient of these two is a constant which may be multiplied by the tangent of the measured slope angle of the speed curve at any point to determine the acceleration. The moment of inertia of the motor and tachometer generator (unless negligible compared to the motor), and normal full load torque may also be included, to give a constant to be multiplied by the slope to give direct values of percentage of full load torque, the term commonly used by motor engineers. (See appendix *A*.) The speed curve is divided into a convenient number of equal sections along the time axis, and the slope and amplitude measured at each section. The results are data for a complete speed-torque curve, percent synchronous speed versus per cent full load torque.

Comparative Tests

A small educational two-unit dynamometer set was chosen for its convenience in illustrating this method of torque test and comparing the results with those of other methods. Both units were the same electrically, rated $7^1/_2$ horsepower as motors; but one (normally the motor) had a phase-shifting stator mounting, and the other was cradled as a dynamometer.

The input and dynamometer test readings were taken simultaneously over the speed range at reduced voltage (appendix *B*). Starting oscillograms were taken at full and reduced voltage, with the motor coupled and uncoupled to the dynamometer unit. Deceleration oscillograms, coupled and uncoupled, gave data on friction and windage torque at all speeds.

Figures 1 and 2 show starts at full voltage with the dynamometer coupled, from a standstill (1), and with the rotor coasting slowly in reverse rotation before the start (2). The method of Figure 2 permits the transient effect of power application to pass before zero speed is reached. The first, however, gives a better indication of available breakaway torque. This transient starting torque in induction motors has been discussed by Wahl and Kilgore.[1] Figure 3 shows a full voltage start of the motor unit alone, without the added inertia of the dynamometer unit. It can be seen that for accuracy in analysis, when average torque readings are required, the slower start, as obtained with added inertia, is desirable. A reduced

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C. R. Atkinson is in the motor and generator engineering department and E. G. Downie is in the works laboratory of General Electric Company, Fort Wayne, Ind.

voltage start, for direct comparison against reduced voltage data from the other methods, is recorded on Figure 4. The tendency of the machine to hunt is shown on the starting curves by the speed pulsations above and below synchronous speed, which did not die out until after the end of the oscillogram.

The speed-torque curve obtained from Figure 4 is plotted on Figure 6, with points added from input and dynamometer tests, all at 140 volts on the 220 volt connection. Agreement is fairly good between acceleration and dynamometer tests but the input method does not show the half-speed dip in torque which is unquestionably present. Figure 7 is for full voltage starting with acceleration torque data from Figure 2 and added points from input and dynamometer tests, with rechecks at some speeds, corrected for 220 volts. Again the dynamometer tests are in best agreement, with input tests not showing the half-speed dip (appendix *C*). Acceleration oscillograms show friction and windage torque (both units) of 4.1 per cent at 600 rpm and 5.8 per cent at 1,200 rpm. These values should be added to the acceleration torque values for exact comparison with input test values.

Variations of the Method

Acceleration may be shown directly on the film by passing the charging current to a condenser (from the d-c tachometer generator) through an oscillograph galvanometer. It can be shown (appendix *D*) that:

Acceleration

$$\frac{d^2r}{dt^2}=\frac{I}{CK_G}+\frac{R}{K_G}\times\frac{dI}{dt}$$

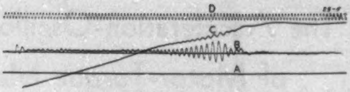
where

I = capacitor charging current
C = capacitance of capacitor
R = total circuit resistance
K_G = volts per revolution per second of generator

The term $I/(CK_G)$ is dependent only on current magnitude and is obtained directly from galvanometer deflection on the film. Correction for the other term would be tedious so it must be kept small by low circuit resistance and low current. The obvious requirement then is a high sensitivity, low resistance galvanometer element.

Figure 5 shows acceleration by this direct method. Unfortunately, the acceleration deflection was not set high enough for accurate reading on smooth acceleration, but shows clearly its value in recording torque pulsations such as those in the synchronous motor approaching synchronous speed. We have found slope measurement of a speed curve more generally useful, because of simplicity of taking the oscillogram, ease of calibration, and the need for average rather than instantaneous torque values.

Figure 5. Starting oscillogram with direct acceleration indication (coasting backward before start)



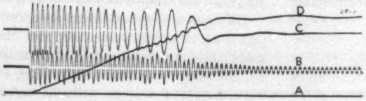


Figure 1. Full-voltage starting with dynamometer coupled (from standstill)

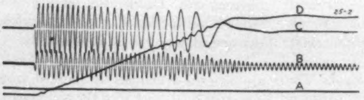


Figure 2. Full-voltage starting with dynamometer coupled (coasting backward before start)

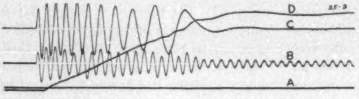


Figure 3. Full-voltage starting without dynamometer coupled (coasting backward before start)

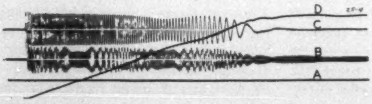


Figure 4. Reduced-voltage starting with dynamometer coupled (coasting backward before start)

A—Zero speed *B*—Line current *C*—Field discharger current *D*—Speed

A speed-torque curve may be traced directly on the screen of a cathode-ray oscilloscope during the starting of a motor by applying the tachometer generator voltage to one pair of plates, and passing the capacitator charging current through deflecting coils (or a shunt and d-c amplifier) to produce torque deflection in quadrature with the speed deflection. This has been done experimentally with good results but has not yet been applied to production torque testing.

The general principle of speed oscillograms for determination of starting, and friction and windage torque has been applied to a number of applications where the tachometer generator could not be used, and speed indication is obtained on the film by other means. Some tests involve high speeds (up to 20,000 rpm) and many do not permit any loading effect by the indicating device. Three methods have been used successfully:

1. Small magnets on the motor shaft generate a voltage in a stationary coil, which is amplified and applied to an oscillograph galvanometer.
2. A mirror on the motor shaft reflects a light directly on the oscillograph film, while a galvanometer traces a timing wave.
3. A mirror or shutter mechanism on the motor shaft controls light on a photocell, and the resulting impulses are amplified and applied to an oscillograph element.

In all of the cases, speed is calculated at any point by counting revolutions per unit time, or measuring time elapsed per revolution.

Conclusions

1. Neither the power input nor dynamometer torque tests are entirely satisfactory in the analysis of synchronous (or induction) motor starting characteristics.
2. The acceleration oscillogram torque test is simpler to set up and run, is taken at full voltage with no corrections, gives consistent, accurate results through the whole speed range, and may be used to determine transient as well as average torque.
3. Variations in the method allow it to be applied to many types and sizes of motors, for determining losses as well as starting torques.

Appendix A. Derivation of Acceleration Formula

Consider a point on the time-speed curve, H millimeters on the horizontal time scale and V millimeters on the vertical speed scale.

t = time in seconds
r = revolutions
k_h = time calibration, seconds per millimeter
k_v = speed calibration, revolutions per second per millimeter
θ = slope angle of curve
WR^2 = moment of inertia of rotor, pound-feet^2

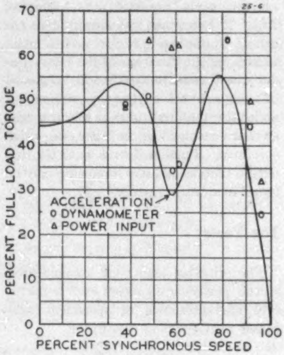


Figure 6. Reduced-voltage (64 per cent) starting torque by various methods

G = acceleration of gravity, feet per second per second

$$\frac{dr}{dt} = VK_v \quad t = HK_h$$

$$\frac{d^2r}{dt^2} = \frac{dKV_v}{dt} \quad dt = dHK_h$$

$$\frac{dr}{dt^2} = \frac{dVK_v}{dHK_h} = \frac{K_v}{K_h} \tan\theta$$ acceleration, revolutions per second per second

$$\text{Torque (pound-feet)} = \frac{WR^2}{G} \times 2\pi \times \frac{d^2r}{dt^2}$$
$$= \frac{WR^2}{G} \times 2\pi \times \frac{K_v}{K_h} \tan\theta$$
$$= K \tan\theta$$

Appendix B. Input and Dynamometer Test Methods

For the input method readings the cradled unit with the dynamometer attachment was connected synchronously to a variable frequency source of sufficient capacity to afford practically infinite bus to the test machine. The motor then was coupled to the dynamometer and connected electrically to a variable-voltage 60-cycle source for short periods during which time the input and torque readings were made. This setup should have provided an ideal arrangement for taking torque tests by either input or dynamometer method, but practically it left much to be desired. Briefly, the range of frequency control was limited, frequency or speed control at very low speeds (including standstill) was not available, and the torque reading on the dynamometer scale varied throughout a reading period, due perhaps to change in the torque characteristics of the armature and amortisseur windings from heating.

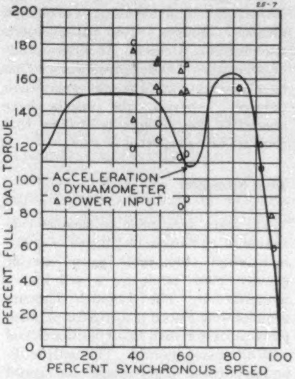


Figure 7. Full-voltage starting torque by various methods

Appendix C. Peculiar Motor Characteristics Shown

The agreement in the lower speed portion of Figure 7 was not good, particularly near half speed, but this was pretty much to be expected. In fact, queer antics of synchronous motors at and near half speed often have been hard to explain in the light of heretofore obtainable tests. In some cases one was inclined to doubt the accuracy of the test readings, in others, the inherent accuracy of the method. We anticipate therefore that this new method of recording torque directly and instantaneously will confirm some previous observations that one hardly dared to believe.

For instance, some motors have shown a tendency to lock or synchronize at half speed (in fact a great many show a slight tendency in this respect). Others seem to show such a characteristic only when being started on a voltage considerably reduced from their rated voltage. A torque test on such a motor by the power input method, at more nearly rated voltage might indicate a peak of torque at half speed. Knowing when to believe such an observation will go far in helping to determine the cause.

Appendix D. Derivation of Acceleration-Current Formula

E_G = tachometer generator voltage (open circuit)
E_C = capacitor voltage
I = capacitor charging current
C = capacitance of capacitor
R = total circuit resistance
K_G = volts per revolution per second of generator

$$E_C = E_G - IR \qquad E_G = K_G \frac{dr}{dt}$$

$$I = \frac{CdE_C}{dt} = C\,\frac{(dE_G - RdI)}{dt}$$

$$I = \frac{CdE_G}{dt} - CR\,\frac{dI}{dt}$$

$$I = \frac{CK_G d\,\frac{dr}{dt}}{dt} - CR\,\frac{dI}{dt}$$

$$\frac{d^2r}{dt^2} = \frac{I}{CK_G} + \frac{R}{K_G} \times \frac{dI}{dt} = \text{acceleration}$$

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Temperature and Electric Stress in Impregnated-Paper Insulation

J. B. WHITEHEAD
FELLOW AIEE

W. H. MacWILLIAMS, JR.
ENROLLED STUDENT AIEE

THE effect of temperature elevation on impregnated paper insulation is to increase conductivity and dielectric loss and to accelerate such chemical action as may be possible among the constituent paper, oil, and adjacent electrodes. Both effects increase rapidly above, say 60 degrees centigrade, but are relatively small at ordinary atmospheric temperatures in dry and air-free insulation.

The effect of high stress is also well known. Power-factor-voltage curves in thoroughly impregnated paper (no gaseous ionization) are generally flat up to 300 or 400 volts per mil at temperatures up to 40 or 50 degrees centigrade and at higher stresses rise only slightly until breakdown is approached. Further temperature elevation is immediately reflected not only in large increases in power factor but in characteristic changes[1] in the shape of the power-factor-voltage curve. These immediate (as distinguished from long-time) effects have usually been considered from the standpoint of the values of dielectric loss reached and permissible limits of temperature elevation.

Little attention has been paid to the study of the long-time effect of combined high temperature and high stress. The slow increases in power factor and loss in cables operating at relatively low values of stress and temperature have usually been attributed to chemical changes inherent in the constituent materials and to increasing gaseous ionization. Safe restrictions are placed upon these in design with due reference to the influence of temperature.

However, such time changes have been noted by manufacturers' engineers but, as is so often the case, not published. Time changes in power factor in laboratory samples have been reported by one of the present authors.[1,2] Perhaps the most conspicuous data are those of Proos.[3] In a series of load-cycle studies on power cables he noted the following:

(*a*). Electric stress at low temperature for long time, no permanent change in power factor at high stress.

(*b*). Load (and temperature) cycles plus high stress, large permanent changes in power factor at high stress.

Proos apparently had his eye on gaseous ionization, and says nothing as to possible chemical changes. This paper reports the results of a careful laboratory study of the matter. Although only one oil has been studied so far, and others may show different behavior, the results have seemed to the authors so striking that they are being reported at once.

Test Samples

The tests were made on samples consisting of 10 flat circular layers of 0.004-inch wood pulp cable paper, specific gravity 0.997, made by a well known manufacturer. The 6.25-inch diameter samples were placed in a circular parallel-plate capacitor with brass electrodes and with the usual guards. The outside diameter of the high-voltage and guard electrodes is 5.13 inches. The capacitor was mounted in a Pyrex glass dish so that the impregnated specimen was under oil at all times. The capacitor and dish were mounted in an outer brass cell, with thermostatic control, which could be heated to 105 degrees centigrade and which could be evacuated to a pressure of 0.05 millimeters of mercury. Connections were provided for measuring and for guard electrodes and for voltage up to 15 kv on the high-voltage electrode. A-c measurements were made by a transformer bridge modified to permit rapid determination of power-factor-stress characteristics. Further details of the capacitor and other auxiliary measuring equipment will be found in an earlier paper.[2]

The oil used was prepared by a well known manufacturer for cables of the solid type. The principal properties are given in Table I.

The paper in all cases was vacuum dried and degassed at a pressure of 0.05 millimeter of mercury and a temperature of 105 degrees centigrade for 60 hours, with appropriate measurements of d-c conductivity and 60-cycle power factor at intervals to insure uniformity of final conditions. The oil was stored in a steel drum under nitrogen, and as needed was degassed and dehydrated at 0.15 millimeter mercury.[2] Some of the samples tested were impregnated with oil which had been exposed for 65 hours to oxygen at a pressure of five centimeters mercury, and a temperature of 80 degrees centigrade. The paper for these samples was dried in the usual manner, the oxygen at a pressure of five centimeters was admitted to the impregnating cell, and thereafter the oil saturated with oxygen at the same pressure was admitted. After 12 hours air was admitted to the cell at atmospheric pressure and so maintained for the duration of the test. Under these conditions, the oil contained 0.7 per cent of oxygen by volume, or about five hundred times as much oxygen as that contained in the normal air-dried specimen. Further details as to the oxidation process and measurements may be found in an earlier paper.[2]

The Tests

The principal tests were a comparison of the power-factor-stress and temperature characteristics of specimens subjected to two sets of conditions,

(*a*). Without electric stress, and at a temperature of 80 degrees centigrade.

(*b*). Under an a-c stress of 400 volts per mil, at the same temperature.

These runs lasted approximately one week each, and were interrupted at intervals of approximately 24 hours for the measurement of power-factor-stress relations between 25 and 400 volts per mil. At the end of approximately a week the tests were interrupted and the temperature lowered to that of the atmosphere and another power-factor-stress curve taken. The specimen was then dismantled and power-factor measurements made on individual layers at atmospheric

Table I. Salient Properties of the Oil

Pour point	−18 degrees centigrade
Flash point	279 degrees centigrade
Dielectric strength	30,000 volts per 0.1 inch

	40 Degrees Centigrade	80 Degrees Centigrade
Specific gravity	0.902	0.872
Viscosity	5.1 poises	0.2 poises
20-minute d-c conductivity		$8.6\ 10^{-14}$ mho per centimeter
Power factor	0.0005	0.0018
Dielectric constant	2.27	2.22

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J. B. WHITEHEAD is director of the school of engineering and W. H. MACWILLIAMS, JR. is associated with the school of engineering of the Johns Hopkins University, Baltimore, Md.

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temperature and a stress of 50 volts per mil.

Results

Test 1. No Stress, No Oxygen, 80 Degrees Centigrade

Figure 1 shows the results of a test run of an air- and oxygen-free sample maintained at 80 degrees centigrade for a period of one week. Time was measured from the termination of the impregnation period. It will be noted that the curves are relatively flat and of low absolute value. The slight increase in power factor at 150 volts per mil over the period of seven days is 0.0005. The curves show the beginnings of a characteristic of all such curves at higher temperatures: namely, a maximum in the neighborhood of the stress mentioned. The power-factor-stress curve, at room temperature and after the test, is absolutely flat. The layer power-factor curve is in no way suggestive of action at the electrodes.

Test 2. Sustained A-C Stress at 400 Volts Per Mil, 80 Degrees Centigrade—No Oxygen

As will be seen in Figure 2 the power-factor-stress curve at the start is approximately flat at a relatively low value and comparable with the initial curve of test 1.

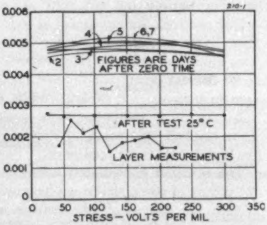

Figure 1. Changes in power factor due to temperature (80 degrees centigrade) alone. No oxygen

However, the curves taken on succeeding days

(*a*). Indicate a rapid increase of power factor with time.

(*b*). Take on the characteristic form indicated in Figure 2.

The salient features of these curves are the rapid increase in power factor at low stresses, with maximum values at approximately 125 volts per mil, and thereafter the continuous decrease of power factor up to and beyond 400 volts per mil. The increase in the maximum value is approximately linear with time in the lower range, with some indication of a more rapid increase beyond 48 hours. The overall increase in power factor for the period of the run is 0.022, or about 0.0056 per day, which may be compared with the practically negligible increase of test 1 (not greater than 0.00013 per day). The power-factor-stress curve at atmospheric temperature, after the test, is close to the initial curve at 80 degrees and rises slightly with increasing stress, this indicating a permanent change in the dielectric as a result of the run at high stress and 80 degrees centigrade; compare with Figure 1. Again the layer power-factor curve contains no special suggestion of action at the electrodes.

Test 3. Sustained A-C Stress at 400 Volts Per Mil, 80 Degrees Centigrade, No Oxygen

Figure 3 shows three power-factor-stress curves on a specimen similar to that in test 2. Results similar to those of test 2 were obtained in this test which was continued to 112.9 hours. After 112.4 hours at 400 volts per mil, curve *A* was taken. After an additional half-hour at 400 volts per mil, stress was removed, and after a further period of 20 hours without stress curve *B* was taken. After a further 24 hours without stress curve *C* was taken. The three curves of Figure 3 indicate that, while rapid increases in power factor accompany any prolonged application of stress, these increases cease almost immediately upon removal of stress. The high values reached appear to be permanent, indicating a definite deterioration of the insulation. Changes in the power-factor-stress curves at decreasing values of temperature following a run of 91 hours at 80 degrees centigrade and 400 volts per mil are indicated in the curves of Figure 4. As seen, the characteristic maximum in the power-factor curves does not appear below 60 degrees. There is, however, the suggestion that these maxima might appear at 60 degrees centigrade (or lower), if the stress were carried to higher values.

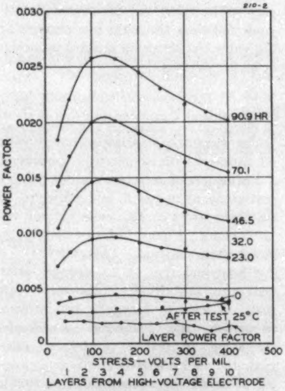

Figure 2. Changes in power factor due to combined temperature (80 degrees centigrade) and stress (400 volts per mil). No oxygen

Test 4. Sustained A-C Stress at 400 Volts Per Mil, 60 Degrees Centigrade, No Oxygen

The procedure in this test was identical with that in test 2, except that the sustained temperature was 60 degrees centigrade. The results of the test of 92 hours duration are shown in Figure 5. The trend of the power-factor-voltage curves is similar to that found for the temperature of 60 degrees in Figure 4. Moreover, there is the steady increase of power factor with time at each value of stress. The increase is practically uniform as shown by the graph giving the increasing values at 125 volts per mil. The rate of increase of power factor, 0.000336 per day, is noticeably less than the corresponding figure of 0.0056 per day for temperature 80 degrees centigrade found in test 2. In this case the power-factor-stress curve at room temperature was perfectly flat, at the approximate value 0.003, as com-

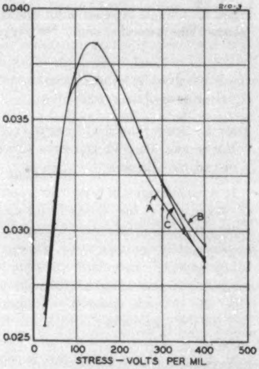

Figure 3. Changes in power factor after removal of stress (80 degrees centigrade). No oxygen

A—112.4 hours under stress
B—112.9 hours under stress, 20 hours off
C—112.9 hours under stress, 43.9 hours off

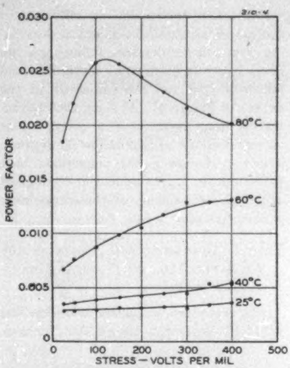

Figure 4. Changes in power factor with temperature after removal of stress. No oxygen

monly observed for all specimens throughout the course of the work.

Test 5. Sustained A-C Stress at 400 Volts per Mil, 40 Degrees Centigrade, No Oxygen

In a similar series of tests over a period of seven days the power-factor-stress curves are all approximately flat and have approximately the same value, the curves being grouped irregularly within the range 0.0027 and 0.0035 at 125 volts per mil. No uniform variation was found, and for the purpose of discussion in this paper, it is assumed that the time variation of power factor in this test is negligible. (0.00011 per day overall average as compared with 0.0056 per day for 80 degrees at 400 volts per mil.)

Test 6. Oxygen, No Stress, 80 Degrees Centigrade

This specimen was impregnated with oil which had been saturated with oxygen at a pressure of five centimeters mercury and a temperature of 80 degrees centigrade for 65 hours; impregnation also took place in an atmosphere of oxygen under the same pressure. The power-factor-stress curves over a period of seven days are shown in Figure 6. The steady rise in power factor (0.0017 overall) is more than three times greater than that of Figure 1 (0.005), which may be attributed to the continuous process of oxidation. There is also a suggestion of the appearance of maxima similar to those in Figure 2. The decrease in power factor between curve "0" day and "1" day is due to the change of atmosphere from oxygen at five centimeters to air at a pressure of 76 centimeters of mercury. The curve for 25 degrees centigrade following the main test remains at low value, but shows an upward tendency at higher stresses.

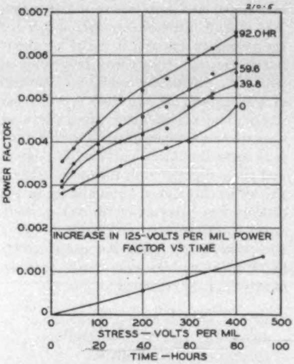

Figure 5. Changes in power factor due to combined temperature (60 degrees centigrade) and stress (400 volts per mil). No oxygen

Test 7. Oxygen, 400 Volts per Mil, 80 Degrees Centigrade

The specimen was impregnated with oil saturated with oxygen at a pressure of 5 centimeters of mercury and 80 degrees centigrade, as in test 6, and subjected to sustained stress at 400 volts per mil for 116.5 hours. Figure 7 shows the power-factor-stress curves. The curves for "0" hour and that at 25 degrees after test, are practically coincident with that of test 6 (oxygen, no stress). Otherwise, the behavior is closely the same as test 2 (stress, no oxygen), the characteristic maxima appearing at about the same values of stress, and the rate of overall time increase being slightly lower than that for the oxygen-free specimen. The time increase of power factor is closely linear in each case, being 0.0056 per day for the oxygen-free specimen and 0.0051 per day for that containing oxygen. The curve for layer variation of power factor again shows no evidence of electrode activity. Figure 8 shows the power-factor-stress curves at decreasing values of temperature following the time-run at 80 degrees. The behavior is closely the same as that shown in Figure 4.

Rate of Power-Factor Increase Versus Temperature—No Oxygen

In Figure 9, based on the data of the foregoing tests, the rate of increase in power factor per hour under sustained stress at 400 volts per mil is plotted as ordinate with temperature as abscissa. As will be seen, an appreciable steady increase is found at 60 degrees, the hourly or daily rate increasing rapidly above that temperature, the increase between 60 degrees and 80 degrees being approximately seventeen times.

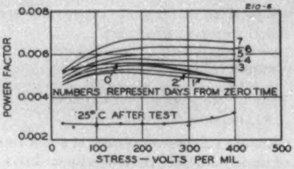

Figure 6. Changes in power factor due to oxidation at 80 degrees centigrade

Discussion of Test Results

The outstanding result of the tests is the very great increase in the time rate of increase of power factor, when high temperature and high stress are applied simultaneously, over the rates observed when the effect of each is observed separately. The relative rates of power-factor increase are: at 80 degrees centigrade and no stress, 0.0005 per week; at 80 degrees centigrade and 400 volts per mil, 0.0392 per week, an increase of at least 78 times in the rate of power-factor increase This figure is startling. Temperatures up to 80 degrees centigrade are not uncommon in high voltage cables. While 400 volts per mil is somewhat high, stresses up to one-half that value are now in use, the higher values are spoken of.

The question immediately arises as to the nature of the underlying phenomena. Within the ranges of temperature and stress here studied, there is a uniform linear rate of increase of loss, hence of conductivity, hence in the number of free ions present. Apparently oxygen plays a relatively small part, an increase of 500 times in the amount of oxygen actually causing a slight reduction in the rate of power-factor increase (which suggests the elimination of possible small active components). We thus fall back on the question of possible interaction among the three constituent materials—paper, oil, and metal.

It has long been known that oil withdrawn from contact with paper and metal electrodes suffers a deterioration as indicated by increased values of power factor and loss. Whitehead and Jones[2] made separate studies of the influence of the contact of oil with paper, with brass, and

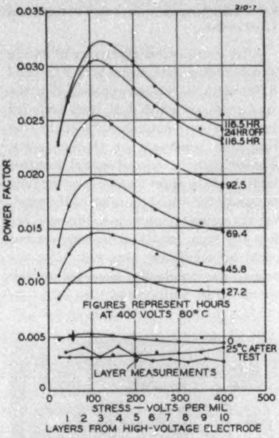

Figure 7. Changes in power factor due to combined influence of stress, temperature, and oxidation. Oil contains 0.7 per cent by volume of oxygen

with both, and found that the increase in power factor over the range 20 degrees to 80 degrees centigrade was greater than the changes caused by 0.013 per cent by volume of oxygen in the oil. Thus, even with substantial amount of oxygen in the oil, observed values of the rate of deterioration up to 80 degrees centigrade are entirely accounted for by the contact of the oil with paper and with metal.

It is commonly assumed that there is no inherent electrolytic dissociation in insulating oils. Chemists are very positive in their statement that pure hydrocarbon liquids are free from such dissociation and that insulating oils should also be found in this class. However, it is well known that most such liquids, and certainly the purest insulating oils, have some residual conductivity. This has commonly been attributed to residual electrolytic impurities, but all attempts to identify such impurities, to measure them, and to eliminate them, have failed, and the liquid still shows a residual conductivity. On the other hand, some chemists[4] have maintained that all such liquids should show some electrolytic dissociation and conductivity, albeit of very low values. More recently,[5] studies of the large increase of conductivity of insulating liquids in contact with metals and under high stress have been attributed to large increases in the electrolytic dissociation within the liquid.

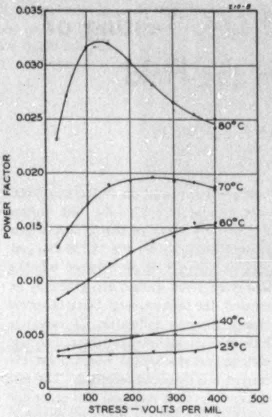

Figure 8. Changes of power factor with temperature after removal of stress. Oil contains 0.7 per cent by volume of oxygen

If an electrolytic dissociation for an insulating oil is admitted, an explanation of the results of this paper seems to be within sight. The powerful influence of temperature in increasing electrolytic dissociation is well known. If to this we add the effect of high stress as a powerful influence for upsetting the stable equilibrium of a complex oil molecule, it is easy to see that in the combined influence of temperature and stress we might find some form of cumulative increase with time of the number of ions present. The deteriorated condition at high temperature appears to remain when the stress is removed. When the temperature is lowered loss and power factor are greatly reduced but the oil does not return to its original condition. This behavior is not inconsistent with the tentative picture presented.

It is a suggestion, therefore, of this paper, that the results reported are due to the fact that the oil itself has, or perhaps acquires as a result of contact with paper and metal, an inherent electrolytic dissociation, and that the rapid changes due to the simultaneous application of temperature and stress are due to the well known influences of these conditions on electrolytic action.

The shape of the power factor–stress curves of Figures 2 and 5 may be explained as follows: increasing stress in the low range is accompanied by increase in the lengths of the excursions of the free ions present in oil spaces, and consequently increases in conductivity and loss. However, as the voltage increases, more and more ions reach the paper barriers until all are active in each cycle, and the loss component tends to a limiting value. The capacitance component of the current increases with stress and ultimately more rapidly than the loss component. Consequently, with increasing stress the power factor passes through a maximum and then begins to decrease as shown. Power factor–stress curves of this shape were reported by one of the authors in an earlier paper,[1] and substantially the same explanation suggested. C. G. Garton and P. Böning have also discussed curves of this type.

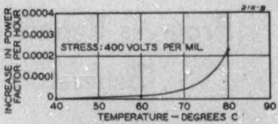

Figure 9. Rates of increase of power factor due to combined influence of stress and temperature

It is hoped to continue similar studies with other oils.

Conclusions

1. Chemical instability and deterioration of impregnated paper insulation, due to the combined influence of temperature and electric stress, may begin earlier and increase more rapidly than commonly supposed.

2. Using a high grade insulating oil and a standard cable paper, the time rate of increase of dielectric loss, at 80 degrees centigrade and 400 volts per mil, is over 50 times greater than the rate of increase at 80 degrees without stress, or at 400 volts per mil stress and temperature 40 degrees centigrade. It is recognized that different values may pertain to other oils.

3. Oxygen plays only a relatively small part in the chemical changes in the oil here reported.

4. It is suggested that the behavior reported is due to electrolytic dissociation in the oil, inappreciable at low stresses and temperatures, but increasing rapidly for values which are now being approached in the operation of high-voltage cables.

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Progress Report of D-C Testing of Generators in the Field

E. R. DAVIS
ASSOCIATE AIEE

M. F. LEFTWICH
ASSOCIATE AIEE

Synopsis: For many years the company with which the authors are associated endeavored to find some satisfactory method of testing generator insulation in the field. With this in mind, it was decided to experiment with higher d-c potentials. Several d-c testers were built and a series of tests made. This paper describes the progress during the first ten years of testing generators in the field with high d-c voltage.

Testing Equipment

3,000-Volt D-C Tester

A 3,000-volt d-c tester was built in 1931 using the simplest of rectifying tube circuits. This tester consisted of two rectifying tubes, filament and plate transformers, and necessary rheostats for voltage control. For purpose of measuring leakage current through armature insulation to ground, a milliammeter was used. A voltmeter was installed for measuring applied potential. Rheostats were installed in the input side of the filament and plate transformers which permitted close control of d-c test voltages from 0 to 3,000.

8,000-Volt D-C Tester

Due to the need for a higher test voltage, an 8,000-volt, one-kilowatt tester, Figure 1, was built in 1934, using practically the same rectifying tube circuit as the 3,000-volt tester. This tester permitted testing with d-c voltages ranging from 0 to 8,000.

14,000-Volt D-C Tester

In order to obtain capacity and voltage believed necessary to test the larger capacity units, a 14,000-volt 2-kw tester was built in 1940, Figures 2, 3, 4, and 5, using a standard bridge rectifying tube circuit.

Test Procedure

3,000-Volt Tests

The first routine tests were made April 6, 1932, on armatures of 3,750-kva 2,300-volt generators and on 18,750-kva 6,600-volt generators. The d-c test voltage applied was 3,000 for the 3,750-kva generators and 2,500 for the 18,750-kva generators. Test voltage between winding and ground was applied and gradually increased, the milliammeter being observed closely for any indication of excessive leakage or breakdown, until specified test voltage was reached at which point the current was read and recorded. The test voltage was then removed and generator armature grounded to discharge winding. If, however, during the initial application of test voltage, evidence of undue leakage or breakdown was noticed, test voltage was immediately removed and reapplied, slowly increasing voltage to determine the point at which this indication was observed.

8,000-Volt Tests

A new series of tests was instituted June 12, 1934, using same procedure and recording milliampere readings as described before. The d-c test potential applied on these tests was as follows:

3,000 volts for generators rated at 2,300 volts.

8,000 volts for generators rated at 6,600 and 13,000 volts.

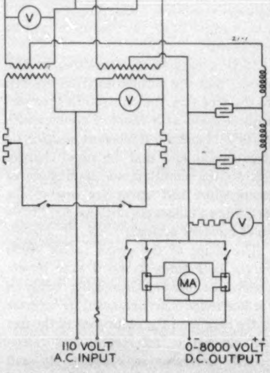

Figure 1. Schematic diagram of high-voltage d-c tester

After considerable experience in testing and recording data, using both 3,000 and 8,000 volts, it became apparent that the spot readings which had been recorded heretofore were not indicative of the true insulation resistance or characteristics. Consequently, a series of special tests was undertaken in order to determine the time necessary for initial charging of the winding at various temperatures (Figure 6) before the true insulation leakage current could be accurately observed. D-c potential was gradually increased to the specified test voltage and then held constant continuously on the armature for remainder of test with a current reading recorded at the end of the first minute of the specified test voltage application and continuing with each minute of test. The specified test voltage applied for this series of tests was 3,000 for the generators rated at 2,300 volts and 8,000 for the generators rated above 2,300 volts. In addition to current readings, data on relative humidity, barometric pressure, and core iron temperature were also recorded. The generator winding was grounded prior to beginning of tests and also between each five- or ten-minute test of a series.

Figure 2. Testing instruments showing carrying case

From experience it was found that high per cent relative humidity during test periods has no bearing upon the test readings provided the temperature of the generator under test is higher than ambient. However, if any outdoor bus is included in the test, high per cent relative humidity will cause the milliampere readings to increase sharply, thus giving false indications of insulation leakage or resistance.

The age of insulation appears to have no effect upon the magnitude of milliampere test readings except in a new machine or new windings in old machines. It is evident that the new insulation goes through an aging process and becomes dryer. This aging condition is illustrated by curves, Figure 8.

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E. R. Davis and M. F. Leftwich are both with the Duke Power Company, Charlotte, N. C.

Figure 4. Field testing of an 18,750-kva 6,600-volt generator

Results

3,000-Volt D-C Tests

During the course of the first routine testing, one breakdown occurred on a 3,750-kva, 2,300-volt bar-wound generator at a d-c test potential of 2,500 volts. On inspection of the point of breakdown, it was apparent from the condition of the surrounding insulation that leakage had been occurring at this point for some time. Eventually, the winding would have failed at this point with probable severe damage to core iron and adjacent coils. One new armature bar was installed in repairing the generator.

An 18,750-kva 6,600-volt lap-wound generator broke down on d-c test at 1,500 volts. The generator was then tested with a conventional tester which showed the insulation resistance to ground to be 150 megohms. The insulation of this winding consists of partly mica and partly varnished cambric which is more commonly known as "composite" insulation. Upon close inspection of the winding, a broken piece of core iron was found to have cut into a coil. The probable reason breakdown did not occur while generator was in service was that this coil was located near grounded neutral. Six new coils were installed in repairing the generator.

8,000-Volt D-C Tests

The use of the higher d-c test voltage proved beneficial in that several cases of excessive leakage were observed on routine test, which would not have been noticeable had the lower test voltage been used. In one case, the milliammeter read off-scale, 100 plus at 4,000 volts on routine d-c test of an 18,750-kva 6,600-volt lap-wound generator, when for this generator the current, as indicated from test records, should have been approximately 0.12 milliampere at 8,000 volts. In this generator the insulation in the slot portion is mica and the coil ends are of class *A* (fabric). This reading immediately indicated serious leakage to ground. The phases were separated and leakage located in slot portion of a coil midway between top and bottom of core iron. Upon examination of coils when generator was dismantled, it was found that at some time a nut had dropped down between the rotor and stator while generator was in service, cutting into core iron and coil insulation in the front of the coils (Figure 7) but not quite through to the copper. Four new coils were installed in repairing the generator.

Table I

Year	Armature Tests		Total No. Tests	Generators Tested		Generators Failing on Test	
	8,000 Volts	3,000 Volts		Number	Kilovolt-Amperes	Number	Kilovolt-Amperes
1932	—	117	117	75	934,805	3	11,250
1933	—	121	121	77	942,730	1	3,750
1934	125	166	291	80	943,855	3	41,250
1935	90	65	153	87	904,834	1	294
1936	131	85	216	87	904,834	2	3,000
1937	187	112	299	86	927,084	2	7,190
1938	226	154	380	90	1,014,083	1	12,500
1939	191	90	281	87	1,012,834	2	26,750
1940	218	116	334	90	1,028,334	0	0
1941	146	73	219	88	1,131,834	3	26,250
Total	1,314	1,097	2.411				

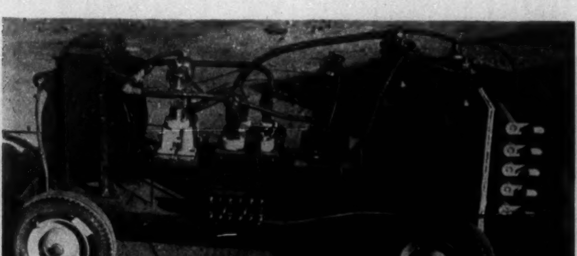

Figure 3. Portable 14,000-volt rectifier with cover removed

In another case, on 8,000-volt d-c test of an 8,000-kva 11,000-volt turbogenerator, the current reading was 8.5 milliamperes which began climbing steadily after about 15 seconds from beginning of test and had increased to 16.0 milliamperes within one minute; therefore, the test voltage was removed. The test voltage was reduced to 4,000 and the current reading remained constant at 3.7 milliamperes for the entire ten-minute period of test voltage application. Next, the test voltage was increased to 6,600 at which the initial current was 6.5 milliamperes but began to rise rapidly as before, so test voltage was removed. Finally, the test voltage was slowly increased from zero in order to determine at what applied d-c voltage the current to ground began to increase, and this was found to be at 6,500 volts. The generator lead cables were disconnected and the generator was again tested with breakdown occurring at 3,500 volts. The end connectors were removed and the generator tested at 8,000 volts with the current varying from 2.64 to 2.5 milliamperes at 58 degrees centigrade within the standard ten-minute test period. Upon examination of the end connectors there was evidence of slight arcing

Table II

Year	Generator Kva	Generator Kv	Standard D-C Test Volts	D-C Test Volts Breakdown Occurred	Dangerous Condition Indicated	Nature of Trouble or Breakdown	Repairs Required
1932	3,750	2.3	3,000	2,500	—	Bar failure	1—new bar
	3,750	2.3	3,000	2,500	—	Bar failure	4—new bars
	3,750	2.3	3,000	2,500	—	Bar failure	1—new bar
	3,750	2.3	3,000	—	Rising current	Cable ground	New section
1933	3,750	2.3	3,000	2,500	—	Bar failure	1—new bar
1934	3,750	2.3	3,000	3,000	—	Bar failure	1—new bar
	3,440	2.3	3,000	—	Rising current	Cable ground	New section
	18,750	6.6	8,000	3,000	—	Coil failure	6—new coils
	18,750	6.6	8,000	3,000	—	Coil failure	4—new coils
1935	294	2.3	3,000	3,000	—	Coil failure	10—new coils
1936	1,500	2.3	3,000	1,000	—	Coil grounded	1—new section
	1,500	2.3	3,000	3,000	—	Coil grounded	12—new coils
1937	3,440	2.3	3,000	2,000	—	Coil failure	1—new section
	3,750	2.3	3,000	1,000	—	Coil grounded	1—new bar
	7,800	6.6	8,000	—	Rising current	Cracked insulators	Replaced
1938	12,500	6.6	8,000	3,500	—	Coil failure	½ winding
	7,800	6.6	8,000	—	Rising current	Wet cables	New section
1939	18,750	6.6	8,000	—	Rising current	Mechanical	4—new coils
	8,000	11.0	8,000	—	Rising current	End connectors	Reinsulated
1940	3,750	2.3	3,000	—	Rising current	Cable ground	1—new section
1941	3,750	2.3	3,000	300	—	Bar failure	1—new bar
	3,750	2.3	3,000	3,000	—	Bar failure	½ winding
	3,750	2.3	3,000	—	Rising current	Cable ground	1—new section
	18,750	6.6	8,000	1,000	—	Coil failure	1—Coil cut out

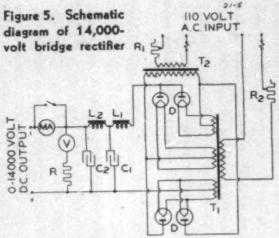

Figure 5. Schematic diagram of 14,000-volt bridge rectifier

T_2—110/20,000-volt transformer
T_1—110/2.5-volt transformer
R—500,000-ohm resistor
R_1—Rheostat R_2—Rheostat
C_1—Capacitance—0.5 microfarad
C_2—Capacitance—1.0 microfarad
L_1—Choke—12-38 henries
L_2—Choke—20 henries
V—Voltmeter
MA—Milliammeter
D—Rectifier tube—type 866A

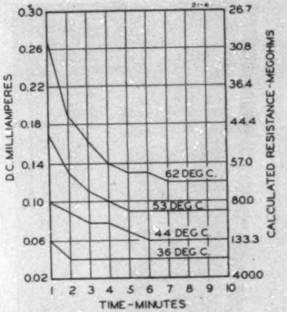

Figure 6. Variation of leakage current with coil temperature

Temperature obtained with imbedded detectors

Table III

Generator Kva	Generator Kv		1934 1	1934 2	1935 1	1935 2	1936 1	1936 2	1937 1	1937 2	1938 1	1938 2	1938 3	1938 4	1939 1	1939 2	1939 3	1939 4	1940 1	1940 2	1940 3	1940 4	1941 1	1941 2	D-C Test Volts
			Periodic Test Readings																						
43,750	13.2	Milliamps.	0.06	0.08	0.85	0.40	0.06	0.10	0.04	0.05	0.10	0.10	0.52	0.34	0.46	0.55	—	—	0.36	0.48	0.22	0.09	0.15	0.14	8,000
		Temp. °C.	30	27	35	23	48	39	25	27	17	17	15	18	20	52	—	—	45	38	38	45	36	35	
18,750	6.6	Milliamps.	0.90	1.0	1.4	0.10	0.07	0.08	0.10	0.06	0.06	0.51	0.28	0.03	0.28	0.38	0.38	0.02	0.08	0.70	0.07	0.06	0.06	0.10	8,000
		Temp. °C.	45	65	47	27	48	54	50	38	48	35	48	24	48	50	52	22	38	44	30	38	40	40	
22,500	6.6	Milliamps.	0.25	0.20	0.26	1.1	0.15	0.06	0.06	0.08	0.03	0.80	0.11	0.04	0.03	0.34	0.20	0.03	0.09	0.46	0.10	0.03	0.02	0.17	8,000
		Temp. °C.	45	60	58	71	36	42	44	48	40	52	48	24	39	29	36	32	44	58	46	32	26	42	
12,500	6.6	Milliamps.	0.60	0.30	0.24	0.60	0.10	0.06	0.08	0.04	0.06	0.03	0.29	0.07	0.15	0.38	0.35	*	0.09	0.18	0.22	0.04	0.02	0.24	8,000
		Temp. °C.	60	65	61	69	55	53	58	50	40	42	54	46	55	52	48		23	30	42	26	24	50	
18,750	6.6	Milliamps.	0.68	1.5	1.6	1.6	1.4	0.02	0.13	0.10	0.13	1.5	0.60	0.10	0.21	0.36	0.64	0.12	0.18	0.78	0.17	0.14	0.05	0.25	8,000
		Temp. °C.	43	65	62	50	54	28	49	42	56	60	60	36	48	42	58	42	52	50	36	47	36	53	
10,625	6.6	Milliamps.	0.40	0.30	0.18	0.40	0.06	0.04	0.04	0.05	0.03	0.62	0.18	0.06	0.07	0.43	0.78	0.06	0.01	0.24	0.10	0.08	0.02	0.18	8,000
		Temp. °C.	49	51	51	32	28	42	46	42	41	24	34	42	40	48	48	42	26	30	26	44	26	40	
18,750	13.2	Milliamps.	0.64	0.64	0.10	0.89	0.02	0.06	0.01	0.05	0.00	0.02	0.30	0.05	0.01	0.04	0.16	0.08	0.02	0.14	0.06	0.11	0.02	0.03	8,000
		Temp. °C.	29	33	17	34	43	28	30	37	8	15	36	19	10	18	38	47	27	36	28	22	20	27	
3,440	2.3	Milliamps.	0.10	0.06	0.04	0.14	0.01	0.01	0.03	0.04	0.01	0.02	0.07	0.05	0.09	0.02	—	0.12	0.01	0.02	0.06	0.05	0.02	0.07	3,000
		Temp. °C.	50	40	18	21	56	49	61	60	42	20	25	18	54	50	—	67	44	50	48	58	50	66	
14,000	6.6	Milliamps.	1.0	0.65	0.52	0.80	0.04	0.09	0.10	0.09	0.09	1.2	0.91	0.54	0.24	0.46	0.88	0.32	0.34	0.44	0.40	0.31	0.28	0.35	8,000
		Temp. °C.	70	64	59	65	18	78	78	73	58	80	80	76	32	30	72	20	24	42	40	24	24	32	
3,750	2.3	Milliamps.	0.17	0.40	0.02	0.20	0.02	0.04	0.13	0.03	0.01	0.39	0.14	0.05	0.09	0.29	0.34	0.01	0.02	0.07	0.16	0.02	0.01	0.30	3,000
		Temp. °C.	58	60	26	76	32	74	29	15	24	30	24	30	19	35	34	12	45	25	28	13	11	71	
7,500	6.6	Milliamps.	0.64	0.70	0.60	2.6	0.04	0.01	0.24	0.06	0.06	0.50	0.20	0.08	0.16	0.26	0.66	0.01	0.01	0.20	0.06	0.02	0.02	0.05	8,000
		Temp. °C.	50	55	51	67	38	52	48	32	44	34	50	38	48	50	64	8	12	32	26	12	7	30	

* Generator failed.

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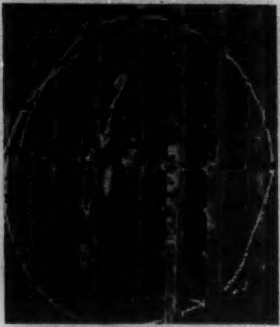

Figure 7. Mechanical damage to a large generator which was located with d-c tester

from one end connector to metal supporting bracket. The end connectors were repaired and replaced and final test at the end of the ten-minute test period at 8,000 volts showed a current leakage of 0.84 milliampere at 23 degrees centigrade.

14,000-VOLT D-C TESTS

Up to the time this paper was written, no higher d-c test voltage than 8,000 has been used. However, all d-c tests since 1940 have been made using the 14,000-volt test apparatus. It is planned at some later date to make tests on the 13,200-volt machines at nearer rated voltage than has been done before. The results of these tests when they are performed are merely a matter of conjecture, but from previous experience in testing, it is believed that the higher d-c voltage will prove more satisfactory than that used at present on the higher-voltage machines.

INITIAL HIGH CHARGING CURRENT

The curves shown in Figure 8 illustrate initial high charging current with the current gradually decreasing to a constant value and remaining constant for several minutes toward the end of the standard ten-minute test period. These curves are quite interesting because they were taken from the time the generator was new, June 28, 1938, up to January 3, 1941. Curves 1, 2, and 3 show the average of periodic tests, and, from a study of these curves, it is evident that the insulation leakage current has made a noticeable decrease over an operating period of approximately two and one-half years. The foregoing curves are of a generator rated at 68,750 kva 13,200 volts, having an average loading factor during this period of 95 per cent. The insulation of this generator is continuous mica tape, ASA classification *B*.

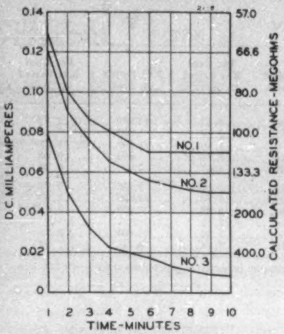

Figure 8. Periodic tests showing insulation characteristics of a 68,750-kva 13,200-volt generator armature

Number 1—June 28, 1938—46.8 degrees centigrade coil temperature

Number 2—March 2, 1940—48.9 degrees centigrade coil temperature

Number 3—January 3, 1941—50.0 degrees centigrade coil temperature

In all tests represented by curves shown, an attempt was made to duplicate as nearly as possible the temperature conditions of previous tests.

DEFECTIVE AUXILIARY APPARATUS

Defective apparatus was found on test such as metering and synchronizing potential transformers, cracked bus insulators, and, in one case, an insect's mud nest on bus insulators caused high milliampere readings. In another case, a defective 13,000-volt potential transformer was found on a routine d-c test at 8,000 volts. The milliampere readings were found to increase steadily with each minute's duration of test voltage application. The curves shown in Figure 9 illustrate this condition as compared with previous test on this particular generator. This increase immediately indicated trouble because, as has been shown previously, the current is higher at the beginning of the test, dropping to a constant value as the test period continues. The potential transformer fuses were removed and the current immediately dropped to 0.10, showing definitely that the excessive leakage was due to the defective transformer.

TEST SUMMARY

A summary of the d-c testing in the field by the company with which the authors are associated is shown in Table I, in which the total number of tests and failures is listed as compared with number of generators tested.

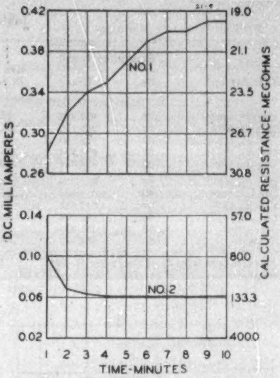

Figure 9. Characteristics of defective apparatus

Curve 1—Before removing transformer fuses; excessive leakage indicated by increasing current

Curve 2—After removing transformer fuses; normal leakage indicated by decreasing current

A detailed listing of generator failures and some miscellaneous troubles found as a result of periodic high-voltage d-c testing is shown in Table II.

The method of correlating periodic tests on individual generators by a comparison of milliampere readings and temperatures is given in Table III.

The considerable variation between some of the periodic readings can be attributed in most cases to long periods of complete shutdown. When any abnormal increase in milliampere readings is found, further investigation is carried on by testing each phase separately to ground. During this test the remainder of the winding is grounded. This individual phase test has recently been incorporated as an annual test along with the regular periodic test schedule.

A comparison of generator failures while in service and on d-c test is shown in Table IV.

Conclusions

In a series of periodic readings on any one generator at approximately the same temperature with identical test voltage applied, any change in magnitude of milliampere readings denotes a change in the insulation characteristics. In order to determine the normal leakage or to detect changes in leakage current, accurate records must be kept of tests on each generator so that a comparison of periodic readings may be made.

Table IV

Year Ending	Generators Tested		Kva Failed in Service	Per Cent Kva Failed in Service	Kva Failed on Test	Per Cent Kva Failed on Test
	No.	Kva				
Dec. 31, 1932	75	934,805	32,750	3.51	11,250	1.21
Dec. 31, 1933	77	942,730	64,900	6.88	3,750	0.40
Dec. 31, 1934	80	943,855	34,500	3.66	41,250	4.37
Dec. 31, 1935	87	904,834	35,125	3.88	294	0.03
Dec. 31, 1936	87	904,834	36,769	4.07	3,000	0.33
Dec. 31, 1937	86	927,084	21,975	2.36	7,190	0.77
Dec. 31, 1938	90	1,014,083	42,062	4.15	12,500	1.23
Dec. 31, 1939	87	1,012,834	49,384	4.88	26,750	2.64
Dec. 31, 1940	90	1,028,334	26,850	2.61	0	0
Oct. 1, 1941	88	1,131,834	15,800	1.39	26,250	2.32
Average	85	974,523	36,012	3.74	13,223	1.33

Averages calculated as of December 31, 1941.
Minimum capacity of generators tested—250 kva.
Maximum capacity of generators tested—68,750 kva.

Figure 12. Defective section of coil insulation of a 12,500-kva 6,600-volt generator located with d-c tester

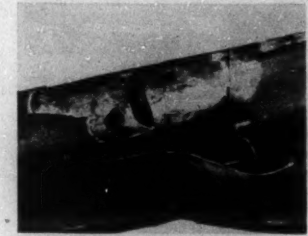
Figure 13. Defective section of coil insulation of an 18,750-kva 6,600-volt generator located with d-c tester

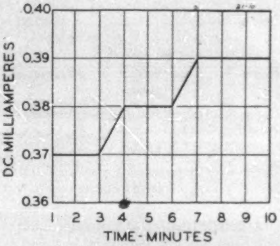

Figure 10. Characteristics of defective insulation

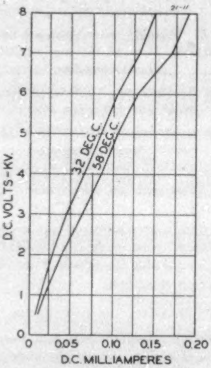

Figure 11. The relation between leakage current and applied voltage

It was found that the 3,000-volt d-c test voltage will give excellent results in testing generators rated at 2,300 volts, 3,750 kva and below. No definite conclusions have been made as to the proper test voltage to be used for testing larger-capacity generators rated at 6,600 volts and above, although 8,000 volts has given very good results so far.

From a series of special tests it was found that the length of time required for the current to reach a constant value which is taken as the criterion of insulation resistance varied with the size, voltage, and coil insulation of the generators. Thus, the following conclusions for time of application of test potential were reached:

Five minutes at 3,000 volts for generators rated up to and including 3,750 kva, 2,300 volts

Five minutes at 8,000 volts for generators rated up to and including 18,750 kva, 6,600 volts

Ten minutes at 8,000 volts for generators rated up to and including 68,750 kva, 13,200 volts

From a study of curves (Figure 11) it may readily be seen that more accurate readings can be obtained at the higher d-c voltages. This permits using a rugged, portable milliammeter which increases the practical value of the testing unit. The values given in these curves were taken on a 22,500-kva 6,600-volt hydrogenerator which was out of service for repairs and was kept heated with space heaters so that no change in temperature occurred between readings. The generator winding was grounded for ten minutes before each voltage test. Each test voltage was applied for the standard ten-minute test period with a current reading taken each minute. The final reading at the end of the test period under each test voltage was plotted so that these curves show no initial charging current to the winding.

It has been the ultimate goal in this testing to detect, if possible, insulation weaknesses before actual failure occurred; therefore, higher d-c voltages have decided and definite possibilities in generator testing because actual breakdown of incipient insulation weaknesses was accomplished which had hitherto not been done with conventional testers. A definite indication of subsequent failure of generator insulation is denoted by a steady or sudden increase in magnitude of milliampere test readings during the test period (Figure 10). This condition, when found, demands immediate investigation. These latent weak spots were found without serious damage to adjacent coils and core iron, which would have occurred if the generator had been allowed to fail in service.

From our experience in d-c generator testing, considerable knowledge has been gained concerning the causes of generator failures and it is believed that the beginning of a majority of generator failures may be traced directly to some mechanical defect.

The testing of generators and the location of troubles before failure in service with the higher d-c voltages over a period of ten years has given us excellent and economical results.

Reference

1. INSULATION RESISTANCE OF ARMATURE WINDINGS, R. W. Wieseman. AIEE TRANSACTIONS, volume 53, 1934 (June section), pages 1010–21.

Relative Value of Different Types of Overcurrent Protection for Distribution Circuits

G. F. LINCKS
ASSOCIATE AIEE

LINE sectionalizing in the main feeder and individual protection at branch-line junctions offers a practical solution to the problem of service interruption caused by overcurrent faults on distribution lines. Actual operating data have shown that as high as 96 per cent of all consumer minutes outage (minutes of outage per consumer per year) are caused by faults out beyond the substation, with 85 to 90 per cent of the outage time resulting from faults occurring on the primary lines. These faults are divided between those which persist so as to require servicing by a line crew (permanent faults) and those which are temporary, so that they cause a negligible momentary opening of the circuit or a momentary collapse of the voltage.[1] Operating experience shows that the percentage of temporary faults varies from 15 to 85 per cent of the total,[2] depending on local conditions and the amount spent on such things as tree trimming and pulling up slack.

The judicious use of overcurrent protective relays and breakers, reclosers, and fuse cutouts depends upon a knowledge of the function and characteristics of these devices[1] and the relative benefit each affords. Automatic opening and reclosing of the circuit can be accomplished with all three types. The relay and some types of reclosers reset automatically after a temporary fault, whereas other types of reclosers and reclosing fuse cutouts require manual resetting or the installation of a new fuse link after a prescribed number of operations. Operating records (Table I) show that the first reclosing of the circuit is the most effective in service restoration, tapering down substantially for subsequent reclosures.[3]

Successful line sectionalizing and branch protection depend on the adequacy of available equipment to meet operating requirements. Advancements in the design and manufacture of fuse links for distribution cutouts have made them approach the high degree of accuracy and dependability of the induction relay.[4] Service records of selectivity of operation over four or five years are being secured with fuse links connected in series and in series with relays, revealing no improper operations.

The major problem is, therefore, when and how best to apply these different devices to provide the best service continuity that can be justified economically. There is a real need for actual operating data evaluating the benefits numerically. This would provide information on the devices under the differing conditions of the various systems. It would not, however, give a comparison of the relative benefits of the different devices under identical conditions. It seems there is little prospect of an early investigation being conducted in service to determine this relationship, so a mathematical study has been substituted. Naturally, this study is limited by the very rigidity of the assumptions, even though they are made as closely as we know how, to operating experience. However, we believe the study should provide a greater knowledge of the relative improvements in service continuity provided by available equipment. This will aid in the selection and application to suit the needs and economics of a particular circuit.

Calculations and Presentation of Data

Separate studies were made on two different setups of the distribution lines, namely: one for branch protection, as in Figure 1, and another for line sectionalizing with protective devices connected in series, as in Figure 2. Using the assump-

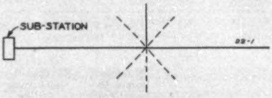

Figure 1. Individual branch line protection added to 30-mile feeder

Branches located at center of feeder for average of even spacing along feeder

No. Branches	Total Length of Line 1-Mile Branches	5-Mile Branches	10-Mile Branches
0	30	30	30
1	31	35	40
2	32	40	50
3	33	45	60
4	34	50	70
5	35	55	80
6	36	60	90

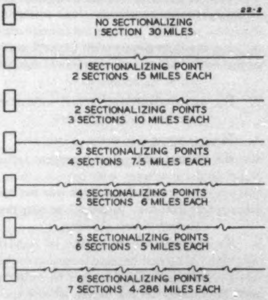

Figure 2. Line sectionalizing with protective devices connected in series on 30-mile line

Table I. Distribution-Feeder Oil-Circuit-Breaker Operation Analysis*

Breaker Held on	1935 No. of Faults	% of Total	1936 No. of Faults	% of Total	1937 No. of Faults	% of Total	Three-Yr Total No. of Faults	% of Total
1st reclosure	100	61.3	96	57.5	141	58.8	337	59.1
2d reclosure	14	8.6	20	12.0	32	13.3	66	11.6
3d reclosure	8	4.9	11	6.6	17	7.1	36	6.3
4th reclosure	1	0.6					1	0.2
Lockout	40	24.6	40	23.9	50	20.8	130	22.8
Total	163	100.0	167	100.0	240	100.0	570	100.0

* From operating experience of the New York Power and Light Corporation, Albany, N. Y., covering portions of the lines to first sectionalizing point on the feeder. The values after "reclosure" indicate the number for which service was restored, or after "lockout" the number for which a permanent outage occurred.

Paper 42-2, recommended by the AIEE committees on power transmission and distribution and protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26-30, 1942. Manuscript submitted April 15, 1941; made available for printing October 28, 1941.

G. F. LINCKS is electrical designing engineer in the distribution fuse cutout section of General Electric Company, Pittsfield, Mass.

The author wishes to thank those who co-operated in making this study, especially Charles R. Craig who did many of the detailed calculations.

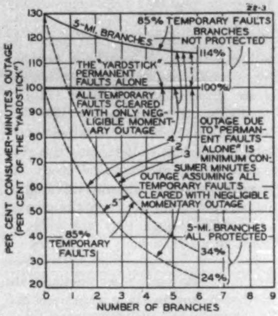

Figure 3. How to read the curves of Figures 4 to 13 inclusive

The curves shown duplicate those of Figure 4 for a nonreclosing substation breaker and single-element fusing at the branches

The "yardstick" equals the best service continuity obtainable with substation equipment alone (see Table II)

The upper pair of solid-line curves gives consumer minutes outage without branch protection and the lower pair of dash-line curves with branch protection. The important observations to be made in this and succeeding figures are (see reference figures on curve):

1. That the consumer minutes outage for the actual substation protection alone is higher than the "yardstick" because the breaker trips on some or all temporary faults as well as on permanent faults (see assumptions and Table IV in the appendix)
2. The improvement over actual substation protective equipment alone
3. The improvement over the "yardstick"
4. The improvement when permanent faults alone produce outages with branch protection—or line sectionalizing (this may be the improvement over the "yardstick" or over the actual substation protection)
5. The consumer minutes outage for manual restoration of service because temporary faults actually cause prolonged outages by tripping both the breaker or the branch or line sectionalizing device (note that for branch protection Figures 4 through 9, this is less than observation 1)

These observations can be converted into actual numerical values, as:

Per cent of ultimate improvement attainable =

$$\frac{\text{observation 2}}{\text{observation 4}}=\frac{114-34}{114-24}=86.7 \text{ per cent}$$

Table II. Calculated Values of Consumer Minutes Outage for Permanent Faults Alone With and Without Line Sectionalizing

(Based on Assumptions as Outlined in Appendix)

	Per Cent Temporary Faults					
	25	50	60	75	85	0-99.9+
Number of Sectionalizing Points	Consumer Minutes Outage for Permanent Faults Alone					*Per Cent Consumer Minutes Outage
♯ None (the "yardstick")	2,137.5	1,425	996.25	712.0	427.5	100
1	1,363	919	638.0	456.0	273.8	64.0
2	1,138	758	552.5	380.2	228.0	53.3
3	1,025	682	478.0	342.0	205.5	48.0
4	951	633	442.5	316.0	189.9	44.4
5	906	602	421.5	302.0	181.0	42.3
6	874	583	407.5	291.5	175.0	40.9

$$\text{* Per cent}=100\times\frac{\text{Actual consumer minutes outage calculated for setup}}{\text{The ``yardstick''}}$$

$$=100\times\frac{\text{Consumer minutes outage caused by permanent faults alone with line sectionalizing}}{\text{Consumer minutes outage caused by permanent faults alone without line sectionalizing (or branch protection)}}$$

For example:

$$=\text{for 1 sectionalizing point 25\% temporary faults}-\frac{1,363}{2,137.5}=64.0\%$$

$$=\text{for 1 sectionalizing point 75\% temporary faults}-\frac{456.0}{712.0}=64.0\%$$

♯ In a corresponding table for branch protection, the consumer minutes outage equivalent to these values—for "permanent faults alone" with no branch protection will vary with each different length and number of branches and will thus compensate for the increasing length of the total line (Figure 1). Consequently, the percentages can be compared, as in Figures 4 to 9, inclusive, to determine the relative improvement.

Table III. Maximum Number of Sectionalizing Points Possible With One-, Two-, and Three-Element Cutouts

	To Secure Proper Sequence of Operation	
Type of Cutout	Minimum Spread Between Fuse Ratings	*Maximum Number Sectionalizing Points Possible
One-element	Every second rating	7
Two-element	†Every second (sometimes every third)	Generally 7 (sometimes between 4 and 7)
Three-element reclosing	Every third or fourth rating	♯3or 4

* Based on 14(N) ratings of fuse links available from 10 to 100 amperes; 10-ampere assumed to be minimum sectionalizing fuse employed.

† See reference 7.

♯ Always at least one less than for two-element cutouts.

tions outlined in the appendix, the number of minutes outage per consumer per year (consumer minutes outage) on the whole circuit was determined for each type and for practically all combinations of types of protective devices now in general usage.

Generally, the problem of whether to use protective devices connected in series on the main feeder or on the branches centers around the advisability of combining these protective devices with nonreclosing equipment at the substation. Thus the "yardstick," chosen for comparison of the relative values of the different equipments and applications, relates everything to the best service continuity obtainable with substation protection alone, that is, without any line sectionalizing or branch protection.

In determining the best service continuity, it was taken into consideration that something might be done about automatic restoration of service after temporary faults, but permanent faults require the time and the work of a line crew before service can be restored. Thus when the consumer minutes outage caused by "permanent faults alone" is the "yardstick," it represents the minimum consumer minutes outage that can be attained with substation protection alone. Such a "yardstick" is universally applicable to any system and, therefore, was used as the 100 per cent base to which all types of protection were related in terms of a percentage of this base.

$$\text{Per cent}=100\times\frac{\text{Actual consumer minutes outage calculated for specific setup}}{\text{The ``yardstick''}}$$

$$=100\times\frac{\text{Consumer minutes outage caused by permanent plus temporary faults for any specific system setup}}{\text{Consumer minutes outage caused by permanent faults alone with no branch protection or line sectionalizing}}$$

Comparisons made in terms of this percentage tend to eliminate the effect of variations of actual practice from the assumptions employed in the study, in so far as this is possible. With any specific system setup, comparisons to the "yardstick," to each other, and to the ultimate attainable can be made of the following:

1. The improvement obtainable with reclosing versus nonreclosing relays and breakers.

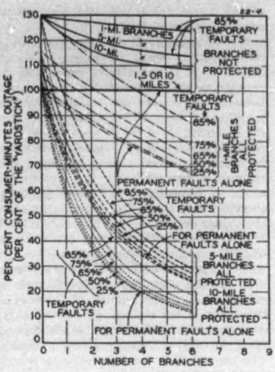

Figure 4 (left). Single-element fusing at branches with nonreclosing breaker at substation

The curves for no branch protection exceeds 100 per cent, because the nonreclosing breaker opens on temporary faults. Five observations should be made:

1. The very large reduction in consumer minutes outage for the "permanent faults alone" with branch protection and the very small additional outage time caused by the branch fuses blowing or breaker operation on temporary faults
2. The longer branches show much greater benefits in reduction of outage time
3. The spread between the curves for permanent faults alone and 85 per cent temporary faults is substantially less for the longer branches
4. The outage time due to temporary faults opening breakers or blowing branch fuses does not reach substantial values until 50 per cent, or until the temporary faults become greater in number than the permanent faults
5. Above 50 per cent the increased outage time on temporary faults increases much faster

2. The additional benefit with various amounts of branch protection or line sectionalizing.

3. The benefit of different combinations of substation and line equipment.

4. The effect of maintenance of line to reduce the percentage of temporary faults.

Curves Provide for Studying Individual Problems

It will be impossible to discuss adequately in the span of one paper all phases of the problem covered by this study. Even if this were attempted, there would be many other questions that would present themselves in actual operating practice on different systems. Therefore, curves (Figures 4 to 9 inclusive for branch protection, and Figures 10 to 14 inclusive for line sectionalizing with a number of protective devices connected in series) have been plotted, in view of their future usefulness in studying specific problems on different systems that may not be covered in the general conclusions to be drawn. The curves present a large share of the data compiled in this study, which may not fit a specific system exactly but should provide a fairly close approximation. The curves for one particular system setup, such as branch protection with nonreclosing breakers, Figures 4 and 8, are all plotted on one or two curve sheets to permit visual determination of the relative benefits with different equipments, percentages of temporary faults, lengths of branches, and so forth. Numerical values can be determined or visualized as described in Figure 3 on "How to Read the Curves."

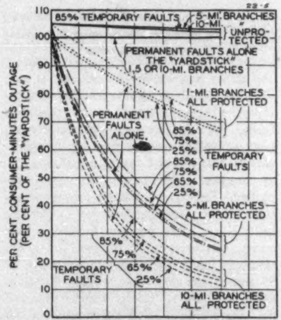

Figure 5 (lower left). Single-element fusing at branches with reclosing breaker at substation

The reclosing breaker obviously reduces to a negligible value the outage-time when clearing temporary faults. Observe three things:

1. That the outage time caused by the blowing of branch fuses on temporary faults is of not much importance up to 75 per cent or even 85 per cent values
2. That the spread for temporary faults covers a very narrow range and does not vary materially with the length of the branch as it did in Figure 4
3. That the improvement provided by the reclosing breaker is not so great with an increasing number of branches, because the branch protective devices afford this improvement with the nonreclosing breaker

For example, compare the top line (for 85 per cent temporary faults) of the groups marked "five-mile branches all protected" in Figures 4 and 5. With 0 branches the curves start at 130 per cent in Figure 4 and at 104 per cent in Figure 5 or a difference (improvement) of 26 units. With three branches the improvement is ten units (60 per cent in Figure 4 and 50 per cent in Figure 5) and with six branches it is six units (34 per cent in Figure 4 and 28 per cent in Figure 5)

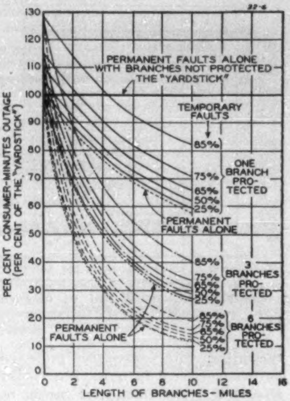

Figure 6. Value of branch protection with different lengths and number of branches with single-element fuse cutouts. Nonreclosing breaker at substation

These curves are similar to those in Figure 4, except that the variable is the length rather than the number of branches. A reclosing breaker at the substation would show, of course, the same improvement over the values in Figure 5 as the above shows over Figure 4. Observe that, while the major improvement is provided with branches four to ten miles in length, there is an appreciable gain over substation protection alone, even with branches one mile long especially where there are several of them

The Effect of Feeders Shorter or Longer Than 30 Miles

The curves in Figure 14 are intended for modification of the other curves (Figures 10 to 13 inclusive) when sectionalizing feeders shorter than 30 miles. If the percentages in Figure 14 are added directly to the values of Figures 10, 11, 12, and 13, the result will be reasonably accurate. For rural feeders longer than 30 miles the reduction from the percentages of Figures 10, 11, 12, and 13 would be so small that they can be neglected.

Combining Branch Protection and Line Sectionalizing

Individual protection of several comparatively short branches and of line sec-

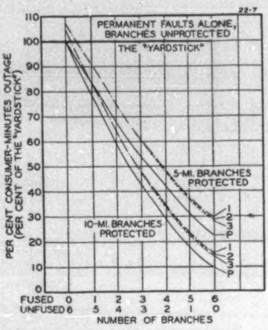

Figure 7 (left). Partial branch fusing, using similar cutout at substation—75 per cent temporary faults

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
P = Permanent faults alone

Many rural lines employ the same type of protection at the substation as that used on the branches, as is illustrated by these curves. It is obvious from these curves that all the branches should be protected. For the effect with less than six branches see Figure 8. Observe that two- or three-element fuses do not show much improvement over single-element fuses

Curves for the resetting recloser would fall just below those for the three-element fuses as in Figure 8

tionalizing with overcurrent protective devices connected in series, was studied separately. The cumulative benefit resulting from a combination of these can be approximated. The curves for such a combination (drawn similar to Figures 4 to 9 inclusive) would start at a percentage value for zero branches which is identical to that shown for the degree and type of sectionalizing employed in Figures 10, 11, 12, and 13. The whole curve would not be moved down bodily as the improvement would decrease with an increasing number of branches.

With a circuit comprising several long branches, the effect of the individual branch protection will be governed by the data in curves of Figures 4 to 9 inclusive. The use of devices connected in series on individual branches should be studied by treating each branch separately in accordance with the data in Figures 10 to 14 inclusive.

Some Protective Devices Can Be Connected in Series in Greater Numbers Than Others

An important factor that should not be overlooked in comparing different equipments, when employed for line sectionalizing, is the number that can be connected in series and still provide proper selectivity of operation[5–6] from the service entrance fuse to the feeder relay and breaker. For example:

1. In comparing two- and three-element reclosing fuse cutouts, it is possible to secure discriminative operation with more of the two- than of the three-element devices connected in series. If an equal number could be employed, the extra reclosure of the third element would account for 10 to 18 per cent reduction in consumer minutes outage (at 85 per cent temporary faults) tapering down to zero (at 25 per cent temporary faults) as shown in Figures 10 and 11. However, splitting the line up into a greater number of sections with the two-element cutouts[6–7] (see Table III) generally affords as good or better service continuity than with three-element cutouts. Sometimes an attempt is made to secure closer fusing by allowing an individual fault to blow two or more section fuses simultaneously, depending on the reclosing fuse nearer the source of supply to maintain service. This voids the benefit of the reclosing cutout closer to the substation, unless all such blown fuses are always renewed before an outage occurs.

2. In regard to automatic resetting reclosers, there are several different ratings available, such as 3, 6, 12, 25, and 50 amperes. Generally, all of these will co-ordinate properly when connected in series. However, it is not always practicable to provide automatic selectivity of operation between the smaller size reclosers and the transformer fuse. This either limits the minimum rating of the recloser usable, and thus the number of sectionalizing points, or necessitates manual closing of the circuit through a fuse shunting the recloser, in order to blow the transformer fuse and thereby locate the fault. In this latter case, the values determined in this study do not apply, since the transformer faults would slightly increase the number of line outages with the reclosers, above the one per mile used for the other types of equipment or system setup. The amount of decrease in the value of the recloser as shown in the data obtained in this study, would depend upon the ratio of transformer-to-line faults. The number of transformer faults which would cause lockout of the recloser are, in general, substantially less than the number of line faults.

For Reclosers Which Do Not Reset Automatically

The conclusions drawn and the data in the curves for reclosing fuse cutouts apply

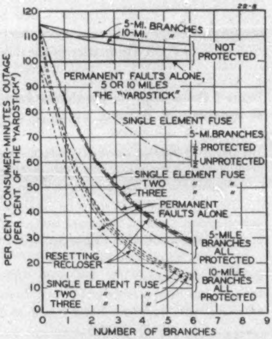

Figure 8. Different types of branch protection with nonreclosing breaker at substation for 75 per cent temporary faults

If a reclosing breaker were used at the substation, the curves would be shifted as in Figure 5. Observe that the reduction in outage time by the reclosing devices below that provided by single-element fuse is very small, due to the few consumers affected by the outages on one branch, caused by temporary faults that otherwise would have had service restored automatically by the reclosing devices (this difference would increase slightly with 85 per cent temporary faults and become negligible at lower percentages)

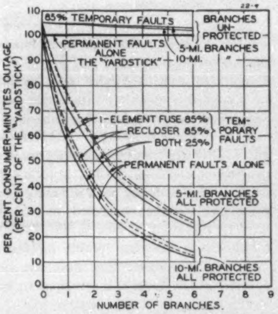

Figure 9. Branch protective devices delayed beyond first instantaneous opening of reclosing substation breaker

The curves start above 100 per cent at zero branches and 85 per cent temporary faults, because the breaker locks out on some temporary faults. The curves for two- and three-element fuses would fall between those for the single-element fuse and the resetting recloser, but, as the range is too narrow, they were not plotted. The curves for 25 per cent temporary faults are so close to those for "permanent faults alone," that they are plotted as one curve. The curves for 50, 65, and 75 per cent temporary faults would fall between those for 25 and 85 per cent about proportionately to those in Figure 5. Observe that here again the outage time is not reduced much below Figure 5 to any type of apparatus with an equal number of reclosers and without the feature of automatically resetting after clearing a temporary fault.

Conclusions

The study permits drawing general conclusions, which may be helpful in system planning.

Is Substation Protective Equipment Enough?

1. Combining overcurrent branch protection or line sectionalizing—with reclosing breakers at the substation—provides a cumulative reduction in consumer minutes outage that neither method alone can provide (compare curves of Figures 4 with 5, 8 with 9).

2. The cumulative reduction from this combination is not so great as with an increasing number of branches or sectionalizing points, but a number of branches do provide substantial combined benefit (see discussion in captions of Figures 5 and 11).

3. Overlapping the substation reclosing protection with line protective devices provides some additional improvement with branch protection and a major improvement with line sectionalizing. With such overlapping, the breaker trips and recloses once, without the branch or line protective device opening for all faults out to the ends of the line, and then the relay provides time delay, so that the branch or line sectionalizing protective device disconnects the faulted portion of the circuit ahead of the second tripping of the relay. Single-element fusing at the branches or sectionalizing points with this overlapping protection approaches very closely to providing the minimum consumer minutes outage attainable for the specific system setup.

4. Where such overlapping protection as in paragraph 3 reaches only part way out on the line, the improvement over a reclosing breaker will be approximately proportional to the percentage of the line so protected, that is, if half the line is so protected, the improvement will be about one half of the difference in percentages between that percentage with a reclosing relay Figure 5 or 11, and that with the overlapping protection covering the whole line, Figure 9 or 12 respectively.

Of What Value Is Branch Protection?

1. Overcurrent protection of individual branches provides a greater improvement over substation protection alone, than is provided by line sectionalizing (compare corresponding curves Figures 4–9 with Figures 10–13).

2. The length of the branch has a major effect on the benefit secured (see Figures 4, 5, and 6).

3. Protecting even a number of short branches provides a cumulative improve-

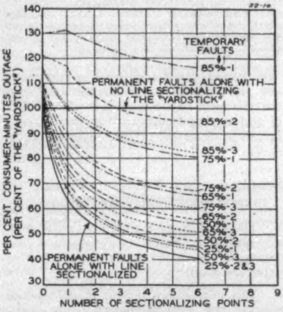

Figure 10. Line sectionalizing with single-element fuse cutouts and with two- and three-element reclosing fuse cutouts with same type at substation as at sectionalizing points

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse

These curves represent conditions found on many rural circuits where the same type of protection is employed at the substation and out on the lines. A nonreclosing breaker could be substituted for the single-element fuse at the substation without changing these curves but, when substituted for the two- and three-element fuses, would raise these curves. The curves exceed the "yardstick" (100 per cent) because of the temporary faults causing outages. Observe three things:

1. The very large reduction in consumer minutes outage for "permanent faults alone" with line sectionalizing, but the much greater increase in this outage time caused by opening on temporary faults by sectionalizing fuses as compared with branch fuses, Figure 4
2. Single-element fusing provides an improvement (for all but one sectionalizing point at 85 per cent temporary faults) which with 65 per cent and lesser percentages of temporary faults is more than half of that obtainable
3. Two- and three-element fuses show much more benefit for sectionalizing than for branch protection

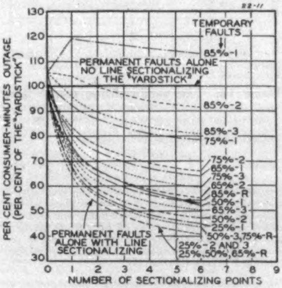

Figure 11. Line sectionalizing with reclosing breaker at substation

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
R = Resetting recloser

The curves exceed the "yardstick" (100 per cent) with 0 sectionalizing points due to the breaker locking out on some temporary faults (see Table IV in appendix). The reclosing breaker obviously reduces to a negligible value the outage-time when clearing temporary faults with no sectionalizing. Observe:

1. That this reduction is not so great with an increasing number of section points (as with branch protection, Figure 5), because line sectionalizing affords some of this reduction anyway, and because the benefit is restricted to the first section beyond the substation, which becomes shorter as the number of section points is increased
2. That single-element fusing provides almost an equal degree of improvement over the reclosing breaker alone as with a nonreclosing breaker, Figure 10
3. That resetting reclosers show a substantial improvement over three-element fuses with temporary faults exceeding 50 per cent

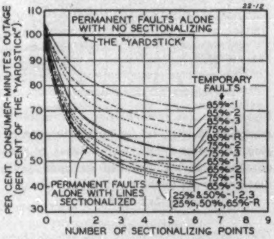

Figure 12. Line sectionalizing with reclosing substation breaker, set for instantaneous tripping on first opening, with all types of sectionalizing devices delayed to open only ahead of second opening

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
R = Resetting recloser

It is assumed that all faults cause operation of the instantaneous trip, corresponding to ground relaying only. The improvement shown will be decreased approaching the corresponding curves of Figure 11, about proportionately to the percentage of faults that are line to line. Observe three things:

1. The major reduction in outage time with all types of sectionalizing equipments, because this overlapping protection is the equivalent to adding one automatic resetting reclosure to the device at each section point
2. The outage time is negligible due to fuse blowing or breaker opening on temporary faults even with single-element fuses at 65, and lesser percentages of temporary faults
3. Reclosing fuses or resetting reclosers at the section points become effective at 75 and higher percentages of temporary faults

ment that is likely to justify at least single-element fusing (see Figures 4, 5, and 6).

4. Individual protection should be applied to all the branches. Protecting only a portion of the branches affords less benefit, Figure 7, than protecting all the branches, Figure 4 (see also Figure 8).

5. Single-element fusing of branches provides approximately 85 to 95 per cent of the total improvement obtainable (see value worked out in example in Figure 3; see also Figures 4, 5, and 8).

6. Two-element reclosing fuse cutouts provide a slight additional improvement in service continuity (see Figures 7 and 8).

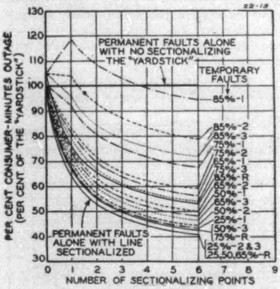

Figure 13. Line sectionalizing with reclosing substation breaker, set for instantaneous tripping on first opening, with all types of sectionalizing devices delayed to open only ahead of second opening of breaker. Breaker protection for only half of line

1 = Single-element fuse
2 = Two-element fuse
3 = Three-element fuse
R = Resetting recloser

Quite often the minimum obtainable pickup current setting of the relay only permits protecting part of the line or on delta circuits the overlapping protection only reaches out part way on the line. These curves show the effect of such relaying covering the first half of the 30-mile feeder. This does not overlap the sectionalizing device with just one point which is located at the center of the line so that the values under this condition are the same as in Figure 11. It should be noted that these curves are located about half way between the respective curves of Figures 11 and 12, or about proportionately to the section of the line on which the overlapping protection is provided

7. Three-element reclosing fuse cutouts afford so little benefit over the two-element reclosing fuse cutout that it raises the question as to the justification for the extra premium paid for the third fuseholder, Figures 7 and 8. (This is true even though the assumptions of Table IV in the appendix favor the three-element device.)

8. Reclosures which reset automatically after clearing a temporary fault provide only a slight additional improvement over that accomplished by reclosing cutouts, even when fuses are not renewed until an actual outage occurs (see Figure 9).

Of What Value Is Line Sectionalizing?

1. Sectionalizing the main feeder and long branches with protective devices connected in series generally affords additional improvement over the best service continuity that can be afforded by overcurrent protective equipment located at the substation.

2. Sectionalizing with single-element fuse cutouts is generally very effective. They

(*a*). Will afford the major portion of the total obtainable improvement at 65 or a lesser percentage of temporary faults with nonreclosing or reclosing substation breakers (see Figures 10 and 11).

(*b*). Will be more effective at 80 per cent temporary faults with the reclosing substation breaker which provides overlapping operation, that is, tripping ahead of all the fuses on the first operation, than three-element reclosing cutouts with a reclosing breaker but no overlapping protection—under similar conditions, at 65 or a lesser percentage of temporary faults will equal the best of reclosing equipment without overlapping protection (compare Figure 12 with 11).

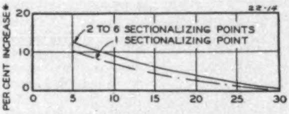

Figure 14. Line sectionalizing shorter lines

Same as on other curves so that these can be added directly to values in Figures 10, 11, 12, and 13

(*c*). Require more than two points with a reclosing breaker or more than one point with a nonreclosing breaker in order to improve rather than impair service at 85 per cent temporary faults (see Figure 10).

3. Sectionalizing with two and three-element reclosing fuse cutouts is much more effective than the use of similar equipment for branch protection. Each affords respectively

(*a*). Some additional improvement over no sectionalizing at 85 per cent temporary faults with a reclosing cutout or breaker at the substation (see Figures 10 and 11).

(*b*). About 25 to 35 per cent additional improvement over the effective operation of the single-element fuse cutout at 75 per cent temporary faults with a reclosing cutout or breaker at the substation (see Figures 10 and 11).

(*c*). About 10 to 15 per cent additional improvement over the much greater effectiveness of the single-element cutout at 85 per cent temporary faults with overlapping reclosing breaker operation (see Figure 12).

The additional improvement referred to in (*a*), (*b*), and (*c*) above is in per cent of the ultimate obtainable between the curves for "permanent faults alone," with and without line sectionalizing. These percentages do not include any of the advantages possible from discovery and renewal of blown fuses with a service interruption (see Table V in the appendix).

4. Generally, two-element reclosing fuse cutouts are more effective than three-element designs because a greater number of the two-element cutouts can be connected in series and still provide selectivity of operation (see Table III). Figures 10 and 11 show this even with the more favorable assumptions for the three-element fuse of Table IV in the appendix.

5. Sectionalizing with reclosers, which reset automatically after clearing a fault, is most effective at the higher percentages of temporary faults where the resetting feature has a greater opportunity to decrease the number of outages of long duration caused by temporary faults, Figure 11.

(*a*). With 85 per cent temporary faults and a reclosing substation breaker, this amounts to 40 to 60 per cent additional improvement over three- and two-element cutouts respectively (see Figure 11).

(*b*). This additional improvement tapers down from about 25 and 35 per cent at 75 per cent temporary faults, to zero at 25 per cent (see Figure 10).

(*c*). With reclosing breakers at the substation which clear ahead of the protective devices on the first opening, the additional improvement is only about 15 to 25 per cent (see Figure 12).

The percentages of additional improvement (*a*), (*b*), and (*c*) above do not take into consideration any difference in the number of reclosers and cutouts that can be connected in series and provide proper co-ordination.

Should Branch Protection and Line Sectionalizing Be Combined?

1. There is a cumulative benefit obtainable by combining branch protection and line sectionalizing which cannot be provided by either one alone.

2. This cumulative benefit is obtainable, whether the system comprises one main feeder with a number of short branches, or two or more long branches with a short main feeder. (With the latter, the branches would be sectionalized with protective devices connected in series.)

3. The cumulative benefit does not increase directly with an increasing number of protected branches.

Should the Percentage of Temporary Faults Be Considered?

1. Any decrease obtainable in the percentage of temporary faults tends to permit securing an equal improvement in service continuity with lower cost overcurrent protective devices.

2. It might prove valuable to make a study in which the costs of improving line construction, tree trimming, and so on are compared with the savings in protective equipment which the improvements made possible.

Use of Curves for Specific Studies

It has been shown by this study that individual protection of branches and/or line sectionalizing can be combined effectively with the reclosing substation relay and breaker to reduce the consumer minutes outage on distribution systems. The data presented in the curves should facilitate the studying of specific problems.

This may make possible more efficient use of available equipment to meet the particular needs and justifiable economics of different circuits and systems.

Appendix. Assumptions Employed in Mathematical Study

1. The Distribution Lines Studied (*Length of Lines*).

(*a*). In one part of the study, one to six unprotected and protected branches, each one, five, or ten miles in length, were added at the mid-point of an unsectionalized 30-mile main feeder, increasing the total length of line, as in Figure 1. (The difference in total length might introduce a slight error in comparing the benefit provided with and without branch protection, but not in comparing the relative benefits afforded by the different equipments that might be employed.) Connecting the branches at the mid-point provided an average for an even spacing of the branches along the feeder.

(*b*). In the other part of the study, a 30-mile main feeder was broken up into from one to seven sections by zero to six overcurrent protective sectionalizing devices connected in series as in Figure 2. (The effect of the shorter, 5 to 30-mile feeders, was checked.)

2. Equipment Employed. Both studies included checking comparative results with overcurrent protection provided by:

(*a*). Substation breakers actuated by nonreclosing relays, automatic reclosing relays, and automatic reclosing relays which overlap all branch and line protective equipment, so that the breaker trips and recloses once without the branch or line devices opening, and then provides time delay before the second tripping of the relay on more persistent faults; each combined with

(*b*). Sectionalizing or branch-line single-element or two- and three-element reclosing fuse cutouts or reclosers which reset automatically after clearing a temporary fault.

3. Number of Consumers. Three per mile uniformly distributed. (Any other number per mile might have been used without affecting the percentages used for comparison.)

4. Number and Type of Faults. One per mile per year uniformly distributed with temporary faults equaling 25, 50, 65, 75, and 85 per cent of the total. (One per mile is probably somewhat high but any other value might have been used without changing the percentages used for comparison.)

5. Attended Substation. A period of five minutes was allowed after the fault occurred for notification and for the attendant to close the breaker, thus restoring service if a temporary fault had cleared itself after the breaker locked open. (Notification by an alarm system at the substation would lower the percentages used for comparison to the extent that the manual closing of the breaker approached that of a momentary outage. Conversely, any lengthening of the time, such as that required to get to an unattended station, would raise the percentages. In both cases, the degree of change would decrease slightly with an increasing number of protected branch or sectionalizing points.)

6. Trouble Crew. Always available at the substation to start instantly with no time allowed for notification or preparation. (More often the crew will be out on the system, sometimes closer to and sometimes farther from the fault location, so this assumption provided an average. If any longer time than the zero had been employed it would have indicated slightly greater benefits for branch protection, line sectionalizing, and single-element fusing.)

7. Locating Fault and Restoring Service. The trouble crew:

(*a*). Traveled 30 miles per hour to the sectionalizing point at which the protective device had opened. No time was allowed for examining sectionalizing or branch protective devices en route, as it was assumed these would have indicating features visible from the car.

(*b*). Spent five minutes to climb the pole and to restore service if a temporary fault had caused the outage at the sectionalizing or branch protection point.

(*c*). Traveled 15 miles per hour to the mid-point of the branch or section on which the fault persisted. The mid-point provided the average for the uniform spacing of faults.

Table IV. Assumptions and Operating Experience on Service Restoration

By Reclosing the Circuit	Service Restored Per Cent of Total Faults on Line‡	
	Values Used in Calculations (Per Cent)	Values From Operating Experience* (Per Cent)
Once	50	50 to 60
Twice	15 Additional	10 to 15 Additional
3 Times	5 Additional	5 Additional
4 Times	3 Additional	1 Additional

‡ If the percentage of temporary faults is less than these values, service is restored only up to the actual number of temporary faults. If greater than the sum of the percentages of restoration for that number of reclosures, it was assumed that the remaining temporary faults persisted longer than the lockout time of the protective device, causing a permanent outage until the substation breaker, line sectionalizing device, or branch protective device was closed manually. Thus perfect operation ("permanent faults alone" causing an outage) was assumed to be provided up to the sum of the 50+15+5 values or 70 per cent of the total number of faults for devices which reclose three times (50 per cent for those which reclose once, 65 per cent for those which reclose twice, and 73 per cent for combinations which reclose four times).

* See Table I in text and reference 3.

Maximum advantage was given to three-element reclosing fuses and other multireclosing devices as compared with two-element reclosing fuse cutouts, since the "values used in the calculations," Table IV, are low for the first reclosing, and high for the second as compared with the "values from operating experience." Some data show as high as 75 to 90 per cent restoration of service following the first reclosure. This would make all the curves for two-element reclosing fuses at branch or line sectionalizing points, as in Figures 7, 8, and 10 to 13 inclusive, approach more closely the curve for "permanent faults alone" for that setup. Of course, there would be a corresponding improvement with three-element fuses and resetting reclosers in approaching or equaling the curves for "permanent faults only."

(*d*). Spent 30 minutes repairing a permanent fault with the outage persisting on the entire faulted portion until the fault was repaired. (The inclusion of some sort of manual sectionalizing or cutting out of the faulted portion would have decreased the number of consumers affected, and thus decreased very slightly the improvement indicated, as resulting from line sectionalizing and branch protection. For example, if the permanent faults had been repaired in zero time for all consumers, the percentages used for comparison would have been raised only about five units.)

(*e*). After repairing the fault:

1. Either the substation attendant was notified instantly when he was required to close the breaker (although some time probably would be required for notification, which would have enhanced the value of line sectionalizing and branch protection).

2. Or the crew traveled 30 miles per hour to return to the sectionalizing or branch protective device to restore service.

8. Service Restoration by 1st, 2d, 3d and 4th Reclosures was assumed to be in accordance with the values in Table IV. Service restoration is expressed as a percentage of the total faults on the line.

9. No Inspection With Reclosing Fuse Cutouts Was Assumed. Because such inspection would have permitted re-fusing before an outage occurred, the data indicate only the minimum benefit for these devices. (This benefit would be increased to a degree approaching that of the device that resets automatically as such fuse renewal before the occurrence of an outage approaches 100 per cent. The percentage of such discovery and renewal is fairly high, because linemen and trouble crews are on the lookout for the indicating devices as they pursue their regular duties along the lines, Table V. Thus all reclosing fuse data are ultraconservative.)

10. The Protective Orbit of the Substation Breaker (which is determined by minimum pickup current of the relay) included the whole feeder and all of the branches. In many instances, distribution lines have outgrown this orbit. Consequently, the lower current individual protection of the branches and line sectionalizing on the otherwise unprotected portion may prevent burning down lines, annealing of the conductors, and loss of revenue, in addition to any advantages shown by the study. Conversely, this lower current protection will cause some outages (momentary or prolonged, depending on the type of equipment) for faults which would have burned clear. The combined effect of these few additional operations will balance off to the extent that faults which otherwise would have burned clear,

Table V. Operating Record of Two-Element Reclosing Cutouts on Line With Practically No Special Patrolling

Year	Total No. Faults	No. of Permanent Faults	Number of Temporary Faults Restored By Cutout				
			Discovered Without Outage	Not Discovered Before 2d Fuse Blew	Total Restored	Caused Outage	Total
1939	36	8	8	10	18	10	28
	100%	22.3%	22.3%	27.7%	50%	27.7%	77.7%
1940	59	15	14	15	29	15	44
	100%	25.5%	23.7%	25.4%	49.1%	25.4%	74.5%

Service was restored on $^{2}/_{3}$ of the temporary faults, approximately $^{1}/_{3}$ being made possible by discovering and renewing first fuse link before an outage occurred, and $^{1}/_{3}$ by the reclosing operation of the cutout.

cause greater or lesser consumer minutes outage than faults for which additional protection is provided. No data are available on this relationship. However, they probably would have slight effect on the percentages used for comparison.

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Current-Transformer Performance Based on Admittance-Vector Locus

A. C. SCHWAGER
MEMBER AIEE

Synopsis: In the past designers as well as users of current transformers have employed ratio error and phase-angle curves in which the abscissa represents primary or secondary current to a linear scale. Numerous curves were necessary both for obtaining a clear picture of the performance characteristics as well as for determining the errors for the multiplicity of possible secondary burdens. Part I of this paper shows that a more functional picture of current-transformer operation is obtained by replacing these commonly used curves by the admittance-vector locus of the secondary winding with the primary open circuited, the end point of the vector representing the independent variable in a curvilinear co-ordinate system. The numerous ratio and phase-angle curves resulting from various secondary burdens and, in case of multiratio transformers, from different numbers of turns, when referred to this new co-ordinate system revert to one single curve. Ratio error and phase angle for any burden at any power factor, turn ratio, and secondary current can be scaled or read directly from a chart using as a basis the admittance vector locus of the steel forming the magnetic circuit of the transformer. For designers as well as users it is often advantageous to be in possession of analytical expressions for ratio error and phase angle. In part II of the paper general formulas are set up which express the performance in terms of the various constants and variables of a transformer, making it unnecessary to refer to charts for the analysis of important design or performance factors.

Part I

BASED on the fundamental paper by P. G. Agnew[1] published in 1911, a practically standard procedure of computing current-transformer performance has been established in the past three decades[3] consisting of the following steps:

1. The transformer is most readily analyzed by introducing the 1/1 ratio type in which the number of primary turns is made equal to the number of secondary turns.

2. The above transformer can be represented by the equivalent circuit shown in Figure 1 and the corresponding vector diagram Figure 2 in which

$OA = I_2$ = secondary current
$OB = I_e$ = exciting current
$OC = I_m$ = magnetizing-current component of I_e
$CB = I_w$ = watt-current component of I_e
$OD = I_2R_B$ = voltage drop across resistance of burden
$DE = I_2X_B$ = voltage drop across reactance of burden
$EF = I_2R_2$ = voltage drop across resistance of secondary winding
$FG = I_2X_2$ = voltage drop across leakage reactance of secondary winding
$OE = I_2Z_B = E_B$ = voltage drop across burden
$OG = I_2Z = E_2$ = voltage drop across total secondary impedance
ϕ = phase-angle lag between E_2 and I_2
θ = deviation of phase angle between I_1 and I_2 from 180 degrees
ϕ_0 = phase-angle lag of exciting current referred to the reversed secondary voltage E_2

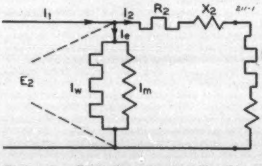

Figure 1. Equivalent circuit of a 1/1 ratio current transformer

3. Magnetizing and watt components of the exciting current are computed from charts showing watts, reactive volt-amperes, and volt-amperes per pound for the steel used in function of the flux density; see Figure 3. The conventional procedure consists of computing the voltage E_2 for the secondary current and burden for which the errors are to be determined, and calculating the flux density from

$$B = \frac{E_2 10^8}{A N f\, 4.44} \text{ gausses} \tag{1}$$

where

A = cross section of core in square centimeters
N = number of secondary turns
f = frequency

and obtain I_m and I_w as follows:

$$I_w = W/E_2;\quad I_m = RVA/E_2;\quad I_e = VA/E_2 \tag{2}$$

The curves shown in Figure 3 apply to a conventional grade of silicon steel and will be used as a basis in the following. If a sample of the transformer to be investigated is on hand I_w, I_m, and I_e are available from the excitation curves of the secondary winding.[3]

4. Ratio and phase-angle can be computed by means of the following formulas:

$$R(1/1) = \frac{I_2 + I_m \sin\phi + I_w \cos\phi}{I_2 \cos\theta} \tag{3}$$

$$\tan\theta = \frac{I_m \cos\phi - I_w \sin\phi}{I_2 + I_m \sin\phi + I_w \cos\phi} \tag{4}$$

$$R_{(N)} = \frac{N}{N_1} R(1/1) \tag{5}$$

where N_1 = number of primary turns.

$$\text{Ratio error} = \Delta(1/1) = R(1/1) - 1 \tag{5a}$$

In order to obtain curves showing the errors over a current range, the above procedure must be repeated for numerous secondary-current values. In the following it will be shown that a clearer picture of current-transformer performance results if numerical calculations of ratio error and phase angle are not executed at such an early stage. The method used is similar to that of representing the per-

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A. C. Schwager is vice-president, in charge of engineering, of Pacific Electric Manufacturing Corporation, San Francisco, Calif.

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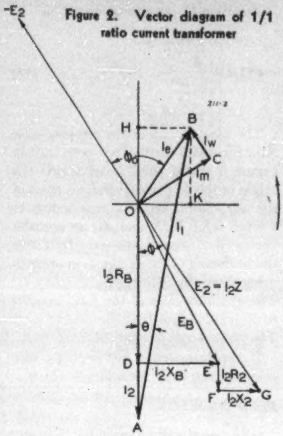


Figure 2. Vector diagram of 1/1 ratio current transformer

formance of power transformers and induction motors by means of circuit loci, or specifically by means of the circle diagram. Referring to Figure 2, extending calculations into the complex plane, the complex ratio $\boldsymbol{R}(1/1)$ can be expressed as follows:

$$\boldsymbol{R}(1/1)=\frac{\boldsymbol{I}_1}{\boldsymbol{I}_2}=\frac{-\boldsymbol{I}_2+\boldsymbol{I}_e}{\boldsymbol{I}_2}=-1+\frac{\boldsymbol{I}_e}{\boldsymbol{I}_2} \qquad (6)$$

Since $\boldsymbol{I}_2\boldsymbol{Z}=\boldsymbol{E}_2$, (6) can be written as follows:

$$\boldsymbol{R}(1/1)=-1+\frac{\boldsymbol{I}_e}{\boldsymbol{E}_2}\boldsymbol{Z} \qquad (7)$$

The vector $\boldsymbol{I}_e/\boldsymbol{E}_2$ can be recognized as the negative admittance vector of the secondary winding with the primary winding open circuited; denoting it with $\boldsymbol{Y}$ it follows:

$$\boldsymbol{R}(1/1)=-1-\boldsymbol{YZ} \qquad (8)$$

The ratio of a 1/1 transformer is equal to the negative real number one decreased by the product of the admittance vector and the total secondary impedance vector.

It is customary to represent the performance of transformers at various fixed secondary burdens Z_B. In bushing-type transformers with equally distributed secondary winding, the leakage reactance is negligible in comparison to the burden impedance and in wound-type transformers it can, in many cases, be considered constant over the normal metering range. Under these conditions Z remains constant when considering ratio error and phase-angle changes due to variations in the secondary current. The admittance vector becomes the basic current-transformer curve, curves for various burdens being similar. An investigation of the admittance-vector locus is therefore indicated.

The Admittance-Vector Locus

It appears advisable to refer to a specific transformer at this time. Due to its most frequent use a multiratio bushing transformer will be investigated, but none of the generalities of the study is thereby sacrificed. The transformer has N secondary turns equally distributed on a core of $17^1/_4$ inches inside diameter, $21^3/_4$ inches outside diameter, and $7^3/_4$ inches height, giving a cross section of 112.5 square centimeters and a weight of 265 pounds; it is used in conjunction with 115-kv oil circuit breakers. The admittance-vector locus is determined in accordance with the following procedure:

From formula 1 the flux densities B are determined for various assumed secondary voltages E_2; referring to chart, Figure 3, the corresponding VA/lb and W/lb values are determined. The power factor is computed for each voltage, and the total volt amperes are obtained by multiplying the VA/lb by the weight. The magnitude of the admittance vector is equal to

$$Y= VA/E_2 \qquad (9a)$$

If curves showing the exciting current and its components in relation to the secondary voltage are available,[3] the admittance vector and its components can be obtained as follows:

$$\boldsymbol{Y}=\boldsymbol{I}_e/\boldsymbol{E}_2;\ \text{conductance } G=I_w/E_2;\ \text{susceptance } B=I_m/E_2 \qquad (9b)$$

For each voltage E_2 the calculated admittance vector is plotted. Due to its frequent use the admittance vector corresponding to one secondary turn has been computed and is shown in Figure 4. As can be seen, the locus has the shape of a hairpin, the admittance decreasing from 365 mhos for approximately 0.003 volt to 30 mhos for 1.5 volts and increasing again to large values for increasing voltage.

Similarly, the phase angle with respect to the secondary-voltage vector (positive real axis) decreases to a minimum with increasing voltage and increases with further increase in voltage. It is apparent that this curve is a function of the characteristics of the steel only; a more basic curve is shown in Figure 5, where the flux density is chosen as the independent variable. In the range shown the admittance decreases for flux-density values of from 10 to 5,000 gausses, where a minimum is reached, and increases thereafter with increasing flux density. Minimum admittance corresponds approximately to maximum permeability of the steel; the right-hand branch of the curve indicates the region below saturation, the left-hand branch the region in which saturation occurs. ϕ_0 is the phase angle of the exciting current of the steel. It is advantageous to refer to this basic curve, having B or a quantity directly proportional to B as independent variable. Obviously, the value of volts per turn E_2/N is such a quantity, having the advantage that it is related most directly to the important variables of the problem, namely applied voltage and secondary turn numbers. If the admittance vectors for a given core and varying turn numbers are compared at identical flux densities or volts-per-turn values,

Figure 3 (below). Volt-amperes, reactive volt-amperes, and watts for one pound of steel in relation to flux density

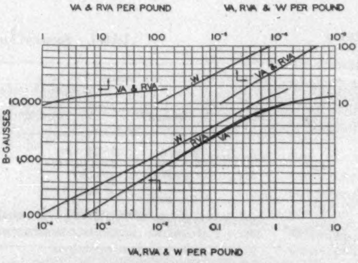
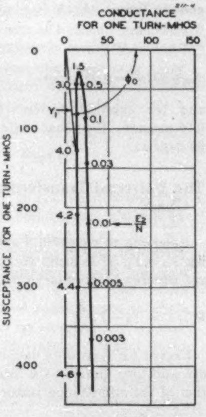
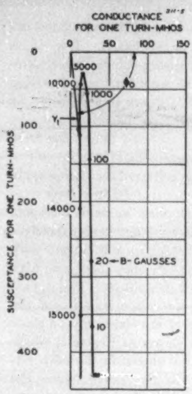


Figure 4 (right). Admittance vector Y_1 in mhos for one turn as function of volts per turn for 115-kv bushing transformer

Admittance for N turns computed according to $Y_N=Y_1/N^2$





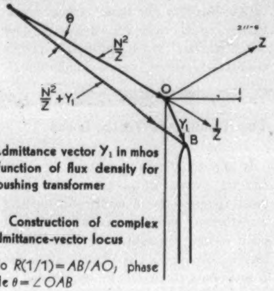


Figure 5 (left). Admittance vector Y_1 in mhos for one turn as function of flux density for 115-kv bushing transformer

Figure 6 (right). Construction of complex ratio from admittance-vector locus

Magnitude of ratio $R(1/1)=AB/AO$, phase angle $\theta=\angle OAB$

then the following well-known formula results:

$$Y_N(B)=Y_1(B)/N^2 \tag{10}$$

This relation is indicated in Figure 4 showing Y_1, making it useful for determining the admittance for any number of turns. In order to profit by the application of this equation, volts-per-turn values are used as independent variable in the following.

Aside from its application in the following analysis, Figure 4 is valuable for determining the exciting-current vector in idle transformers in conjunction with multiple interconnected circuits, such as in bus-differential scheme protection.[4]

The use of this chart shall be shown in conjunction with an example:

Determine the exciting current of the above transformer for a voltage of 20 volts and 40 turns. E_2/N is computed to be 0.5 volt per turn. The resulting admittance Y_1 is 49 mhos, therefore

$Y_{40}=49/40^2=0.031$ mho

and the exciting current $=0.031\times 20=0.62$ ampere; the phase angle amounts to 66 degrees.

The Universal Transformer-Ratio Chart

Referring to equation 8 and substituting $Y_N=Y_1/N^2$ in order to generalize for any number of secondary turns, it follows:

$$R(1/1)=-1-Y_1\frac{Z}{N^2} \tag{11}$$

Except for constants defining the burden and turn number, the ratio is a function of the admittance vector Y_1 only.

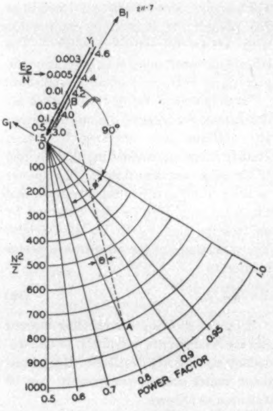


Figure 7. Chart for calculation of ratio and phase angle for any number of secondary turns N, any total burden Z at any power factor and any desired secondary current I_2, provided $N^2/Z<1{,}000$

Example: $N=40$, $Z=2$ ohms, pf=0.8, $I_2=84$ amperes. $E_2/N=I_2Z/N=4.2$ volts per turn, giving point B on locus. $N^2/Z=800$, giving point A on 0.8 pf line. Ratio $(1/1)=AB/AO=1.19$, $\theta=\angle OAB=9$ degrees 30 minutes

Equation 11 can also be written as

$$-R(1/1)=\frac{\frac{N^2}{Z}+Y_1}{\frac{N^2}{Z}} \tag{12}$$

The construction of the complex ratio R in accordance with 12 is developed in Figure 6 where point O represents the origin of the Y_1 plane, B the end point of the admittance vector corresponding to a given value E_2/N, and AO the complex vector N^2/Z with its end point shifted to the beginning of the vector Y_1 in order to bring about the addition.

The absolute value of the ratio is equal to $R(1/1)=AB/AO$

The phase angle is equal to $\theta=\angle OAB$

If the transformer has a single turn primary, the ratio according to (5) is

$R(N)=N\times R(1/1)$

Using the construction shown in Figure 6, the chart in Figure 7 has been prepared from which ratio and phase angle for various burdens, secondary turns, and secondary currents can be quickly determined.

Assume a secondary current I_2, a secondary burden Z in ohms of power factor pf, and a given number of secondary turns N, the procedure followed is

1. Compute volts per turn $=I_2Z/N$ and mark point on locus Y_1 corresponding to this value as B.

2. Compute N^2/Z and mark corresponding point A at proper power factor resulting in
True ratio $=N\times AB/AO$
Phase angle $=\angle OAB$

Except for low ratio bushing-type transformers, Figure 7 can be modified so that the ratio error and phase angle can be read directly off scales. For bushing-type transformers with a large number of secondary turns and for wound-type transformers, the phase angle θ becomes so small that I_2 and I_1 can be considered parallel. Referring to Figure 2, the projection OH of OB upon the secondary-current axis divided by the secondary

Table I. Formulas for Transformer Performance

		Flux-Density Range	
Line	Quantity	Low 100–5,000 Gausses	High 12,000–18,000 Gausses
1	Watt/lb	$7\times 10^{-9}B^2$	$5\times 10^{-9}B^2$
2	RVA/lb	$3.5\times 10^{-7}B^{1.6}$	$5\times 10^{-41}B^{10}$
3	G_N mhos	$20.6/N^2$	$14.7/N^2$
4	B_N mhos	$\frac{40.2(E/N)^{-0.4}}{N^2}$	$\frac{2.24\times 10^{-3}(E/N)^6}{N^2}$
5	$\Delta R(1/1)$	$34.8(Z/N^2)\{(E_2/N)^{-0.4}+0.296\}$	$1.94\times 10^{-3}(Z/N^2)\{(E_2/N)^6+3{,}790\}$
6	θ(min)	$69{,}100(Z/N^2)\{(E_2/N)^{-0.4}-0.888\}$	$3.85(Z/N^2)\{(E_2/N)^6-11{,}370\}$

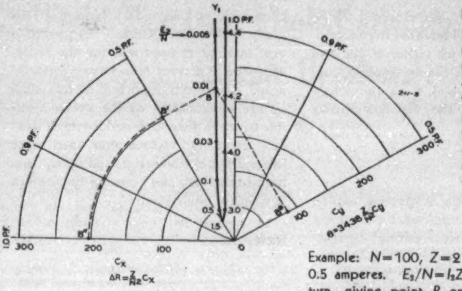


Figure 8 (left). Chart giving ratio error and phase angle for any number of secondary turns N, any total burden Z at power factors of 1.0, 0.9, and 0.5 and any desired secondary current I_2, provided $N^2/Z \gg 1{,}000$

Example: $N=100$, $Z=2$ ohms, pf=0.5, $I_2=$ 0.5 amperes. $E_2/N=I_2Z/N=0.01$ volts per turn, giving point B on locus. Project B perpendicularly upon 0.5 pf line in left quadrant giving B'; follow B' along circle about O to B'' and read value $C_x=203$, giving: $\Delta R=Z/N^2 \times 203=0.04$, resulting in $R(1/1)=1.04$ and $R(N)=104$

Project B perpendicularly upon 0.5 pf line in right quadrant giving B^*; read value $C_y=88$, giving: $\theta=3{,}438 \times Z/N^2 \times 88=60$ minutes

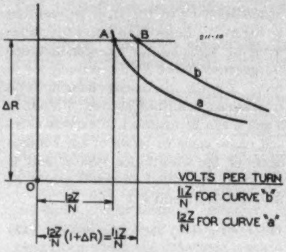


Figure 10. Transformation of I_2Z/N abscissa into I_1Z/N abscissa giving curve "b" from which ratio errors at given primary current can be read

current closely approximates the ratio error $\Delta R=R(1/1)-1$. Similarly, the projection, OK of OB, is directly proportional to the phase-angle. Figure 8 shows the transformation of Figure 7 for the case of "small errors" for burden power factors of 0.5, 0.9, and 1.0. The readings obtained from the scales have to be multiplied by Z/N^2. Since Figures 4, 7, and 8 are based on the Y_1 curve, they can, if desired, be combined into one chart providing all the essential data required for the following calculations:

1. Exciting current of secondary winding.
2. Ratio error and phase angle for few secondary turns.
3. Ratio error and phase angle for a large number of turns.

It is believed that these charts give a more functional picture of the transformer performance than the large number of ratio and phase-angle curves previously required to serve the same purpose. Essentially there exists only one curve, the admittance-vector locus of the core into which all other curves revert by proper transformation. The practical advantage of the Figures 7 and 8 are numerous. In the past relay engineers, when determining errors for a certain burden were forced to interpolate from a large number of curves representing different burdens and power factors. The new methods give accurate values from one single curve which gives equal emphasis to the normal load and overload range. The problem of matching transformers becomes particularly simple; ideal matching requires point A on two transformers to slide simultaneously over identical E_2/N values.

In the case of bushing-type current transformers used for overcurrent relaying it is not important to know the phase angle. Figure 8 can, therefore, be changed to permit the use of a linear scale for E_2/N as shown in Figure 9, which indicates how the Y_1 curve is transformed into the conventional ratio-error curve "*a*." It follows that for transformers with "small errors" the ratio error ΔR for any number of turns and for any burden at a given power factor and any secondary current can be represented by a single curve in a rectangular co-ordinate system with linearly progressing secondary current scale.

It is often preferable to know the ratio error in terms of primary rather than secondary current. From curve "*a*" Figure 9, which shows the ratio error in function of secondary volts per turn, I_2Z/N, and therefore of I_2, another curve can be constructed which represents the error in function of the primary current. Since $I_1=(1+\Delta R)I_2$, the following relation exists:

$$I_1Z/N=(1+\Delta R)I_2Z/N$$

Curve "*b*" in Figure 10 is obtained from curve "*a*" by determining I_1Z/N for various I_2Z/N values in accordance with the above relation and assigning to the I_1Z/N values so obtained ordinates equal to those corresponding to the respective I_2Z/N or E_2/N values.

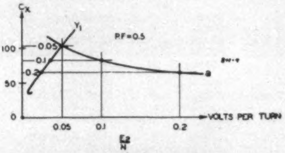


Figure 9. Transformation of Y_1 curve into conventional ratio-error curve "a", total burden power factor=0.5

Different curves result for differing Z/N^2 values; however, since, in standard multiratio transformers with burdens of 0.5, 1.0, and 2 ohms, several combinations of burdens and turns give identical Z/N^2 values, many of the curves coincide, resulting in a corresponding reduction in the total number of curves previously required.

In Figures 7 to 10, Z designates the total secondary impedance. However, since, in bushing-type transformers with equally distributed secondary winding, the leakage reactance is negligible and the resistance of the winding is small when compared to the resistance of the external burden, Z closely approximates the external-burden impedance.

The results obtained by the use of these charts are identical with those obtained by previous methods. For any current value, any secondary burden and secondary number of turns, ratio and phase-angle values calculated from these charts are identical with those obtained by the use of previous methods.[1-4]

Part II

Referring to Figure 5 it can be seen that the admittance-vector locus consists essentially of two straight lines which are parallel to the imaginary axis, indicating constant conductance for both low- and high-density range. It has been shown that the reactive volt-amperes in these two ranges can be closely approximated by formulas containing constant exponents for the flux density B.[5] Lines 1 and 2 in Table I, give the formulas for watts/pound and reactive volt-amperes/

pound applying to the steel represented by Figure 3. Lines 3 and 4 give the corresponding formulas for the conductance G_N and susceptance B_N respectively for N turns, for the particular bushing-type transformer described above. Formulas 3 and 4 can be modified to express ratio and phase angle in terms of the components of the admittance vector, and if the study is limited to small errors or $N^2/Z \gg 1{,}000$, result in

$$R(1/1) = 1 + Z(B_N \sin\phi + G_N \cos\phi) \quad (13)$$

$$\theta_{(\text{min.})} = 3{,}438Z(B_N \cos\phi - G_N \sin\phi) \quad (14)$$

Substituting for G_N and B_N the values from lines 3 and 4 of Table I into equations 13 and 14 and limiting the investigation to the commonly used power factor of 0.5, lines 5 and 6 result for ratio error and phase angle respectively. Although a wide range of flux density is omitted from formulation, it can be seen from Figure 5 that it represents a relatively small absolute range around the bend of the admittance-vector locus. A fair approximation can be obtained for this range by assigning it constant values corresponding to the values resulting from $B = 5{,}000$ in the low-flux density formulas of Table I, lines 3 to 6 inclusive. If the expression E_2/N is eliminated from formulas, lines 5 and 6, for either of the two flux-density ranges, linear equations result between ΔR and θ as previously pointed out for the low-flux-density range.[6]

Conclusions

From the foregoing analysis it can be seen that the admittance-vector locus is a useful means of representing current-transformer performance. It is advantageous to introduce volts per turn as independent variable. This procedure results in charts from which normal and overload performance of a particular transformer at any burden and turn ratio can be obtained from one single curve, the admittance-vector locus of the steel used in the core.

A considerable saving in drafting effort is accomplished in case of multiratio bushing-type transformers where heretofore a large number of curves was required. If desired, reference to charts can be avoided by the use of simple formulas giving ratio error and phase angle for the entire load range and for any desired turn ratio and secondary burden. This study is intended primarily to present a new method of approach for the representation of current-transformer performance. Numerous important factors, such as the modification of the vector locus due to wave distortion and methods used in measuring volt-amperes and core losses, have not been reported upon; it is hoped that they can be made the subject of a future publication.

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Field Tests on High-Capacity Station Circuit Breakers

H. D. BRALEY
ASSOCIATE AIEE

Synopsis: The development of high-capacity air-blast breakers in this country has taken place without the background of operating experience which has attended the use of oil circuit breakers and has, therefore, emphasized the need for testing this equipment at levels comparable with the assigned interrupting ratings.

Although field short-circuit tests to verify circuit-breaker performance have been made on operating systems previously, they have been limited in most instances to moderate duty levels or, in the larger interrupting ratings, to the testing of high-voltage apparatus. Staging severe short-circuit tests at 15 kv introduces operating problems of greater significance since it virtually requires the application of the fault to the generating-station bus. This paper presents:

1. A résumé of the studies which indicated that it would be practicable to stage short-circuit tests ranging up to 2,000 megavolt-amperes directly on a particular generating-station bus.
2. A description of the physical plant equipment assembled for test purpose.
3. Operating experience during a series of 14 short circuits ranging from 200 to over 1,500 megavolt-amperes at 14.5 kv.

IN 1939 a trial installation of two 15-kv air-blast circuit breakers with a rated interrupting capacity of 500 megavolt-amperes was placed in service at the Waterside II station of the Consolidated Edison Company of New York, Inc. As a result of this operating experience a total of 24 15-kv air-blast circuit breakers ranging in rated interrupting capacity from 500 to 2,500 megavolt-amperes were purchased for installation in the Sherman Creek station.

The success with which the manufacturers of oil circuit breakers had been able to predict the performance of this equipment, as the result of field tests and extrapolation of laboratory tests at lower levels of duty, had been generally satisfactory, and there was, therefore, reasonable expectation that the same procedure could be extended to the air-blast design. However, the guide posts were not as well-marked as those in the oil-circuit-breaker field, and it seemed desirable to make tests up to the full rated interrupting capacity on one of the air-blast circuit breakers in the higher interrupting range.

When confronted with a serious proposal to stage not one but a series of major short-circuit field tests, it was only natural that the initial reaction of the more conservative minded should be one of considerable concern. The more progressive souls decreed that in addition to making an investigation of the feasibility of staging field short-circuit tests, consideration should also be given to the practicability of exposing the entire system to the short-circuit disturbance, on the hypothesis that it would be better to learn the effects under controlled conditions than possibly to be required to encounter a similar disturbance unexpectedly. The information obtained as a result of the field tests proved the wisdom of this course of action.

Preliminary Analysis

The first approach to this undertaking for making tests ranging up to 2,000 megavolt-amperes was to select the generating plant best adapted to the purpose with regard to capacity in generators, tie feeders, switching flexibility, space available for test apparatus, and facilities necessary to supply the required current and voltage. It was quickly ascertained that the Hell Gate station was outstanding in all the desired requirements by comparison with other stations in the system. Preliminary studies indicated that the mechanical stress imposed upon the windings of any generator would be not more than 50 to 60 per cent of that normally obtained with a short circuit on the generator terminals and should, therefore, be within safe limits if all winding insulation and bracing were in first-class condition. Furthermore, machines in this station had been subjected to short circuits in which the calculated mechanical stresses were of the same order as those expected in the proposed tests, and these had resulted in no apparent damage.

The second step involved short-circuit studies to determine the number of machines and tie feeders which would be necessary to furnish the required test current at each test level, making such allotment of generators and tie feeders as to expose each machine to a minimum number of short circuits. It was concluded that the three-phase short-circuit tests should be undertaken with the necessary generators isolated from the system load, except through such tie feeders as might be necessary for loading machines prior to the application of the fault and for providing the additional capacity to furnish the required fault current. This decision was based on two factors:

First, a three-phase fault directly on the Hell Gate load bus would result in a serious disturbance to the low-voltage network system served directly from that bus at generator voltage, and this procedure could not, therefore, be undertaken without annoyance to customers.

Second, the effect on the system would be greatly diminished by the cushioning impedance of the tie feeders. Moreover this would be a more severe test of the system performance than disturbances which had

Table I. Calculated and Actual Test Currents at 14.5 Kv

			Three-Phase Test Current and Duration (60-Cycle Base)				Connected Megavolt-Ampere Capacity	
			Calculated		Test			
Test	Date	Duty	Amperes	Cycles	Amperes*	Cycles	Generators	Ties
1	9-21-40	*O*	8,900	6	9,400	6		60
2	9-21-40	*O*	24,000	6	22,000	6	106	60
2*A*	9-21-40	*CO*	24,000	6	25,000	5	106	60
3*A*	9-21-40	*CO*	40,000	6	34,000	28	188	60
3	9-22-40	*O*	40,000	6	36,000	6.5	188	60
4	9-22-40	*O*	53,000	6	45,000	6.5	294	60
5*B*	9-22-40	*O*	80,000	2.4	65,000**	26.0	294	160
1*A*	5-10-41	*O*			24,000	6.2	106	60
2*A*	5-10-41	*CO*			23,000	6.2	106	60
3*B*	5-10-41	*O*			48,000	6.1	294	60
4*B*	5-10-41	*CO*			47,000	6.2	294	60
5*C*	5-10-41	*O*			62,000	3.3	294	160
6*C*	5-10-41	*CO*			59,000	5.5	294	160
7*D*	5-11-41	*O*	32,000†	12.	26,000†	14.5	188	60

* Initial arc current, highest phase. ** Current after 2.4 cycles. † Line-to-ground test.

Paper 42-12, recommended by the AIEE committees on power transmission and distribution and protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted October 25, 1941; made available for printing November 10, 1941.

H. D. BRALEY is division engineer in the electrical engineering department of Consolidated Edison Company of New York, New York, N. Y.

been encountered previously as a result of a three-phase operating failure just outside of a tie-feeder reactor and it would, therefore, afford a measure of system performance under severe fault conditions.

Since the maximum current tests were the determining factor governing the stresses on station equipment, the power requirements for this case were first determined. It was found that to attain the desired level of 2,000 megavolt-amperes, it would be necessary to raise the test voltage from the nominal operating value of 13.6 kv to 14.5 kv and to use all of the machines that could be spared from service while still maintaining the desired reserve. This required the use of a 60-megavolt-ampere tie feeder to Waterside Station II, a 100-megavolt-ampere tie feeder to the Niagara-Hudson system and three generator units of 43.75-, 62.5- and 188-megavolt-ampere capacity. In addition, it was found necessary to pretrip the test breaker in order to take advantage of the current asymmetry. The calculated currents together with the necessary facilities in machines and tie feeders for the various tests are shown in Table I.

A complete analysis of short-circuit forces was made on the connections and equipment of the feeder position supplying the test breaker. All calculations were based on the maximum peak value of current expected under the highest capacity test. Full offset at "zero" time was assumed and proper allowance made for the decay of both a-c and d-c components, resulting in a maximum instantaneous peak current, at the first half-cycle following the fault, of 200,000 amperes at 14.5 kv.

Since the station buses and feeder outlets are of isolated phase arrangement, forces arising from the interaction of phase currents are of little or no importance until the several phase conductors converge in the station cable vault. The arrangement of equipment grounding coppers within the isolated phase portion of the electrical galleries was such that any possible failure to ground would require ground-current flow at right angles to the phase conductors. This eliminated the necessity of considering interaction of phase and ground-fault current. Consequently, within the galleries, the actual forces considered were only those on the test-feeder circuit, arising from the current flow in the several sections of the phase conductor, as it looped, turned, and doubled back on itself, in passing through oil circuit breakers and the reactor of its own phase.

The maximum force concentration appeared as a cantilever load on the wall bushings entering the oil circuit breaker compartments. This amounted to approximately 5,000 pounds at the end of the bushing and normal to its axis. A greater force, 6,000 pounds, was found to act on the oil-circuit-breaker moving contact, tending to open the breaker against the restraint of its latching mechanism.

Since the wall bushing mentioned above was rated by its manufacturer as able to withstand only about 600 pounds at the end of the stud, it was decided to eliminate these forces by placing a short-circuiting strap across the external bushing studs, removing the circuit breakers and reactors of the feeder from the circuit, and thus eliminating virtually all cantilever loading on the wall bushings.

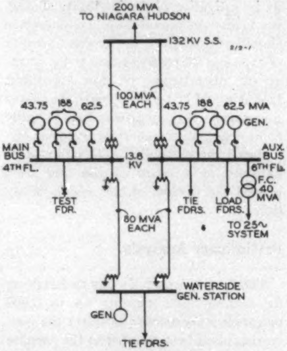


Figure 1. Simplified diagram of system connections to Hell Gate station in the maximum-capacity test

Within the station cable vault, where the separate phase conductors making their exit from conduit were racked in a parallel flat arrangement on the wall of the vault, maximum instantaneous forces of 3,700 pounds per foot were found. In regions where the cables converged to enter a joint with three-conductor cable, the sheaths were in contact, and the maximum instantaneous value of the forces tending to separate the cables was found to be 11,500 pounds per foot. Suitable wrapping and bracing were provided to hold these cables securely in place during the short circuit. Disconnecting switches in the test circuit, where rated below the expected test current, were removed and replaced by copper bus work.

Concurrently with the foregoing studies, an investigation was undertaken to determine the likelihood of instability between generators following clearance of the trouble. It was assumed here that the fault might have to be cleared by the backup protection, and that the total clearing time would be approximately 0.5 second. These calculations indicated that there would be no question whatever of stability, since no generator should swing from its initial position relative to the others by more than 10 degrees. Furthermore, the load readjustment throughout the system, after fault clearing, should not be sufficient to result in any material disturbance, even though the Waterside tie feeder should trip. The calculated voltage dips during the highest capacity fault were 18 per cent on the Hell Gate load busses, 15 per cent at Waterside II, 20 per cent at Sherman Creek, and 40 per cent at the Dunwoodie substation. A simplified one-line diagram of the 60-cycle system tie connections is shown in Figure 1.

Organization

A working committee was set up, comprising representatives of the production, system operation, construction, technical service, and electrical engineering departments of the company and also representatives of the General Electric Company. Schedules and dates were set up to cover:

A. Drawing and engineering information.

B. Construction dates and equipment deliveries.

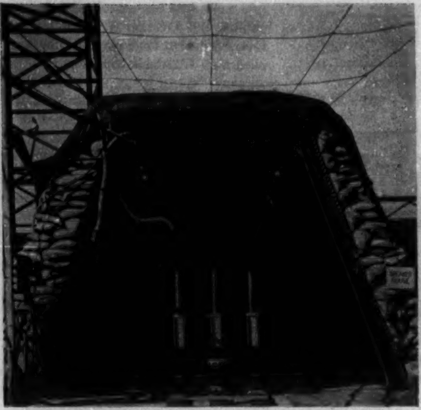
C. Test procedure.

D. Personnel assignments.

To a key man in each group was delegated the responsibility of executing the work assigned by the chairman of the working committee. Through this organization it was possible to keep all work moving on schedule and settle all of the points, with a minimum of delay and with complete understanding as to the action to be taken by all concerned. This procedure was responsible for the success which was attained in completing all preparations on schedule and eliminating confusion as to procedures and responsibilities, during the actual carrying out of the test work.

Test Facilities

The breaker selected for test was a General Electric Company type *A R*-20-150 three-phase 60-cycle 1,200-ampere 15-kv unit with a rated interrupting capacity of 1,500 megavolt-amperes. The detailed description of this circuit breaker is covered in a companion paper,[1] and its



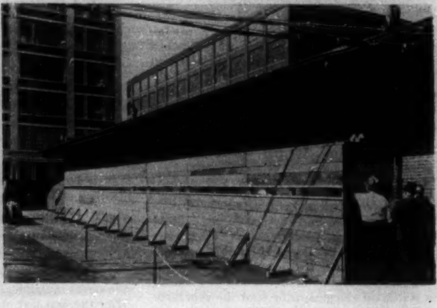


Figure 2 (left). Air-blast circuit breaker installed in structure

Control wiring and instrument wires to control station shown at upper left corner of structure

Figure 3 (below). View of observation barricade

Control cables from circuit breaker may be noted at the top

performance will be referred to here only insofar as it was concerned with the testing operations.

One of the type *H* circuit breakers used for closing duty in the testing station of the General Electric Company was used for the same duty in these tests. This breaker was installed in the breaker test house in close proximity to the air-blast breaker.

The clearance of material stored in the transformer and cable yard to make space available for the breaker test cubicle and other structures, at first appeared to present a task of gargantuan proportions, what with scores of cable reels, economizer tubes, grate bars, and an indescribable assortment of material accumulated by a decade of "string savers." At the very beginning, therefore, the test program began to pay dividends, to the delight of the station superintendent. It made necessary a general house cleaning of the storage yard, by transfer of some of the equipment to other locations, and a more orderly restacking of the remainder.

The breaker house structure, consisting of a steel framework closed on all three sides and the top with corrugated steel sheeting, was supported on a concrete mat. All enclosing sides and the top of the structure were covered with two layers of sandbags, as a protection to numerous transformers and other equipment against damage from possible fire and flying debris.

The open side of the structure was laid out to face a one-story brick storeroom structure. A barricade of two-inch thick lumber, erected in ten-foot sections for quick assembly and removal, for protection of the observers, was placed under the overhanging roof of the storeroom, directly facing and approximately 70 feet distant from the breaker house. A space in the storeroom, with a separate entrance door facing the breaker house, provided a convenient area for all of the magnetic oscillograph equipment required by the General Electric Company. The control station, for operation of the closing breaker and test breaker, as well as the communication facilities, with the generating station control room, was also located at this point. The remainder of the equipment required for the test included separate, standard wood construction shanties, such as were readily available in the market. Four such structures were used for housing the field construction office, dark room, air compressors, and cathode-ray oscillograph. Photographs of the breaker test house and the observation barricade are shown in Figures 2 and 3 respectively.

An independent three-point communication system was installed to provide communication between the test-control station in the yard, the generating-station control board, and the generating-station control-cable terminal room, where was located the Company's automatic recording instruments. A public address system was also installed, for information of the observers concerning safety regulations, announcement of test procedures, and the results of the various tests.

A standard feeder outlet position was used for the test outlet from the station bus, and the circuit from this point to the breaker test house consisted of two three-conductor 800,000-circular-mil lead-covered cables in parallel.

After careful consideration, the decision was reached that the station breakers should be used for backup protection against failure of the test air-blast breaker since there was some element of risk in using the control breaker in the breaker test house, due to its close physical proximity to the test breaker, and furthermore the control breaker might also fail as a result of its use in applying short circuits. The bus setup in the station could be arranged so that two 5,000-ampere, 1,500-megavolt-ampere bus-tie oil circuit breakers, in the section of bus to which the test feeder was connected, would divide the total test current, so that neither one would be subjected to a duty in excess of two-thirds of its rated interrupted capacity. Current transformers, installed on the bus section to which the test feeder was connected, were disconnected from the bus-differential relay circuit and connected, in such a manner as to totalize the total test current to a backup overcurrent induction relay, arranged to trip the two bus-tie oil circuit breakers. These connections are shown in Figure 4.

Operating Procedures

In order to maintain suitable safeguards and to avoid confusion and misunderstanding, the following operating procedure was established:

A. A production department representative with five assistants was stationed in the test area. The operator in charge of this group was responsible for the enforcement of safety regulations. He saw that the test area was cleared, protective barriers were in place, and so on, before each test. He also issued and cleared the necessary work permits for inspection of the test breaker after each test and stationed his watchmen at various points in the test area before each test. This operator alone gave all of the orders to the high-voltage operator in the station for energizing and de-energizing the test feeder. Thus by delegating this authority to only one individual, any confliction of orders was avoided.

B. In the electrical galleries the doors between adjacent bus sections were blocked open, and qualified observers stationed in the gallery corridors on all floors had an unobstructed view of the gallery compartments during test periods. This procedure provided a means of detecting the nature and location of any trouble and also prevented the unauthorized access of other personnel.

C. A representative of the steam engineering department was stationed at the throttle of each turbine, and these men were under instructions as to the action that they should take in the event of trouble developing either on the turbine or the generator. The station electrical mechanics maintained a watch on the generator, and the generator cable ducts, and neutral cable duct runs, to watch for any indication of trouble in those areas.

D. A simple signal system was set up to warn all concerned that a test was about to take place. Signals were given throughout the generating station ten minutes in advance of each test. Each inspector or observer then reported by telephone to the high-voltage control board that he was in position. Five minutes before each test, a signal was given on the turbine-room call whistle and a "standby" signal put up on the signal stand on each turbine being used for the test. When all observers were reported as being in position, the production department representative in the test yard was notified and then proceeded to give the necessary switching instructions, relative to making the test feeder alive. One minute before each test communication was established between the production representative in the yard, the high-voltage operator, and the representatives in charge of the test instruments set up in the cable-terminal control room. Time was then counted off, and the test applied. Communication was maintained until all observers had reported to the high-voltage operator. At the high-voltage operating board an operator was stationed at the controls of each generator and was under instructions as to the steps which should be followed should trouble develop. Observers were also stationed at each street manhole through which the test feeder was carried, all manhole covers being removed during the test period.

F. An electrical construction crew was maintained in the station construction office for any necessary work which might develop as a result of unexpected trouble. For the same purposes a crew from the underground department was also maintained during the entire test period, to be available for any necessary repair work which might be required.

G. Beginning with the test at the 1,000-megavolt-ampere level and above, inspections were made of the equipment in the electrical galleries, the station cable bay, and the street manholes, for evidence of damage after the first test at each level.

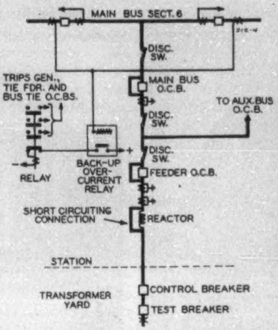


Figure 4. Diagram of relay connections for backup protection

1940 Tests

The first series of tests was undertaken Saturday, September 21, and the following Sunday, September 22, 1940. Test 1 at 200 megavolt-amperes was in the nature of a trial test shot for checking all of the equipment and the operating procedure. In this test the Waterside II tie feeder was tripped at Waterside number 2 by the directional impedance relays. This was the first of the unanticipated events. Later inspection of the oscillographic recording disclosed that the relay operation was undoubtedly due to the asymmetry of the current, which had the effect of increasing the operating range of the relay by some 40 per cent. This relay was blocked on subsequent tests, but protection was maintained by backup time delay over current relays.

On test 2 at 550 megavolt-amperes the same tie feeder was tripped again, but, on this occasion, by the directional impedance relay at the Hell Gate end of the feeder. This was unanticipated operation number 2. This relay was also blocked out for succeeding tests. Subsequent tests on this relay disclosed that there was excessive "wipe" in the contacts of the directional element. The incorrect operation was, therefore, attributed to failure of the directional contact to open before the impedance contacts closed. This may be better understood when it is noted that prior to the fault the power flow on the tie feeder was from Hell Gate towards Waterside, the tie feeder being used to load the generator prior to the test. The directional elements of the impedance relay were, therefore, closed under this condition, but when the fault was applied, the direction of power flow was reversed due to fault current being supplied from Waterside to Hell Gate. The tripping of the tie feeder, of course, caused a loss of load to the two generators after clearing the fault. The machines were, however, resynchronized to the station load bus without difficulty.

Test 2A at 550 megavolt-amperes was run off without undue incident.

In setting up the busses for the next test, a 160-megawatt unit, which had not been used in any of the tests up to that time, was placed on the test bus. One of the gallery observers heard a heavy static discharge, which appeared to come from the vicinity of the generator breaker compartment. This was unexpected event number 3. Although not directly related to the breaker test, this condition might have resulted in an operating failure if it had not been for the observers posted in the electrical galleries. The machine was immediately removed from service and testing of the circuit undertaken. It was ascertained that one of the stress cones at the termination of one of the cables was the seat of the trouble. The cable terminal was remade and gave no further trouble.

On test 3 at approximately 800 megavolt-amperes the air-blast breaker arced over on two phases and failed to clear the fault. The fault was cleared by the backup bus-tie breakers in the station in 29.5 cycles. Subsequent inspection of the bus-tie breakers disclosed that the 6-7 section bus-tie breaker had thrown some oil on the *B* phase pole unit. While the performance of the bus-tie breakers was not unusual in this respect, it was considered that a little more discretion might profitably be exercised, as means were available for so doing. The psychological effect of the flash and the accompanying sound effects, in connection with the arc to ground at the test breaker, undoubtedly lent some emphasis to the need for more caution. As a consequence, the backup relay in the generating station was arranged to trip all generators and tie feeders connected to the test bus, as well as the bus-tie breakers, since this would operate to distribute the duty among the various circuit breakers in the station somewhat more equitably.

It is well worthy of note that, although the air-blast breaker was subjected to severe arcing for nearly one-half second, there was no ensuing fire after arc extinction, no damage to any of the immediately adjacent apparatus in the breaker house, and the necessary servicing and adjustment were completed in approximately two hours. This performance may be contrasted with that commonly experienced with oil-circuit-breaker failure of like nature.

After repairs and adjustments to the air-blast breaker, test 3 at approximately 860 megavolt-amperes and test 4 at approximately 1,000 megavolt-amperes were completed without undue incident.

On test *5B*, at approximately 1,500 megavolt-amperes, the air-blast breaker again failed to clear the fault, and it was cleared by the backup protection in 26 cycles. The damage to the air-blast circuit breaker was confined principally to the glaze of interphase brazing insulators and dislocation of some parts of the arc chute. As in test *3A*, there was no damage to adjacent equipment. There was no evidence of stress on any of the station circuit breakers except to a minor degree on the 6-7 bus-tie breakers. The voltage dip on the network distribution system ranged from 15 to 30 per cent in the Manhattan and Bronx districts, approximately 50 per cent at the Dunwoodie substation, and 10 to 20 per cent on the Niagara-Hudson system. A one-tenth cycle variation in frequency was recorded. A number of railroad synchronous converters in the Westchester area tripped out as a result of the disturbance. There were no operations of automatic network protector units noted at 24 key observation points. All generators and tie feeders were restored to the system within $4^{1}/_{2}$ minutes.

An examination of the end windings of all generators and equipment in the electrical galleries following these tests disclosed absolutely no evidence of any damage to the equipment.

1941 Tests

The second series of tests was made on Saturday, May 10, and Sunday, May 11, in 1941. The preparation was comparatively simple, since all the facilities had been retained from the previous year. A total of six three-phase short-circuit tests and one single-phase short-circuit test was scheduled. The three-phase tests consisted of one "*O*" and one "*CO*" test at approximately 600-, 1,200- and 1,500-megavolt-ampere levels.

The breaker tested in this series was the same as that used previously, except for modifications to increase the air pressure from 150 to 250 pounds per square inch, and certain modifications in the design of the arcing chamber and the exhaust stack. A special air-blast breaker designated as type *AR*-20-250*Y*, three-pole, 15-kv, was used for the closing control breaker in this series of tests, in lieu of type *H* breaker used previously. An additional cathode-ray oscillograph was provided for measurement of the voltages from the short-circuiting connection on the test breaker to ground.

All of the short-circuit tests were successfully interrupted by the air-blast breaker, and, as a consequence; all test work was completed very close to the schedule. During the tests at the higher levels of duty, there were several reports of flashes and sparks in the electrical galleries in the generating plant. These were, naturally, a matter of some concern, until a careful inspection disclosed only minor burns on the circuit-breaker operating rods at the clevis pins on the third floor and between a lighting-fixture outlet box and a ground bus on the second floor. As the short-circuit connection on the test breaker was ungrounded, and there was no indication of system ground current, it was evident that these manifestations in the galleries could have been caused only by induced voltages set up by the test current. Additional clearance was established at the points concerned and no further reports of sparks were received.

It is of interest to note that, on tests at the 1,500-megavolt-ampere level, the railroad synchronous converters in the Westchester area, which were subjected to a 50 per cent voltage dip, did not trip out as in the tests made the previous year. This very clearly indicates the benefits of high-speed fault clearing with respect to its effect on operating equipment.

In the "*O*" test "*5C*", it was hoped that, by setting the pretrip relay to operate 1.2 cycles after initiation of the fault, it might be possible to increase the test duty over that obtained in the previous year. For some unexplained reason, this was not entirely successful, although the pretripping time was checked by tests, before the test current was applied. The oscillographic record shows that arcing did not start until nearly three cycles after initiation of the fault, rather than 1.2 cycles as anticipated.

It may also be noted that the actual current obtained on tests was appreciably lower than the calculated values, particularly in the tests in excess of 1,000 megavolt-amperes.

This discrepancy may be attributed to two factors:

1. The reactance of the generator cables, busses, and test circuit within the station was not included in the calculations.

2. The subtransient and transient reactance constants of the large generator (188 megavolt-amperes) were probably higher than the calculated constants.

The line-to-ground test (*7D*) was made for the purpose of obtaining test data on the zero phase sequence reactance of the 188,000-megavolt-ampere unit and the station grounding system, rather than for testing the air-blast breaker. The 188-megavolt-ampere generator is composed of two 94-megavolt ampere generators driven by a cross compound turbine. The neutral of only one of the generators is grounded. In this test this generator unit and the tie to the Waterside II station were isolated from the rest of the system. The breaker tripping time was intentionally delayed in order to obtain a steady-state value of current. While the measured values were lower than those which had been calculated, this was attributed to the reactance introduced by the various multiple paths over which the ground current could flow between the generator neutral connection and the termination of the test cable in the electrical galleries. A recalculation of this circuit has shown that the difference between the actual test current and the calculated current would be accounted for by 0.05 ohm.

Conclusions

1. High-capacity short-circuit tests directly on the bus of a large power station may be made without unreasonable risk to the system and equipment, if the undertaking is preceded by careful planning, and if safeguards are established to protect against the abnormal conditions which customarily prevail as the result of short-circuit phenomena.

2. System tests under controlled conditions furnish valuable data on system and equipment performance. Such tests not only serve to check the performance of the apparatus ostensibly under test but also demonstrate the adequacy of other parts of the system, under the most severe conditions they are designed to meet.

3. Calculations of generating-station bus faults, in which the reactance of even very short runs of connections are neglected, apparently result in currents which are appreciably higher than actual values and are, therefore, on the conservative side in determining the interrupting duty of switching equipment.

4. Field testing tends to instill more confidence in the science of stability computations which has contributed so materially to the establishment of sound engineering principles in the design and operation of power systems.

5. It has been realistically demonstrated that the fire hazard inherent in the oil-circuit-breaker design is greatly minimized by the air-blast design.

Reference

1. FIELD TESTS ON HIGH-CAPACITY AIR-BLAST STATION TYPE CIRCUIT BREAKERS, H. E. Strang and W. F. Skeats. Scheduled for AIEE TRANSACTIONS, volume 61, 1942 (February section).

Frequency-Modulated Carrier Telegraph System

F. B. BRAMHALL
MEMBER AIEE

J. E. BOUGHTWOOD
ASSOCIATE AIEE

Synopsis: Voice-frequency carrier-current telegraph systems used in this country and abroad have until now employed amplitude modulation, analogous to single current signalling in d-c telegraphy. A variety of so-called two-tone telegraph systems has been tried out by various workers, but none of these was adopted on a commercial scale because all required the employment of at least double the frequency spectrum space ordinarily assigned for amplitude modulation. The system described employs true frequency modulation to derive the advantages of polar current signalling, with the same spectrum efficiency as conventional amplitude systems, and secures at the same time freedom from attenuation change in the transmission medium and greater immunity to extraneous disturbing currents.

FREQUENCY-modulated systems for radiobroadcasting and facsimile transmission have been developed and placed in operation with remarkable success during the past few years. Publications covering practically all phases of this type of modulation, particularly with reference to its interference suppression qualities, have been legion. The fundamental principles have been soundly established and today are perhaps as thoroughly understood as those of amplitude modulation. Consequently, this paper will be confined to a brief outline of recent land-line voice-frequency telegraph-carrier developments culminating in the adaptation of frequency modulation.

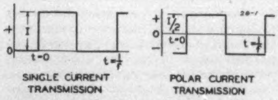


Figure 1. Relative current requirements in single- and polar-current systems for equal interference susceptibility

Theory

Virtually all telegraphic communication circuits fall under either of two basic types of operation:

(*a*). Single-current operation wherein transmission of current indicates a marking signal and absence of current a spacing signal.

(*b*). Polar operation wherein transmission of current of one sense or sign indicates a marking signal and current in the opposite sense or sign indicates a spacing signal.

Relative current requirements for equivalent interference susceptibility in single and polar operation are given in Figure 1. The resulting power relations are as follows:

$$\frac{\text{Power per cycle single operation}}{\text{Power per cycle polar operation}} = \frac{I^2R(t/2)}{(I/2)^2Rt} = 2$$

$$\frac{\text{Peak power single operation}}{\text{Peak power polar operation}} = \frac{I^2R}{(I/2)^2R} = 4$$

The two-to-one reduction in average power and the four-to-one reduction in peak power pertaining to the polar method constitutes a major reason for its adoption on all but relatively short d-c telegraph circuits. In addition, it is inherently immune to the bias losses experienced in single current operation as a result of variations in circuit transmission equivalent. Where peak current permissible is the limiting factor a two-to-one susceptibility gain is realized.

From their earliest inception down to the present time practically all telegraph carrier circuits have been founded on the basic form shown in Figure 2 wherein a carrier frequency is amplitude modulated, energy being transmitted on marking signals and interrupted on spacing signals in the usual single current mode of operation. The resulting square-topped wave trains existing at section *A* are modified by the restricted bandwidth of the channel tuners to the envelope form shown at section *B*. The envelope is then converted by a linear demodulator to a similarly shaped unidirectional pulsating current flowing through the receiving relay, as indicated by *C*. Faithful reproduction of the original modulation is secured by applying to the receiving relay a local bias of such magnitude as to produce equal marking and spacing intervals from the relay armature, curve *D*. It is self-evident from the sinusoidal nature of *C* that any alteration in its magnitude relative to the local operating bias—as, for example, might result from varying line attenuation—will destroy the equal time interval relationship between marking and spacing pulses introducing a loss in the form of biased relay action.

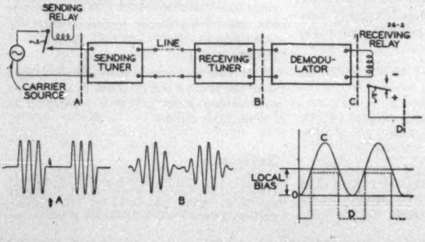


Figure 2. Fundamental carrier telegraph circuit

A—Keyed carrier
B—Received carrier
C—Relay current
D—Relay operation

Inasmuch as the advantages of polar operation have always been clearly recognized, the development literature of the carrier telegraph has been replete with suggestions and schemes for its realization either in full or in part. To overcome bias susceptibility, for example, more or less complicated devices have been evolved which function either to maintain the demodulator input at a predetermined level or to regulate the receiving relay bias in accordance with received carrier level. Such artifices have been in use for a number of years with creditable results, their effectiveness being of the order indicated in Figure 3.

Of a more fundamental nature is the oft proposed "two-tone" polar carrier system involving two carrier sources of somewhat different frequency operating over separate channels and terminating differentially in a receiving relay as shown in Figure 4. Energy of frequency F_m is transmitted over one channel for a marking pulse and energy of frequency F_s over the other channel for a spacing pulse, thus closely approximating the requirements for polar transmission and providing the increased stability pertaining thereto. In theory, but not always in practice, both channels are similarly affected by variations in circuit attenuation, thus fulfilling the polar conditions for bias-free recep-

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F. B. Bramhall and J. E. Boughtwood are both with the Western Union Telegraph Company, New York, N. Y.

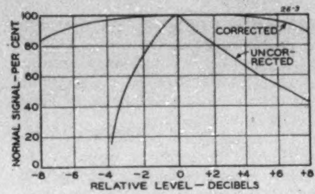


Figure 3. Effectiveness of automatic bias corrector

tion. From an operating aspect, however, this circuit is economically infeasible, requiring at least twice the frequency spectrum per telegraph channel as a single-current system. Suggestions for circumventing this obstacle usually involve a closer spacing of F_m and F_s so that both frequencies will be accepted by a tuner of the type normally provided for amplitude-modulation signalling. The fallacy herein can be seen by inspection of Figure 5, illustrating the distribution of carrier and sideband voltages in amplitude modulation of a carrier frequency.

In a single-current system (Figure 5a) the two intelligence-bearing sidebands created by the act of modulation and the carrier are symmetrically located with respect to, and lie entirely within the available band. The side bands, of course, are separated from the carrier by an interval equal to the modulating frequency. If an attempt is made to pass two carriers F_m and F_s representing marking and spacing frequencies through a similar band, the impossible conditions illustrated in Figure 5b obtain. Both carriers are amplitude modulated, each having an independent set of sidebands, the higher sideband of F_s and the lower of F_m lying outside the passband. The remaining components reaching the receiving demodulator will produce in its output not only a portion of the original modulation but also the beat between the two carriers and the two sidebands. The result will be severe distortion which can be eliminated only by sufficient separation and selection of F_m and F_s and their respective sidebands.

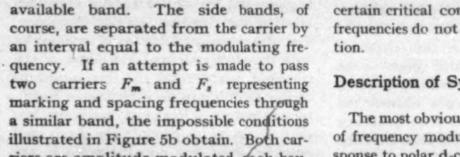


Figure 4. Two-tone polar carrier system employing separate channels for marking and spacing frequencies

A mathematical analysis of carrier and sideband components resulting from the sine wave frequency modulation of a carrier F_o between the limits F_m and F_s at a rate equal to $(F_s-F_m)/2$ (unity modulation index) discloses the relative voltage relationships shown in Figure 6. A theoretically infinite number of sidebands are created symmetrically spaced with respect to the carrier and separated by intervals equal to the modulating frequency. Practically, however, the energy content of sideband components lying beyond the second is sufficiently minute to eliminate them from consideration. In addition, for telegraphic purposes, it has been found possible to further eliminate second-order sidebands without introducing serious distortion, leaving a spectrum during modulation similar to Figure 5a. The small distortion component can be eliminated by simple resistance-capacity shaping of the received signal if so desired. Under steady-state or d-c conditions the F_m or F_s frequency only is present, depending upon the transmitting relay position, and thus we have a method for deriving a "two-tone" polar system requiring no greater bandwidth than a conventional single-current amplitude-modulated carrier system. Except under certain critical conditions the F_m and F_s frequencies do not appear during modulation.

Description of System

The most obvious and practical method of frequency modulating a carrier in response to polar d-c telegraph signals is to vary the frequency of an oscillator by means of the transmitting relay. Figure 7 illustrates the method wherein the frequency determining circuit LC, tuned to mid-passband frequency F_o, is shunted by a control network comprising L_1 and L_2C_2 separately in series with rectifier elements A_1 and A_2. A cycle of operation, starting with the transmitting relay on spacing, connects positive battery to the control circuit establishing across R_1 a potential, positive with respect to ground, of magnitude slightly greater than the peak oscillating voltage existing across LC. Under this condition the impedance of rectifier A_2 approaches infinity, effectively isolating L_2C_2. Simultaneously rectifier A_1 becomes biased in the conducting direction, R_1 acting to limit the control current to a value slightly greater than the peak oscillating current flowing through A_1. As C_2 presents a low impedance to the carrier frequency, L_1 effectively parallels LC and the frequency approaches

$$\frac{1}{2\pi\sqrt{\frac{LL_1}{L+L_1}\,\mathrm{C}}}=F_s$$

In like manner marking or negative battery applied by the transmitting relay isolates L_1, and L_2C_2 effectively parallels LC, the frequency approaching

$$\frac{1}{2\pi\sqrt{\frac{LL_2}{L+L_2}\,(C+C_2)}}=F_m$$

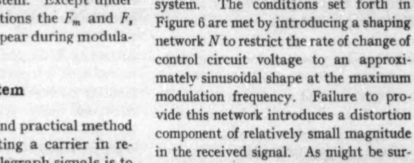
L_2 is necessary to provide a d-c path for the rectifier polarizing current similar to that provided by L_1. In practice, L_1, L_2, and C_2 are so proportioned that

$F_o-F_m=F_s-F_o=70$ cycles

The modulation index becomes equal to unity at a keying speed of 70 cycles per second, the nominal maximum of the system. The conditions set forth in Figure 6 are met by introducing a shaping network N to restrict the rate of change of control circuit voltage to an approximately sinusoidal shape at the maximum modulation frequency. Failure to provide this network introduces a distortion component of relatively small magnitude in the received signal. As might be surmised from Figure 7, the impedances of rectifiers A_1 and A_2, varying between maximum and minimum in inverse relationship, produce an amplitude factor in the modulated wave. This component is considerably smaller and, fortunately, inverse in phase to a similar component introduced by the curvature inherent in the attenuation characteristic of a normal carrier telegraph channel. Frequency

Figure 5. Carrier and side-band relationships in amplitude modulation

F_A = modulation frequency



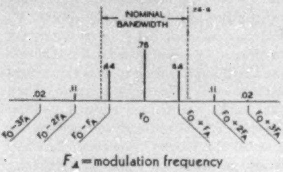


Figure 6. Carrier and side-band relationships in frequency modulation for unity modulation index

deviation accuracy of a high order is achieved as the percentage deviation is independent of voltage (above a certain critical minimum value) applied to the control circuit.

The receiving terminal employs a current limiter, converter, and differential detector as illustrated in Figure 8. The current limiter is perhaps unique in that regenerative action is utilized to secure the high order of sensitivity required on certain classes of systems where terminal repeaters are not justifiable. Regeneration is set to give stable limiting action down to minus 45 decibels, a gain in sensitivity of approximately 25 decibels over the unregenerative state. Thus a practically constant value of effective relay operating current is assured for all conditions conceivably encountered in commercial carrier practice. Conversion from frequency modulation to amplitude modulation prior to detection is effected by a discriminator comprising two parallel-tuned circuits L_1C_1 and L_2C_2 operating in series and tuned respectively to marking and spacing frequencies. The two discriminator output voltages, after being separately detected, are differentially added before being applied to a d-c power amplifier stage for operating the receiving relay. A differential biasing circuit is provided to compensate for slight circuit or relay misalignment. Figure 9 shows the mechanical arrangement employed in transmitter and receiver design, each unit occupying $3\frac{1}{2}$ inches of vertical rack space.

Comparative Test Results

A series of laboratory and field tests were made to determine the relative merits of frequency-modulated and conventional amplitude-modulated systems. The following data were taken at a line speed of 60 cycles per second under identical circuit conditions on a basis of equal peak carrier voltages as a criterion. A transmission testing machine was used to determine operating margins under any given set of conditions in terms of milliseconds loss, a method universally recognized for its accuracy in telegraph transmission studies.

A comparison of circuit susceptibility to single-frequency crosstalk (simulating severe unbalance in four-wire carrier systems as may be encountered under emergency conditions) is given in Figure 10. With amplitude modulation the signal loss is substantially proportional to the receiving tuner attenuation characteristic, becoming a maximum of 3.15 milliseconds for a 12 decibel peak ratio between signal and crosstalk at mid-band frequency. Conversely the frequency modulation characteristic displays the familiar double hump resulting from its triangular noise spectrum wherein the loss due to a single interfering frequency is proportional to its separation from the carrier frequency. As a deviation ratio of unity is employed, maximum loss occurs at a crosstalk frequency differing from the carrier by an interval equal to the deviation frequency. The signal loss at this point of greatest susceptibility is approximately 1.2 milliseconds, or a decrease of 8.3 decibels from the maximum loss with amplitude modulation for interference of this character.

A common form of undesirable noise encountered in telegraph carrier circuits is a random or fluctuating type produced by battery supplies, shot effect in amplifiers, etc. wherein the interference contains frequency components more or less uniformly distributed throughout the channel pass-band. Figure 11 is the average peak loss in milliseconds as measured at the receiving relay contacts for various values of fluctuating noise—a particularly difficult task to perform accurately owing to the probability factor inherent to random interference. A substantial improvement of roughly 9 decibels obtains to frequency modulation for large signal-to-noise ratios as compared with the theoretical value of $2\sqrt{3}\,F_d/F_a$ or 10.8 decibels.

A third form of interference to which open wire carrier circuits are particularly vulnerable is the impulsive type, usually attributable to lightning, wherein shock excitation of the receiving tuner by a steep wavefront produces in the terminal equipment an exponential wave train having a fundamental frequency in the neighborhood of the nominal carrier frequency. The ultimate signal loss is a function of both the amplitude and the phase of the transient relative to the instantaneous carrier, resulting in an interference fortuitous in nature. Peaks of maximum loss thus occur on a probability basis and must be given due consideration in the method of loss measurement. Figure 12 is a comparison of amplitude and frequency modulation under these conditions and closely resembles Figure 11 for fluctuation noise. A reduction of approximately 10.5 decibels in average peak loss is secured for large signal-to-noise ratios as compared to the theoretical improvement factor $4F_d/F_a$ or 12 decibels.

Operational Considerations

It is common practice in high-frequency carrier systems to employ group modula-

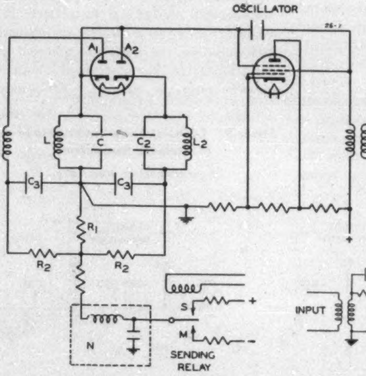


Figure 7 (left). Schematic diagram of frequency modulator

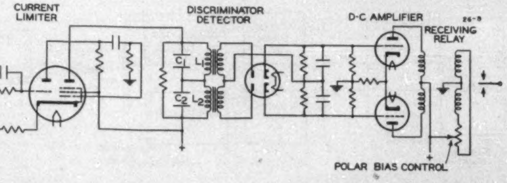


Figure 8 (below). Schematic diagram of frequency-modulation receiver

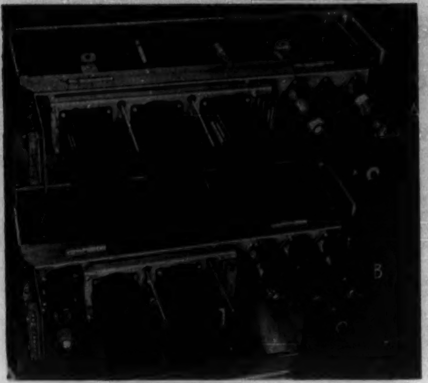


Figure 9. Frequency-modulation equipment

A—Modulator-oscillator

B—Demodulator-amplifier

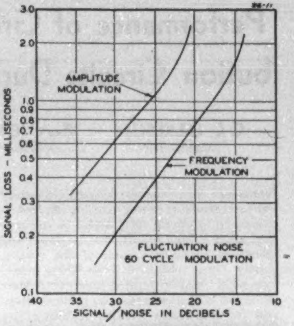


Figure 11. Effect of fluctuation noise on amplitude- and frequency-modulated systems

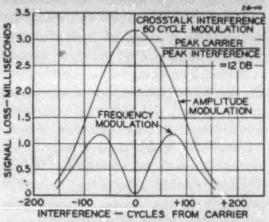


Figure 10. Effect of single-frequency cross talk on amplitude- and frequency-modulated systems

tion and demodulation for transferring the fundamental carrier channels to their assigned frequency band for transmission over the line. In the perfect case, channel frequencies leaving the receiving group demodulator are identical to those entering the transmitting group modulator. Practically, however, a frequency shift exists equal in magnitude to the instantaneous frequency difference in the carrier sources available for group modulation and demodulation which, by the very nature of frequency modulation, introduces a bias component in the received signal. It is required, therefore, that each frequency translating oscillator be a highly stable carrier source having an absolute frequency variation of less than plus or minus two cycles. Similarly, the tolerances on channel tuner stability are somewhat more stringent than for amplitude modulation but can be easily attained in production when proper precautions are observed. The difficulty of accurate level regulation on long carrier circuits is reduced to a problem of maintaining the aggregate repeater loads safely below the distortion point. This is easily accomplished by various well known techniques. Where higher grade carrier channels are required on an existing amplitude system, frequency-modulated channels can be directly substituted without disturbing the rest of the installation.

Evidence thus far presented would indicate a substantial improvement in carrier circuit performance wherever existing amplitude-modulated systems are converted to frequency modulation. This has been amply substantiated in practice during the past three years. Perhaps the outstanding advantage from a carrier attendant's viewpoint lies in the immunity of frequency modulation to received level variations. This feature provides greater flexibility as a channel group can be instantaneously swapped between routes having widely different equalization characteristics without readjusting individual channel levels for optimum performance; an important consideration where synchronism must be maintained on automatic telegraph circuits. In addition, detrimental effects of small instantaneous level fluctuations continually occurring on open wire carrier circuits are completely eliminated.

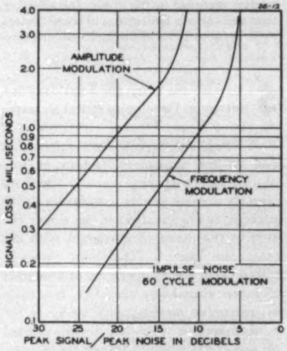


Figure 12. Effect of impulse noise on amplitude- and frequency-modulated systems

Polar operation and the triangular noise spectrum of frequency modulation combine to produce a real and substantial improvement in circuit interference susceptibility, reflected in greater operating margins and reduced testing and regulating work.

Performance of Ground-Relayed Distribution Circuits During Faults to Ground

C. L. GILKESON
MEMBER AIEE

P. A. JEANNE
MEMBER AIEE

J. C. DAVENPORT, JR.
MEMBER AIEE

Synopsis: An extensive oscillographic study has been made on power distribution feeders primarily to obtain data useful in the consideration of joint use of poles by power and telephone facilities. Some of the results, chiefly those obtained from three-phase, four-wire, multigrounded neutral feeders equipped with instantaneous ground relays and for immediate breaker reclosure, are believed to be of general interest and are presented herewith. Included are data on the performance of the protective devices utilized for clearing ground faults on the feeders included in the study, the effectiveness and certain limitations of these devices, and characteristics of the faults experienced.

STUDIES made by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System have shown that one of the important factors in promoting safety where power and telephone circuits are on jointly used poles is the prompt de-energization of the power circuit in the event of a contact with the telephone plant.[1] The Joint Subcommittee with the co-operation of a number of power companies, therefore, has made an extensive oscillographic study of the performance of protective devices utilized on power distribution feeders for clearing ground faults. Some of the feeders selected for study were of the three-phase, four-wire, multigrounded neutral type equipped with instantaneous ground relays and for immediate reclosure of the breaker. This paper deals primarily with the data obtained from this type of feeder regarding the performance of the protective equipment in clearing ground faults and the characteristics of the faults experienced which are thought to be of most general interest.

Since the oscillographs usually were arranged to record only residual currents in the power feeders, multiple-phase faults were recorded only when ground was also involved. As some disturbances were self-clearing and some were due to reclosures on or recurrences of sustained faults, somewhat special meanings have been attached herein to the terms "fault" and "disturbance."

"Fault" is considered to be any occurrence which caused a measurable oscillogram of power system current, which, in so far as could be established, was not from reclosure of a breaker or fuse on a previously existing trouble, from repeated oscillograph operations on a sustained case of trouble, or from load unbalance.

"Disturbance" is a fault as defined above, a reclosure on or recurrence of a previously established fault. In many instances a single oscillogram showed more than one disturbance due either to the fault re-establishing after being self-cleared or to reclosure of some protective equipment on sustained faults.

Extent of Data

The principal data presented in this paper concerning feeders equipped with instantaneous ground relays and for immediate breaker reclosure were obtained from feeders of four power companies. The extent of these observations is summarized in Table I. A total of 1,498 oscillograms, representing 1,279 separate faults and 1,758 disturbances, was obtained. The various feeders were under observation for periods ranging from about 4 to 48 months and had a total mileage of approximately 450 miles. Some data regarding tree-leakage currents and the performance of repeater-type fuses and pole-top reclosures were taken from observations on feeders equipped with inverse-time phase relays only or with inverse-time phase and ground relays. Details of these feeders are not given herein.

Table II gives the relay settings and reclosure practice applying to each feeder during the observations. The instantaneous ground relays locked out prior to first reclosure of the breaker on the feeders of companies *A*, *B*, and *C* but controlled the breaker for the first two openings in the case of company *D*. Following lockout of the instantaneous relays the feeders were protected by inverse-time phase and in some cases, ground relays.

Performance of Relays and Circuit Breakers

A. Manner of Fault Clearance and Automatic-Reclosure Performance

The manner of clearance of all faults, as deduced from the appearance of the oscillographic records in conjunction with the correlation data supplied by the power companies, is summarized in part *A*, Table III. The percentage of faults cleared by circuit-breaker operation ranged from 13 to 76 per cent. This wide spread in percentage is largely attributable to differences in fusing practice and to use of open-gap lightning protection on transformers of some feeders. The remainder of the faults in each case were either self-clearing or were cleared by other protection, such as transformer and branch-line fuses or pole-top reclosers located out on the feeder. No distinction has been made in tables and charts between these manners of fault clearance since the data were not sufficient, in most instances, for making such a classification. On one feeder (not otherwise considered in this paper) the observing equipment was arranged so that operations of pole-top reclosers, which protect most of the branches, could be identified. It was definitely established that they cleared 27 per cent of the faults. Undoubtedly other faults were also cleared in this manner but the data were not conclusive. The main breaker, which is controlled by inverse-time relays, cleared only about 22 per cent of the faults.

Part *B* of Table III was prepared to show the results of instantaneous relaying and immediate automatic reclosure. Under section *B*1 all initial breaker operations are considered, irrespective of whether the fault current persisted until the breaker opened; that is, included in this category are cases in which a fault initiated a breaker operation, but the current was interrupted by a fuse or otherwise before the breaker started to open. In section *B*2 of the table only those cases are included in which the fault current persisted until the breaker first opened, the breaker being responsible for initial interruption of the fault. This latter category would appear to be the better indication of the benefit that can be attributed directly to instantaneous relaying and rapid reclosure. The data indicate that from 50 to 100 per cent of faults falling in this category were clear on first reclosure, the average for all feeders being about 70 per cent.

There are, of course, a number of factors which affect the manner in which faults are cleared. One of these factors is

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C. L. Gilkeson, formerly with the Edison Electric Institute, New York, N. Y., is now with the Virginia Electric and Power Company, Richmond, Va. P. A. Jeanne and J. C. Davenport, Jr. are engineers with the Bell Telephone Laboratories, New York, N. Y.

Table I. Data Regarding Power Feeders and Observations

(Refer to Table II for Relaying Details)

Power Feeder*	Feeder Mileages: No. of Phases on Line 1	2	3	Total	Oscillographic Observations**: Oscillograph Trip Current (Amp)	Period of Observation: Period	Mos.	Extent of Records Obtained§: Oscillograms	Disturbances	Faults	Tree Conditions	Remarks
Company A												
L-402 4 kv	10.9	0	6.6	17.5	50	6-14-34 to 1-1-36	19	31	31	30	Bad	Lightning arresters are used with all distribution transformers. On new installations they are connected inside transformer fuses while on some old installations they are connected outside transformer fuses. The four kilovolt feeders are primarily urban residential lighting circuits; feeder *K*-1202 supplies chiefly rural residential load, while feeder *E*-1215 supplies chiefly rural and suburban residential load. There are a few industrial loads taken from both feeders.
L-406	4.7	0	10.3	15.0	50			13	13	13	Good	
K-1202 12 kv	13.3	7.7	4.3	25.3	30	10-25-33 to 8-15-34 2-18-37 to 5-12-38	25	36	44	36	Good	
E-1215	72.0	0	25.0	97.0	75	6-8-34 to 5-12-38	48	164	187	157	Fair	
Company B—6.9 kv												
19	3.9	0	6.6	10.5	35	11-27-34 to 10-13-38	47	40	50	24	Average	Lightning arresters are used with all transformers. They are connected outside the fuses and have separate driven grounds. These feeders serve urban territory.
20	14.0	0	23.0	37.0	35	10-23-34 to 10-13-38	48	150	201	69	Average	
Company C—12 kv												
3003	90.9	0	57.7	148.6	45	7-2-37 to 3-7-39	20	683	785	633	Average	In areas where fuses and relays can be co-ordinated, transformers are equipped only with spill gaps (two gaps in series) for lightning protection. Where fuses and relays cannot be co-ordinated, lightning arresters are used. Fuses are placed on the transformer side of the arresters; where gaps are separate units the fuse is on the line side of the gap. This feeder serves chiefly residential load in suburban and rural areas.
Company D—13.8 kv												
7*M*35	4.0	4.0	6.0	14.0	40	3-9-38 to 7-11-38	4	26	26	22	Light	Plain spill gaps or expulsion type spill gaps are used for lightning protection of transformers. This feeder serves urban territory and the load is about 75 per cent residential and 25 per cent commercial.
Halls	71.0	10.0	0	81.0	20	5-26-36 to 7-11-38	25	355	421	295	Light	No lightning arresters are used but gaps, connected outside the fuse, are used with every transformer. This feeder supplies rural territory which has a customer density of about seven per mile.
Totals	284.7	21.7	139.5	445.9				1,498	1,758	1,279		

* All feeders are equipped with an instantaneous ground relay and for immediate reclosure of the breaker. Feeders of companies *A*, *C*, and *D* are three-phase, four-wire, multigrounded neutral feeders, while those of company *B* are three-phase, three-wire with the neutral grounded through a grounding transformer bank.

** Power system residual current was recorded in each instance.

§ These records are from accidental disturbances and do not include records from unbalanced load currents and staged tests. Where the oscillograph repeated on a sustained fault the series of records obtained has been counted as one instance.

the nature of the fault itself, that is, it may be of very short duration, probably due to a momentary flashover which is self-clearing before a breaker or fuse has time to operate or it may be cleared by the operation of small transformer fuses. Where instantaneous ground relays are used it is intended that they operate to clear such faults before other protective equipment, excepting small fuses, can operate.

In the case of feeder *E*-1215 of company *A*, a cold cathode tube relay was used as the instantaneous ground relay for a portion of the observation period. At first it was used with no purposely introduced time delay; later, time delay of two cycles was introduced to prevent unnecessary tripping. Without time delay its operating time was approximately 0.007 second. As it has no inverse-time characteristic, operation is initiated practically as soon as the operating current is reached. This is an advantage if the fault is going to persist. However, if the fault consists solely of very short lived kicks, it is desirable to have a small delay in relay operation. A comparison of the operation of the tube-type relay with and without time delay of two cycles is given in Table *A*. From these data it is evident that slowing

Table II. Type of Relaying and Reclosure Practice

Power Feeder	Phase Relays: C.T. Ratio	Phase Relays: Pri. Pickup Current (Amp)	Phase Relays: Time Lever Setting	Phase Relays: Current for 1 Sec. Operation (Amp)*	Ground Relays, Inverse Time: C.T. Ratio	Ground Relays, Inverse Time: Pri. Pickup Current (Amp)	Ground Relays, Inverse Time: Time Lever Setting	Ground Relays, Inverse Time: Current for 1 Sec. Operation (Amp)*	Ground Relays, Instantaneous: Pri. Pickup Current (Amp)	Reclosure Practice
Company A										
L-402 and *L*-406 (4 kv)	40/1	320	3	650	None				80††	1 (immediate) automatic**
K-1202 (12 kv)	40/1	160	3	640	None				80	3 automatic—first is immediate**
E-1215 (12 kv)	60/1	300	3	1,200	None				150§§	1 (immediate) automatic**
Company B—6.9 kv										
19	60/1	360	2½	650	60/1	90	5	900†	60	1 (immediate) automatic**
20	60/1	360	2½	650	60/1	80	5	800†	60	
Company C—12 kv										
3003	40/1	320	2	800	None				40	1 (immediate) automatic**
Company D—13.8 kv										
7*M*35	40/1	400 400§	2½	800	40/1	40	2½	80	80	Before 5-24-38: 3 automatic: immediate, 30 seconds and 105 seconds***
	40/1	320 400§	1	400	40/1	40	6½	200†	100	After 5-24-38
Halls	5/1	60 60§	7	260†	25/1	50	6	160†	25	3 automatic: immediate, 45 seconds and 120 seconds***

*Refers to relay operation only; does not include breaker time.

**Instantaneous ground relay locks out following initial breaker operation leaving inverse-time relays in control until recloser is manually reset.

***Instantaneous ground relay has control until after second trip-out.

§Instantaneous phase relays.

§§Solenoid type relay from 6-8-34 to 4-15-35. Gas tube relay without time delay from 4-15-35 to 10-10-35 and from 3-15-37 to 5-12-38. Gas tube relay with 2 cycles delay from 10-10-35 to 3-15-37.

†Current required for 1½ second operation.

††Experimental only.

Table III. Manner of Fault Clearance and Automatic Reclosure Performance

	Company A								Company B				Company C		Company D			
	4 Kv				12 Kv				6.9 Kv				12 Kv		13.8 Kv			
	L-402		L-406		K-1202		E-1215		19		20		3003		7M35		Halls	
	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent
Total faults	30	100	13	100	36	100	157	100	24	100	69	100	633	100	21	100	295	100
A. Manner of fault clearance																		
a. Breaker-cleared	4	13	4	31	9	25	29	19	4	17	40	58	375	59	10	48	225	76
b. Self-or fuse-cleared	26	87	9	69	27	75	128	81	20	83	29	42	258	41	11	52	70	24
B. Automatic reclosure performance																		
(1) Considering all initial breaker operations																		
a. Total	15	100	5	100	18	100	110	100	7	100	65	100	570	100	17	100	274	100
b. Clear on first reclosure	15	100	4	80	14	78	91	83	5	71	42	65	464	81	13	76	221	81
c. Clear following first reclosure†			4	80	17	94	107	97	7	100	50	77	568	100	13	76	224	82
d. Clear on second reclosure††			4	80	18	100	109	99			53	82	569	100	16	94	243	89
(2) Considering only cases where fault current persisted until breaker first opened																		
a. Total	4	100	4	100	12	100	42	100	6	100	48	100	479	100	10	100	254	100
b. Clear on first reclosure†	4	100	3	75	8	67	26	62	4	67	25	52	373	78	6	60	202	80
c. Clear following first reclosure†			3	75	11	92	42	100	6	100	33	69	477	100	6	60	205	81
d. Clear on second reclosure††			3	75	12	100	42	100			36	75	478	100	9	90	224	88

†Faults which did not involve a second breaker opening. This item is the sum of (*b*) plus those faults which were fuse or self-cleared following first breaker reclosure.

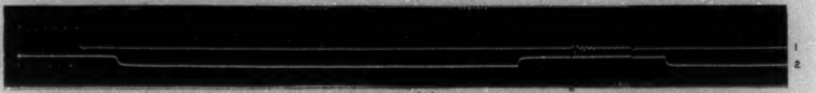
††Sum of faults in (*c*) above plus those cleared on second breaker reclosure. Second reclosure was not immediate except for company *D*.

down the relay saved a number of unnecessary trippings on very short duration disturbances.

B. SPEED OF CLEARANCE AND RECLOSURE

The recorded average time from initiation of fault to first breaker opening ranged from 7 to 15 cycles for the various feeders; the average time from initiation of fault to first immediate reclosure ranged from 34 to 57 cycles. In arriving at these times, one cycle has been added to the duration shown by the oscillograms to allow for the time it takes the oscillograph film to get in motion.

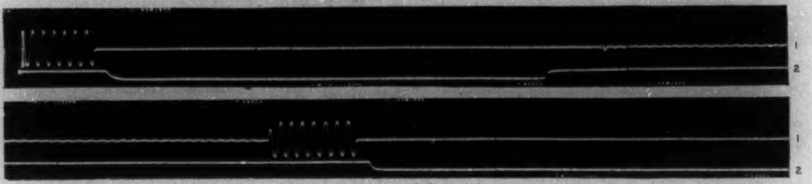
The oscillograms pictured in Figure 1 (A) and (B) illustrate faults which were breaker-cleared by operation of instantaneous ground relays which control the breaker for two openings. (Element 2, which indicates breaker operation, was controlled from a contact on the breaker whose opening lagged the actual breaker



(A)

ILLUSTRATES- OPERATION OF INSTANTANEOUS GROUND RELAY AND IMMEDIATE RECLOSURE; RECLOSURE TRANSIENT; FALSE BREAKER OPERATION FROM RECLOSURE TRANSIENT.

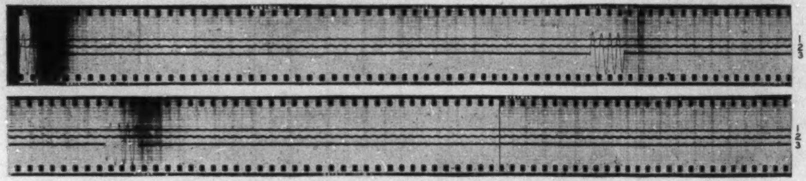
EL. 1-RESIDUAL CURRENT EL. 2-INDICATES BREAKER OPENINGS AND RECLOSURES



(B)

ILLUSTRATES:-TWO OPERATIONS IN SEQUENCE OF INSTANTANEOUS GROUND RELAY AND FIRST IMMEDIATE RECLOSURE.

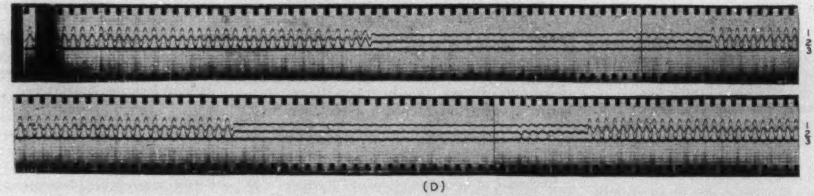
EL. 1-RESIDUAL CURRENT EL. 2-INDICATES BREAKER OPENINGS AND RECLOSURES



(C)

ILLUSTRATES:-THREE OPERATIONS IN SEQUENCE OF REPEATER-TYPE FUSES; ONE LINE-TO-GROUND FAULT.

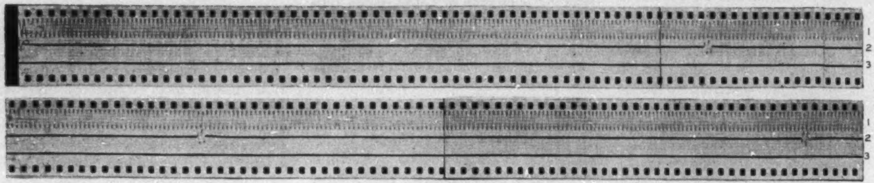
EL. 1 AND 2-PHASE C AND B CURRENTS EL. 3-RESIDUAL CURRENT



(D)

ILLUSTRATES:-THREE OPERATIONS IN SEQUENCE OF REPEATER-TYPE FUSES; LINE-TO-LINE FAULT.

EL. 1,2 AND 3-PHASE A, B AND C CURRENTS



(E)

ILLUSTRATES:-OPERATIONS IN SEQUENCE OF AN FP-19 POLE-TOP OIL CIRCUIT-RECLOSER

EL. 1-INDICATES WHEN SUBSTATION BREAKER OPENS OR CLOSES-FEEDER VOLTAGE AT SUBSTATION EL. 2-NEUTRAL CURRENT EL. 3-NOT USED

Figure 1. Illustrative oscillograms

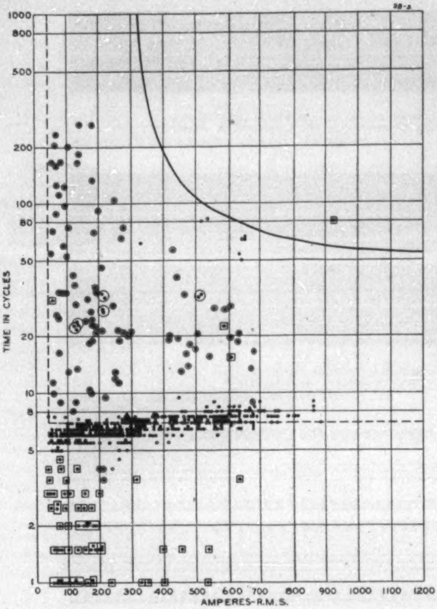


Figure 2. Magnitude and duration of fault currents

Company C-12 kv Feeder 3003
7-2-37 to 3-7-39

———— Approximate phase-relay characteristic—seven cycles allowed for breaker time

— — — Instantaneous ground-relay characteristic including breaker time

• Breaker-cleared disturbances

⊡ Fuse- or self-cleared disturbances

⊙ Fuse- or self-cleared after first reclosure

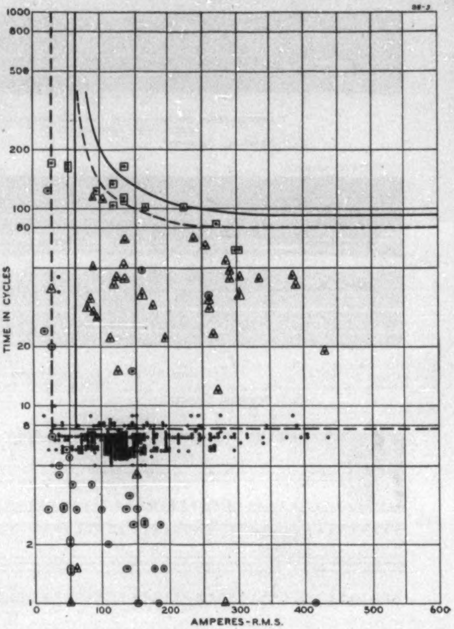


Figure 3. Magnitude and duration of fault currents

Company D-13.8 kv Halls feeder
5-26-36 to 7-11-38

———— Approximate phase-relay characteristic—eight cycles allowed for breaker time

— — — Approximate ground-relay characteristics including breaker time

•	Breaker-cleared disturbances	Instantaneous relays in service
⊙	Fuse- or self-cleared disturbances	
⊡	Breaker-cleared disturbances	Instantaneous relays locked out
△	Fuse- or self-cleared disturbances	

openings and consequently closed slightly ahead of the breaker.) The first illustration also shows a typical transient following breaker reclosure and an instance in which the transient caused a false relay operation. These transients are discussed in the following section. The second illustration shows two operations in sequence of the breaker, both initiated by the instantaneous relay. In this latter case the fault was temporarily cleared but became reestablished about 40 cycles after first reclosure took place.

Observations on several feeders which were equipped near the substation with repeater-type fuses (three-shot), indicate an average reclosure time of the fuses of 32 cycles. Other observations indicate that pole-top reclosures, of the type used, operate in from two to four cycles and reclose in from two to four seconds.

The oscillograms pictured in Figure 1(C) and (D) illustrate operation in sequence of repeater-type fuses. The first illustration is for a one line-to-ground fault while the second illustration is for a line-to-line fault. Figure 1(E) illustrates the sequence of operation of a pole-top recloser.

C. Transients Following Breaker Reclosure

Table A. Operation of Cold-Cathode-Tube Relay

	Without Delay* (20 Months)	With Delay (17 Months)
Total breaker openings considered	60	26
Number showing fault current duration of less than two cycles	34 (57%)	9 (35%)
Average time (cycles)—initiation to breaker opening	7.0♯	8.8♯
Average time (cycles)—initiation to reclosure	34.3♯	35.0♯

* Operation in 0.007 second.

♯ One cycle added to allow for time required for oscillograph film to get in motion.

Reclosure of breakers on sound feeders usually produced residual current transients of fundamental frequency and nonsymmetrical wave form caused by the inrush of magnetizing current to distribution transformers connected between phase wires and neutral. The general nature of these transients is illustrated in Figure 1(A) which shows an oscillogram of a fault cleared by an instantaneous relay on a feeder equipped for immediate reclosure. In this instance the transient operated the instantaneous relay and retripped the breaker.

These transients have been studied with regard to their possible limitation on relay pickup current settings. It may be said, in general, that where instantaneous relays have control until after first reclosure (Halls feeder and feeder 7*M*35 utilize this condition) such transients may place a minimum limit on relay pickup current settings. However, this limit need not be the maximum value of the reclosure transient (except perhaps in the case of tube relays which have no purposely introduced time delay) because the maximum magnitude of the transient occurs only within the first half cycle, the transient attentuates rapidly and, due to the almost complete suppression of the upper (or lower) half of the wave, the effective value is much less than that of a sine wave of equivalent peak value. This is well illustrated by the fact that only eight cases of false relay operation were recorded on the Halls feeder from reclosure transients although the instantaneous ground relay was set to pick up on 25 amperes and the recorded maximum currents from reclosure transients ranged up to 125 amperes. The currents from this cause recorded on the other systems ranged up to 300 amperes but no relay operations could be definitely attributed to reclosure transients. Current values quoted for reclosure transients are peak amperes divided by $\sqrt{2}$.

D. UNBALANCED LOAD CURRENTS

A study of the oscillographic data was made to determine whether unbalanced load currents in four-wire, multigrounded neutral feeders might impose an important limitation on the pickup current settings of ground relays in the range from 50 to 100 amperes. The data obtained are summarized in Table *B*.

Unbalanced currents due to switching and temporary two-phase operation, as well as those due to normal load unbalance, have been included in this tabulation. The figures under the column headed "Total No." represent separate instances in which the unbalanced currents reached values sufficient to produce oscillograms. Momentary load surges sometimes tripped the oscillograph as is evinced by the fact that sustained currents lower than the oscillograph tripping currents were recorded. Only in the case of the four-kilovolt feeders of company *A* did the unbalanced load current exceed the relay pickup current, and the only instance of breaker operation due to unbalanced load current occurred on one of these feeders. The ground relays on these feeders were an experimental installation, and it seems to have been the practice to cut out these relays when, during emergency operation, a feeder was unbalanced by load being switched to or from another feeder. Most of the large unbalanced currents were recorded during such emergency operation.

Table B. Data Regarding Unbalanced Local Currents

Feeder	Approx. Oscillograph Trip Amps.	Instantaneous Ground Relay Pickup Amps.	Oscillograms Due to Unbalanced Load Currents				Max. Normal Load Phase Current Amps.
			Total No.*	Above Current of Amps	Above Current of No.	Current Magnitude Range Amps.**	
Company A—4 kv							
L-402	50	80	16	80	1	40–110	150–200
L-406	60	80	17	80	6	45–140	150–200
Company A—12 kv							
K-1202	30	80	112	40	0	20	10–20 (1934)
E-1215	75	150	4	75	2	25–90	100–150 (1934)
Company C—12 kv							
3003	45	40	Few			40	60 (1935)
Company D—13.8 kv‡							
Hall's	20	25	7			5	2 (1935)

*In this tabulation a series of oscillograms obtained while the oscillograph was continuously operating due to the same unbalance is counted as one oscillogram.

**Sustained current magnitudes: In a few instances the current magnitudes momentarily exceeded these values.

‡No records of measurable magnitude were obtained from Feeder 7*M*35.

These data are limited but indicate that ground relay settings of from 50 to 100 per cent of normal maximum load current but not less than 25 to 35 amperes would be realizable.

Effectiveness of Circuit Breakers and Fuses in Clearing Faults

In order to study the efficacy of relays, circuit breakers and fuses in clearing faulted lines, "shotgun" diagrams showing the magnitude and duration of all observed ground-fault currents having durations of one cycle or over were prepared for each feeder. On each of the diagrams the approximate relaying time-current characteristic curves also were plotted. Illustrations of this type of diagram are given in Figures 2 and 3 for feeder 3003 and the Hall's feeder respectively. In these figures the lines and curves coded as relay characteristics include relay plus breaker time. For instantaneous relays the time of operation, considered the same for all currents above pickup current, was taken as the average duration of instantaneously relayed faults, as determined from oscillographic records, plus one cycle to allow time for the oscillograph to get in operation. The plotted points represent the durations of the individual disturbances, as shown by the oscillograms, without any allowance for oscillograph starting time, a procedure followed to simplify handling the data. This, together with the fact that it is not always possible to determine accurately the duration from every record, accounts to some extent for the distribution of points above and below the instantaneous relay characteristic.

In general, the maximum current magnitude shown by each oscillographic record was used in plotting the points in the figures. Since most of the oscillograms showed a fairly uniform magnitude wave trace, the relays and fuses were usually subjected throughout the duration of the disturbance to currents of approximately the values plotted. A code is used to indicate the manner of clearance of the disturbances.

The results of these analyses, which were made for all feeders, indicate that the manner of fault clearance was consistent with what would be expected from the relay and fuse characteristic curves and other known circumstances at the times of the disturbances.

Performance of Fuses

In every case which could be used for this phase of the study, the fuse was found to have cleared within the time shown by its characteristic curve. In many instances where the clearing was apparently much faster than expected, the correlation data indicated that more than one phase conductor was involved in the fault. Some cases of damage to fuse holders were reported but there is no indication that the arc was sustained after the fuse blew.

Effectiveness of Fuse and Relay Co-ordination

Where high-speed (instantaneous) relaying and rapid reclosure are employed, the type of co-ordination sought is to protect, in so far as possible, feeder and

Table IV. Faults Classified as to Primary Cause and Nature of Trouble*

	Company A				Company B		Company C		Company D			
	4 Kv		12 Kv		6.9 Kv		12 Kv		13.8 Kv			
	L-402 and L-406		K-1202 and E-1215		19 and 20§		3003		7M35		Halls	
Description	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent	No.	Per Cent
A. Classification as to primary cause												
1. Lightning or storm during lightning season (April 15–Sept. 15)	24	56	108	56	46	39	10	1	2	9	81	28
2. Primary cause unknown—occurred during lightning season	8	18	26	13	11	9	372	59	9	41	115	39
Sum of above—probably due to lightning in most instances	32	74	134	69	57	48	382	60	11	50	196	67
3. Wind, rain, sleet, and/or snow	3	7	11	6	34	29	1	0.1	3	14	25	8
4. Foreign objects in line (other than caused by 3 above)	2	4	20	10	9	8	6	0.9	8	36	17	6
5. Primary cause unknown—occurred outside lightning season (Sept. 15–April 15)	6	15	28	15	18	15	244	39	0	0	57	19
Total faults	43		193		118		633		22		295	
B. Classification as to nature of trouble												
1. Flashovers	37	87	146	76	62	52	610	96.3	10	45	260	88
2. Insulator or bushing failure	1	2	0		0		0		0		1	0.3
3. Wires down	1	2	15	8	41	35	2	0.3	3	14	15	5
4. Birds or animals in line	0		1	0.5	0		3	0.5	8	36	0	
5. Poles broken or blown over	1	2	2	1	2	2	0		0		0	
6. Whipping conductors	0		0		5	4	1	0.2	0		0	
7. Crossed wires	0		7	3.5	0		1	0.2	0		0	
8. Equipment failures	0		17	9	7	6	16	2.5	0		12	4
9. Trees or limbs in line (exclusive of those causing broken or crossed conductors)	3	7	5	2	1	1	0		1	5	7	2.7

*Correlation data were most complete in the case of companies *A* and *B* and feeder 7*M*35 of company *D*.

§These data include some faults not recorded by the oscillograph but which were indicated by the correlation data.

Explanation of items used in classification:

Flashovers—only cases where no damage other than fuse operation was reported. While a flashover is not definitely known to have occurred, the presumption from the character of the disturbance, is that such was the case. Many faults of short duration fall in this category.

Conductors down—phase conductors, ground wires, etc.—no structures reported down.

Equipment failure—transformers, lightning arresters, etc.

Whipping conductors—contacts with other conductors, line structures, or structures adjacent to line.

Foreign objects—wires, trees, or limbs in line, automobiles striking poles, etc.

branch-line fuses during the first, and in some cases the second, tripout, so as to give faults, such as arc-overs due to lightning, an opportunity to clear before branch circuits or sections of the main feeder are interrupted and before permanent damage occurs. To provide against the contingency of permanent faults, the instantaneous relay is usually cut out prior to or in some cases following the first reclosure, and inverse-time relays are then depended upon for back-up protection of the circuit. The inverse-time relay in this case is set to allow branch-line fuses to clear before the breaker again operates. Thus, the objective is to prevent fuses from blowing by the use of instantaneous relays, while allowing them to blow before the inverse-time relay operates.

In the case of the 12-kv feeders of company *A* and the feeders of company *B*, branch-line fuses were co-ordinated with inverse-time relays and some benefit in the protection of these fuses apparently resulted from the use of instantaneous relays. However, with the speed of breaker operation utilized on feeders of the capacity of these it appears impracticable to protect branch-line fuses of about 40 amperes capacity or less, as these smaller rated fuses usually blew before the breaker, operated by an instantaneous relay, could clear the fault. Co-ordination between fuses and inverse-time relays is evidenced by the fact that the inverse-time relays were seldom called upon to clear a disturbance.

In the case of company *C*, it may be said that co-ordination appears to be good, since so many faults were cleared by the instantaneous relay and since so few of those which persisted beyond first reclosure were breaker-cleared.

The Halls feeder has relatively few fuses, the maximum size being 40 amperes. Since the feeder is fused near the substation, a fault at almost any location will have a fuse between it and the substation. Only 27 branch-fuse operations have been correlated with records of disturbances on this feeder and in each case they operated following lockout of the instantaneous relay but before the inverse-time relay contacts could close. Operations of the inverse-time relays occurred on other faults following lockout of instantaneous relays. In all but four cases the current magnitude, with respect to the duration shown on the oscillogram, was below that for which a 40-ampere fuse would co-ordinate with the inverse-time relays. In three of these four cases there was apparently no fuse between the fault and substation; in the other case the circumstances are not known. Thus, benefit apparently accrued from the protection of branch-line fuses by instantaneous relays. Also, the use of instantaneous relays may have prevented some cases of wire failure.

The data from the four-kilovolt feeders of company *A* and feeder 7*M*35 of company *D* are too meager to give much indication on this subject. However, it is probable that some benefit accrued from the protection of branch fuses by instantaneous relays.

Effect of Ground Relaying on Continuity of Power Supply

This study indicates that as compared with feeders utilizing inverse-time relays only, the use of instantaneous ground relays, while causing more frequent breaker operations, has increased the continuity of power supply, in that some branch-fuse

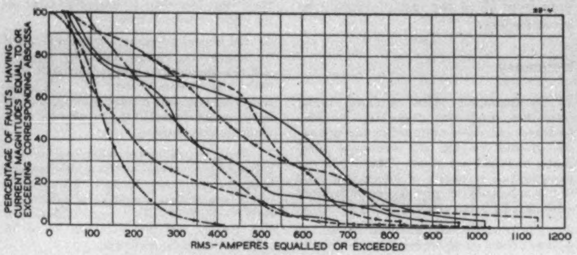


Figure 4. Magnitude Distribution of Recorded Sustained* Residual Currents

Code	Feeder	Feeder Kv	Approx. Max Fault Current at Substation Bus—RMS Amperes⁕	Highest Current Recorded —RMS Amperes*
— ————	L-402 & L-406	4	4,500	1,020
— — — —	19	6.9	3,000	1,140
●——●	20	6.9	3,000	960
●— — —●	K-1202	12	5,000	850
————	E-1215	12	2,400	680
○———○	3003	12	4,000	1,030
●——— ●	Halls	13.8	425	420†

† Data from feeder 7M35 were not sufficient for preparing a curve.

* Largest magnitude sustained for three cycles or more.

⁕ For one line-to-ground fault.

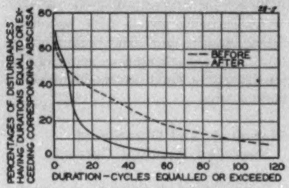


Figure 5. Distribution of disturbance durations

Data obtained from feeder K-1202 before and after instantaneous ground relay installed

Table V. Classification of Faults Involving Broken or Burned-Down Wires

	Company A		Company B	Company C	Company D	
	4-Kv	12-Kv	6.9-Kv§	12-Kv	13.8-Kv	
Primary Cause	L-406	K-1202 and E-1215	19 and 20	3003	7M35	Halls
1. Lightning	0	1	13	0	1	1
2. Wind	1	6	19	0	2	8
3. Acts of man	0	4	7	2	0	5
4. Trees or limbs—other than those cut or blown into line	0	4	0	0	0	1
Total faults	1 (7.7)*	15 (7.8)*	39 (42.0)*	2 (0.3)*	3 (13.6)*	15 (5.1)*

*Figures in parentheses give faults involving broken or burned-down wires in per cent of total faults recorded.

§25-Cycle feeders are carried on the same poles with feeder 20 at certain locations. Eight of the faults involving broken wires could be associated with contact or flashovers between the wires of these two feeders.

operations were prevented and probably some wire failures as well. Also, the instantaneous relays clear the fault much more quickly than do inverse-time relays. Thus, in the case of faults which were not permanent, momentary outages on the feeders were substituted for long time outages. Where instantaneous relays are set to pick up at much lower current than the inverse-time phase relays (see Figure 2) they furnish added assurance of positive disconnection in the fault current range between the two relay pickup current values, 40 to 320 amperes in this case. It should be pointed out that, except on feeders having limited fault current, instantaneous ground relays would not be expected to save branch-line fuses below 30 or 40-ampere rating.

Cause and Nature of Faults

As far as it has been practicable, all oscillographic records have been correlated with cause and nature of the trouble on the power feeders. In Table IV all faults are classified in accordance with the attributed primary cause and nature of the trouble. The lightning classification has been divided into two parts; one includes faults definitely correlated as due to lightning or to a storm during the lightning season; the other includes correlated plus uncorrelated faults whose causes and natures were unknown but which occurred during the lightning season (April 15–September 15). Experience indicates that these latter disturbances are, in most instances, due to electrical storms. The sum of these two is believed to represent more nearly the true effect of lightning. The data show that lightning has been the major cause of trouble, accounting in general for about 60 per cent of the total faults.

Trees have been reported as being responsible for only a small percentage of the faults and the trouble from them was largely due to limbs or trees falling into, or being blown or cut into the lines. The data from none of the systems gave evidence of intermittent or sustained leakage currents which might be a possible limitation to ground relaying. This has been substantiated by staged tests, conducted by two of the co-operating power companies, utilizing bare copper wires on 4 and 11-kv power circuits. The tests were made when the sap was in the trees and contact with the energized conductor was much better than would ordinarily occur with a wire in accidental contact with a tree limb. The maximum fault current of 10.5 amperes was recorded for a test in which the conductor was connected to a spike driven into a live willow tree trunk two feet above ground. The next highest current (nine amperes) was obtained from a test in which the conductor was wrapped several times around a dogwood tree trunk seven feet from the ground and pulled tight enough to break through the outer bark.

The above indicates that one or a few tree grounds are not likely to draw enough current to cause ground relay operation except where pickup currents are quite low, say below 25 amperes. It, therefore, appears likely that most tree trouble is due to branches getting across phase conductors, between phase conductors and neutral, or pushing the wires together, rather than to leakage through the trees to ground.

Faults Involving Broken and Burned-Down Wires*

All correlation data from each power system have been examined to determine, as far as practicable, those faults which apparently involved broken wires. The results as regards the causes of these faults are summarized in Table V. The major causes were wind and acts of man.

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* In this section no distinction is made between broken and burned-down wires or between phase conductors and ground wires.

Although the data regarding the question of broken wires are, in some instances, rather meager, they indicate that of the total faults experienced on most of the feeders only a small percentage involved broken wires.

Magnitude and Duration of Recorded Residual Currents

Data obtained regarding the magnitude distribution of sustained fault currents (largest magnitude sustained for three cycles or more) are given by cumulative percentage curves in Figure 4. The ordinates of these curves indicate the percentage of faults in which the current magnitudes equalled or exceeded the corresponding abscissa. Since, in most instances, the wave traces are fairly uniform, the distribution of maximum current values would be essentially the same as is shown in Figure 4.

The duration distribution of the currents recorded on feeder *K*-1202 of company *A* are given by cumulative percentage curves in Figure 5. One curve is for the records obtained after the feeder was equipped with an instantaneous ground relay; the other curve is for the period when the feeder was equipped only with inverse-time relays and is included to show the effect of instantaneous relaying. The first mentioned curve is typical of the data from all of the instantaneously relayed feeders.

Summary

1. Oscillographic data obtained under operating conditions regarding the performance of feeders equipped for instantaneous ground relaying and immediate automatic reclosure are given. These data are presented as a matter of information and not in advocacy of any particular form of ground-fault protection.

2. The observations described herein, as well as others made during the same study, indicate that relays, fuses, and circuit interrupters operate consistently and reliably on currents at or above which they are set or designed to operate.

3. Considering only faults which persisted at least until initial breaker-opening, the percentage in which feeders were clear on first (immediate) reclosure ranged from 50 to 100 per cent, the average being about 70 per cent.

4. The average time from initiation of fault to first breaker-opening ranged from 7 to 15 cycles for the various feeders; average time from initiation of fault to first reclosure ranged from 34 to 57 cycles.

5. A main objective of instantaneous relaying and immediate reclosure is to substitute a brief outage on the whole feeder for a long time outage on a branch by preventing branch-line fuse outages on temporary faults such as flashovers. This was successfully accomplished in most instances for the feeders under observation except where fuses were less than 40 amperes rating on the higher capacity feeders.

6. On four-wire feeders, a transient caused by magnetizing current taken by distribution transformers on breaker reclosure may limit the pickup current for which instantaneous ground relays can be set if these relays retain control of the breaker for more than the first opening.

7. Data regarding the magnitude of unbalanced load current on the multigrounded neutral circuits were limited. However, it would appear that ground relay settings of from 50 to 100 per cent of normal maximum load current but not less than 25 to 35 amperes would be realizable.

8. Lightning was the major cause of trouble, accounting in general for about 60 per cent of the total faults.

9. Trees were responsible for only a small proportion of the total faults. Trouble from them was largely due to branches or trees falling into or being cut or blown into the lines. On no system was there evidence of intermittent or sustained tree leakage being a possible limitation to ground relaying with pickup currents of the order indicated in (7) above. This is further substantiated by staged tests in which the current drawn by well made tree grounds was measured.

Reference

1. PROTECTION FEATURES FOR THE JOINT USE OF WOOD POLES CARRYING COMMUNICATION CIRCUITS AND POWER-DISTRIBUTION CIRCUITS, ABOVE 5,000 VOLTS, J. O'R. Coleman and A. H. Schirmer. AIEE TRANSACTIONS, volume 57, 1938 (March section), pages 131–40.

High-Capacity Circuit-Breaker Testing Station

J. B. MacNEILL
MEMBER AIEE

W. B. BATTEN
ASSOCIATE AIEE

TEN years' experience with a high-power laboratory, giving approximately 1,100,000 three-phase initial symmetrical kilovolt-amperes on short circuit at the machine bus,[1] has defined its usefulness and limitations and has resulted in an addition to its installed equipment. By taking advantage of assymmetry this capacity has been sufficient to demonstrate ratings up to 1,500,000 kva, and the increased testing capacity is required to demonstrate interrupting ratings of 2,500,000 kva.

The previous equipment was capable of testing most circuit breakers up to their interrupting rating, but for the largest sizes special methods of testing had to be used to approximate the conditions at the breaker rating.

1. Single-phase testing on three-phase devices. This method is generally satisfactory providing the influence of adjacent phases on closing operation, speed of contact action, and restored voltage conditions are allowed for. It is particularly valuable on large designs with good phase isolation, but must be carefully supervised on compact designs where the influence of adjacent phases may be great.

2. Double-phase to ground on a single-pole,[2] with the contact cross bar grounded, was first used in Europe and is valuable on multiple-interrupter devices as it increases the total voltage on the pole unit for a given current and gives tank pressures corresponding to energy losses not otherwise obtainable. However, its use requires care, as the first interrupter to clear opens only 87 per cent of phase-to-phase voltage, and the voltage across the second interrupter is limited to the phase-to-phase value. Thus, a double 66-kv connection used to demonstrate 132-kv is limited in that the first interrupter opens 57.4-kv and the second opens 66-kv.

3. The use of high superposed voltage on a low-voltage circuit[3] has been advocated and discussed widely. Our experience with it indicates it is not sufficiently reliable for general use since the current wave form of the low-voltage circuit as it approaches final zero is affected, and leakage currents in the arc space around current zero are altered.

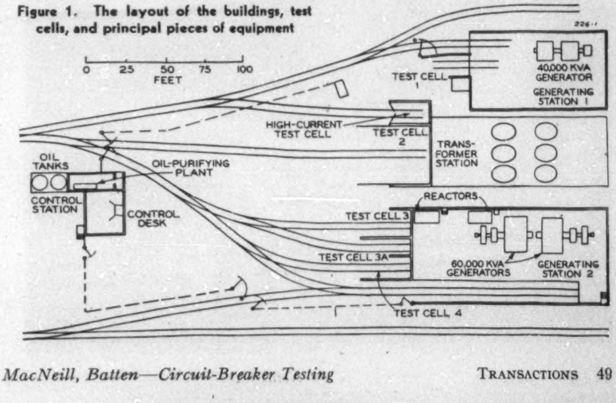
Because of these limitations in testing capacity, it has been desirable in some cases to design circuit breakers with multiple interrupters arranged for adequate distribution of voltage between them, each of which either singly or in groups can be adequately demonstrated for the maximum duty imposed on it. Eight and ten units per pole have been designed for very severe apparatus conditions and these designs have been presented to the Institute.[4] This practice, while giving satisfactory results in operation, requires a complication of design which should not be necessary in the future with the increased demand for high-power and high-speed switching.

On the other hand, when an attempt is made to simplify the structure of high-voltage and high-power interrupting devices, the need for increased testing capacity is evident, since the burden on the individual interrupter is increased and its reliability must also be above question. As an illustration, at the 1941 winter convention a single interrupter for 2,500,000-kva service at 132 kv was presented.[5] This trend, moreover, will be prominent in future design because of low arc energy, simplification of structure, and reduced cost.

Also, the use of compressed air for powerhouse installations is growing and 2,500,000-kva interrupting-capacity devices have been built and tested using a single break per pole. There are possibilities of space saving with this type, but such space reduction of three-pole devices increases the hazard of adjacent poles, and makes three-phase testing under full power and voltage conditions desirable. The results of such tests made in the laboratory here discussed are given in the paper by Ludwig, Wilcox, and Baker at this convention.

The laboratory described below is felt adequate for the future demands of American switchgear practice; in fact, its complete power is necessary for the largest

Figure 1. The layout of the buildings, test cells, and principal pieces of equipment



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J. B. MacNeill is manager of the engineering switchgear division, and W. B. Batten an engineer, both with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. The authors wish to express their thanks to Mr. R. C. Van Sickle for his suggestions and assistance in preparing this paper.

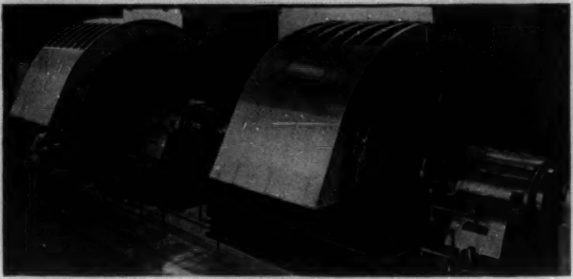

Figure 2. Station 2—a second generator doubles the short-circuit capacity

capacities only. However, it is in these large capacities that the major problems of switchgear design occur, including high-speed interruption, high-speed reclosing, and repetitive duty. These problems require continued activity, not only on existing types of apparatus, but especially on those newer forms now becoming active in this country, and on which a background of field experience at high power is lacking.

Development and Use of the Laboratories

The Westinghouse program of providing equipment for high-capacity short-circuit testing was inaugurated in 1925 when generating equipment capable of producing 400,000 initial three-phase symmetrical short-circuit kilovolt-amperes was installed. This was available at generator voltages only. Extensive use was made of this equipment, but transformers to extend the range of test voltages were needed. A bank of three 33,333-kva transformers together with a suitable high-voltage cell was put in service in 1928 to permit testing up to 230 kv. This transformer bank, which was larger than necessary at the time, was selected as the need for more generating capacity was already apparent. In 1930 the second generating station designed to house two 60,000-kva generators (nominal rating) was constructed, and one generator was installed. During the following ten years, this generator was used in making about 47,000 short-circuit tests. The need for more generating capacity became apparent late in this period and the second 60,000-kva unit was put in service in 1940. At this time a second bank of 33,333-kva transformers was installed to provide testing capacity at the higher voltages commensurate with the increased generating capacity.

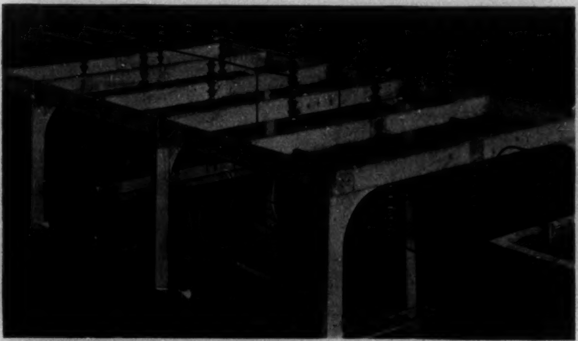

Figure 3. The new transformers and bus structure permit testing to 345 kv three phase

To supplement this equipment, a high-current low-voltage transformer bank and a high-current test cell were added in 1940, and in 1941 a low-temperature test room was provided.

Since 1925 approximately 100,000 short-circuit tests (including both large and small generators) have been made in these laboratories. Although the greater part of this work has been for the purpose of determining interrupting ability of circuit breakers, many other types of equipment have been subjected to short-circuit tests. Included are:

1. Porcelain insulators—resistance to power arcs
2. Lightning arresters—power-follow tests
3. Power transformers—short-circuit tests
4. Current-limiting reactors
5. Current transformers
6. Fuses
7. Bus bar structures
8. High-voltage capacitors
9. Potential devices

Generating Equipment

The physical arrangement of the essential parts of the Westinghouse Electric and Manufacturing Company's high-capacity testing plant is shown in Figure 1. In the original station 1 is located the 40,000-kva set which consists of two 20,000-kva generators driven by a 3,300-horsepower wound-rotor motor. Separately driven exciters are provided. Since the larger generators of station 2 were put in service, the 40,000-kva plant is used for smaller apparatus which it can test to full short-circuit rating. Also, for most of the work requiring the high-current low-voltage transformers, the 40,000-kva plant has adequate capacity. The generators in station 1 have an initial reactance of approximately 10 per cent, hence a short-circuit output of 400,000

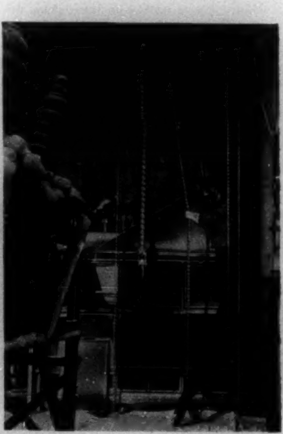

Figure 4. Inside the cold room with a sleet test in progress

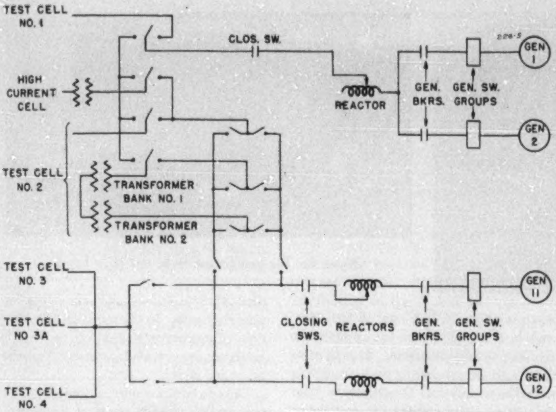

Figure 5. Power circuits combine simplicity and flexibility

initial three-phase symmetrical kilovolt-amperes may be obtained. By different generator coil arrangements full capacity of this plant may be obtained at 3.8, 6.6, 7.6 and 13.2 kv at 60 cycles. No provision is made to operate this plant in parallel with the larger generators in station 2.

In the newer station 2 are located two 60,000-kva (nominal rating) generator sets. These machines when operating in parallel can produce an initial three-phase symmetrical short circuit of approximately 2,000,000 kva at the 13.2-kv test cells. Each of these sets consists of its generator, a 6,000-horsepower wound-rotor driving motor, and a direct-connected exciter. The machines have 14 poles, hence they run at 514 rpm for 60-cycle operation and 219 rpm for 25-cycle operation. Since liquid rheostats are provided for starting, lower frequencies may be obtained by operating with resistance in the rotor circuits.

The two sets are mechanically independent, although provision has been made to couple them if it ever becomes desirable. Because of the 700-kw friction and windage loss of each set, an important saving in power is obtained by operating only one machine when it is adequate.

When parallel operation of the two generators is required, they are synchronized electrically, reactors being provided for this purpose. This operating arrangement has proved very satisfactory. The rotors of the two sets have the same inertia, and under the most severe short-circuit conditions, there is no tendency for the two sets to pull out of step.

From a casual observation (Figure 2) it would appear that the two generators are identical. However, the unit built in 1940 takes advantage of certain refinements in materials and incorporates some of the advances made in generator design. The total weight of each machine is 550 tons, being about equally divided between stator and rotor. The rotor diameter is 12 feet and has a peripheral speed of 22,000 feet per minute. The inertia of each rotor is 8,500,000 pound-feet^2. As is shown in Figure 2, the stators are spring-mounted to cushion the shock on the foundation at the time of short circuit.

Full capacity may be obtained from these generators at 7.6 and 13.2 kv at 60 cycles and at 3.12, 5.5, 6.25 and 11 kv at 25 cycles.

The stored energy in the rotors of these sets is adequate to absorb the losses produced by short circuits, and it is unnecessary to disconnect the driving motors except in special cases where the short circuits are sustained for several seconds. This stored energy presents a problem when bringing the sets to a stop. With no braking, it takes two hours for the sets to come to a standstill. Provision is made to supply d-c to the driving-motor primaries and to let their secondaries feed energy into the liquid rheostats. In this way they can be brought to rest in about eight minutes.

High-Voltage Equipment

To provide for the testing of apparatus at voltages in excess of 13.2 kv, the transformer station is available (Figure 3). This installation includes six 33,333-kva single-phase transformers. They are supplied directly from the generators at 13.2 kv. The secondary of each unit consists of six 22-kv coils. Hence voltages of 22, 38, 44, 66, 76, 88, 115, 132, 152, and 230 kv are available with full capacity. The original three transformers are insulated for 132 kv-to-ground and the newer three for 196 kv-to-ground so that they may be connected in series to give higher voltages. It is, therefore, possible to obtain 345 kv, three-phase *L-L* with the neutral grounded and 396-kv single-phase with mid-point grounded. The 345-kv three-phase connection is made with one winding at 132 kv in series with another at 66 kv. Hence, due to differences in current carrying ability of the transformers, this connection is not suitable for the full capacity of the transformer bank. Various voltages other than those mentioned are

Figure 6. The station operator at the control desk has a direct view of all test cells

Figure 7. Duplicate sequence drums facilitate changes between tests and make possible simultaneous testing at both transformer and generator voltages

Figure 8. The oscillograph table and relay board

obtainable but in most cases not at full capacity as there are unequally loaded transformers involved. The reactance of the transformers is low so that their output, single-phase, is 75 per cent of the generator-bus kilovolt-amperes and on three-phase tests is 65 per cent. Six potential transformers, three insulated for 196 kv-to-ground, are provided for oscillographic recording of voltages.

Power from the transformers is fed to the test cell 2 where all testing at voltages above 13.2 kv is conducted. The only modification required as far as the test cell was concerned, when the second transformer bank was installed, was a new group of roof bushings adequate for 198 kv and a slight increase in the height of the cell to accommodate the larger bushings.

High-Current Transformers

To provide for momentary and five-second current-carrying tests, a bank of low-voltage high-current transformers has been provided. These transformers giving open-circuit voltages of 625, 1,250, 2,500, and 5,000 volts are located in an enclosure which forms a high-current test cell. On the 625-volt connection they are suitable for five-second tests at 200,000 amperes, three-phase or 345,000 amperes single-phase. At these currents the terminal voltage drops less than 50 per cent. These transformers are also useful for interrupting tests at low voltage.

Cold Room

When it was decided to provide a refrigerated room in which high-voltage breakers could be tested under severe sleet and temperature conditions, it became apparent that the high-current test cell could be used for this purpose. This room is about 25 feet long, 12 feet wide, and 25 feet high, hence has ample space for high-voltage breakers. It was insulated and an air-cooling and circulating unit was installed at the end away from the door. The compressor equipment was placed some distance away. Temperatures of −20 degrees Fahrenheit with outside temperature of 90 degrees Fahrenheit are easily obtained (Figure 4). A 110-kv bushing from this cell to the transformer yard provides for high-voltage interrupting tests. This test cell is, therefore, used for fuse testing, high-current testing, and low-temperature work.

Power Circuits

When the second 60,000-kva generator was installed, a number of changes was made in the bus system providing a more direct run to the test cells and the transformers. The arrangement of the power circuits (Figure 5) was made with the idea of flexibility as one of the principal considerations. Each generator has its own breaker, current-limiting reactor, and closing switch. Disconnecting switches are provided so that either or both 60,000-kva generators can supply power to the 13.2-kv test cells, either or both to either transformer bank, and either or both to the high-current low-voltage transformers, or directly to the high-voltage test cell 2. The two 20,000-kva units can supply power to cell 1, to the high-current transformers or the high-voltage cell, and to transformer bank 1.

The connections from the generator coils to the generator setup switches consist of flexible cable firmly cleated in place. From the setup switches to the closing switches, copper tubing covered with Micarta tubing is used. This assembly is supported by Micarta cleats spaced at short intervals. Beyond the closing switches, the bus carrying the output of both generators consists of a square tubular copper conductor similarly insulated with Micarta tubing and supported by Micarta cleats. In the test cells the same type of conductor is used but porcelain insulators are provided instead of Micarta tubes and cleats.

The two banks of current limiting reactors provide a wide range of flexibility. Each bank consists of 18 units (6 per phase). By means of disconnecting switches nearly 100 combinations are available, hence the reactance can be varied from its maximum of 5 ohms per phase to 0 in small steps.

It will be noted from Figure 5 that there is a total of six test cells available, thus making possible the assembling or dismantling of apparatus in some cells while tests are being conducted in others.

Control

The operation of the equipment in station 2 is controlled from a central control desk located in the control house (Figure 6), so arranged that the operator observes the equipment under test. This desk is very complete with indicating instruments, protective relays, annunciators, and buzzer for all protective devices and test jacks for checking all measuring and control circuits.

At various points about the laboratory and yard are located safety switches. Anyone working in the area covered by one of these switches moves the switch to the safe position and every switch must be restored to the operating position before voltage can be applied to the generators. Indicating lamps on the control desk show the position of these various switches. In addition, it is the operator's duty to see or otherwise account for all the individuals working around the yard before energizing the equipment.

The station is equipped with all the usual protective relays as well as photocells which clear the circuits and deenergize the generator fields in case of a power arc anywhere in the station. Furthermore, an observer is stationed in a soundproofed booth in the generator room. He has control of an emergency switch which can be tripped in case of any trouble in the station. The observer also changes the reactor switches when changes in current are required, but, as was stated previously all control of the plant is located at the control desk.

Air-closing switches are used instead of breakers for initiating the short circuit, when opening tests on breakers are being made. These switches can start the short circuit at any desired point on the voltage wave, thus controlling the degree of current asymmetry on single-phase tests. As the closing time of these switches must remain constant, they are spring-closed and are operated by a solenoid mechanism which latches the switches in the open position. Synchronized closing is accomplished by tripping a grid-glow tube from a small impulse generator adjustably coupled to the main generator shaft. The grid-glow tube energizes the unlatching coil on the closing switch. As the operating time is constant within 0.0015 second, a close control of current asymmetry is possible.

In the control room are located (Figure 7) sequence drums which control the actual operations for a test, once the proper series reactance has been chosen and the generators excited to the proper voltage. In a normal opening test, the sequence drum starts the oscillograph, closes the closing switches, trips the test breaker, trips the generator backup breakers and trips the generator fields. In case of a close-open test, the operator closes the closing switches before starting the sequence drum and the drum closes and opens the test breaker.

Two sequence drums are provided, making it possible to test in two locations simultaneously. For instance, one generator may be used for a test in one of the 13.2-kv cells while the other is used for a test in the high-voltage cell. Adjustment of the time interval between the functioning of the various devices controlled by the sequence drums is accomplished by moving the cams with respect to each other. The cam-operated switches are connected to plugs as shown in Figure 7, and all essential controls are brought to jacks on the panels above the sequence drums. This arrangement provides great flexibility of control.

Measurements

Magnetic oscillograph records are almost invariably made of all tests. Figure 8 shows the oscillograph table (also located in the control room). Two multielement oscillographs are available and when desired, they can be operated as a single unit thus making available a total of 18 records. All potential and current transformer secondaries are brought to a plug board above the oscillograph table. Current transformers are used for all current measurements, specially designed units being provided to assure accurate current records. Potential transformers are located on the 13.2-kv bus supplying test cells 3, 3*A*, and 4. No potential transformers are supplied in the high-current cell. When required, portable units are used. Currents in the high-current transformers are measured on the primary or 13.2-kv side. Several relays are available from each test cell to the oscillograph table to take care of travel records, pressure indicators, and any other desired measurements.

A cathode-ray oscillograph of the cold-cathode type for measuring recovery-voltage transients is located on the second floor of the control house. An overhead transmission line provides the connection between the oscillograph and the test cells.

Transient Recovery Voltage

The severity of circuit-breaker testing depends not only on the voltage and current of the circuit but also on the rate at which the voltage appears across the breaker contacts during the recovery transient. The layout of the laboratory with the reactors close to the test cells makes it possible to obtain high natural frequencies, and values up to about 200,000 cycles per second have been recorded. Transients and breaker performance on these test circuits can be the equivalent of those obtained under severe service conditions.

Observation

A limited number of witnesses can be accommodated in the control room, but for the larger number of observers present for a demonstration test, a large room affording a good view of the test cells is provided on the second floor of the control house.

Operating Experience

The location of the high-capacity testing station within a manufacturing plant has been justified by sixteen years successful operation and the close effective cooperation thus obtained among the design, manufacturing, and test departments.

This high-capacity testing laboratory is in effect a power station capable of delivering, momentarily, a short-circuit output comparable with that obtainable on large power systems. In contrast, however, it carries no continuous load, and a short circuit is a normal instead of an abnormal condition. The flexibility of the testing station permits the duplicating of many voltages and currents corresponding to different service classes and ratings of equipment. Accurate control of the conditions and complete records of performance produce experimental data which, when properly used, result in circuit breakers and other equipment of adequate design and proven ratings.

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Electrical Drives for Wide Speed Ranges

G. A. CALDWELL
ASSOCIATE AIEE

W. H. FORMHALS
ASSOCIATE AIEE

THE electrical drive for a wide speed range is obtained by the addition of a rotating regulator called a Rototrol to a conventional variable voltage system. This combination will give a speed range of 120 to 1 or more, and can be used effectively in many industries to simplify the mechanical design of the machine which it drives. Its use eliminates elaborate gear-change mechanism, clutches, and so forth, and at the same time gives a more flexible control scheme with the complete speed range under the control of the operator without leaving the work or stopping the machine.

The conventional variable voltage or Ward-Leonard scheme of control is the accepted method of obtaining speed ranges in excess of 6 to 1 with 20 to 1 about the maximum range. This range is obtained by combination of motor-field and generator-voltage control. The amount of field control is limited by motor stability, and the amount of voltage control is limited by speed regulation and maximum torque. The addition of regulating devices and other refinements to improve the characteristic of this scheme make it possible to extend the range into the field that can be defined as a wide speed range.

The increase in speed range has been accomplished by widening the range of voltage control, the range in field control remaining not more than 4 to 1 and preferably only 2 to 1. Therefore this type of drive is best suited to a load which has constant torque characteristics. The machine tool industry has many applications of this type, and it is here that the wide-speed-range drive has been applied. Feed drives on boring mills, milling machines, automatic screw machines, and so on, have constant torque characteristics as the load consists mostly of overcoming friction of the moving parts. Wide ranges of feed speed are required to satisfactorily machine the wide variety of sizes and kinds of metals. With the wide-speed-range drive, these speeds are always immediately available to the operator without the necessity of leaving the work and stopping the machine to change gears. Speed regulation is good, and when desired the rheostats can be calibrated to read feed speed directly.

To develop a satisfactory wide-range variable-voltage drive it has been necessary to compensate for certain characteristics at the low speed that are not important at high speeds. The two most important factors are the residual voltage of the generator and the *IR* drop of the system.

Most commercial generators have a residual voltage of between three and four per cent. This would mean that the no-load speed of a motor which is being driven from such a generator by variable-voltage control is limited to a no-load speed range of about 25 to 1, since the residual will not permit a voltage lower than itself to exist. If it is desirable to extend the range beyond 25 to 1, it becomes necessary to make some arrangement to overcome the residual voltage. Also, since the residual is a function of the previous magnetic history of the generator, it is necessary that the equipment used to overcome residual must be able to determine the actual conditions which exist.

The speed of the d-c motor will normally drop as load is applied due to the *IR* drop in the system. Since a feed mechanism must run at constant speed regardless of the load, it is necessary that the speed-torque characteristic of the motor driving the feed be practically flat. Since the normal drop on a d-c motor is due almost entirely to *IR* drop, some arrangement must be made to increase the applied voltage by the amount of the *IR* drop as the load is applied so that the induced voltage, and therefore the speed of the motor, will remain constant. These requirements would indicate that some means of regulation must be supplied in order to maintain the desired characteristics. It has been found by experience that a rotating regulator or regulating generator is satisfactory for this particular function and has been widely applied.

This type of rotating regulator was first developed for elevators and a large installation was made in Radio City in 1934. Since that time the Rototrol has been used in many industrial applications. It is the heart of the variable-voltage planer drive and has been proven in service over a period of several years. Other applications include paper mill drives, electric shovels, and skip hoists. In appearance it looks the same as a standard d-c generator and is usually mounted as a unit of the variable-voltage motor generator set.

In the use of any regulator which is to vary the voltage of a generator, it is possible to have the regulator take care of only the changes which are required, or to incorporate in it the total excitation of the machine to be regulated. Where very great voltage range is required and where the response should be quick and accurate, we have found it desirable to incorporate in the rotating regulator only the regulating functions which are desired and to supply the normal excitation from some other source. The most satisfactory method to supply the regulating current to the fields of the generator is by use of a Wheatstone bridge circuit in which the armature of the rotating regulator is placed where the galvanometer in the conventional bridge is located. This is shown in Figure 1.

In this circuit generator fields *GF*1 and *GF*2 are identical as are regulator fields *RF*1 and *RF*2. Resistors *R*1 and *R*2 are equal to each other and to the sum of the resistances of a regulator and a generator field. Thus all four legs of the bridge are of the same resistance, and a balanced bridge results.

It can be demonstrated that in a balanced-bridge circuit of the type described when a source of voltage is connected to points *A* and *C*, points *B* and *D* have the same potential, and consequently no current from said source tends to flow through any circuit connected between *B* and *D* such as the armature of the regulator. The converse is also true that a source of voltage applied to points *B* and *D* will not cause any current from said source to circulate in a circuit connected to points *A* and *C* such as the exciter circuit.

It can also be demonstrated that in a circuit as described, if the exciter voltage applied alone to points *A* and *C* circulates *Ia* amperes in each leg *A-B* and *C-D* (containing the variable-voltage generator and regulator-field windings) and if the regulator armature voltage applied alone to points *B* and *D* circulates *Ib* amperes in each leg *A-B* and *C-D*, then when the two voltages are applied together they will circulate in each leg *A-B* and *C-D* a current equal to the algebraic sum of *Ia* and *Ib*. Thus the use of a balanced-bridge circuit permits complete independence of

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G. A. CALDWELL is in the industry engineering department and W. H. FORMHALS in the motor engineering of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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the excitation and regulating currents even though they have common paths in the field circuits of the generator.

Figure 1 shows a schematic diagram of the equipment required for a wide-speed-range feed equipment for a machine tool. For the purpose of simplicity many of the control elements such as operating coils for the various contactors, control stations, and so on, have been omitted as they are not required for this discussion. It can be seen that this circuit is basically the variable-voltage system except for the addition of the regulating generator and its associated circuits. These additions, however, extend the range of speed adjustment and give much better regulation than can be obtained with a straightforward variable-voltage system. The generator differential field *GF*3 and its contactor *BR* are a part of the variable-voltage scheme which prevents creeping in the off position and are not involved in obtaining the wide speed range.

If the generator-field rheostat be set at some position and the forward directional contactors *F* be closed, current will flow from the exciter through the two branches *ABC* and *ADC* of the bridge. This current, *Ia*, flowing through the generator fields *GF*1 and *GF*2 will cause the generator to produce a voltage across its armature and thus across the motor armature. Since the motor field is excited, the motor will run at the speed determined by its field strength and its applied armature voltage, assuming no load and thus no current and no *IR* drop in the armature circuit.

The current *Ia* flowing in the regulator fields *RF*1 and *RF*2 will set up a magnetomotive force in the regulator which will cause it to generate a voltage which will tend to circulate a current *Ib* in the direction so as to strengthen the fields of the generator and regulator in the bridge circuit.

This magnetomotive force is not permitted to act alone in the regulator but is balanced by the magnetomotive force of field *RF*3 which is connected so as to be differential to *RF*1 and *RF*2. Its strength is adjusted by resistor *R*5, so that at this condition of operation, its ampere-turns are equal in magnitude to the ampere-turns of *RF*1 and *RF*2. Thus the fields in the bridge circuit get a cue as to the value of voltage which is desired and the field across the armature circuit measures the actual voltage which is obtained. Since at no load the speed of the motor is determined by its applied voltage, we might consider these two regulator field circuits to compare actual speed with desired speed.

If the calibration of the fields has been made at the point being described then the resultant regulator magnetomotive force is zero and no voltage is present across its armature terminals and thus the regulating current *Ib* is zero.

Let it be assumed, however, that this condition of operation being described is not at the calibration point but at some other value of speed, and that, due to shape of the saturation curve or to effect of the hysteresis loop of the generator, the voltage generated is not as great as it should be to give the motor speed desired. Then the differential ampere-turns in *RF*3 are not so great as the cumulative ampere-turns in *RF*1 and *RF*2, leaving a resultant ampere-turns which cause the regulator to circulate current *Ib* in the direction shown in Figure 1. This current causes an increase in actual generator-field current, as shown earlier in this paper, and thus causes the generator voltage to increase to the value it should have.

To maintain this regulating current at the necessary value, the constants of the circuit consisting of the regulator armature, the resistances of the bridge circuit, and the regulator fields *RF*1 and *RF*2 must be such as to make the circuit self-energizing. This can be done by adjusting resistor *R*3 so that the field characteristic line of the combined effects of fields *RF*1 and *RF*2 will be coincident with the air-gap line of the no-load saturation curve of the regulator generator for those fields alone. Thus the regulator will force enough regulating current *Ib* through the generator fields until the proper operating point is reached. At this time the ampere-turns due to the

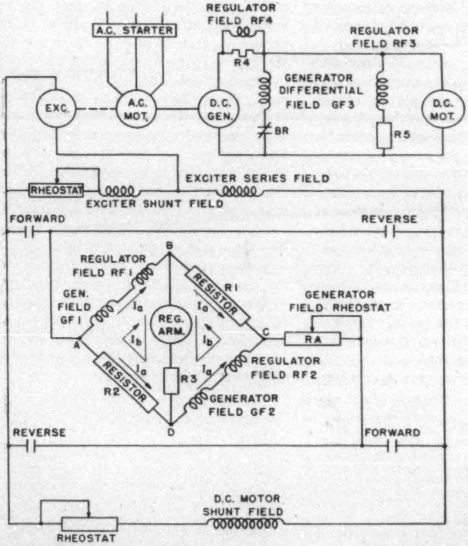

Figure 1. Schematic diagram of main circuits of the wide-speed-range drive

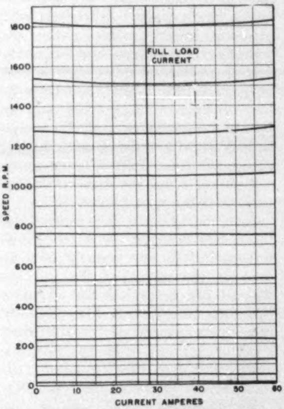

Figure 2. Speed regulation curves covering a range from 15 to 1,800 rpm

Maximum speed regulation at any speed is less than five per cent

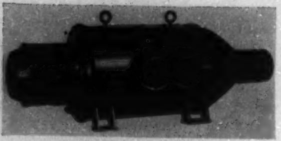

Figure 3. Typical variable-voltage motor-generator set showing the units used in a wide-speed-range drive

This is a four-unit set with a main generator, exciter, and regulating generator (on the right-hand side) driven by a squirrel-cage induction motor

Figure 4. This horizontal boring, milling, and drilling machine has an electrical feed drive with a speed range of 120 to 1

The electrical drive eliminates the use of change gears, clutches, and so on, and greatly simplifies both the construction and manipulation of the machine

current from the exciter *Ia* in the fields *RF*1 and *RF*2 will be balanced by the ampere-turns in field *RF*3 due to the proper voltage across the armature of the generator and the regulator current *Ib* through fields *RF*1 and *RF*2 will just maintain the current *Ib*.

It should be noted that if the regulating generator is not self-energizing that it would not be possible to get a correct operating point, since, to have any corrective current circulated there would have to be some difference in the cumulative ampere-turns due to *Ia* and the differential ampere-turns due to voltage. In such a case the voltage would approach but never reach the correct value and the accuracy of regulation would be greatly decreased.

An ordinary generator when set up with a self-energizing field under the conditions above described would tend to build up to the point at which saturation begins to take place. In this regulating generator, however, this action is prevented since any charge from the correct operating point causes a corrective magnetomotive force, since the balance between cumulative ampere-turns and differential ampere-turns in the three regulator fields will be destroyed. Analysis shows that the difference which results will always cause the regulator output to return to the correct value.

From the foregoing it is evident that the no-load speed of the motor will always be made to agree with the value desired by the setting of the generator rheostat. If mechanical load is put on the motor, it will slow down, thus decreasing its counter electromotive force and thereby permitting a current to flow in the armature circuit of the two machines. A stable point of operation occurs when the drop in speed and counter electromotive force is just sufficient to make available enough of the generated voltage to circulate the current required.

To bring the speed back to the no-load speed necessitates the raising of the generator voltage by the amount of the *IR* drop. This is done by the regulator by adding on its magnetic structure another field *RF*4, which is connected in the armature circuit so as to produce cumulative ampere-turns. By proper adjustment of *R*4 this field can cause the regulator to correct almost perfectly for *IR* drop and armature distortion of the motor over a wide range of load thus giving a very flat speed-load curve.

Other conditions than load tending to change the speed of the motor might be thought of, but it will be found that the regulator generator will make appropriate corrections. Thus burning or shifting of the brushes of the generator often tends to cause stalling of the motor with the conventional variable-voltage drives. Also varying values of residual magnetism in the variable-voltage generator cause different voltages for the same rheostat setting. Such conditions however are corrected by the regulator.

In Figure 2 are shown the speed–load characteristics as obtained from actual tests on a $7^{1}/_{2}$-horsepower machine-tool feed equipment using a regulating generator such as just described. These curves show the regulation of the motor to double full-load current over the range of from 15 rpm to 1,800 rpm or a speed range of 120 to 1. Of this range, speeds up to approximately 1,000 rpm are obtained by variable-voltage control and from 1,000 to 1,800 rpm by motor-field control.

It can be seen by reference to these curves that the change in speed due to application of load is so small as to be negligible, and there is no tendency for the motor to stall even when the speed has been reduced to $^{1}/_{60}$th by voltage control. It is to be noted that at light loads at this speed the voltage required is less than residual voltage of the generator. The regulator has, however, caused a reversal of current in the fields of the generator and thus bucked the voltage down to that required to get the speed called for.

From the results shown it is evident that the use of a regulator scheme such as indicated gives speed range far beyond the supposed limits of a few years ago without greatly complicating the electrical apparatus or going to laboratory-type equipment. The machine being driven can be a simpler, more flexible and more efficient machine tool.

Measurement of Maximum Demand

PAUL M. LINCOLN
FELLOW AIEE

Synopsis: "Maximum demand" has become an increasingly important item in rates for the sale of electric service since the appearance of Doctor John Hopkinson's notable paper, "Cost of Electricity Supply," in the year 1892—nearly 50 years ago. The writer estimates that the demand-measuring equipment which is now being used by the public utilities of the United States has a value of the order of $100,000,000. This is an index of its present importance. The object of the present paper is to describe in detail the thermal-storage method of measuring the maximum demand of a user of electric service. A comparison is made between this method, which at each instant of time indicates the logarithmic average load over some nominal time interval, and the commonly used "block-interval" method, which indicates the arithmetic average load over the same time interval. Also, the effect of using a modified design of thermal-demand meters is discussed. The writer has contributed a number of previous papers dealing with this same general subject, reference to which will be found at end of paper.

The Block-Interval Meter

As is well known, the commonly used block-interval demand meter measures the arithmetic average load over some definite interval of time. Its construction and operation are such that, at the end of the month, the maximum arithmetic average block of load over some definite time interval is indicated on the meter scale. The meter is then reset to zero so that the maximum arithmetic average load for the following month may be made available when the meter is again read and reset at the following month's end. As is well known, the commonly used "block-interval" demand meter measures this maximum arithmetic average load over some definite interval of time, such as 5 minutes, 10 minutes, 15 minutes, 30 minutes, and occasionally 60 minutes. Other time intervals have been proposed, even as low as one minute, but so far as the writer knows, no time intervals of less than 5 minutes nor more than 60 minutes are actually being used at the present time. The 15-minute time interval is the one most frequently used, followed by 30 minutes and 60 minutes. Knowlton, in his "Electric Power Metering," says, "A survey by the National Electric Light Association rate research committee in 1931 showed the 15-, 30-, and 60-minute intervals to be in vogue in the ratio of 40:8:1 respectively." Knowlton does not mention any time intervals shorter than 15 minutes, although to the writer's certain knowledge time intervals as short as one and two minutes have been used in the past. To the best of his knowledge the five-minute time interval is still being used to a limited extent.

Basic Weakness of Block-Interval Meter

As mentioned in the preceding paragraph, the "block-interval" demand meter measures the maximum arithmetic average load over some definite interval of time. The arithmetic average, as measured by the standard block-interval demand meter, is a discontinuous function of time. Any arithmetic average must necessarily be a discontinuous function of time, and, as such, be subject to a possible error of 50 per cent—a possibility which can become a certainty at the option of the service user. When it becomes necessary to use two or more block-interval demand meters on the same load, it is customary to synchronize the meters so that they reset at the same instant. This synchronization has no effect on the inherent inaccuracy of the block-interval demand meter, but it does prevent the utility and the utility's customers from becoming aware of the inaccuracy.

While we are considering possible inaccuracies of measurement when using the standard block-interval demand meter, a situation which recently came to the writer's knowledge may be of interest—a situation the possibility of which is mentioned in the preceding paragraph. This service user, as reported to the writer, is taking deliberate steps to "split peaks." His loading schedule is so timed that his demand meter resets, as closely as possible, midway during load applications. If this method of taking load is carried out to its possible extreme, the maximum demand measured and billed can be made one-half the actual arithmetic maximum demand. Such a method of taking service is, of course, open to any user of service whose maximum demand is measured by means of the standard block-interval demand meter. In the case of deliberate "peak splitting," the utility is always on the losing end.

Maximum demand may be accurately measured only by a continuous function of time. Further, even if it were possible so to modify the demand attachment of the standard block-interval meter and thereby obtain a true and accurate arithmetic average, the maximum arithmetic average thereby obtained would still be defective. For instance the maximum arithmetic average—or logarithmic average—of a user of electric service who takes a perfectly steady load of 100 kw would, of course, be 100 kw, and his bill for service would be based on 100-kw demand. Another user whose maximum demand is measured by means of a standard 30-minute block-interval meter and who takes during each half hour 100,000 kw for 1.8 seconds, or 10,000 kw for 18 seconds, or 1,000 kw for 180 seconds (3 minutes), or 500 kw for 6 minutes, or 300 kw for 10 minutes, or 200 kw for 15 minutes, would have exactly the same arithmetic average as the first user—provided there were no "peak splittings." This latter group of cases certainly warrants a higher maximum-demand assessment than the first user. Also, the earlier mentioned members of this group warrant a higher maximum-demand assessment than the later mentioned members.

The Thermal-Meter Principle

On the other hand, the thermal wattmeter measures the logarithmic average of the load under measurement. (For a definition of the term "logarithmic average," see reference 1 at end of paper.) There is a basic difference between the logarithmic and arithmetic averages. The logarithmic average is a continuous function of time. That is, at each instant of time, it indicates the logarithmic average over the immediately preceding 15-minute, 30-minute, 60-minute, or other time interval. The length of this time interval is dictated by the value assigned to an adjustable constant k, the character of which is discussed later in this paper. The load taken by a user of electric service is continuous—but not necessarily steady—and its maximum demand can be consistently measured only by a continuous function of time. Even if it were possible to measure the maximum arithmetic average accurately—which is not the case with any of the existing types of block-interval meters—the thermal type would still be preferable, since the heating effect

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Paul M. Lincoln is president of Therm-Electric Meters Company, Inc., Ithaca, N. Y.

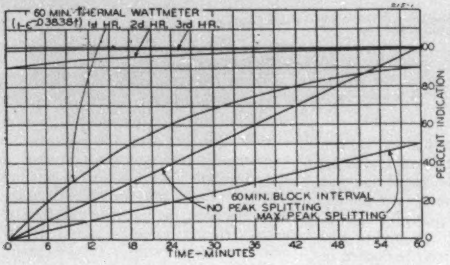

Figure 1

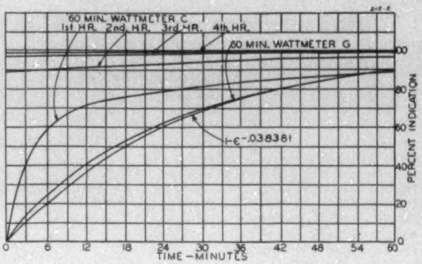

Figure 2

of any given load on the equipment serving the load is a logarithmic or exponential function of time and not an arithmetic function.

While the thermal wattmeter has been fully described in previous papers (see references 1 and 2), it might be well to give a brief description of its operation. A small transformer whose primary is connected across the supply voltage causes a current to circulate continuously through two equal resistances, which we will call r. Let us call this circulating current E, since its value is always proportional to voltage. The load current is caused to pass through these same resistances. Let us call this load current I. The load current is caused to pass through the resistances in such a manner that one-half its value adds to the circulating current in one resistance and subtracts from it in the other. The total current in one resistance is, therefore, $E+I/2$ and in the other $E-I/2$. The total heat applied to one resistance is, therefore $\left(E+\frac{I}{2}\right)^2 r$ and to the other $\left(E-\frac{I}{2}\right)^2 r$. The thermal wattmeter is so constructed that it constantly indicates the difference in temperature between two masses of matter which are being heated by these two currents. The difference in heat applied is obviously $2EIr$, a function which is always directly proportional to the watts entering the load. Since the masses of matter do not and cannot respond instantly to the heat applied, the result is a demand meter measuring the logarithmic average load.

Mathematical expressions for the indications of the two types may readily be obtained. For the arithmetic average (block-interval) meter, the mathematical expression for its indication is

$$\text{Meter indication} = \frac{1}{t_1-t_2}\int_{t_2}^{t_1} w\,dt \qquad (1)$$

The logarithmic average may be expressed mathematically as follows:

$$\text{Meter indication} = k\int_0^t w\epsilon^{-kt}\,dt \qquad (2)$$

In the above expressions

w = instantaneous value of watts
t = time
k = an adjustable constant
ϵ = base of Napierian logarithms
t_1-t_2 = time over which arithmetic average is measured

Figure 1 gives the solution of these two equations when the time interval for demand measurement is 60 minutes—the maximum time interval now being used. The relative indications of the two types for load durations up to 60 minutes may readily be seen from Figure 1.

As actually constructed, however, the relative indications of the thermal-demand meter may be made to depart radically from the purely theoretical ratios shown in Figure 1, or may be made to conform closely to it, depending on design. This comes about because the diffusion of the heat that necessarily enters or leaves the working parts of the thermal wattmeter during normal operation does not and cannot take place instantaneously. As is well known, heat does not and cannot diffuse instantly throughout any mass of matter that is being heated or cooled. The process of heat diffusion takes time. The values given in Figure 1 take no cognizance of this. Also, this factor depends to a marked extent on the design of the thermal wattmeter. This matter is treated further later in this paper.

Referring to mathematical expression 2, it might be pointed out that when the value of kt reaches 6.9078, ϵ^{-kt} becomes one-tenth of one per cent of what it is when $kt=0$. One-tenth of one per cent is as small a quantity as can be read accurately on the demand-meter scale. Therefore, while the integration of the thermal wattmeter extends from zero to infinity, the only significant part of this integration is that which takes place during the interval of one or two hours—or at most three hours—immediately preceding the instant of observation. This holds true when $kt=2.3026$, the value assigned to kt in the edition, 1941 Code for Electricity Meters, the code sponsored by the American Standards Association. This also assumes that the time interval for demand measurement shall not exceed one hour. If the time interval for demand measurement is 15 minutes, the time interval most frequently used in the United States, the only time interval of any significance is the 45 minutes immediately preceding the instant of observation.

Demand Measurement—a Problem in Transients

The measurement of maximum demand is a problem in transients unless the load during maximum is perfectly steady. If the loads taken by users of electric service were always steady, the standard block-interval demand meter would measure maximum demand with perfect accuracy. The arithmetic and logarithmic averages of a steady load are exactly the same. An unvarying load requires no demand meter. Under the conditions of the actual use of service, the only existing method of assuring accuracy of measurement is by the use of a continuous function of time. (The thermal wattmeter is an example.) Our usual conception of a transient is the phenomena that occur in a stroke of lightning or a power interruption. Anyone who dealt with the problem of transients is, of course, aware that time must appear as an exponential function, as it does in the thermal wattmeter. When dealing with thermal wattmeters, the time involved, instead of being of the order of microseconds, as in the usual type of transient, is now of the order of minutes, or even of hours.

Comparison of Block-Interval and Thermal-Demand Meters

Let us assume a case where a service user is taking a continuous steady load of such a value that it causes the demand meter to reach a value which we will call 100 per cent. Let us assume further that this load is being measured by a standard 60-minute block-interval demand meter and also by a 60-minute thermal-demand wattmeter. Let us assume further that the thermal wattmeter is the 60-minute wattmeter *G* which is described later in this paper. If this user's load is perfectly steady, both wattmeters will indicate 100 per cent demand. If however, business falls off to such an extent that it is necessary to use service only during alternate hours, Figure 6 shows the relative indications of the two demand meters. The data shown in Figure 6 are the results of a carefully made test. Inspection of Figure 6 shows that the indications of the thermal and block-interval types cross at approximately 52 minutes when there is no peak splitting. Therefore, if the time of load duration is less than approximately 86.5 per cent of the nominal time interval, the logarithmic average is always in excess of the arithmetic, whether peak splitting occurs or not. In this case, also, the utility using the block-interval meter is always on the losing end.

Ithaca Tests

In order to get some definite information on the question of how much difference might be expected in the indications of the two types of demand meters on actual loads, there has been conducted in the city of Ithaca, beginning in March 1939, and continuing for about 18 months, a series of tests which may be of interest. Ithaca, at that time, was using a 15-minute demand interval, since then increased to 30 minutes. For each of four customers, three demand meters were installed. These four customers were two garages, a beauty parlor, and a photographic studio. Number 1 meter was a 15-minute block-interval meter of standard make. Number 2 meter was an identical block-interval meter so adjusted that its time interval "broke joints" with number 1. That is, number 2 meter reset $7^1/_2$ minutes after number 1. Number 3 meter was a 15-minute thermal-demand wattmeter. For a time, there were two thermal wattmeters on some of the loads. This was done to show by direct experiment what theoretical considerations tell us; that is, that the logarithmic average, being a continuous function of time, must always be the same on any given load. In no case was any material difference found between the two thermal wattmeters.

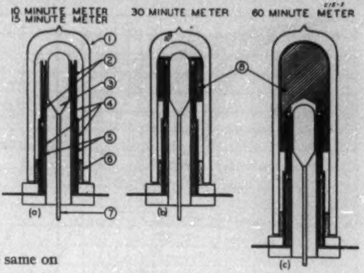

Figure 3. Diagrammatic cross section of 15-minute, 30-minute, and 60-minute thermal wattmeters

1—Thermos bottle
2—Heater resistances
3—Reservoir
4—Insulating tubes
5—Heater resistance leads
6—Felt packing
7—Capillary tube
8—Mass

Now, comparing the two block-interval meters, if we exclude one monthly reading, which for some unknown reason—presumably failure to reset—showed a higher difference than theory can account for (240 per cent), the maximum difference between the two was 27.3 per cent. From this, the difference ranged down to 0 per cent. In only six cases out of about 60 monthly readings did the two block-interval meters give the same indications. The average difference for the entire 60 monthly readings was 10.1 per cent. In some cases number 1 meter indicated the higher value, and in other cases number 2. This difference in indication is, of course, due to "peak splitting" either by number 1 or number 2 meter, or perhaps by both.

Now comparing the thermal-wattmeter readings with the two block-intervals, in only one case out of about 60 monthly readings did all three demand meters indicate the same values. In some cases, the thermal meter indicated higher than either block interval, in some cases, lower than either, and in still other cases, between the two block intervals. (For other comparisons between the two types see references 2, 4, and 6.)

When a public utility deals with its customers' pocketbooks, the degree of inconsistency shown by these Ithaca tests is, in the writer's opinion, intolerable. Accuracy in the measurement of kilowatt hours is and always has been extraordinarily high. Unfortunately, the same cannot be said of the measurement of maximum demand as evidenced by the Ithaca tests.

Advantage of Bourdon Tube Over Bimetal Strip

In the thermal wattmeter of the past, the means by which temperature difference has been indicated has been the bimetal strip. In the year 1924, Chester W. Rice of Schenectady contributed a paper to the AIEE entitled, "Free Convection of Heat in Gases and Liquids—II" (see reference 9). In this paper he showed that when the loss of heat from a hot body occurs by free convection, the rate of heat loss is not directly proportional to temperature elevation, but to the temperature elevation raised to 1.25 power. The writer did not become aware of this important contribution to our technical literature until about 1927. For many years previously, the writer had noted an error in the thermal wattmeter on low-power factors, for which he could give no satisfactory explanation. As soon as Mr. Rice's 1924 paper came to his attention, the reason for this error became obvious at once. In the bimetal-strip type of thermal wattmeter, practically all the heat escapes by free convection. The bimetal-strip type of construction does not lend itself to any means of heat escape except free convection. The only answer, therefore, seems to be to adopt some means of heat escape other than free convection. To accomplish this end, it was necessary to consider means of temperature detection other than the bimetal strip.

The writer therefore decided to try out the Bourdon tube. He had previously experimented with the Bourdon tube, but without success. Beginning about 1928, the writer began experimenting with Bourdon tubes in earnest. By about 1934, the Bourdon tube method of measuring maximum demand had arrived. A complete description of the Bourdon-tube demand wattmeter was contributed to the AIEE in 1935 (see reference 5).

The Constant k and Its Determination

Mathematical expression 2 given above brings into the picture of demand measurement the value of the adjustable constant *k*. There is a direct relationship between this matter of time interval and

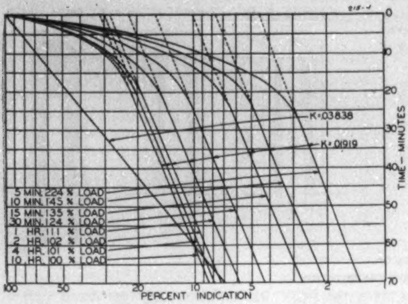

Figure 4. 60-minute thermal wattmeter C, zero-load performance

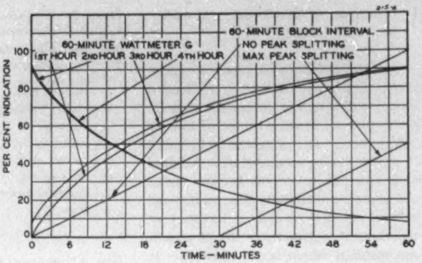

Figure 6. Comparison of 60-minute thermal and block-interval wattmeters

the value of this adjustable constant *k* appearing in mathematical expression 2. The existing Code for Electricity Meters fixes the time interval for thermal-demand meters as the "time required for the instrument to indicate 90 per cent of the full value of the steady load, which is thrown suddenly on it." This provision fixes the value of *kt* in equation 2 at $kt = 2.3026$. This, in turn, fixes the value of *k* at that value which is obtained by dividing 2.3026 by 5, 10, 15, 30, 60, or whatever value of time interval the utility has adopted. Table A gives these values.

Effect of Heat Diffusion on Constant k

When we apply a given constant value of watts *w* to a thermal wattmeter in accordance with the procedure specified in the Code for Electricity Meters and take readings at equal intervals of time, the function we observe is the function $w(1-\epsilon^{-kt})$. The response for the two thermal wattmeters *C* and *G* shown in Figure 2 were obtained in this manner. If, when the meter indication has reached some value w', we throw the load entirely off and observe meter indications as they approach zero, the function we observe is $w'\epsilon^{-kt}$. This, a pure exponential function, plots as a perfectly straight line on semi-log paper. However, when we plot these data from the thermal-demand meter, we find a departure from a straight line, the amount of this departure being controllable by design. Figure 4 shows the performance of 60-minute wattmeter *C* with zero load, and Figure 5 that of 60-minute wattmeter *G*. Examination of Figures 4 and 5 makes it obvious at once that the value of *k* is not constant, but can be made to vary over a very considerable range during the first few minutes of the application or absence of load.

The reason for this anomaly of the variable constant is not far to seek and has already been intimated. Heat cannot be made to diffuse instantly throughout any mass of matter. In the thermal wattmeter, the source of heat is, of course, the heater shown in cross-section in Figure 3. In order to actuate the meter, the heat originating in the heater must reach the reservoirs shown in Figure 3. From the data given in Figures 4 and 5, the time required for the rate of heat flow to become constant varies over a very considerable range. From Figure 4, it is obvious that this rate of heat flow in wattmeter *C* does not become constant for some 20 to 30 minutes. From Figure 5, it is evident that the rate of heat flow for wattmeter *G* becomes constant in considerably less than 10 minutes. About five or six minutes would closely fit the experimental data seen in Figure 5. In other words, after a change in load occurs, *k* is not a constant, but a variable, and does not become a constant for some 5 to 30 minutes, the length of time being subject to meter design.

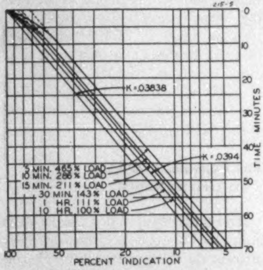

Figure 5. 60-minute thermal wattmeter G, zero-load performance

Table A

Time Interval	60	30	15	10	5
k	0.03838	0.07675	0.1535	0.23026	0.4605

Time Interval—How Long?

What is the proper time interval to use in the measurement of maximum demand? As indicated at the beginning of this paper, United States public utilities are using various time intervals in the measurement of maximum demand. It is obvious that the longer the time interval used in demand measurement, the less is the probability of a steady load during that time interval, and, therefore, the greater is the probability of a difference between the logarithmic and arithmetic averages. The 15-minute time interval is by far the most frequently used. The shorter time intervals have been brought about by the desire on the part of public utilities to obtain adequate compensation from those service users whose loads are inherently of the short-time, high-peak variety, such as welding, short-time heating, shovels for excavation purposes, and so on. It might be pointed out that the thermal-demand meter automatically recognizes the short-time, high-peak demands in this class of loads. Inspection of Figure 1 shows this clearly. The thermal-demand meter—even assuming instantaneous heat diffusion—gives higher demand indications on short-time loads, approximately 2.3 times that of a block interval of the same time rating. The phenomenon of heat diffusion increases this ratio still further.

Examination of the data in Figure 2 shows that if we use 60-minute wattmeter *C*, its indication on short time loads (load durations of a minute or two) is approximately equivalent to a five or six minute block interval. This ability of the 60-minute thermal-demand wattmeter to read high on short-time loads may be still further emphasized by suitable design, if the users of demand meters so desire. It should be pointed out, however, that if the design be so altered as to make its short-time response equivalent to a two- or three-minute block interval, the rate of response for load durations of more than 90 minutes is so slow that it would require some 12 or 15 hours of perfectly steady road to cause the meter to reach 99.9 per cent of its final indication. In the writer's opinion, the modification of the 60-minute thermal wattmeter's design to the point where it is equivalent to a five- or six-minute block interval is as far as the modification should be carried. Even this modification has reduced the permanent value of the constant k to one-half that of the 60-minute thermal wattmeter when heat diffusion is assumed to be instantaneous—$1-\epsilon^{-0.03838t}$. Also, it is highly questionable in the writer's opinion, whether or not wattmeter C, while it meets the meter-code specifications, can honestly be called a 60-minute meter.

A 60-Minute Meter

On the assumption that if the time interval for demand measurement is ever standardized, such standardized time interval would not exceed the maximum time interval now used—60 minutes—the writer has been experimenting with 60-minute thermal wattmeters. The most obvious way of obtaining a 60-minute thermal meter is to add mass to the 15-minute meter, as shown in Figure 3, so that the time to heat this mass up to 90 per cent of its final temperature is increased from 15 minutes to 60 minutes. When a steady load was applied to this 60-minute meter, wattmeter C of Figure 2 shows the test results. It will be noted that when a steady load was applied to this 60-minute wattmeter C, it reached 50 per cent of final value in approximately 4 minutes, 60 per cent in approximately 6 minutes, 70 per cent in approximately 10 minutes, 80 per cent in approximately 26 minutes, and 90 per cent in approximately 60 minutes. The design of the meter was then modified so that the mass to heat and cool during normal operation was reduced to a small fraction of that necessary in meter C. Wattmeter G of Figure 2 shows the test results on this redesigned 60-minute meter. It will be noted that wattmeter G reaches 50 per cent of its final in approximately 17 minutes, 60 per cent in approximately 23 minutes, 70 per cent in approximately 30 minutes, 80 per cent in approximately 42 minutes, and 90 per cent in approximately 60 minutes.

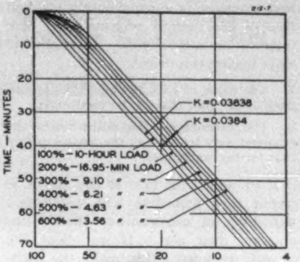

Figure 7. 60-minute thermal wattmeter I, zero-load performance

Let us examine this matter a little further from a purely theoretical standpoint. When we integrate mathematical expression 2 above, the result of this integration is, of course, meter indication $=w(1-\epsilon^{-kt})$. With large values of time (t), ϵ^{-kt} approaches zero and meter indication approaches w (watts). If t=infinity (steady load), meter indication $=w$. If, however, t is of a small value making it necessary to use the limits of integration, meter indication $=\dfrac{w(1-\epsilon^{-kt})}{\text{per cent time}}$. In the foregoing sentence, the adjective "small" may be defined as any value of t (time in minutes) such that the product kt does not exceed 6.9078.

After a change in load occurs, wattmeter C acquires approximately 80 per cent of the value of this change during the 20 or 30 minutes k is a variable. The remaining 20 per cent requires approximately five hours additional time. Wattmeter G acquires approximately 20 per cent of final indication in the five or six minutes that k is a variable while the remaining 80 per cent requires approximately an additional three hours.

Now, examining 60-minute wattmeter G shown in Figure 2, it is obvious that its response is fairly close to the theoretical value given in equation 2. For very short time loads, its rate of response is some 30 per cent or 40 per cent higher than if heat diffusion could be made instantaneous, but this higher rate of response lasts for only five or six minutes. In this meter, the value of the adjustable constant k is close to the theoretical value of k for a 60-minute meter. Tests on a number of these redesigned 60-minute thermal wattmeters have shown a departure in the permanent value of k of not over two per cent or three per cent from the theoretical value—0.03838. Also, its temporary value is much less than in wattmeter C. On short-time loads, load durations of a minute or two, wattmeter G is equivalent to approximately a 20-minute block interval. The design of the 60-minute thermal wattmeter is capable of adjustment to almost anything the public utilities may desire between wattmeters C and G shown in Figure 2. The writer would appreciate an expression of opinion by those using demand meters as to which design shown in Figure 2 they would prefer. Or, perhaps some intermediate design between C and G would be preferable. It must be borne in mind, however, that wattmeter C can be made available for any time interval from 15 minutes up, while wattmeter G is available in 60-minute time interval only.

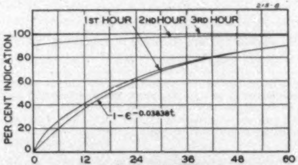

Figure 8. 60-minute thermal wattmeter, three-hour 100 per cent load applied

Statement Re Accuracy

The writer wishes to correct an impression which might be drawn from the preceding discussion. The accuracy of the standard block-interval demand meter in measuring an arithmetic average is as high as human ingenuity can attain, provided we limit this statement to the particular time interval which the meter happens to select. However, we must bear in mind that the particular time interval selected is only one out of an infinite number that might be selected. The writer does not wish his statement concerning the accuracy of the block-interval demand meter to be interpreted in any other manner.

New Data on 60-Minute Thermal Meters

Also, further experimental work has demonstrated that the time interval during which the constant k is a variable can be somewhat reduced below that shown in Figure 5. By taking all possible precautions to make the diffusion of heat as rapid as possible, Figure 7 shows the resulting zero load performance. The test results shown in Figure 7 indicate that the constant k is a variable for a time interval but little over three minutes, even when the load applied is six times full load for 3.56 minutes. Figure 8 shows this meter's performance with full load applied for three hours. The constant k of 60-minute wattmeter I becomes 0.03838 when the two curves shown in Figure 8 become parallel; Figure 8 shows this to occur at approximately three minutes. Also, Figure 7 shows that the permanent value of the constant k departs from the theoretical value by only a negligible amount.

Ammeters Versus Wattmeters

There is one further point that should be considered. Thermal meters are available in both wattmeters and ammeters. These two instruments differ in at least one important respect. The wattmeter scale is uniform throughout its entire range, while the ammeter scale is inherently a scale of squares. Under the existing definition of time intervals, the time interval of both these instruments is defined as the "time required for the instrument to indicate 90 per cent of the full value of a steady load which is suddenly thrown on it." Since the ammeter scale is inherently a scale of squares, this means that the time interval of the thermal ammeter is approximately one-half that of the thermal wattmeter as time intervals are now defined. There are two ways to cure this defect:

1. The value of k for the ammeter may be fixed at one half that of the wattmeter.
2. The percentage of final indication for the ammeter may be fixed at the square root of that for the wattmeter.

If we adopt the first of the above, it means that ammeters and wattmeters will differ in construction, thereby increasing their cost. If we adopt the second, we must either make the wattmeter time interval the time to arrive at 90 per cent of final, as at present, and the ammeter time interval the time to arrive at 95 per cent of final, or we must make the ammeter time interval the time to arrive at 90 per cent of final, as at present, and the wattmeter time interval the time to arrive at 81 per cent of final. It is suggested that those who are responsible for the Code for Electricity Meters give careful attention to this matter and choose one of the above alternatives.

Conclusion

Summarizing, we must obtain the logarithmic average and not the arithmetic average if we would have consistency as well as adequacy in the measurement of maximum demand. Time must appear as an exponential function and not an arithmetic function, if the demand is to be metered in accord with the basic character of the quantity being measured. The writer would also urge that consideration be given to the matter of standardizing the time interval used in demand measurement.

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A New Voltage-Regulating Relay Plus Line-Drop Compensator

H. J. CARLIN
ASSOCIATE AIEE

THERE are numerous applications where it is desired to measure a voltage at one point of a line or feeder by the use of an instrument placed at some other point. The instrument measures the voltage at the point of the line to which it is connected, and also measures the line drop to the remote point. These two measurements are combined so that the resultant indication is the voltage at the remote point of the feeder. If the instrument has contacts on it so that it may actuate control circuits, it is known as a voltage-regulating relay plus line-drop compensator. Such a device finds application to regulating equipment such as tap-changing transformers, capacitor-switching schemes for power-factor control, and the like.

Many of the applications for a voltage-regulating relay require a time delay. Usually such delay has been obtained by the use of timers which interpose a fixed lag between the operation of the relay and the device which is being controlled (for example a tap-changing regulator) independent of the magnitude or rate of voltage change. The relay described below eliminates the use of an external timer, since it has its own inherent time delay. In addition, this time delay is not a fixed quantity but varies inversely as the change in voltage. In this way the sensitivity of the regulating device is increased without introducing a large number of unnecessary operations.

The Voltage-Regulating Relay

An induction-type voltage relay with a permanent damping magnet inherently has an inverse time characteristic; hence, such a relay became the basis for design. Time curves for different voltage settings on an induction voltage relay with front and back contacts are shown in Figure 1.

The two major problems to be solved in the completed design were:

1. To make the temperature error negligible, for applications often call for outdoor installations where ambient temperature may vary over an extreme range of 0 to 110 degrees Fahrenheit.

2. To provide the relay with a self-contained line-drop compensator.

Starting with the final result, we may show how these two problems were solved.

TEMPERATURE ERROR

Figure 2 is a wiring diagram of the relay. Neglecting the compensator winding, the principle of operation of the relay is quite familiar.[1] When the potential winding is energized, the transformer winding on the lower pole feeds current to the upper-pole windings. This current induces an upper-pole flux which is out of phase with the potential flux, and a torque on the disk results which satisfies the equation:

$$T = K\Phi_1\Phi_2 \sin a \qquad (1)$$

where Φ_1 is the lower-pole flux, Φ_2 the upper-pole flux, and a the phase angle between them.

There are two sources of temperature error in such a relay:

1. For constant voltage across the relay, change in ambient temperature produces a change in the resistance of the upper-pole circuit. Hence, both the magnitude of upper-pole flux Φ_2 and the phase angle "a" will change. According to equation 1, the torque will, therefore, vary.

2. Change in ambient temperature may also produce variations in relay impedance so that the current in the potential circuit varies with the temperature even though the voltage remains constant.

In appendix I is a derivation which shows that when upper-pole reactance equals upper-pole resistance, temperature error due to variation of upper-pole impedance is a minimum. Accordingly, the upper-pole winding was designed to have a 45 degree impedance angle.

To correct for the second source of temperature error, a swamping reactor is placed in series with the potential coil. Hence, variations in relay burden have little effect on the total impedance of the voltage circuit, so that the current in the relay remains constant independent of temperature. Experimental tests made in a refrigerator and an oven over a range of temperatures from 0 to 110 degrees Fahrenheit showed an average temperature error of less than 0.018-volt per degree Fahrenheit. This ballast impedance method of correction was chosen so that the reactor could serve a dual purpose. Besides correcting for temperature error, it is an essential part of the line-drop compensator circuit. This function is described below.

THE LINE-DROP COMPENSATOR

The line-drop compensators in general use today employ what amounts to an artificial or replica line. That is, the compensator is a variable resistance and reactance which may be independently adjusted to suit a particular line impedance. These elements are placed in series with the voltage-regulating relay, and a current is fed to this compensator from current transformers in the main line, so that the drop across the resistor and reactor is proportional to the line drop and is subtracted from a voltage proportional to the sending-end potential. Thus, the voltage across the relay actually simulates the voltage at the load center, if the resistance and reactance of the compensator have been correctly adjusted.

The relay described in this paper has no such artificial line. Instead the equivalent of two separate torques is superimposed on the induction disk. One of these is proportional to the sending-end voltage, the other is proportional to the line drop, and the relay combines these so that the resultant torque is proportional to the receiving-end voltage. The former torque component is obtained by impressing on the relay-potential coil a voltage proportional to that at the sending end or regulator location. The torque component due to the line drop is obtained by means of a second upper-pole winding (see Figure 2). Current flowing in this winding from a current transformer in the line induces a flux, which reacts with the main-pole flux to produce a torque on the disk which opposes the main-relay torque.

If the relay is to perform correctly, the compensation torque must satisfy certain conditions which are a function of the electrical properties of the line, the magnitude of the load, and the power factor of the load. Consider a simple line, such as shown in Figure 3a. The vector diagram, Figure 3b, is drawn for a constant magnitude of load, I_L, and a varying load-power factor, θ. ϕ is the impedance angle of the line. This diagram shows clearly that the sending-end voltage, E_S, necessary to maintain constant receiving-end voltage, E_R, with varying power factor, has a maximum magnitude when δ, the angular

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H. J. CARLIN is with Westinghouse Electric and Manufacturing Company, Newark, N. J.

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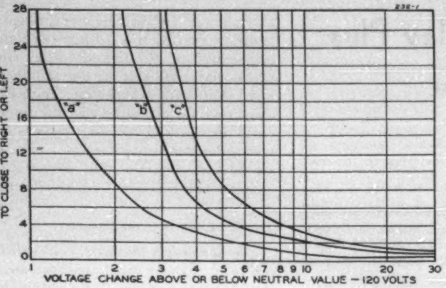

Figure 1. Voltage-regulating-relay time curves

(*a*). Right-hand contact set at 121 volts; left-hand contact set at 119 volts

(*b*). Right-hand contact set at 122 volts; left-hand contact set at 118 volts

(*c*). Right-hand contact set at 123 volts; left-hand contact set at 117 volts

shift between E_S and E_R is zero. This occurs when $\theta=\phi$. In order words, for any given load the relay must produce a maximum of compensation when the power factor of the load is equal to the impedance angle of the line.

What does this mean as far as the design of the relay is concerned? Referring to the general torque equation 1, it is seen that the compensator torque will be a maximum for any given load, when the angle between the compensator-winding flux and the potential-coil flux is 90 degrees. Therefore, a necessary condition for correct compensation is that when the load-power-factor angle equals the line-impedance angle, the compensator flux must be 90 degrees from the main-pole flux.

To see how this condition is translated into the actual structure of the relay, refer to the vector diagram, Figure 4.

Here E_1 is the voltage impressed across the relay terminals, and E_{1G} lags this somewhat due to the leakage drop in the potential coil. Φ_1, the main-pole flux lags E_{1G} by 90 degrees, and for maximum compensation torque Φ_C lags Φ_1 by 90 degrees. The compensator excitation which is proportional to the current in the compensator current transformer, leads ϕ_C by a small angle due to the eddy currents in the disk and the hysteresis loss in the upper-pole iron. Thus, the relay primary voltage, E_1, and the compensator current, i_c, will be very close to 180 degrees out of phase for maximum compensator torque. Actual measurements showed the angle to be 180 degrees ± 3 degrees.

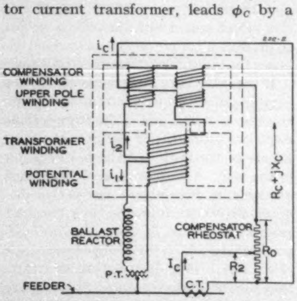

Figure 2. Schematic wiring diagram of voltage-regulating relay

As shown above, if the relay is to compensate correctly, maximum compensation torque must occur when E_S is in phase with E_R, and the load-power-factor angle is equal to the impedance angle of the line. Hence, in Figure 4, E_S is drawn at an angle $\theta=\phi$ ahead of I_L, or $180-\phi$ behind i_c. Then, E_1 must lag E_S by the impedance angle of the line. This means that a phase shifting network must be placed between E_S, the sending voltage, and E_1, the relay primary voltage. If the effective shift of this network is equal to ϕ, the relay will compensate correctly.

In appendix II is an analytical derivation which corroborates the conclusions reached above. The voltage equation for a simple line is set up and compared with the torque equation of the relay, and it is shown mathematically that the necessary and sufficient conditions for correct compensation are that the compensator-winding excitation be proportional to the magnitude of line drop and that the relay primary voltage lag behind the sending voltage by an angle equal to the impedance angle of the line.

This will be clearer if we consider for a moment the line-drop compensators almost universally used at present. These devices must also satisfy two conditions if correct compensation is to result. The compensator-reactance drop must be proportional to the line-reactance drop and the compensator-resistance drop must be proportional to the line-resistance drop. To get this proportionality for different lines, the compensator has an adjustable resistor and an adjustable reactor. In the new relay we fit the two line constants by having one adjustment for the impedance of the line in ohms, and another for the phase angle of the line impedance. In other words, we compensate for $Z\underline{/\phi}$, instead of $R+jX$. In both cases there must be two variables of adjustment to match two line constants.

The phase-angle adjustment in this new relay is provided by a phase-shifting network. Theoretically, this should be adjustable to vary the phase position of the output voltage, while keeping the magnitude of output voltage constant. But to obtain such results requires relatively complex networks. However, as will be shown later, a great simplification in design is possible.

Figure 2 shows the circuit for obtaining magnitude adjustment. This consists of a potentiometer which is connected to the line current transformer and the compensator winding so that as the slide is moved, the magnitude of current in the compensator varies while the phase angle of the current stays constant. This is easily seen to be true, for if I_c is the current in the transformer secondary, then the compensator current is given by

$$i_c=I_c\frac{R_2}{R_0+R_c+jX_c} \tag{2}$$

in which the various quantities are as indicated on Figure 2. The denominator of equation 2 is a constant, hence only the magnitude of i_c varies with R_2.

The rheostat may be calibrated in line ohms, or volts line drop. The latter method is actually used on the relay.

Settings

It is usually desirable that voltage-regulating relays be as inexpensive and simple in operation as possible, consistent with a minimum sacrifice in operating features. For this reason it is desirable to simplify the relay described in the pre-

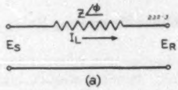

Figure 3a. Schematic single-phase feeder

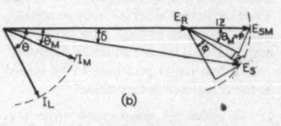

Figure 3b. Vector diagram of feeder

θ_M—Power factor for maximum line-drop compensation

I_M—Load current for maximum line-drop compensation ceding paragraphs, even though this might mean some limitations in operating performance.

The part of the relay most susceptible to simplification is the phase-shifting network of the line-drop compensator. As shown above, this network should provide adjustable voltage phase-angle shift without affecting the magnitude of voltage. Circuits to operate in this manner can be constructed with inductors, resistors, and capacitors. The finer the adjustments, the larger the number of elements required and the more difficult the calibration. Such networks were experimentally constructed and they operated satisfactorily.

The simplified network consisted of the single reactor used also as ballast impedance for temperature correction. Obviously, once the reactor is chosen, the phase-angle adjustment is fixed, and the relay is theoretically adapted for only one line. Nevertheless, it is possible to take a relay so constructed and apply it to a wide range of lines without excessive error in voltage regulation. Suppose the reactor has been designed so that when placed in series with the potential coil of the relay the angle between the impressed voltage and the voltage across the relay is 40 degrees. This means that when used on a 40-degree line, the relay operates correctly for any load and any power factor, if the compensator-voltage scale is set for the true magnitude of full-load line drop. Now, suppose we place this 40-degree relay on a line whose impedance angle is 70 degrees. The relay would no longer give correct voltage indications if the compensator-voltage scale is set for the true magnitude of full-load line drop. However, for any given power factor, a setting of the voltage scale can be found which makes the relay operate correctly for any load *at that power factor*. This setting in volts will obviously not be equal to the true line drop. We define this compensator setting, in volts, divided by the true line drop, in volts, as the "compensator-correction factor." In effect, an intentional error in the compensator-voltage setting is introduced to correct for the error in the relay-phase-angle setting.

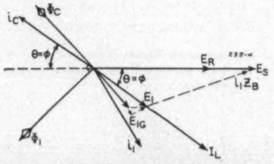

Figure 4. Vector diagram of voltage-regulating relay drawn for maximum compensation torque

Figure 5 is a family of curves to show how this factor varies as a function of load power factor when a 40-degree relay is used on various lines. Forty degrees was chosen because it is the impedance angle of the average low-capacity line.

The derivation of the correction factor curves is shown in appendix III. In order to verify these curves experimentally, apparatus was set up to simulate a line whose impedance angle could be varied, and a load whose power factor could be varied. Agreement between experimental and theoretical curves was very close.

A glance at the correction factor curves shows what errors may be expected in operating the relay on lines other than 40 degrees. Suppose we have a 60-degree line where the power factor varies from 80 to 90 per cent. The correction factor for 80 per cent is 0.91, for 90 per cent it is 0.85. The average setting would then be 0.88 and the variation plus or minus 0.03. Thus, if the line drop were 10 volts, the relay-compensator scale would be set at 0.88×10=8.8 volts and the maximum error plus or minus 0.3 volts. Obviously, the narrower the range of power factor, the smaller would be the relay-voltage errors. Also, the closer the line-impedance angle is to 40 degrees, the smaller is the error. As is the case with the compensators in use at present, calculated settings are only tentative, and final settings are usually chosen on the basis of field tests.

Conclusions

Due to the inverse time characteristic and integrating properties of the relay described above, circuits controlled by it are operated after a relatively short time delay when voltage conditions on the line require this. However, unnecessary operations are prevented when short-interval voltage surges occur. At the same time the relay is simplified to have only one adjustment for line-drop compensation without introducing excessive errors in indicated voltage.

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Appendix I. Symbols

E_S = sending-end voltage
E_R = receiving-end voltage
E_1 = voltage impressed on relay-potential coil
e_2 = voltage across N_2
T_c = compensator-torque component
T_S = sending-voltage-torque component
Φ_1 = flux due to potential-coil excitation
Φ_2 = induced upper-pole flux
Φ_c = flux due to compensator-winding excitation
i_1 = potential-coil current
i_2 = induced upper-pole current
i_c = compensator winding current
I_c = current-transformer secondary current
I_L = line current
N_1 = potential-coil turns
N_2 = turns of transformer winding on lower pole
N_3 = turns on each upper pole
N_c = turns per pole of compensator winding
$Z_2 = R_2 + jX_2$ = upper-pole impedance
Z_B = ballast impedance
$Z_c = R_c + jX_c$ = compensator impedance
R_2 = portion of compensator resistance across line current transformer
R_0 = total resistance of compensator
a = angle between Φ_1 and Φ_2
b = upper-pole impedance angle
θ = load-power-factor angle
ϕ = impedance angle of line
δ = angular shift between E_S and E_R
F = correction factor
β = angle between ϕ_1 and ϕ_c

Appendix II. Derivation for Minimum Temperature Error

By equation 1

$$T_S = K\Phi_1\Phi_2 \sin a$$

Referring to Figure 6

$$\sin a = \sin(90 - b) = \cos b$$

Therefore

$$T_S = K\Phi_1\Phi_2 \cos b$$

$$= K\Phi_1\Phi_2 \frac{R_2}{\sqrt{R_2^2 + X_2^2}}$$

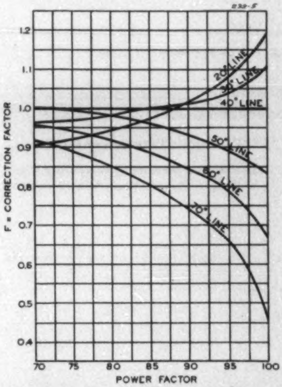

Figure 5. Correction-factor curves for 40-degree voltage-regulating relay. See appendix IV

Now

$$\Phi_2 = ci_2 = \frac{ce_2}{Z_2} = \frac{k}{\sqrt{R_2^2+X_2^2}}$$

where c and k are constants.
Or

$$T_S = \frac{kK\Phi_1 R_2}{R_2^2+X_2^2} = \frac{AR_2}{R_2^2+X_2^2}$$

Differentiate this expression with respect to R_2 to find the value of R_2 which results in no change in T_S for small changes in R_2.

$$\frac{dT_S}{dR_2} = 0 = \frac{A}{R_2^2+X_2^2} - \frac{2AR_2R_2}{(R_2^2+X_2^2)^2}$$

where A is a constant.
Solving

$$R_2 = X_2$$

This is the condition for minimum temperature error.

Appendix III

Derivation to prove that the relay performs correctly if:

(*a*) The compensator-winding excitation is proportional to the magnitude of line drop.

(*b*). Maximum compensator torque occurs when the line-impedance angle ϕ, equals the power-factor angle θ. That is, β, the angle between Φ_c and Φ_1 shall be 90 degrees when $\theta = \phi$.

1. The Voltage Equation for the Line

Refer to Figure 3b and take E_S as reference vector.

$$\begin{aligned} \boldsymbol{E_R} &= \boldsymbol{E_S} - \boldsymbol{I_L Z} \\ &= E_S\underline{/0} - I_L\underline{/\delta-\theta}\cdot Z\underline{/\phi} \\ &= E_S\underline{/0} - I_L Z/\overline{\theta-\delta-\phi} \end{aligned}$$

Let

$$\theta-\delta-\phi = \gamma$$

Then

$$\boldsymbol{E_R} = E_S - I_L Z\cos\gamma + jI_L Z\sin\gamma$$

or

$$|E_R| = \sqrt{E_S^2 - 2I_L Z E_S\cos\gamma + (I_L Z)^2} \quad (3)$$

2. The Torque Equation of the Relay

The two components of relay torque are T_S and T_c. The resultant relay torque is T.

$$T = T_S - T_c$$

$$T_S \,\alpha\, \Phi_1\Phi_2 \sin a \,\alpha\, \frac{N_1^2}{N_2}\cdot i_1^2\cdot \sin a \,\alpha\, \frac{N_1^2}{N_2}\cdot \sin a\cdot E_S$$

Since

$$i_1 \,\alpha\, E_S$$

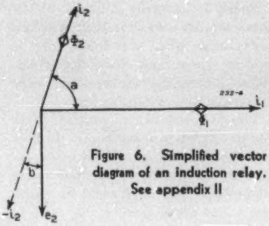

Figure 6. Simplified vector diagram of an induction relay. See appendix II

therefore

$$T_S = kE_S^2 \quad (4)$$

Since $(N_1^2/N_2)\sin a$ is a constant

$$T_c \,\alpha\, \Phi_c\Phi_1 \sin\beta$$

$$T_c = CI_cE_S\cos(\beta-90)$$

where C is a constant
Since $I_c \alpha \Phi_c$ and $\Phi_1 \alpha E_S$, therefore

$$T = kE_S^2 - CI_cE_S\cos(\beta-90) \quad (5)$$

The relay is initially calibrated with the compensator scale set for zero volts compensation, so that if E_R' is the scale reading of the relay, equation 4 applies and we have

$$|E_R'| = \sqrt{T/k}$$

Substituting for T from equation 5

$$|E_R'| = \sqrt{E_S^2 - KI_cE_S\cos(\beta-90)} \quad (6)$$

Equations 6 and 3 are identical (if we neglect the term $(IZ)^2$ which is of the second order) when

(*a*). $KI_c = 2I_L Z$

(*b*). $(\beta-90) = \gamma = \theta-\delta-\phi$

That is, when $\theta = \phi$, and $\delta = 0$; $\beta = 90°$.

This is what we have set out to prove for (*a*) states that KI_c is proportional to the line drop, and (*b*) states that the angle between Φ_c and Φ_1 is 90 degrees when $\theta = \phi$.

Appendix IV. Derivation of Correction-Factor Curves

1. Assume the relay is adjusted to compensate for a line of impedance angle ϕ' and a voltage drop $(I_LZ)'$. The true line drop (I_LZ), and the true line angle is ϕ. Then, for a load power factor θ, the relay develops a torque in accordance with the voltage equation

$$E_S/\overline{\delta} = E_R\underline{/0} + (I_LZ)'\underline{/\phi'-\theta} \quad (7)$$

E_R is taken as reference vector, and is the required receiving-end voltage.

2. The true line angle is ϕ, and the true line drop I_LZ. Then, since E_S is the head-end voltage of the line determined by the relay, the voltage equation of the line is given by

$$E_S/\overline{\delta} = E_{R_0}\underline{/\alpha} + I_LZ\underline{/\phi-\theta} \quad (8)$$

Where E_{R_0} is the actual voltage at the end of the line.

In the equations below $(I_LZ)'$ is determined as a function of the line and relay constants so that $E_{R_0} = E_R =$ the required receiving end or load center voltage.

Equate (7) and (8)

$$E_R\underline{/0} + (I_LZ)'\underline{/\phi'-\theta} = E_R\underline{/\alpha} + (I_LZ)\underline{/\phi-\theta}$$

Solve for (I_LZ)

$$(I_LZ)'\underline{/\phi'-\theta} = E_R\underline{/\alpha} - E_R\underline{/0} + (I_LZ)\underline{/\phi-\theta}$$

$$(I_LZ)'\sin(\phi'-\theta) = E_R\sin\alpha + (I_LZ)\sin(\phi-\theta)$$

$$(I_LZ)'\cos(\phi'-\theta) = E_R\cos\alpha - E_R + (I_LZ)\cos(\phi-\theta)$$

Divide these equations to eliminate $(I_LZ)'$, simplify and solve for (I_LZ)

$$(I_LZ) = E_R\frac{\sin\alpha - [\tan(\phi'-\theta)\cdot(\cos\alpha-1)]}{\cos(\phi-\theta)\tan(\phi'-\theta) - \sin(\phi-\theta)}$$

Solve for $(I_LZ)'$

$$(I_LZ)\underline{/\phi-\theta} = E_R\underline{/0} + (I_LZ)'\underline{/\phi'-\theta} - E_R\underline{/\alpha}$$

$$(I_LZ)\sin(\phi-\theta) = (I_LZ)'\sin(\phi'-\theta) - E_R\sin\alpha$$

$$(I_LZ)\cos(\phi-\theta) = (I_LZ)'\cos(\phi'-\theta) - E_R\cos\alpha + E_R$$

Divide these equations to eliminate (I_LZ), simplify, and solve for $(I_LZ)'$

$$(I_LZ)' = \frac{E_R\cdot\tan(\phi-\theta)\cdot(\cos\alpha-1) - \sin\alpha}{\cos(\phi'-\theta)\tan(\phi-\theta) - \sin(\phi'-\theta)}$$

$$F = \frac{(I_LZ)'}{(I_LZ)} =$$

$$\frac{[\cos(\phi-\theta)\tan(\phi'-\theta) - \sin(\phi-\theta)]\cdot[\sin\alpha - \tan(\phi-\theta)(\cos\alpha-1)]}{[\cos(\phi'-\theta)\tan(\phi-\theta) - \sin(\phi'-\theta)]\cdot[\sin\alpha - \tan(\phi'-\theta)(\cos\alpha-1)]}$$

For line drops of the order of 10 per cent δ is very small and hence α must be even smaller.

Therefore

$$\sin\alpha = \alpha$$
$$\cos\alpha = 1$$

or

$$F = \frac{\cos(\phi-\theta)\tan(\phi'-\theta) - \sin(\phi-\theta)}{\cos(\phi'-\theta)\tan(\phi-\theta) - \sin(\phi'-\theta)}$$

The curves on Figure 5 are graphs of F versus θ for various line-impedance angles, ϕ. $\phi' = 40$ degrees.

A Static Voltage Regulator Insensitive to Load Power Factor

C. M. SUMMERS
MEMBER AIEE

T. T. SHORT
NONMEMBER AIEE

THE voltage regulator described in this paper is intended for those applications where the operating efficiency is of secondary importance to other requirements. Excluding the consideration of efficiency, the output voltage of an ideal voltage regulator should be of sinusoidal wave form and should remain constant with any variation in input voltage and frequency and any variation in load volt-amperes and power factor.

These are ideal requirements, and a regulator has not been designed which will fulfill them completely. Several types of static regulators have been available in recent years, but all of them have had one or more serious defects. The more serious of these defects are sensitivity to frequency and power-factor variations and slow speed of response. Since most voltage sources have good frequency regulation, the power-factor sensitivity has been the most prevalent defect in previous regulators. Because of this, a given regulator has had a limited flexibility in its application.

The voltage regulator described in this paper has the important distinction fundamentally of being independent of load power factor. It will meet all of the other requirements except frequency variations of an ideal regulator within limits of which can be summarized as follows:

The regulator can be designed to maintain an output voltage which will not vary more than ±2½ per cent for a simultaneous variation of 30 per cent in line voltage, 100 per cent in load, and any desired range of power factor. For fixed loads, the regulation can be held to less than ±0.5 per cent for 30 per cent variation in line voltage. The regulator will re-establish the load voltage within three cycles after a sudden change of 30 per cent in line voltage or 100 per cent in load is imposed on it. A variation of one-half cycle above or below the rated frequency will change the level of output voltage but will not materially affect the regulation of the new frequency. The wave form at or near full load will have a maximum harmonic content of about 6 per cent, and at open circuit its harmonic content reaches a maximum of 20 per cent. The efficiency of a one-kilovolt-ampere regulator or larger is 90 per cent or higher at full load.

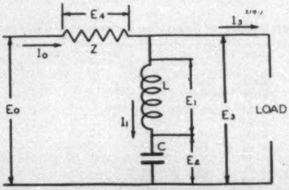

Figure 1. General circuit diagram for voltage regulator

Fundamental Circuit

The fundamental circuit employed in the regulator is simple and consists of a reactor and capacitor connected in series and shunted across the load as in Figure 1. The reactor has its individual characteristics, however, and these must be properly correlated with those of the capacitor. The reactor contains a special magnetic circuit[1] employing a bridge gap which will produce the volt-ampere curve E_1 in Figure 2. The capacitor is chosen so that its volt-ampere curve E_2 will be parallel to that of E_1. In an ideal circuit where L and C are pure inductance and capacitance, the voltage E_3 at the extreme terminals of the series circuit will be the numerical difference between the inductive and capacitive voltages. In Figure 2, therefore, the voltage E_3 can be represented by the vertical distance between the two volt-ampere curves, and this will remain constant between values of current represented by i_1 and i_1'. (In the following discussion capital letters are used to represent general values while lower-case letters represent minimum values and lower-case letters with primes ['] represent maximum values.) The permissible variation in series current is utilized in a series impedance Z in Figure 1 to compensate for changes in line voltage and load characteristics.

Assume, for example, an ideal circuit as in Figure 1 with the series impedance considered as pure resistance. Assume also that the circuit is operated at no load on a line voltage of some value which causes the minimum current of i_1 to flow through L and C. The vector diagram for this case is given in Figure 3 for which the following equations apply considering that $I_1 = I_0$.

$$e_0^2 = e_3^2 + (i_1Z)^2 \tag{1}$$

$$(e_0')^2 = e_3^2 + (i'Z)^2 \tag{2}$$

Subtracting equation 1 from equation 2

$$(e_0')^2 - e_0^2 = (i_1'Z)^2 - (i_1Z)^2 \tag{3}$$

Thus as the line voltage increases, I_1 increases to a value where the impedance drop E_4, (i_1Z), exactly compensates for the increase in line voltage. If the line voltage continues to increase, I_1 may exceed its limiting value of i_1' and the load voltage represented by the vertical distance between E_2 and E_1 in Figure 2 will decrease. This calls for a further increase in E_4 and a still higher value of I_1. Thus the load voltage drops very rapidly when the limits of I_1 are exceeded.

Now consider the same circuit operating on a constant line voltage and a variable resistance load. The vector diagram in Figure 4 shows the relation between I_1 and load current where it is important to note the minimum value of I_1 concurs with the maximum load current and that I_1 increases as the load decreases. The change in I_1 is dictated again by the amount of voltage drop in the series impedance Z necessary to compensate for the change in drop caused by the change in load current. In general the following vectorial equations apply:

$$E_0 = I_0Z + E_3 = E_4 + E_3 \tag{4}$$

and

$$I_0 = I_1 + I_3 \tag{5}$$

The load current may have any value and phase angle with respect to E_3; and E_0 may fluctuate over any range, with the only restriction being that the limits of

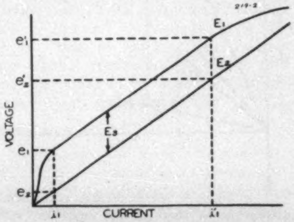

Figure 2. Volt-ampere curves of nonlinear reactor E_1 and associated capacitor E_2

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C. M. SUMMERS is in the works laboratory of General Electric Company, and T. T. SHORT also is with General Electric Company, both in Fort Wayne, Ind.

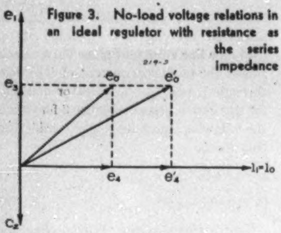

Figure 3. No-load voltage relations in an ideal regulator with resistance as the series impedance

I_1 are i_1 and i_1' as prescribed by Figure 2. If the load current becomes too high I_1 drops below the limiting value of i_1, and the voltage E_3 drops rapidly.

The characteristics of the series impedance Z materially affects the permissible range over which the various quantities are permitted to vary. A constant current type of series impedance shown in Figure 5 has several advantages. This circuit consists of a reactor and capacitor, connected in parallel, with volt-ampere relations similar to those of Figure 2. The line current is the difference between the capacitor and reactor currents at any value of their common voltage and can be represented by the horizontal distances between the two curves. It can be shown[1] that this circuit will maintain a constant current over a range of voltage from e_4 to e_4' which on Figure 2 would correspond to the voltages e_1 and e_1'. Thus another set of limits for correct operation must be added to those previously mentioned. The voltage drop E_4 across the parallel capacitor and reactor must remain within the limits of e_4 and e_4' over which there is a constant horizontal distance between the volt-ampere curves.

Analysis of the Practical Circuit

An accurate analysis of the regulator circuit must consider the actual power factors of all of the reactors and capacitors in the circuit. A mathematical solution for the operating range can be established, but in practice, a graphical solution from a simplified vector diagram is quicker and accurate enough for design purposes. Hence a discussion of the vector diagram only will be attempted.

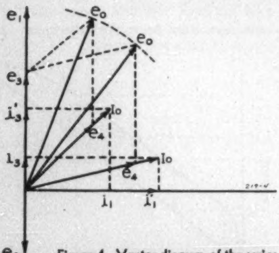

Figure 4. Vector diagram of the series portion of an ideal voltage regulator, with resistance as the series impedance, at constant line voltage and variable load

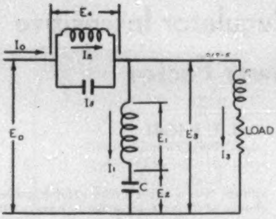

Figure 5. Complete circuit diagram of the preferred arrangement of the voltage regulator

The complete diagram is given in Figure 6. In addition to equations 5 and 6, the following vectorial relations are needed.

$$I_0 = I_2 + I_5 \tag{6}$$

$$E_3 = E_1 + E_2 \tag{7}$$

The diagram shows all of the currents and voltages in their proper phase relations for a general load. The power factor of the reactors changes over their operating range which complicates an accurate vector analysis. However, experience has shown that average values of α and ϕ are accurate enough for the graphical calculations.

A simplified diagram may be constructed by using average values for α and ϕ. The vectors E_1, E_2, I_2, and I_5 may be omitted, and the voltage E_4 can be represented by a closing vector between E_3 and E_0. The diagram must include the range of the important variables if it is to be useful. Hence, it is convenient to represent the limits of E_4 by the arcs of two circles having the end of E_3 as their common center. The line-voltage vector will change in direction because I_1 has been taken as the reference axis, but its limits can be established by the arcs of two circles with their common centers at the origin. The locus of the constant line current is the arc of a circle, and the limits of I_1 can be indicated on the reference axis. A diagram incorporating these limits as well as variations in load is shown in Figure 7.

The actual design of a voltage regulator is based on a diagram similar to Figure 7. The variation in line voltage and the variation in load are prescribed by circumstances of the application. The limits of I_1 and E_4 are established by the parallelism of the volt-ampere curves and, under practical conditions, may cover ranges of 3 to 1 and 2 to 1 respectively. After a few adjustments of I_0, the values of I_1 and E_4 can be made to fall within their respective limits. Then, since the numerical values of line voltage and load current are known, all of the other currents and voltages can be scaled off of the diagram. A comparison between calculated and measured values of regulator currents and voltages is given in Table I.

Fundamentally, it is possible to design a regulator to operate any load over any range of power factor, leading or lagging; but, it is more economical to supply inductive power-factor correction for leading loads and limit the design of the

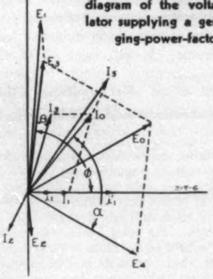

Figure 6. Complete vector diagram of the voltage regulator supplying a general lagging-power-factor load

Table I. Comparison of Calculated and Measured Values of Regulator Currents and Voltages

	I_0		E_4		I_1		E_3			
E_0	Calculated	Measured	Calculated	Measured	Calculated	Measured	Calculated	Measured	I_3	Load Power Factor
95	3.9	4.0	120	118	2.5	2.75	90	89.5	2.77	1.0
115	3.9	4.1	149	146	2.5	2.80	90	90.0	2.78	1.0
130	3.9	4.1	167	162	2.5	2.73	90	89.8	2.78	1.0
95	3.9	4.1	150	144	1.6	1.88	90	90.3	2.78	0.8
115	3.9	4.0	168	166	1.6	1.75	90	90.6	2.79	0.8
130	3.9	3.8	190	182	1.6	1.58	90	90.4	2.78	0.8
95	3.9	4.07	184	170	3.9	4.07	90	87	0	
115	3.9	3.7	204	196	3.9	3.7	90	90	0	
130	3.9	3.3	220	212	3.9	3.3	90	92	0	

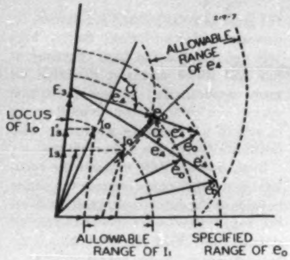

Figure 7. Design vector diagram

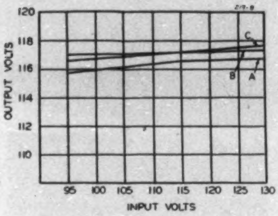

Figure 8. Voltage regulation curves

A—Open circuit
B—Full-load unity power factor
C—Full-load 0.8 power factor lagging

regulator to lagging loads. The reason for this is that an increase in power factor in a lagging direction reduces the value of current I_1, while an increase in a leading direction increases the value of I_1 beyond that required at no load. Thus, the capacity of every element of the circuit would have to be increased if the regulator were designed for both leading and lagging loads.

Characteristics

General

The equivalent characteristic of L and C in the series circuit is inductive, and the vector sum of its current I_1 and the load current must be constant. Therefore, I_1 will decrease for both an increase in load and an increase in lagging power factor.

The magnitudes of all quantities except E_4 in Figure 7 are independent of line voltage, but their phase angles with respect to the line voltage may change. Between full load and open circuit there is as much as a 90-degree phase shift between input and output voltage.

Stability

Stability of the regulator over its intended range of operation depends on the voltage relation between the series portion of the circuit and the line voltage, that is E_3 and E_0. If the vector E_3, in Figure 7, extends beyond the arc of minimum line voltage, the vector E_4 will intersect this arc at two points. Two values of E_4 will, therefore, satisfy the voltage triangle and the regulator may become unstable. Stable operation can be assured by establishing the value of E_3 somewhat less than the minimum value of line voltage.

Input Power Factor

The angle between E_0 and I_0 in Figures 6 and 7 is the power-factor angle on the line side of the regulator. This angle is leading for all values of load and line voltage. As the input current, I_0, is constant and independent of loading, the input volt-amperes will be proportional to the line voltage E_0. Therefore, as the load is decreased, the input power factor rapidly decreases until at no load the regulator becomes virtually a capacitor.

The input volt-amperes for a typical unit is approximately 150 per cent of the rated output at an average value of E_0.

Harmonic Content

The distortion of the output voltage wave shown in Figure 10a and b is caused primarily by a third harmonic. The analysis in Table II shows the comparison between input and output voltage for several harmonics. While the third harmonic is predominant, it reduces materially with load, whereas other harmonics are nearly independent of load.

Regulation

The curves in Figure 8 show the variation in load voltage for several conditions. This variation depends upon the accuracy of adjustment of the volt-ampere curves of the reactors. Since the curve E_1 is not a perfectly straight line between i_1 and i_1', the regulation is dependent also on the range over which I_1 is permitted to vary.

Table II. Harmonic Analysis of Regulator Voltage in Per Cent of Fundamental

Order of Harmonic	Line Voltage	Regulator Output Voltage: No Load	Regulator Output Voltage: Full Load Unity Power Factor	Regulator Output Voltage: Full Load 0.8 Power Factor
1	100	100	100	100
3	0.11	23.4	5.9	11.5
5	1.10	1.6	1.5	1.75
7	0.22	0.17	0.2	0.15
17	0.10	0.67	0.55	0.60
19	0.05	0.57	0.50	0.57

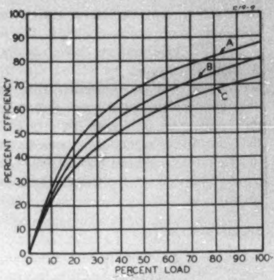

Figure 9. Efficiency curves

Unity-power-factor load
A—500 volt-ampere regulator
B—250 volt-ampere regulator
C—100 volt-ampere regulator

The effective value of output voltage is altered slightly by the change in wave form. It would change by three per cent from no load to full load on the basis of the harmonic content given in Table II. Thus considering the over-all regulation, the distortion does contribute an appreciable part.

The regulation can be improved by an adjustment in the volt-ampere curves of the parallel part of the circuit in Figure 5 so that I_0 decreases at the higher values of E_4.

Effects of Frequency

The effect of frequency on the operation of the regulator can be understood clearly by a reference to Figure 2. The volt-ampere curves are parallel for only one frequency, and, in general, the output voltage varies as the square of the frequency. In addition, the voltage regulation may be positive or negative depending on direction in which the frequency may change.

Efficiency

From the vector diagram in Figure 7 it can be seen that, as the load is decreased, the voltage increases across the various reactors and capacitors in the circuit. As a result, the losses are higher at no load than at full load, the no-load losses being approximately double the full-load losses. The general efficiency curves are shown in Figure 9.

Self-Protection

Mention has been made of the fact that as the load is increased above normal, the current I_1 in the series part of the regulator decreases. This is an important factor, because it guarantees that the regulator cannot be overloaded. When the load current begins to exceed its maximum limit, I_1 drops below i_1, Figure 2, and the load voltage drops rapidly. A short circuit can be placed directly across the output terminals with no damaging effect whatever, as shown by the oscillogram in Figure 10c. In fact the losses in the regulator decrease under short circuit conditions because only the parallel portion of the circuit remains effective.

SPEED OF RESPONSE

The oscillograms in Figure 10 show how rapidly the regulator restores normal voltage when a sudden change in line voltage or load is imposed on the circuit. A transient of about three cycles occurs during which the load voltage may rise or fall about ten per cent.

SIZE

The size and equivalent kilovolt-amperes of the regulator depend on the range over which it has to operate. For a typical unit which operates over a range of 30 per cent variation in line voltage, 100 per cent variation in load kilovolt-amperes and from unity power factor to 80 per cent lagging power factor the size and equivalent kilovolt-amperes will be approximately seven times that of a standard transformer of the same rating. If the range is reduced to 20 per cent line-voltage variation 50 per cent variation in load kilovolt-amperes and from unity to 90 per cent power-factor variation, the size and equivalent kilovolt-amperes will be reduced to approximately four to one.

Conclusions

There has been presented in this paper a new form of static voltage regulator which can be separated into two parts. The first is a series circuit consisting of a capacitor and reactor which, over a predetermined range of current passing through it, will maintain a constant output voltage regardless of load or power factor. The second consists of a circuit containing a capacitor and reactor in parallel which, over a specified range of voltage drop, will maintain a constant current regardless of the line voltage. Thus, the two elements in conjunction convert variable potential to constant current, then to constant potential.

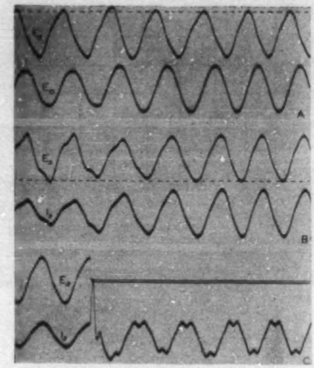

Figure 10. Voltage-regulator oscillogram

A—Transient in output voltage E_3 when line voltage E_0 is suddenly increased from 100 to 125 volts

B—Transient in output voltage E_3 when load current I_3 is suddenly increased from one-half load to full load

C—Transient in load current I_3 when a short circuit is placed on a regulator operating at full load

The regulator has many characteristics such as good voltage regulation, independent of load power factor, high speed of response and high efficiency at full load which makes it desirable for many applications. Its self protection against overloads makes it an ideal source of supply for filaments where the cold resistance approaches a short circuit.

In three-phase delta systems the regulator is unsatisfactory because the phase position of the output voltage depends on the load. However, in wye-connected systems three single-phase regulators can be used in each line to neutral.

The application of this regulator is necessarily limited by its size and cost. In general it will apply where some variation in load and power factor is expected and one or more of the following requirements are essential:

(*a*). High speed of response to eliminate the effect of transients.

(*b*). Very close regulation at any fixed load.

(*c*). Freedom from moving parts.

(*d*). Complete self-protection.

(*e*). Restricted overload capacity (maximum output current may be limited to as low as 125 per cent of rated current).

Sudden changes in line voltages of relatively short duration commonly occur on lines to which motors are connected, and these surges are passed through many types of voltage regulators. The three-cycle response speed of the regulator described above greatly attenuates these voltage fluctuations. This is an important factor in many applications for electronic devices and for testing purposes.

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Multichannel Carrier-Current Facilities for a Power Line

P. N. SANDSTROM
ASSOCIATE AIEE

G. E. FOSTER
ASSOCIATE AIEE

Synopsis: A carrier-current system providing a relatively large number of channels is in service on a high-voltage power line. The facilities provide:

1. Transmission of signals both ways for the opening of the circuit breakers at the remote end by the operation of relays at the near end.

2. Two-way telephone service between any extension telephone at the remote generating station and any telephone connecting to the main telephone switchboard in the central office building.

3. Two-way telemetering (station generation one way and system total generation the other way).

4. Transmission from the system operator's office to the station of automatically generated load-control signals used to increase or decrease automatically the station generation.

5. Two channels in each direction for future requirements.

This paper describes the methods employed to obtain these channels over a single power line, including the special precautions taken with the high-speed transferred trip signals to prevent faulty tripping of the line due to external disturbances which might generate carrier-current tripping pulses.

The Carrier-Current Circuits

A carrier-current system providing 15 channels on a single high-voltage transmission line is now in service. The transmission line connects directly to the transformers at each end, and circuit breakers are provided only on the low-voltage sides of the transformers. The carrier-current channels are used for high-speed transferred tripping, telemetering, load control, and telephone.

Normally just a few carrier-current channels are required on a power line. If more than about four channels are required, many problems arise because of the need of guard bands, the practical limit of two-carrier frequencies per coupling capacitor, complexities of wave traps for separating frequencies, and so on.

The 15 channels are obtained as follows: One carrier frequency is used for single-frequency duplex telephone service since standard equipment is available to perform this function. Two carrier frequencies are established and kept in operation at all times, one in each direction over the line, for the signaling carrier system. These carriers are modulated with the requisite number of audio tones required for the other services. At the receiving point, the audio tones are separated and used to operate relays or other suitable devices. Figure 1 shows a photograph of the equipment required at one terminal for the telephone, eight audio channels transmitted and six audio channels received.

The above plan requires three carrier frequencies. The tuning and line circuits for these frequencies as well as the power frequency are shown in Figure 2.

One will notice that the circuit for each frequency consists of two phase conductors and a grounded center tapped output transformer. The path of the carrier current may be over the two phase conductors, or one phase may be opened or grounded and a path will then exist between the other phase conductor and ground. In this way work may be performed on the line without interrupting the carrier-current circuits. In addition, double-frequency wave traps are interposed between the line grounding switches and the coupling capacitors so that the line may be grounded without disturbing the carrier-current path.

Each coupling capacitor couples two frequencies to each phase conductor. In order to segregate frequencies, separate line-tuning coils and wave traps are provided. At each terminal each carrier frequency is selected by two line-tuning coils, two wave traps, and a matching transformer.

Relay or Signaling Carrier System

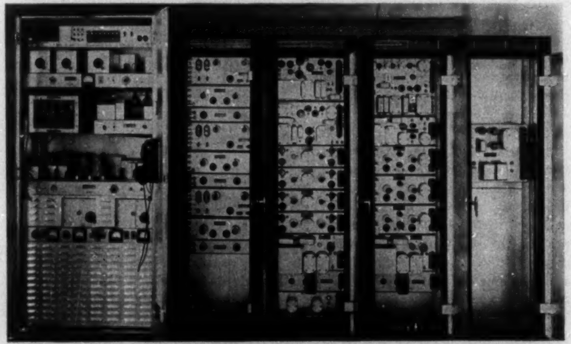
A block diagram of the equipment required for the signaling carrier system is shown in Figure 3. The upper portion of the diagram shows the transmission system and the lower portion the receiving system. Signaling circuits operate the audio-tone generators numbered 1 to 8. The signal may either start or stop the oscillator, as may be required. However, it requires about three milliseconds for the audio frequency to either build up or decay, and, therefore, this limits the speed with which the tone can be controlled. In Figure 4 is shown the circuit of one of these audio oscillators and its amplifier.

The frequency stability of the oscillator must be reasonably good. The oscillator is tuned to the desired frequency by means of small fixed condensers and a coil with a movable powdered iron core. The core is adjusted by means of a screw to give a fine control of the frequency.

The outputs of the various oscillators are added by connecting the output transformers in series with the modulator. This must always be borne in mind, since opening a circuit at one point or even removing an amplifier tube will remove all the audio frequencies. In setting the frequencies of these tone generators, the frequencies must not be harmonics of one another nor should they be within 30 per cent of one another. A study of these

Figure 1. Carrier-current equipment set up for test

From left to right, the cabinets contain the following units: telephone, line tuning, relay transmitter, relay receiver, and transferred trip control



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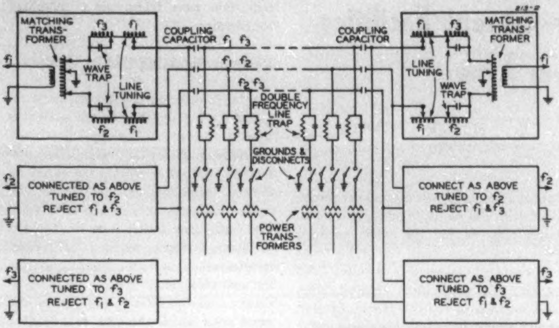
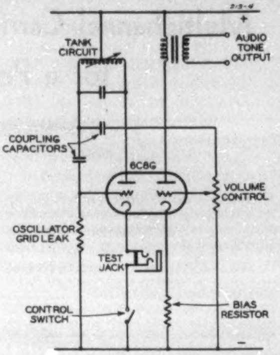


2/3-2
MATCHING TRANSFORMER
f_3
f_1
WAVE TRAP
LINE TUNING
f_2
COUPLING CAPACITOR f_1 f_3
f_1 f_2
f_2 f_3
DOUBLE FREQUENCY LINE TRAP
GROUNDS & DISCONNECTS
POWER TRANSFORMERS
COUPLING CAPACITOR
CONNECTED AS ABOVE TUNED TO f_2 REJECT f_1 & f_3
CONNECTED AS ABOVE TUNED TO f_3 REJECT f_1 & f_2
CONNECT AS ABOVE TUNED TO f_2 REJECT f_1 & f_3
CONNECT AS ABOVE TUNED TO f_3 REJECT f_1 & f_2

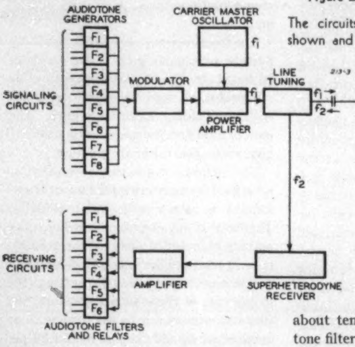
Figure 2 (above). Line-tuning diagram

The circuits for one carrier frequency are shown and a block diagram is used for the other two



2/3-4
+
TANK CIRCUIT
AUDIO TONE OUTPUT
COUPLING CAPACITORS
6C8G
VOLUME CONTROL
OSCILLATOR GRID LEAK
TEST JACK
CONTROL SWITCH
BIAS RESISTOR
−

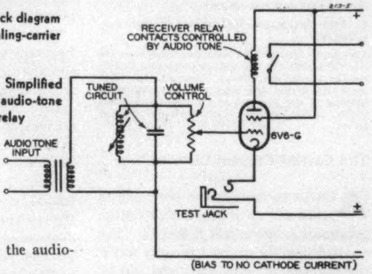
Figure 4. Simplified schematic diagram of audio oscillator and amplifier



AUDIOTONE GENERATORS
CARRIER MASTER OSCILLATOR
f_1
SIGNALING CIRCUITS
F1
F2
F3
F4
F5
F6
F7
F8
MODULATOR
POWER AMPLIFIER
LINE TUNING
2/3-3
f_1
f_2
RECEIVING CIRCUITS
F1
F2
F3
F4
F5
F6
AMPLIFIER
SUPERHETERODYNE RECEIVER
AUDIOTONE FILTERS AND RELAYS

Figure 3 (left). Block diagram of relay- or signaling-carrier system

Figure 5 (right). Simplified schematic diagram of audio-tone receiver and relay



2/3-5
+
RECEIVER RELAY CONTACTS CONTROLLED BY AUDIO TONE
TUNED CIRCUIT
VOLUME CONTROL
AUDIO TONE INPUT
6V6-G
TEST JACK
±
−
(BIAS TO NO CATHODE CURRENT)

conditions shows that the frequency spectrum from 300 to 3,000 cycles will provide only about ten tones which will meet the above conditions. The equipment has been designed to accommodate a maximum of ten tones each way. The carrier frequency is supplied by a master oscillator which drives a power amplifier. This carrier frequency is always maintained so that a channel will be available for any audio tones which may be sent through. The power amplifier is plate modulated by the audio tones. From the power amplifier the carrier frequency is applied to the line tuning system shown in Figure 2.

At the far end of the line the carrier frequency is tuned by the proper line-tuning circuit and applied to a superheterodyne receiver. This receiver is equipped with both a squelch circuit, to prevent the reception of noise when no carrier frequency is present, and automatic volume control, to control the sensitivity to variations in carrier strengths. The output of the receiver is connected to an audio amplifier which provides about ten watts of power to the audiotone filters.

The circuit of one of these audio filters is shown in Figure 5. The tuners are connected in series similar to the series connections in the transmitter. A parallel resonant circuit is tuned to each frequency which may be received by means of fixed capacitors and a coil with an adjustable powdered iron core. The voltage developed on the tuning circuit is applied to the grid of a detector tube which is biased to cut off when no signal is present. The arrival of signal of the frequency to which the circuit is tuned causes current to flow in the tube. This current normally passes through a relay which operates when the frequency is present.

The Transferred-Trip Circuit

One of the principal requirements of the signaling carrier system was to provide a high-speed transferred trip signal. The transformers on this line are not provided with circuit breakers on the line side. In case of a fault in a transformer, it is necessary to open the lower voltage breakers at both ends of the line in order to clear the transformer. The carrier is used to transmit the tripping signal to the far end of the line. Because of the large capacity of the transformers on this line, it was desirable that the time of transfer be as short as possible, and, at the same time, be completely reliable. In order to meet these conditions, it was decided to use three of the audio tones each way. These are arranged to provide the high-speed action desired together with automatic supervision and alarms.

The basic principle of the trip circuit is to have one of the audio frequencies, called a blocking frequency, always alive under normal conditions. This frequency upon reception, holds a relay contact open and prevents tripping. In order to trip, it is necessary to stop the blocking frequency and start another, called the tripping frequency. At the receiving point, a contact on the relay, associated with the tripping frequency, closes, and upon the closure of the contact associated with the blocking frequency, the trip circuit will be completed. Thus two things must oc-

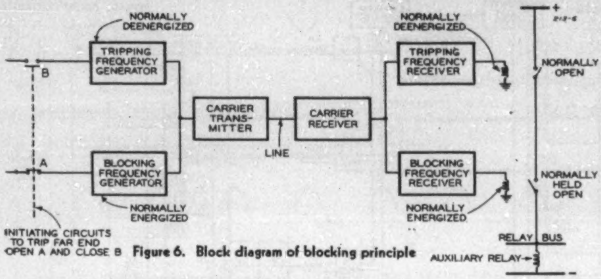

NORMALLY DEENERGIZED
TRIPPING FREQUENCY GENERATOR
B
A
BLOCKING FREQUENCY GENERATOR
NORMALLY ENERGIZED
INITIATING CIRCUITS TO TRIP FAR END OPEN A AND CLOSE B
CARRIER TRANS-MITTER
LINE
CARRIER RECEIVER
NORMALLY DEENERGIZED
TRIPPING FREQUENCY RECEIVER
BLOCKING FREQUENCY RECEIVER
NORMALLY ENERGIZED
+
NORMALLY OPEN
NORMALLY HELD OPEN
RELAY BUS
AUXILIARY RELAY
-

Figure 6. Block diagram of blocking principle

cur to cause a tripping action: First, the blocking frequency must be removed, and second, the tripping frequency must be started. As long as the blocking frequency is present, no tripping can occur even though the tripping-frequency relay might close from time to time, due to static, noise, and so on. Figure 6 shows this scheme in block diagram form.

The existence of the blocking frequency makes possible an interlocking supervising system. A block diagram of the circuit arrangement at the two terminals is shown in Figure 7. Frequencies *F*1 and *F*2 form one of the blocking loops. When the initiating switch 1 is in the normal position, it energizes the blocking oscillator *F*1. The blocking frequency *F*1 is transmitted and received; contacts 11 and 12 close and contact 13 opens. With the initiating switch 2 in the normal position, the closing of contact 11 causes the blocking oscillator to start, and the frequency *F*2 is in turn transmitted. At the other end of the line it is received. This causes contacts 21 and 22 to close and contact 23 to open. The closure of contact 21 lights a pilot lamp which shows that this blocking loop is complete and that the carrier equipment is normal, since any failure would result in the loss of the blocking frequency and the consequent extinction of pilot lamp.

A second blocking loop is provided by frequencies *F*3 and *F*4 in exactly the same way as the first blocking loop. This second loop causes contacts 31 and 32 to close and 33 to open at the remote terminal, and 41 and 42 to close and 43 to open at the supervising terminal. The condition of this blocking loop is shown by pilot lamp *B*.

These have been called blocking loops because they prevent tripping. This action occurs because contacts 13 and 33 are open at one end and, therefore, no feed is provided for the auxiliary relays; contacts 23 and 43 at the supervising end are open, also preventing tripping of this end.

The operation of either initiating switch 1 or 2, will trip the opposite end of the line. Operation of switch 1 removes the feed to the blocking oscillators (*F*1 and *F*3) and puts feed on the tripping oscillator (*F*5). At the far end of the line contacts 13 and 33 close due to the loss of the blocking frequencies, and contact 53 closes because the tripping-frequency receiver is energized. This provides feed to the auxiliary relay and trip circuits. Likewise operation of switch 2 will complete the circuit through contacts 23, 43, and 63, for tripping in the opposite direction.

Supervision of the carrier is obtained by other contacts on the relays. When a blocking loop is closed the push button receives feed through the corresponding contact 22 or 42. This push button tests the tripping frequency by means of a third loop. The test can only be made if one of the blocking loops is complete. Since there is no way of knowing whether the (*F*5) tripping-frequency oscillator or receiver, or the other (*F*6) tripping-frequency oscillator or receiver is in working condition, this test is necessary. Operation of the test push button closes contact 51 which in turn starts the tripping oscillator (*F*6) and closes the contact 61, indicated by the lighting of pilot lamp *C*. Pilot lamp *C* will light only when all the elements of the trip circuit are in operating condition. By connecting an alarm relay as shown in Figure 7, an alarm will be given whenever both blocking loops are lost. This occurs when something has become defective in the carrier circuit. To further aid in preventing trouble, due to the possible loss of carrier blocking coincident with the reception of undesired signals, the tripping-frequency receiver is set about one half as sensitive as the blocking-frequency receivers.

Initiating Circuits

In Figure 7 the initiating circuits are indicated as switches 1 and 2. In actual practice no switches are used for this purpose. The blocking frequencies are removed and the tripping frequency started directly from the closure of the primary relay contacts. Figure 8 shows schematically the essential features for transmission and reception, keeping the notation of Figure 7 as much as possible.

Two separate sets of contacts are provided on the primary relays, for two separate auxiliary relays. The carrier trip circuit must also feed from the same circuits as the auxiliary relays. Tubes *V*1 and *V*2 are provided as insulation between the two relay busses. Since only one carrier trip circuit is provided, the two relay busses must be paralleled, and yet back feeds from one bus to the other must be prevented. Tubes *V*1 and *V*2

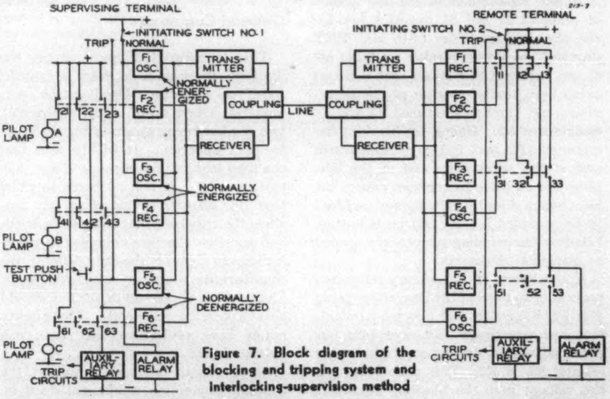
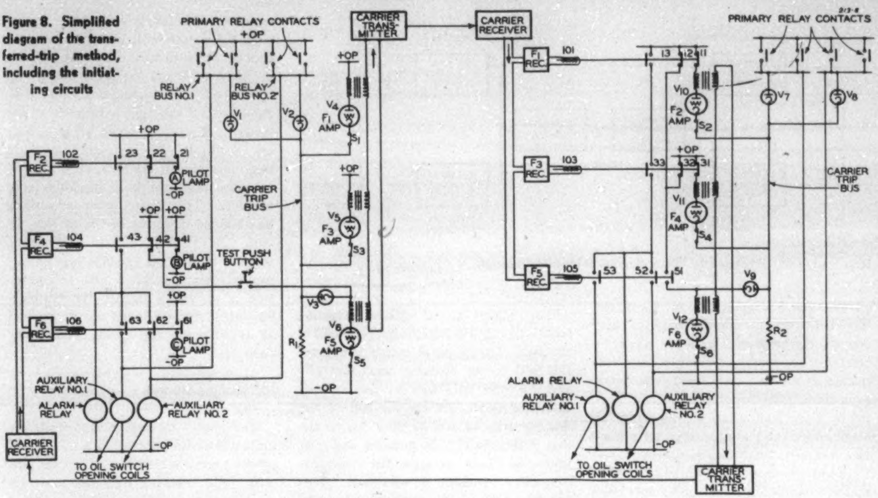

SUPERVISING TERMINAL
+
INITIATING SWITCH NO. 1
TRIP
NORMAL
F1 OSC.
TRANS-MITTER
NORMALLY ENER-GIZED
F2 REC.
COUPLING
LINE
21
22
23
PILOT LAMP A
RECEIVER
F3 OSC.
NORMALLY ENERGIZED
F4 REC.
41
42
43
PILOT LAMP B
TEST PUSH BUTTON
F5 OSC.
NORMALLY DEENERGIZED
F6 REC.
61
62
63
PILOT LAMP C
TRIP CIRCUITS
AUXIL-IARY RELAY
ALARM RELAY
-
REMOTE TERMINAL
INITIATING SWITCH NO. 2
TRIP
NORMAL
+
TRANS MITTER
F1 REC.
11
12
13
COUPLING
F2 OSC.
RECEIVER
F3 REC.
31
32
33
F4 OSC.
F5 REC.
51
52
53
F6 OSC.
TRIP CIRCUITS
AUXIL-IARY RELAY
ALARM RELAY
-

Figure 7. Block diagram of the blocking and tripping system and interlocking-supervision method

Figure 8. Simplified diagram of the transferred-trip method, including the initiating circuits



make this possible since they carry current in only one direction.

When the primary relay contacts are open, tubes $V1$ and $V2$ are nonconducting. Tubes $V4$ and $V5$ are amplifiers in the blocking loops. Under normal conditions they will have their starting switches closed and be in a condition to amplify. Their cathode current flows through resistor $R1$ and causes a voltage drop of about 15 volts on this resistor. This voltage is so small that the current through $V3$ and $V6$ is insufficient to amplify the tripping frequencies. The blocking frequencies ($F1$ and $F3$) are transmitted, and the tripping frequency ($F5$) is not.

At the remote end of the line closure of contacts 11 and 31 provides feed for the blocking amplifiers ($V10$ and $V11$), provided no primary relay contacts are closed. It will be noticed that a circuit exists here similar to that at the supervising end through the tubes $V7$ and $V8$ and resistor $R2$. Hence, the blocking frequencies ($F2$ and $F4$) are transmitted, and at the supervising end of the line, reception of these frequencies causes the pilot lamps A and B to be lighted and feed to be provided for the test push button. Likewise the tripping circuits are opened as previously discussed.

Upon closure of the primary relay contacts at either end of the line, either tubes $V1$, $V2$, $V7$, or $V8$ will become conducting, and practically full operating bus potential will be developed across either resistor $R1$ or $R2$. This action removes the voltage from the blocking amplifiers and causes either tube $V3$ or $V9$ to become conducting and provide feed for the tripping-frequency amplifier. This, of course, closes the trip circuit at the other end of the line as discussed previously. Tubes $V3$ and $V9$ are provided so that when the test push button is closed, the tripping amplifier can be energized without removing the voltage to the blocking amplifiers. Whenever the test push button is closed all six audio frequencies are present on the line. The alarm relays are also provided as described previously. The pilot lights and test push button are extended to the control room so that the condition of the carrier equipment can always be observed by the operators.

General Comments

This transferred tripping circuit was designed to provide as high a transfer speed as possible. Oscillograms were made on a test setup of the equipment, two of these being shown in Figure 9, one for each direction. It will be seen that the time required for tripping is less than 0.015 second. It will be borne in mind that this transfer circuit effectively parallels the trip busses at both ends of the line and that the time occupied in transferring the signal is the only delay in the simultaneous opening at both ends. The opening of one end of the line should always open the other end. The primary relays have preference over the push button circuit.

As the circuit has been explained, the tripping connection would seal itself in. To prevent this, back contacts were placed in the auxiliary relays at one end which would break the circuit as soon as these auxiliaries have operated.

Telemetering and Load Control

An impulse method of telemetering is used over the carrier-current circuit. The starting of the audio frequency used for telemetering indicates one impulse, and the stopping indicates a second impulse. In this way, the control of the audio oscillator is very simple. Load control signals of the time-duration type are also transmitted. These are based on the premise that the more the load is to be changed, the longer the pulse lasts. The starting and stopping of the audio frequencies, associated with the load control transmission, is rapid enough to cause inconsequential change in this timing.

Experiences With Equipment

As will be noted from Figure 2, the line-tuning system is quite complex, and it was considered too difficult to check the frequency response of the line using the actual equipment. A simple method was used for this measurement. Three resistors arranged in a star and grounded at the mid-point were connected at each end of the line. Drain coils were also connected across each resistor. A vari-

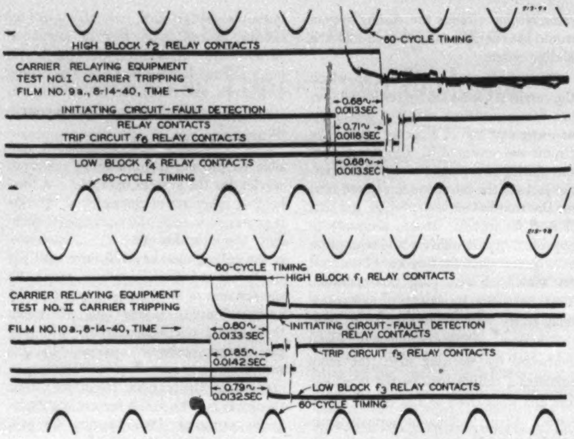


Figure 9 (above). Oscillograms showing time required for transference

The oscillograms were made during tests on the equipment. Test I is one direction and test II the opposite direction

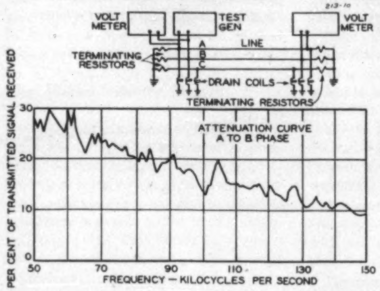


Figure 10 (above left). Circuit diagram of test method and curve of attenuation for the frequency range of 50 to 150 kilocycles

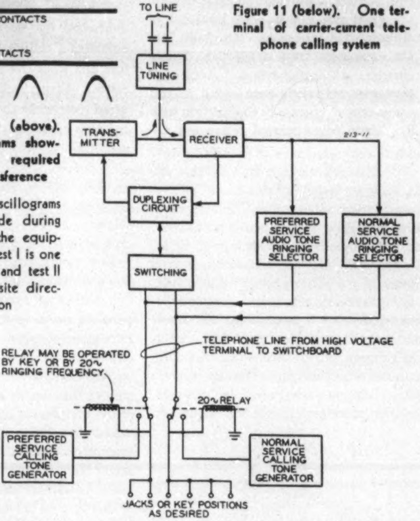

TO LINE
LINE TUNING
TRANS-MITTER
RECEIVER
213-11
DUPLEXING CIRCUIT
PREFERRED SERVICE AUDIO TONE RINGING SELECTOR
NORMAL SERVICE AUDIO TONE RINGING SELECTOR
SWITCHING
TELEPHONE LINE FROM HIGH VOLTAGE TERMINAL TO SWITCHBOARD
RELAY MAY BE OPERATED BY KEY OR BY 20~ RINGING FREQUENCY
20~ RELAY
PREFERRED SERVICE CALLING TONE GENERATOR
NORMAL SERVICE CALLING TONE GENERATOR
JACKS OR KEY POSITIONS AS DESIRED

Figure 11 (below). One terminal of carrier-current telephone calling system

able frequency generator was connected to the phase wires, a pair at a time, and the transmission over the frequency band from 50 kilocycles to 150 kilocycles was obtained. Readings were made by vacuum-tube voltmeters at the sending end and the receiving end. Figure 10 shows the circuit used and the curve of response from *A* to *B* phases. For actual use, the received voltage should be doubled, since the terminating resistors are not present.

It was felt that noise would be the worse possible source of trouble so far as the transferred trip circuit was concerned, particularly noise from lightning. The blocking system was designed to prevent harm from this type of noise, but a defect was present at first. Whenever a noise pulse was received it overloaded the receiver and wiped off the modulation. It also gave a large pulse of negative voltage to the automatic volume-control circuit. When the noise pulse disappeared the automatic volume control was biased too far negative and the carrier signal was not received. As a result the blocking relays dropped out. In order to correct this the time constant of the automatic volume-control circuit was shortened to about $^1/_{60}$ of a second and with this arrangement the blocking relays are not affected when a noise is received. In actual practice these noise pulses are normally due to lightning discharges in immediate vicinity of either terminal. A noise pulse apparently does not affect the tripping-frequency relay.

So far in the operation of the equipment little trouble has been experienced in maintaining the audio signals. In the original setup of the frequencies it was necessary to separate the audio tones by at least 30 per cent and also to see that no frequencies were on the harmonics of lower frequencies. This rather limits the number of audio frequencies available in the band from 300 cycles to 3,000 cycles over which the equipment is designed to operate but there is sufficient space for the ten tones, the maximum number desired on this system. It has been found necessary to keep the percentage modulation of any individual frequency low to prevent interaction between any of the various audio channels. Even so, there is a slight interaction which thus far has not been serious. The signal levels in the modulator and amplifier at both transmitter and receiver must always be kept low to prevent the introduction of harmonics. It is not difficult however, to find working levels which are stable.

The Carrier-Current Telephone System

The problems which developed in the carrier telephone system were mainly due to long extensions and the type of preferred service desired by the system operator. At one end of the line, the problem was simple because the carrier set was near the telephone switchboard. At the other end, however, about eight miles of leased line were required to connect the telephone switchboard and the system operator's office to the carrier equipment.

This distance was too great for the dialing equipment provided in the carrier sets and an additional calling system was devised. Furthermore, the duplexing equipment had to operate from extensions varying greatly in length. It was found that the variation between extensions was more than 20 decibels.

A block diagram of the calling system which was finally adopted is shown in Figure 11. A single carrier frequency is used for the telephone work. The transmitter and receiver are controlled by voice-operated duplexing relays. The manufacturer supplied standard equipment for this purpose, but the control circuit was changed to operate from the automatic gain control to provide compensation for the great variation in voice level due to differences in extensions. Also built into the equipment are line selectors, dialing circuits, and ringing relays. Additional tone-operated relays were added to this equipment to operate in conjunction with it. These tone-operated relays use the same ringing circuits the dialing system would use but without going through the dialing system.

The telephone switchboard, to which the carrier is connected by the leased line, has fifteen positions with the possibility of using any one of 225-cord sets. The circuit was arranged in such a way that the placing of a plug in one of the terminal jacks of the telephone line would turn on the transmitter and prepare the carrier set for service. It was necessary to provide a circuit which would send out a calling tone when the ring key of the cord set associated with plug was operated. This was done by inserting a 20-cycle relay in the jack circuit to operate from the normal switchboard ringing signal controlled by the ring key; the relay connects a calling tone to the telephone line and from there to the carrier transmitter. Upon reaching the normally energized receiver at the distant end, the calling tone operates a relay tuned to its frequency and it, in turn, indicates a call by lighting a call lamp on the telephone switchboard. As soon as the ring key is in its normal talking position, the calling tone stops, and when the called line answers, the equipment is ready for service.

The calling-tone system makes it possible to provide instantaneous preferred service for the system operator. A line-holding relay at the near end of the line is arranged to seize the equipment whenever the operator desires. Operation of the calling-tone key will then send out the preferred-service calling tone. At the far end of the line, reception of the preferred-service calling tone will break the normal telephone circuit and ring the system operator's phone. In this way the system operator can always gain control of the carrier telephone, even though it may be in use for ordinary telephone purposes. Busy lamps are provided to indicate when the line is in use to prevent unnecessary interruptions.

Relays and Breakers for High-Speed Single-Pole Tripping and Reclosing

S. L. GOLDSBOROUGH
ASSOCIATE AIEE

A. W. HILL
MEMBER AIEE

THE advantages of fast reclosing of transmission-line circuit breakers have been realized for a number of years. This experience has been gained on the basis of three-pole reclosures. One step beyond three-pole tripping and reclosure is single-pole operation. It is arranged so that on single phase-to-ground faults only the faulty phase wire is disconnected at each end of the line and then immediately reclosed. This allows synchronizing current to flow over the two sound-phase conductors during the time the faulted phase wire is out of service. With single-pole tripping slower speed reclosing, as compared to three-pole operation, can be utilized with a definite gain in the stability limit.[1]

The Phase-Selector Relays

Single-pole tripping from a relay standpoint has brought up a number of problems. At first thought it would appear to be only necessary to separate the trip circuits of the conventional relay equipments into three separate paths to the single-pole trip coils. This is true to a certain extent, depending upon the type of relay protection being considered. For instance, in the case of the simple overcurrent relay where phase relays will also operate for ground faults, it would be only necessary to arrange each phase relay to trip its respective single-pole breaker. However, it is the general practice at present to employ a separate ground relay operating on residual current and voltage or line residual current and power-bank neutral current in order to obtain adequate sensitivity to ground faults. The chief problem, therefore, involved in single-pole tripping is to find an effective way of indicating to the conventional residual-type ground relay which phase conductor is supplying the ground current. One way of providing this indication would be to employ under voltage relays energized from line-to-ground voltage. A fault to ground on one-phase conductor would drop out the corresponding voltage relay whose back contact in series with the conventional directional ground relay would trip its associated single-pole breaker. Another possibility would be the use of voltage-restrained overcurrent relays operating on line current and line-to-ground voltage. In general, neither of the above schemes is considered satisfactory on the basis that adequate sensitivity is not provided. It is quite possible that a high-resistance ground fault will not reduce the voltage sufficiently to provide a safe margin of discrimination.

The ideal approach to the problem would be to find a method of phase selection which can be made just as sensitive as the conventional directional ground relay and one which is, furthermore, totally independent of all normal conditions and dependent solely on fault conditions.

The method described below utilizing the phase shift of one sequence component with respect to another sequence component meets the above requirements.

Figure 1 shows the phase shift among the three sequence components for single phase-to-ground faults on different phases. It will be noticed that the zero-sequence component rotates around a given phase of the negative-sequence system in 120 degree steps. For instance, on a phase-*A*-to-ground fault the zero-sequence current is equal to and in phase with phase *A* of the negative sequence. This is, of course, on the basis of the total fault current. For a fault on phase *B* the zero-sequence component is in phase with the *B*-phase negative sequence, and, similarly, for a *C*-phase-to-ground fault the zero sequence is in phase with the *C* phase component of the negative sequence. Therefore, a selector element utilizing zero- and negative-sequence current will act in one direction for phase-*A*-to-ground fault but will act in the opposite direction for ground faults on *B* and *C* phases. Thus, if a directional element with watt-element characteristics is supplied with the zero-sequence current in one coil and the phase-*A* component of the negative-sequence current in the other coil, then, on a phase-*A*-to-ground fault this element will have maximum torque in the contact-closing direction. For a fault on phase *B* the zero-sequence current leads phase *A* of the negative sequence 120 degrees, and for a fault on phase *C* the zero-sequence current lags phase *A* of the negative sequence by 120 degrees. The element will, therefore, have torque in the contact-opening direction for phase-*B* and phase-*C* ground faults. If another similar relay element is used with its corresponding coil energized by the zero-sequence current, but its other coil energized by the phase-*B* component of the negative sequence instead of the phase-*A*, then, for faults on *B* phase it will be operating at maximum torque and will close its contacts but will have reversed torque for ground faults on phases *A* and *C*. Likewise, a third element energized by zero-sequence current and the phase-*C* component of the negative-sequence current will close its contacts for phase-*C*-to-ground faults, but open its contacts for faults on the other two phases. The schematic connections for this relay are shown in Figure 2.

The phase-selector relay consists of three elements similar to the conventional directional element. The polarizing coils of all three elements are energized by the zero-sequence current. The other coil on each element is energized by a component of the negative-sequence current obtained from the three-phase negative-sequence filter. This filter is so connected that the phase-*A* component of the negative-sequence system is supplied to the phase-*A* element, the *B* component supplied to the *B* element and the *C* component supplied

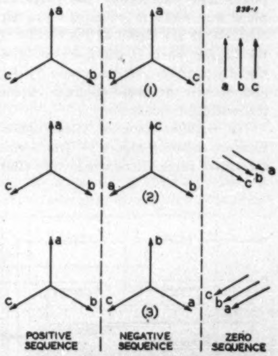
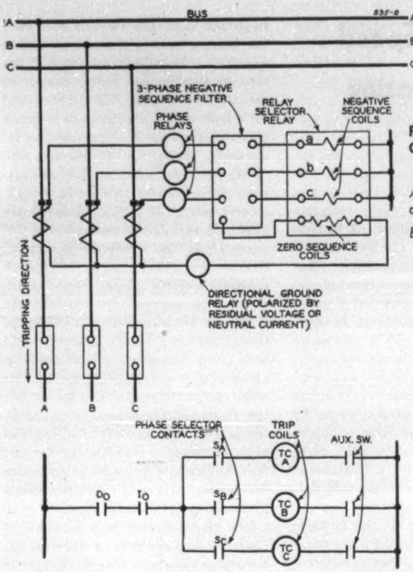


Figure 1. Current vectors for single-phase-to-ground faults on different phases

1. Phase-*A*-to-ground fault
2. Phase-*B*-to-ground fault
3. Phase-*C*-to-ground fault

Paper 42-35, recommended by the AIEE committees on power transmission and distribution and protective devices, for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 27, 1941; made available for printing December 11, 1941.

S. L. GOLDSBOROUGH is design engineer with Westinghouse Electric and Manufacturing Company, Newark, N. J., and A. W. HILL is section engineer, switchgear engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.


BUS
A
B
C
3-PHASE NEGATIVE SEQUENCE FILTER
PHASE RELAYS
RELAY SELECTOR RELAY
NEGATIVE SEQUENCE COILS
a
b
c
ZERO SEQUENCE COILS
TRIPPING DIRECTION
DIRECTIONAL GROUND RELAY (POLARIZED BY RESIDUAL VOLTAGE OR NEUTRAL CURRENT)
PHASE SELECTOR CONTACTS
TRIP COILS
AUX. SW.
D_0
I_0
S_A
S_B
S_C
TC A
TC B
TC C

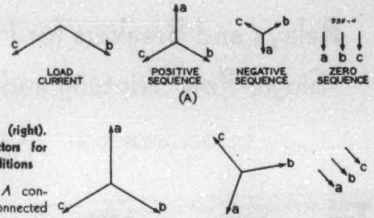

LOAD CURRENT
POSITIVE SEQUENCE
NEGATIVE SEQUENCE
ZERO SEQUENCE
(A)
POSITIVE SEQUENCE
NEGATIVE SEQUENCE
ZERO SEQUENCE
(B)
a
b
c

Figure 4 (right). Current vectors for two conditions

A—Phase-*A* conductor disconnected

B—Phase-*B*-to-*C*-to-ground fault

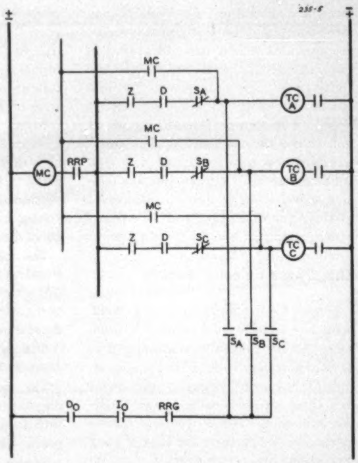

MC
Z
D
S_A
S_B
S_C
RRP
TC A
TC B
TC C
D_0
I_0
RRG

Figure 2 (above left). Schematic diagram of connections of phase-selector relay

Figure 5 (right). Simplified schematic diagram of trip circuits

to the *C* element. The connections (Figure 2) show the inclusion of phase relays which may be of the conventional type and also a conventional directional residual-type ground relay. This relay is necessary to provide directional ground protection as the selector relay, while employing elements similar to the directional element, does not indicate the direction of power flow but merely the particular phase wire which is grounded. The trip connections are shown at the bottom of the diagram where D_0 and I_0 are the directional ground-relay contacts and S_A, S_B, and S_C are the make contacts on the phase-selector elements.

The negative-sequence filter connections are shown in Figure 3. This device consists of three filters similar to a filter which has been described previously and which has been in use for a number of years.[2,3] Three of these standard filter units are connected to form a three-phase unit so that it is entirely unresponsive to zero-sequence currents or positive-sequence currents, and the output consists of all three phases of the negative-sequence system.

Other pairs of sequences besides the negative- and zero-sequence currents may be used to produce a phase-selector action. For instance, zero-sequence current and positive-sequence voltage is a feasible combination, also, zero-sequence voltage and positive-sequence voltage. However, it is considered that the zero- and negative-sequence current combination described above is more desirable, since all voltage connections are avoided and the selector elements operate on quantities which appear only during fault conditions, and, for this reason, its sensitivity can be very easily made to match the sensitivity of the conventional directional residual-type ground relay.

An essential feature of any phase-selection scheme is that after a faulted phase has been selected for tripping, the phase-selector scheme should not operate, under the conditions obtaining while one phase wire is disconnected, to disconnect the unfaulted phases. It can be shown that the above selector scheme gives the same tripping indication after the faulted phase has been tripped out. With one phase wire disconnected, there will be load current

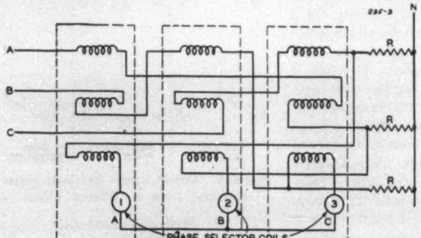

N
A
B
C
R
R
R
1
2
3
A
B
C
PHASE SELECTOR COILS
MUTUAL REACTORS

Figure 3. Schematic diagram of connections of three-phase negative-sequence filter

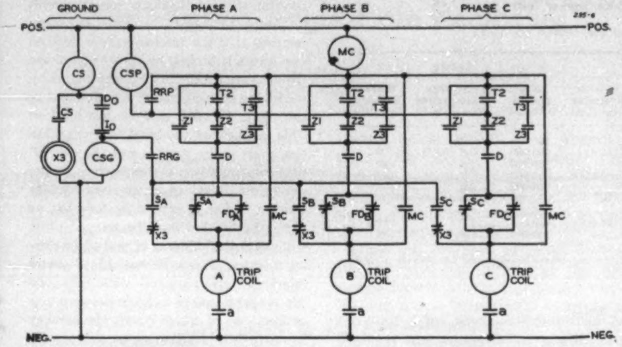


Figure 6. Complete schematic diagram of trip circuits for carrier-current single-pole relaying

on the other two phases, in general, equal and 120 degrees out of phase upon the assumption that each end of the protected line is fed from a grounded source.

Figure 4A shows this condition for the case where phase *A* has been tripped. It is seen that the zero-sequence and the negative-sequence currents are in phase on phase-*A* selector element. The phase selector, therefore, gives the same indication during the time a phase wire is out of service, thus preventing inadvertent tripping of the unfaulted phases. Figure 4B shows the vectors for a two-phase-to-ground fault on phases *B* and *C* and, it will be noted that this is a very similar condition to that obtaining when one phase wire is disconnected, in that the zero- and the negative-sequence currents are essentially in phase. On two phase-to-ground faults, therefore, the phase-selector relay selects the unfaulted phase.

The grounded phase-selector scheme can be applied in any system of phase and ground relays, but a description is given here of its use in a carrier-current step-type distance-relay scheme, as installed on the Public Service Company of Indiana system. In this application the following features were incorporated:

1. All single-phase-to-ground faults to be tripped single pole and reclosed immediately.
2. If fault still exists, then all three poles to be tripped.
3. On phase-to-phase faults, two phase-to-ground and three-phase faults all poles to be tripped and not reclosed.

It is interesting to examine how the simple phase-selector relay as described above is incorporated in a complete distance-type carrier-current system.

The schematic connections in Figure 5 have been simplified in order to show this incorporation as clearly as possible. The ground-relay trip circuit is made through the directional contact, D_0, overcurrent contact, I_0, the carrier-receiver relay contact, *RRG*, and one of the phase-selector contacts, S_A, S_B, or S_C. The phase-relay trip circuit is composed of the *RRP* carrier-receiver contact, the *Z* or distance-element contact and the directional-element contact, *D*. The purpose of the S_A, S_B, and S_C, back contacts in series with the respective phase-relay contacts is to prevent three-pole tripping in case a phase relay responds to a close-in phase-to-ground fault. Distance relays for phase protection will, in general, have a tendency to operate on heavy close-in ground faults and since it is arranged that all three poles are tripped when a phase relay operates, the single-pole tripping feature would not be obtained in the case where a phase relay operates on a ground fault. It is necessary to arrange the scheme so that even though the phase relay operates on a ground fault simultaneously with the ground relay and the ground-phase selector, the phase-relay trip circuit cannot be established. This is done by the inclusion of the back contacts, S_A, S_B, and S_C, which are located on their respective phase-selector elements. In other words, for a phase-*A*-to-ground fault, D_0, I_0, *RRG*, and S_A contacts close to trip the trip coil *A*, and at the same time the S_A back contact opens and prevents the phase-relay contacts, *Z* and *D*, from picking up the *MC* relay which would attempt to trip all three poles. In some applications these phase-selector relay back contacts would not be necessary, since the response of the distance-type phase relay to close-in ground faults depends upon the actual ohm setting of the phase relay and the magnitude of the ground current.

It is seen, therefore, that the additional contacts fundamentally necessary for single-pole tripping consist of the three-phase-selector make contacts which permit the splitting of the conventional ground-relay circuit into three parts. Depending upon the type of phase relay with which single-pole tripping is used, it may be necessary to employ three auxiliary blocking contacts to prevent interference by the phase relays.

The complete schematic connections are shown in Figure 6. The additional contacts which have been added over and above those shown in Figure 5 are the Z_1, Z_3, T_2, and T_3 contacts which will be recognized as being connected in the conventional manner for one of the present standard distance-type carrier systems.[4] No attempt is made here to describe the function of these contacts, as this has been done previously. The significant additional contacts are FD_a, FD_b, and FD_c. These are purely precautionary contacts which have been added in parallel with the back-contact phase-selector contacts, S_A, S_B, and S_C, respectively, for the purpose of preventing incorrect action caused by errors in the current transformers. It will be noted that on phase-to-phase faults not involving ground there is no zero-sequence current present. Therefore, no torque appears in the phase-selector elements, and thus the back contacts, S_A, S_B, and S_C, should remain closed. However, due to errors in the current transformers a small current might appear in the zero-sequence coils of the phase-selector relay. The back contact may, therefore, be inadvertently opened, and this would, of course, prevent tripping by the proper phase relay. The *FD* contacts are back contacts on an auxiliary fault-detector relay energized by the zero-sequence current and set for a current sufficiently above any anticipated error current in the current transformer. If, therefore, any of the back contacts, S_A, S_B, and S_C, are momentarily opened by the current-transformer error current, the circuit

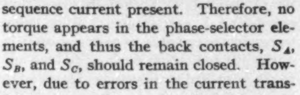


Figure 7. Simplified schematic diagram of reclosing circuits

Table I. Results of 132-Kv Breaker Tests

Public Service Company of Indiana, March 16, 1941

		New Castle			Lenore		
Test	Fault	Kva (Equiv. 3-Phase)	Breaker Interrupting Time (Cy.)*	Breaker Reclosing Time (Cy.)	Kva (Equiv. 3-Phase)	Breaker Interrupting Time (Cy.)*	Breaker Reclosing Time (Cy.)
1	C-Gr. (arc)	310,000	4.0	22.75	190,000	6.25	28.75
2	C-Gr. (solid)	300,000	3.5 (4)	22.5	190,000	5.6 (5.75)	29
3	C-Gr. (arc)				180,000	6.0	29
4	C-Gr. (solid)	50,000	4.5 (4.3)	22.5	415,000	6.2 (4.25)	29
6	B-C-Gr (solid)	170,000	4.25		380,000	6.0	
7	B-C-Gr (solid)	70,000	4.75		230,000	6.3	
9	B-C (solid)	300,000	3.9		290,000	8.25	
16	A-Gr (arc)	240,000	3.75	24.5	190,000	6.3	29.9
17	A-Gr (arc)	260,000	3.5	24.9	190,000	6.4	30

* Interrupting times given in parentheses are for second opening where breaker did not stay in after reclosure.

is still maintained through the fault-detector contact. On any fault involving ground, the fault-detector contacts open and leave the blocking function entirely up to the phase-selector back contacts.

In the Indianapolis installation a condition obtains which requires the addition of a blocking contact, $X3$. In this case the ground relay at both ends of the line was polarized by power-transformer bank neutral current and while one phase wire is disconnected, a zero-sequence current circulates in such a manner as to give the directional element, D_0, at each end of the line a fault indication. In other words, while one phase wire is open, the directional elements at both ends close, thus preventing a carrier signal from being transmitted, and this, of course, results in a tripping indication during the time one phase wire is disconnected. In order to prevent the immediate retripping of the breaker upon reclosure, the $X3$ contact was inserted. The $X3$ relay is energized at the same time that the trip current is established in the ground relay through the operation of the CS auxiliary relay whose contact energizes the $X3$ relay coil. The $X3$ relay picks up and opens its back contact with a time delay of about two cycles and resets with a time delay such that its back contacts become closed again a few cycles after the breaker pole has been reclosed. The choice of this $X3$ blocking contact was demanded by the fact that the ground-relay directional element was current-polarized and by the particular load and grounding conditions on this system. In the general case and also where voltage polarization is used, this contact would not be required.

The Reclosing Relays

The problems concerning the reclosing relays introduced by single-pole operation are simple of solution. The reclosing relays used on the Indiana installation were of the conventional type, and nothing of a particularly novel nature was required. Therefore, for the sake of simplicity, these reclosing relays have been grouped in the box of Figure 7. Each breaker-closing coil is energized through the make contact of auxiliary switches, CS_a, CS_b, and CS_c, which are operated whenever tripping occurs through the phase-selector contacts, S_a, S_b, and S_c. If phase-A conductor is faulted, the CS_a contact will close and energize the phase-A-breaker closing coil. The MC back contacts are on the MC relay (Figure 5), which is operated whenever a phase relay initiates tripping. Thus, on any phase fault the opening of the MC contacts prevents the energization of any of closing coils. To accomplish the feature of three-pole tripping after reclosure of a single pole on a solid fault merely requires the addition of auxiliary relays to the conventional reclosing relays and is not shown, since nothing novel is involved.

The Breakers

The fault having been detected and identified as one requiring a single-pole breaker operation, the next step in maintaining system stability is handled by breakers differing only slightly from the conventional type.

At the New Castle station of the Indiana Public Service Company a new breaker has been installed, each pole independently operated for either closing or tripping and so arranged that upon demand it will reclose within 30 cycles (0.50 second) after the trip coil has been energized. The operating mechanism is a solenoid, a unique feature of which is its "magnetic brain."

For fast reclosure it is desirable that the solenoid be nonmechanically trip-free; that is, that energy applied to the closing coil will arrest the opening movement of the breaker and return it to the closed position without the delay necessary with a trip-free linkage. The latter, of course, requires that the mechanical tie between two levers is broken by the action of the tripping latch and must be restored, as by retrieving springs, before the closing core can again be used to close the breaker. This means that the breaker opens practically its entire stroke, and the over-all time consumed becomes excessive. At the same time, there are circumstances under which trip-free operation is desirable, as when the breaker is to be tripped while still under the influence of pull of the closing magnet. It is here that this magnetic interlock performs, and with only one trip magnet, makes a choice between two latches, one of which opens the breaker "nontrip-free" for fast reclosure from the closed position, while the other provides "trip-free" operation in case the closing coil is still energized.

The operation of this magnetic interlock centers about a floating bar above the trip magnet, either end of which can be blocked to form a fulcrum point and cause the opposite end to rise when the trip coil is energized, thus providing for proper selection between the two latches. The choice of a proper point for blocking this bar is determined by an armature piece located within the field of flux of the closing magnet, but biased by springs to such a position that it releases the "nontrip-free" latch normally. Only when there is some magnetism in the closing coil, is it pulled to the opposite position to permit tripping on the "trip-free" latch.

The electrical connections for this operating mechanism remain as simple as for any of the solenoids in use for many years, as there is only one trip coil. The usual number of auxiliary switches is provided, or this can be amplified as desired if added circuits for other relays are needed.

The entire operating mechanism with control relays is mounted in a weatherproof sheet steel housing and attached to the tank wall, one such assembly for each pole of the three-pole breaker. Within this housing are also mounted the terminal blocks for bushing-type current-transformer secondaries. It is possible to make connections to any of the several transformer taps as required, at this one convenient location, and all conduit to the breaker is brought into the one house, whether intended for transformer secondaries or for control.

The scheme of single-pole operation has been worked out for revamping old three-pole breakers as well as for new apparatus. The experience at the Lenore end of this line demonstrates that the operation can

High-Speed Single-Pole Reclosing

J. J. TRAINOR
NONMEMBER AIEE

J. E. HOBSON
MEMBER AIEE

H. N. MULLER, JR.
ASSOCIATE AIEE

THE first application of high-speed single-pole reclosing to a high-voltage transmission line was recently made, tested, and put in service, on a 50-mile section of 138-kv single-circuit line on the system of the Public Service Company of Indiana, Inc. This paper discusses the reasons for choosing single-pole reclosing, and the installation and field testing of the equipment made under operating conditions. A companion paper by S. L. Goldsborough and A. W. Hill[1] describes the relaying and circuit-breaker equipment in detail.

When a single-circuit transmission line is used to interconnect two major systems and the line is used to transmit firm power between systems, considerable dependence must be put on the performance of this line. Under such circumstances unusual precautions are justified:

1. To prevent faults occurring on the line.
2. To minimize the disturbances created by faults.

The first objective is approached by using good line construction, by designing the line to be as nearly lightning proof as is economically possible, sometimes by the use of protector tubes, or possibly by the use of ground-fault neutralizers to prevent line-to-ground arcs from developing into faults.

It is not economical to design lines so that faults will never occur, although this ideal may be approached. Granted that a certain number of faults will occur, it is important to minimize their effect on the interconnected system. The objective when a fault develops is to remove the fault and re-establish the circuit without losing synchronism between the systems, so that there is no interruption in service over the interconnecting line.

Since the concepts of power-system stability were first recognized and the various factors influencing stability studied, means have been sought to improve the performance of power systems during transient disturbances.[2] A multiplicity of measures are now recognized, most of them directed toward minimizing the severity of the fault. During earlier years attention was particularly focused on improvements in stability limits obtained by design characteristics of machines and transformers, bussing arrangements, location of intermediate switching points, and the design of the transmission line itself. In the last decade high speeds for relay and circuit-breaker operation have become practical and extensively applied. High-speed apparatus, minimizing the duration of transmission-line disturbances, has been a major factor in improving system performance

The use of reclosing circuit breakers provides a means for furthering the advantages made possible by high-speed breakers and relays used for rapid fault isolation. The fundamental idea of reclosing circuit breakers was conceived by F. E. Picketts and filed with the United States Patent Office in 1916. Reclosing is particularly advantageous in the case of single-circuit transmission lines, for, while high-speed apparatus serves to remove the fault in the quickest possible time, loss of synchronism between the sending and receiving systems will quickly result if the line remains open. If loss of synchronism is not to occur, not only must the fault be cleared promptly, but the line must be restored to service after the fault is removed before the two systems have drifted far enough apart to cause instability.[3,4] High-speed reclosing has been used successfully, recently with simultaneous tripping and reclosing of the three-breaker poles.[5–8] This type of operation is known as gang-operated or three-pole reclosing. For a given time of reclosure the maximum power which may be transmitted without loss of synchronism depends on the relative inertias of the two systems (which determines the speed at which the systems drift apart when a particular fault is on, or when the line is open) and the initial operating angle between the systems. The latter is primarily a function of system reactance and transmitted power.

The time that the line can remain open without loss of stability is in most

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J. J. TRAINOR is chief engineer with the Public Service Company of Indiana, Inc., Indianapolis, Ind. J. E. HOBSON is director of the department of electrical engineering of Illinois Institute of Technology, Chicago, Ill. H. N. MULLER, JR., is a central station engineer in the industry engineering department of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

The authors acknowledge the fundamental work done by E. L. Harder on transient power limits obtained by the use of single-pole reclosing, the work done by S. B. Griscom on arc deionization time, and the work of H. M. Smith, Jr., in performing the calculations necessary to the preparation of the paper.

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be practically as fast even with fifteen year old breakers. In this case the contact structure of the old breaker was modernized with "De-ion Grids" to get the benefit of modern high-speed interrupters and, particularly, to have a device well able to withstand instantaneous reclosure.

The results of the recent tests on the Indiana Public Service system show the benefits of single-pole reclosure. Breaker reclosing times of from 22.5 to 30 cycles were secured.

Conclusions

1. The fundamental relay problem introduced by single-pole tripping and reclosing concerns an adequate sensitive means of selecting the faulty phase. This problem has been solved by utilizing the phase-angle shift between two sets of phase-sequence quantities.

2. An actual installation of a phase-selector relay co-ordinated with a distance-type carrier scheme has been made and tested with very satisfactory results.

3. It was observed that the breakers showed no outward display during any of the tests, indicating the suitability of the combination of modern "De-ion Grid" interrupters and fast-reclosing solenoid mechanisms for this duty. Adequate contact travel was secured before the breaker was reclosed, thus providing ample time for the fault arc to become de-energized, without crowding these operations to the limit as would be required for maintaining stability when opening all three poles.

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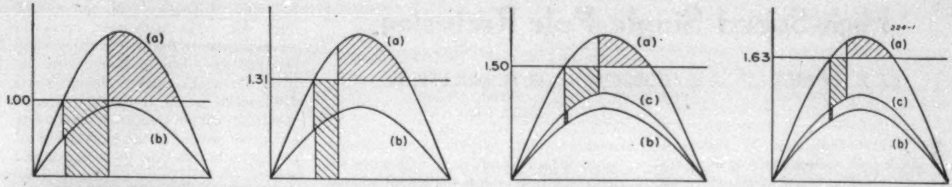

35∿ RECLOSING AND GANG-OPERATED BREAKER

20∿ RECLOSING AND GANG-OPERATED BREAKER

35∿ RECLOSING AND SINGLE-POLE BREAKERS

20∿ RECLOSING AND SINGLE-POLE BREAKERS

Figure 1. Comparison of transient power limits on a hypothetical system using gang-operated and single-pole 8-cycle breakers for reclosing speeds of 35 and 20 cycles

(*a*) Sending-end power-angle curve before fault occurs

(*b*) Sending-end power-angle curve during single line-to-ground fault

(*c*) Sending-end power-angle curve with one phase switched out of service

cases very short, thus high speeds of circuit-breaker reclosing are dictated. The reclosing time is measured from the time the breaker trip coil is energized until the breaker has opened, and the contacts are again closed. Commercial breakers are available today with reclosing times of 35 and 20 cycles at 138 kv on a 60-cycle per-second basis, and several installations employing breakers using such reclosing speeds are in service. The limit to which the reclosing time may be shortened depends upon breaker opening time and the speed attainable with reclosing mechanisms. One other factor, the actual deionizing time of the arc is of primary importance, since nothing will be gained if the circuit breaker is reclosed before the arc path has deionized, for the fault would then restrike after reclosure. Table I gives approximate data (obtained from tests on typical systems) of the minimum reclosing time permissible without re-establishment of the arc. These deionizing times are of course variable, depending upon line design, wind conditions, and so on, and the figures of Table I are to be taken as typical of expected results under average conditions.

The foregoing discussion has assumed simultaneous tripping and reclosing of all three circuit-breaker poles which will be referred to in this paper as gang-operated or three-pole reclosing. In all discussions of high-speed reclosing pertaining to line sections having sources of power at both ends, simultaneous operation of the circuit breakers at both ends of the transmission line is assumed, since, otherwise, the de-energized time of the line would be shortened, and the possibility of restriking would be increased. Simultaneous tripping is accomplished by pilot-wire or carrier-current relaying or, when possible, by setting high-speed impedance-type relays to cover the entire line section.

Utilizing high-speed relays and gang-operated breakers and the high speeds of reclosure mentioned above, cases will arise in which the desired power cannot be transmitted without loss of stability during fault conditions. A step toward raising the transient power limits, beyond three-pole reclosing, may be achieved by single-pole reclosing of circuit breakers. Single-pole reclosing consists of opening only the breaker pole connected with the faulty phase and reclosing this pole after the fault has cleared. Single-pole reclosing has application only to grounded-neutral systems. An increase in the transient stability limit is realized as compared with gang operation, since a considerable amount of power may be transmitted over the sound phases while the faulty phase is isolated. For a given amount of power transmitted over a particular transmission line, the permissible de-energized time to maintain synchronism is appreciably longer, if single-pole reclosing is used as compared with three-pole operation. Hence, in very high-voltage systems where speeds too fast for adequate arc clearing might be required with gang operation, slower speeds, providing ample time for arc clearing may maintain stability conditions if single-pole reclosing is used.

The actual gain in transient-stability limit realized with single-pole operation can be visualized by reference to Figure 1. This figure shows power-angle diagrams for a typical system, illustrating the relative transient-stability limits obtained with 35- and 20-cycle, three-pole and single-pole reclosing. The curves were drawn for a hypothetical single-machine station feeding a high-voltage line terminating in a very large system, assumed to have infinite inertia. The curves serve to illustrate qualitatively the expected increase in transient limits and the relatively large amounts of power which can be transmitted over the unfaulted phases while one phase is open.

Table I. Typical Arc Deionizing Times

Nominal System Voltage (Kv)	Deionizing Time for 95% of Faults (Cycles on 60-Cycle Basis)	Minimum Permissible Reclosing Time With Eight-Cycle Breakers	Minimum Permissible Reclosing Time With Five-Cycle Breakers
23	4	12	9
46	5	13	10
69	6	14	11
115	8.5	16.5	13.5
138	10	18	15
161	13	21	18
230	18	26	23

Application

The principal transmission lines of the Public Service Company of Indiana, Inc., including the major interconnections with neighboring companies, are shown on the map of Figure 2. The principal generating stations of the Public Service Company of Indiana, Inc., are at Dresser and at Edwardsport. Considerable generating capacity is available at Indianapolis, through the interconnection with the Indianapolis Power and Light Company, and from the south through interconnection with the Cincinnati Gas and Electric Company. Interconnection is made at Muncie and Kokomo with the Indiana section of the American Gas and Electric system.

Figure 3 shows in greater detail the Lenore–Newcastle–Muncie interconnection installed during the past year which is the principal subject of this discussion. Approximate generating capacity at the major points of application to the interconnected system is indicated on Figure 3. The Indiana section of the American Gas and Electric system is extensive and includes a multiplicity of sources of generation, so that the generation at Muncie has not been assigned a definite capacity.

The Lenore–Newcastle line, a part of the new interconnection, was also built to supply firm power to the load at Newcastle, the required capacity of the line to be at least 50,000 kva. The Newcastle–

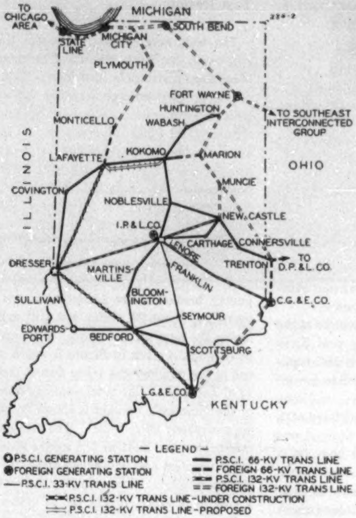
TO CHICAGO AREA
MICHIGAN
SOUTH BEND
STATE LINE
MICHIGAN CITY
PLYMOUTH
FORT WAYNE
HUNTINGTON
TO SOUTHEAST INTERCONNECTED GROUP
MONTICELLO
WABASH
ILLINOIS
LAFAYETTE
KOKOMO
MARION
OHIO
MUNCIE
COVINGTON
NOBLESVILLE
NEW CASTLE
I.P.&L.CO.
LENORE
CARTHAGE
CONNERSVILLE
DRESSER
MARTINS-VILLE
FRANKLIN
TRENTON
TO D.P.&L.CO.
BLOOM-INGTON
SULLIVAN
C.G.&E.CO.
SEYMOUR
EDWARDS-PORT
BEDFORD
SCOTTSBURG
L.G.&E.CO.
KENTUCKY
— LEGEND —
P.S.C.I. GENERATING STATION
FOREIGN GENERATING STATION
P.S.C.I. 33-KV TRANS LINE
P.S.C.I. 66-KV TRANS LINE
FOREIGN 66-KV TRANS LINE
P.S.C.I. 132-KV TRANS LINE
FOREIGN 132-KV TRANS LINE
P.S.C.I. 132-KV TRANS LINE-UNDER CONSTRUCTION
P.S.C.I. 132-KV TRANS LINE-PROPOSED

Figure 2. Major high-voltage interconnected lines of the Public Service Company of Indiana, Inc. and neighboring systems

Muncie line was proposed to provide an interconnection, ordinarily floating, between the Public Service Company of Indiana, Inc., and the American Gas and Electric system. Under emergency conditions interchange of power in either direction is permitted. The Lenore–Newcastle line and the Newcastle–Muncie line are now single-circuit lines. The Lenore–Newcastle line is ultimately planned to be double circuit, as indicated by the tower structure shown in the photograph of Figure 4. The line is designed to have nine insulator units used in suspension and eleven insulator units used in strain. The single ground wire now installed on this structure is three-strand, $^{3}/_{8}$-inch galvanized steel conductor. The line conductors are 300,000 circular-mil copper. The line route is east and west over relatively flat terrain, and the tower-footing resistances test well under five ohms.

Other circuits parallel the new line from Indianapolis to Muncie, as shown in Figure 2. The existing ties are, however, relatively weak and of high impedance. If an interruption should occur on the Lenore–Newcastle line while an appreciable block of load were being transmitted over the line to Newcastle, the paralleling ties through Noblesville, Kokomo, and Marion would not be able to pick up the load, because of their high impedance, and instability would occur. Consequently, it is of prime importance to keep the Lenore–Newcastle line in continuous operation. Although the line is designed to be relatively lightning-proof, some interruptions of the Newcastle supply would inevitably result unless means were taken to re-establish the line immediately following an interruption, and before instability could occur. Consequently, high-speed reclosing was considered necessary to keep interruptions to the Newcastle load at an absolute minimum.

The 138-kv line from Newcastle to Muncie is provided with 20-cycle reclosing circuit breakers, 1,500,000-kva interrupting capacity, at each end of the line section. Conventional carrier-current relaying is used for both ground and phase relaying. The circuit breakers are conventional eight-cycle breakers and are equipped with gang-operated mechanisms.

Both gang-operated (three-pole) reclosing and single-pole reclosing were considered for the line from Lenore to Newcastle. Speeds of reclosing of 35 cycles and 20 cycles were considered. Table II gives the results of calculations made to determine the transient power limits for both speeds of reclosure considered and for both gang-operated and single-pole reclosing. Simultaneous tripping (and reclosing) of two circuit-breaker poles was assumed in the calculations for faults involving two-line conductors. With the type of construction used, a very high percentage of the faults will originate and persist as single line-to-ground. Primary consideration was consequently given to the transient power limits achieved during single line-to-ground fault conditions. Breakers and mechanisms for 35-cycle single-pole reclosing can be obtained at approximately the same price as breakers and mechanisms for 20-cycle gang-operated reclosing. The figures of Table II show considerably higher transient power limits during single line-to-ground faults for 35-cycle single-pole reclosing than the limits achieved using 20-cycle gang-operated reclosing. The power limits for 20-cycle single-pole reclosing are yet higher, but it was felt that the gain in power limits on this application did not justify the considerable increase in cost for higher-speed breakers and mechanisms. On the basis of this comparison, 35-cycle single-pole reclosing was adopted for the line section.

Existing 138-kv breakers at the Lenore end of the line were provided with 35-cycle single-pole reclosing mechanisms. A new breaker was installed at the Newcastle end of the line—a 138-kv, 8-cycle 1.5-million-kva unit, equipped with a 35-cycle reclosing mechanism on each pole. Carrier-current relaying is used to permit simultaneous operation of the breakers at the two ends of the line. The phase relaying is of conventional type, and it was necessary to develop a new ground-relaying scheme to select the faulted phase. The scheme for selecting the faulted phase is described in a companion paper by S. L. Goldsborough and A. W. Hill.[1] The equipment is designed to operate as follows:

For single line-to-ground faults the breaker pole connected to the faulted phase at each

Table II. Calculated Transient Power Limits, for Single-Pole and Three-Pole Reclosing, at 20 and 35 Cycles, Using 8-Cycle Breakers With Carrier-Current Relaying

		Breaker Operation		Comparative Transient Power Limits	
Item	Type of Fault	Reclosing Time	Type of Reclosing	Megawatts	Per Cent*
1	Single line-to-ground	35	Gang......	74.5	100
2		20		95.8	129
3		35	Single-pole.	105.9	142
4		20		116.8	157
5	Double line-to-ground	35	Gang......	71.1	100
6		20		91.1	128
7		35	Single-pole.	81.3	114
8		20		97.3	137
9	Three phase	35	Gang......	70.4	100
10		20		90.1	128

*Transient power limit obtained with 35-cycle, gang-operated breakers taken as 100 per cent for each type of fault.

end of the line is tripped and reclosed. If the fault persists after reclosure, all breaker poles are tripped and locked out. All breaker poles are tripped and locked out for all faults involving more than one conductor.

On most future installations of single-pole reclosing it is expected that two poles will be tripped and reclosed for two-phase faults, and three poles tripped and reclosed for three-phase faults, but such operation is not necessary on the Lenore–Newcastle installation at the present time.

Figure 5 is a photograph of the new 138-kv breakers at the Newcastle substation equipped for single-pole reclosing.

Tests

Extensive field tests were made on the system of the Public Service Company of Indiana, Inc., on March 16, 1941 and were primarily intended to check the functioning of the relays and reclosing equipment. Two oscillographs were stationed at Lenore to record simultaneously the following quantities:

Phase currents in the Newcastle line.
Residual current in the Newcastle line.
Carrier signals at Lenore.
Lenore 138-kv bus voltages.
Phase *C* voltage on the line side of the Newcastle line breaker at Lenore.
Phase *C* current in the Dresser line.
Phase *C* current in the Indianapolis Power and Light Company line.
Phase *C* current in the Lenore condenser.
Phase *C* current in the Columbia line.
Residual current in the Lenore transformer neutrals.
Travel for the reclosing breakers.
Trip coil currents for the reclosing breakers.

Two oscillographs were stationed at Newcastle to record the following:

Phase currents in the Lenore line.
Residual current in the Lenore line.
Residual current in the Muncie line.
Residual current in the Newcastle transformer neutral.
Carrier signals at Newcastle.
Phase *C* voltage on the line side of the Lenore breaker.
Travel of the reclosing breakers in the Lenore line.
Trip coil currents of the Lenore and Muncie line breakers.

Additional oscillographic records were obtained by the American Gas and Electric Service Corporation at Muncie.

Faults were applied to the system at the points marked as F_1, F_2, F_3, and F_4 in Figure 3. The fault locations and types of faults were selected to provide a comprehensive test for the relays, reclosing mechanisms, and breakers associated with the Lenore–Newcastle line. Since it was necessary to run the staged tests on a Sunday, during system light-load conditions, the tests could not indicate directly the ultimate transient-power limits which can be achieved. Indirectly, however, the records should provide a very good indication of stability conditions by showing the disturbance to the system initiated by the fault.

The complete test schedule is outlined in Table III. Arcing faults were initiated by closing a circuit breaker which connected a line conductor to ground through a fine wire suspended across an insulator string. The arc resulting when the switch was closed is shown in Figure 6. Solid faults were also initiated by the closing of a breaker.

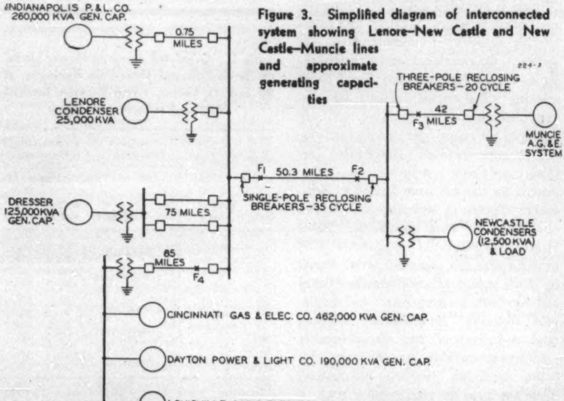
INDIANAPOLIS P.&L.CO. 260,000 KVA GEN. CAP.
0.75 MILES
LENORE CONDENSER 25,000 KVA
DRESSER 125,000 KVA GEN. CAP.
75 MILES
85 MILES
F_4
F_1
50.3 MILES
F_2
SINGLE-POLE RECLOSING BREAKERS–35 CYCLE
224-3
THREE-POLE RECLOSING BREAKERS–20 CYCLE
42 MILES
F_3
MUNCIE A.G.&E. SYSTEM
NEWCASTLE CONDENSERS (12,500 KVA) & LOAD
CINCINNATI GAS & ELEC. CO. 462,000 KVA GEN. CAP.
DAYTON POWER & LIGHT CO. 190,000 KVA GEN. CAP.
LOUISVILLE GAS & ELEC. CO. 225,000 KVA GEN. CAP.

Figure 3. Simplified diagram of interconnected system showing Lenore–New Castle and New Castle–Muncie lines and approximate generating capacities

Test Results

The tests were entirely successful, the single-pole reclosing relays and breakers operating satisfactorily in all tests. Typical of the performance recorded for all tests, the Newcastle breaker in tests 1 and 2 interrupted 1,300 amperes in six cycles after initiation of the fault and four cycles after energization of the trip coil. The line remained de-energized 19 cycles before reclosure, giving a reclosing time of 23 cycles. The breaker remained closed on the arcing fault of test 1 but tripped a second time and locked out on the solid fault of test 2. The reclosing times for the Lenore breakers were somewhat longer, averaging about 29 cycles, but still well under the nominal 35 cycles. A typical oscillogram is given in Figure 7, which is one of the oscillograms taken during test 12 of Table III. The total reclosing time of the Newcastle breaker is shown to be approximately $25^1/_2$ cycles. The breaker interrupted the fault in $5^1/_2$ cycles after fault inception and $3^1/_2$ cycles after trip-coil energization. Trace number 9 shows a flow of residual current for 31 cycles, indicative of the reclosing time at Lenore. The times to arc extinction and reclosure were remarkably consistent on all tests. It is of interest that in test 3 the three-pole 20-cycle reclosing breaker at the New-

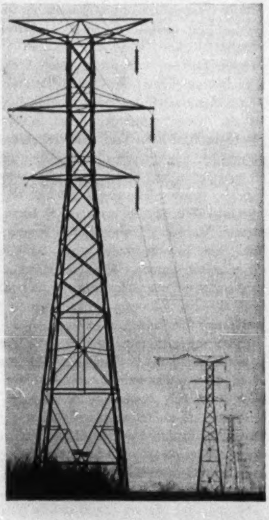

Figure 4. Tower construction and conductor arrangement of Lenore–New Castle 132-kv line

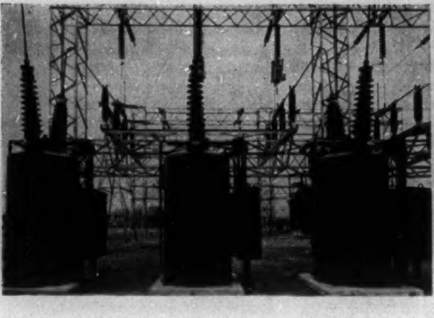

Figure 5. Close-up of 132-kv single-pole reclosing breakers at New Castle substation

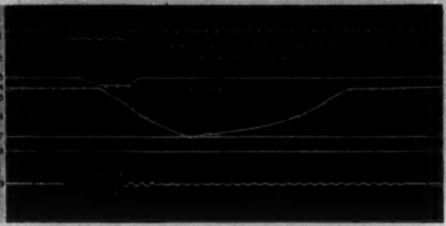

Figure 7. Typical oscillogram showing single-pole reclosing-breaker operation for a phase-A-to-ground arcing fault

1. *A*-phase bus potential
2. *A*-phase line current
3. *A*-phase breaker trip current
4. *A*-phase breaker travel
5. *B*-phase bus potential
6. *B*-phase line current
7. *B*-phase breaker trip current
8. *B*-phase breaker travel
9. Lenore line residual current

castle end of the Newcastle–Muncie line reclosed on the arcing fault on the Muncie line in approximately 17 cycles (because of adjustment for faster reclosing speed than necessary) and the arc restruck. This performance may indicate that times of reclosing less than 20 cycles cannot be used on 138-kv systems.

The disturbance to the remainder of the system on all tests was negligible. On test 12 the total Newcastle load, 40,000 kw, was supplied over the Lenore–Newcastle line, with all paralleling ties open. No disturbance was noted at Newcastle or other points on the system and the oscillograms revealed that very little swinging of the Newcastle condensers and other connected machines occurred. During this test approximately 500 amperes residual current appeared at the inception of the fault in the Newcastle line at Lenore. When the faulted phase was opened, before reclosure, the residual current in the line at Lenore decreased to 85 amperes. The residual current in the Muncie line, during the period that the faulted phase was de-energized, was so small that it could not be accurately scaled from the oscillograms but was well under 50 amperes.

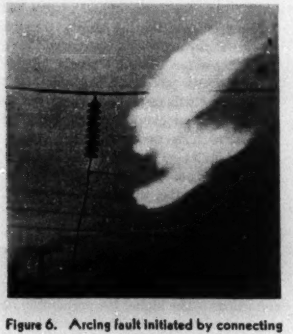

Figure 6. Arcing fault initiated by connecting a fine wire from line to ground

General Discussion

The field tests demonstrated that the relaying scheme developed to select the faulted phase, trip the breaker pole connected to the faulted phase, and reclose the breaker, functioned properly for both arcing and solid faults. The scheme also operated properly for single-phase faults and for faults involving more than one phase. The Lenore–Newcastle line has not had an automatic-breaker operation, other than during the test program, since the line was put in service even though a rather severe lightning season has been experienced, and neighboring lines in the same territory have suffered several interruptions. The past year's experience indicates the adequacy of the line design to minimize the occurrence of faults caused by lightning.

Calculations indicate that single-pole reclosing results in considerably higher transient power limits than can be achieved with three-pole reclosing, if due consideration is given to the high percentage of single line-to-ground faults, the relatively small percentage of faults involving two-phase conductors, and the very small percentage of three-phase faults occurring on a well-designed transmission line equipped with apparatus to provide high-speed clearing of faults. The curves of Figure 8, calculated for an assumed system consisting of a 50,000-kva waterwheel generator feeding into an infinite system (compared to the 50,000-kva station) over a single 138-kv line, illustrate the relative transient-power limits attained with single-pole reclosing for single line-to-ground faults (curves *a*), double line-to-ground faults (curves *b*), and three-phase faults (curves *c*). Two assumed speeds of operation were chosen for the calculations: 4-cycle fault clearing with 20-cycle reclosing (the solid curves), and 8-cycle fault clearing with 35-cycle reclosing (the dotted curves). Length of line connecting the systems was varied to show the effect of change in system "through" reactance. Similar curves calculated for a 250,000-kva steam turbine-generator station feeding into an infinite system over a 138-kv line are given in Figure 9. It is not strictly correct to compare curves *a* and *c* in these figures, for example, to show the improvement in power limits obtained by using single-pole rather than three-pole reclosing, since curves *a* assume a single line-to-ground fault and curves *c* assume a three-phase fault. The error in direct comparison lies only in the fault severity before the fault is cleared, a factor largely concealed by the number of phases opened before reclosure and the time of de-energization of the line.

In the above paragraphs the principle of single-pole reclosing has been described and its advantages with respect to transient stability pointed out. Other advantages accrue from its use, but single-pole reclosing must not be considered as a panacea for all transmission-line problems. Other methods of improving transmission-line operation with respect to transient stability limits and service continuity are available, and the choice of apparatus or method will be a function of several variables in each individual case.

The use of ground wires to prevent transmission-line outage has widespread acceptance. It is not economical to de-

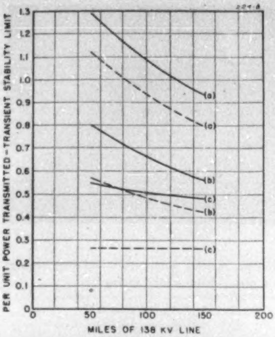

Figure 8. Comparison of transient stability limits with 35- and 20-cycle single-pole reclosing for a 50,000-kva water-wheel generator feeding an infinite system over a 138-kv line

——— 20-cycle single-pole reclosing
– – – – 35-cycle single-pole reclosing

(*a*) Single line-to-ground fault
(*b*) Double line-to-ground fault
(*c*) Three-phase fault

sign lines to completely eliminate fault occurrence, although in the higher-voltage classes, this ideal may be approached. If a line is being operated near its stability limit, or if the few faults occurring on a well-designed line are objectionable from the standpoint of service continuity, high-speed reclosing, either gang or single pole operated, may be used to advantage. It is expected that single-pole reclosing will find considerable application on existing transmission lines, even those lines designed to be relatively lightning proof, particularly as line loads increase and the present transient stability limit is approached. On new lines, the economic choice between high-speed reclosing and other means for improving performance will depend upon line length, soil conditions, stability considerations, line costs, and so on.

Single-pole reclosing can be compared to the result achieved with the ground-fault neutralizer in that its salient advantage applies for single line-to-ground faults. On well-designed high-voltage transmission lines a large percentage of all faults are single line-to-ground in nature, and practically all faults originate as single line-to-ground. Single-pole operation to clear single line-to-ground faults will, in most cases, include two-pole operation for double line-to-ground faults and gang-operated reclosing for phase-to-phase faults. This extension leaves one pole at each end of the line connected during the double line-to-ground fault to achieve some increase in transient stability limits. The installation described in this paper does not include individual pole operation for any faults other than single line-to-ground for reasons indicated above.

Compared with ground-fault neutralizers, single-pole reclosing clears ground faults without the necessity of operation with two-phase wires at full line-to-line voltage above ground, saves the cost of neutralizers, and permits the use of grounded-neutral transformers and lightning arresters so that better lightning protection of equipment connected to the transmission system is attained. During single line-to-ground faults, however, single-pole reclosing involves unbalances, and as much power cannot be transmitted during a line-to-ground fault condition as can be transmitted with neutralizers. During faults involving more than one phase, the reclosing provides a possibility of maintaining stability not enjoyed by the neutralizer system. It is probable, in view of the above comparison, that single-pole reclosing will find preference over neutralizers, particularly on major high-voltage, single-circuit, interconnecting lines.

As a general comparison for a single-circuit line, the conventional system has zero-power transmitting ability through all faults; the neutralizer system will have no outages for transient single line-to-ground faults (about 70 per cent of all faults), but will result in outages for all other faults; single-pole reclosing can be applied to have no outages for all self-clearing faults. The advantages of single-pole reclosing over three-pole reclosing are minimized if the transmitted power is kept low enough so that no outages occur for self-clearing three-phase faults, although there is still the advantage of less shock to the system during the more common single-phase and two-phase fault conditions. The higher the probability of single line-to-ground fault occurrence, compared to multiphase faults, the greater

Table III. Schedule of Single-Pole Reclosing Tests, Public Service Company of Indiana March 16, 1941

Test No.	Time	Fault Location*	Type of Fault	System Conditions
1..	8:01 a.m...	F_3	Phase *C* to ground-arcing	Normal megawatts, Lenore to Newcastle. Muncie line closed
2..	8:57 a.m...	F_2	Phase *C* to ground-solid	.. Normal
3..	9:40 a.m...	F_3	Phase *C* to ground-arcing	.. Normal
4..	11:23 a.m...	F_1	Phase *C* to ground-solid	†I.P. and L. open, Muncie-Newcastle open
5..	11:43 a.m...	F_4	Phase *C* to ground-solid	†I.P. and L. open, Columbia open at Columbia
6..	12:20 p.m...	F_1	Phases *B* and *C* to ground-solid	†I.P. and L. open
7..	12:52 p.m...	F_2	Phases *B* and *C* to ground-solid	†I. P. and L. open, Muncie – Newcastle open
8..	1:31 p.m...	F_3	Phases *B* and *C* tied solidly	.. Normal
9..	2:10 p.m...	F_2	Phases *B* and *C* tied solidly	.. Normal
10..	2:35 p.m...	F_2	Phase *C* to ground-solid	.. Normal
11..	3:16 p.m...	F_2	Phase *A* to ground-arcing	All paralleling ties open, 11 megawatts from Lenore to Newcastle
12..	3:31 p.m...	F_2	Phase *A* to ground-arcing	All paralleling ties open, 40 megawatts from Lenore to Newcastle

*See Figure 3.

†Indianapolis Power and Light Company.

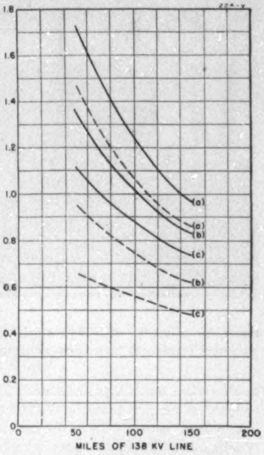

Figure 9. Comparison of transient stability limits with 35- and 20-cycle single-pole reclosing for a 250,000-kva turbine-generator station feeding an infinite system over a 138-kv line

——— 20-cycle single-pole reclosing
– – – – 35-cycle single-pole reclosing

(*a*) Single line-to-ground fault
(*b*) Double line-to-ground fault
(*c*) Three-phase fault

is the advantage of using single-pole reclosing. High-speed reclosing can be used, either three-pole or single-pole, to prevent outages for all types of self-clearing faults, a very high percentage of all faults.

In all applications of high-speed reclosing, the desired power limit, the probability of fault occurrence, the probability of the type of fault, and the percentage of time during which the line is expected to operate at the maximum stability limit should be considered in determining the speed of reclosure and the type of reclosing (three-pole or single-pole) to be applied.

Although this discussion has been confined largely to the application of reclosing on single-circuit lines, it should not be inferred that reclosing (both three-pole and single-pole) does not have application to multicircuit systems. The use of reclosing on power lines can be considered as an alternative to the addition of parallel circuits to obtain a desired transient stability limit. In many cases, this will result in large savings of capital investment. Single-pole reclosing shows up to particular advantage on multicircuit systems, for opening one conductor to clear a single line-to-ground fault, in effect merely inserts a higher impedance in one phase. Ground relaying on lines paralleling the faulted line must be arranged not to trip on the ground current circulated during the interval when one or two phases are de-energized, although relays will usually not trip for this condition if they are adjusted not to trip during the fault. The ground current, while persisting for several cycles, will usually be small—depending upon the load transferred over the lines, line impedances, and so on.

The two principal objections offered to the use of single-pole reclosing, namely, the possibility of telephone interference created by the flow of ground current during the period that one phase is de-energized, and the possibility of false ground-relay operation in adjacent line sections caused by the circulation of ground current during the de-energized period, were not encountered in this installation. It is anticipated that neither factor will be objectionable in most contemplated installations, since the ground current resulting from the open phase is small and persists for only a short time. The disturbance to other ground relaying on the system is quite small if good ground sources are available at both ends of the faulted section.

The Dresser–Lafayette line shown in Figure 2 is now under construction. The breakers for this new line will be equipped for individual reclosing mechanisms, and relaying for single-pole tripping and reclosing will be added when the 132-kv bus is established at Lafayette. Another new 132-kv line—to extend from Dresser south to Bedford, Indiana, and from there to Louisville, Kentucky—will be similarly equipped for single-pole reclosing. This line will constitute a major interconnection between the Public Service Company of Indiana, Inc., and the Louisville Gas and Electric Company.

Conclusions

On high-voltage systems where most transmission-line faults are single line-to-ground in nature, single-pole reclosing provides higher transient-power limits than gang-operated reclosing. This permits the transmission of more power over given line sections, and permits slower speeds of reclosure on very high voltage systems where adequate time allowance for arc clearing becomes of importance. If the design of a particular line is such that a larger percentage of all faults will be double line-to-ground, the advantages of single-pole reclosing over gang-operated reclosing are not as pronounced. Single-pole reclosing will find application on multicircuit as well as single-circuit lines. Entirely satisfactory relay and breaker operation is indicated from comprehensive tests made in the field.

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Evening Courses at Graduate Levels—a Challenge to Colleges of Engineering

ROBIN BEACH
FELLOW AIEE

During the past months with the hysteria of war about us and with the nation struggling to convert its peacetime activities into a gigantic war-time arsenal of fighting and defense implements, the most urgent demands have been made for a trained personnel. The present dearth of technically trained workers and skilled artisans has been strongly felt in nearly every phase of this controversion of industry—ranging from the highest levels of engineering research and design, on through the vast varieties of supervisory positions in manufacture and inspection of materiel, and into the front line trenches of industry where plowshares are being turned into proverbial swords.

Suddenly, in the dismay of a nation awakened to its utter weakness in national defense because of its years of disregard of preparedness, overnight efforts were launched to gear industries into high-speed production of war machines and their munitions, and to multiply national output many times beyond normal. Leaders of industry sought on every hand, among the millions of unemployed, the highly skilled and trained personnel by which to double and treble their plants and to multiply their manufacturing operations. Obviously, and naturally, these leaders met with disappointments on every hand, as they soon found that the personnel which they sought, already trained, was not to be had.

War Demands on Training and Education

Then came the boom in education and training, "the gold rush," if you please, to the classroom, the laboratory, the drafting and design training rooms. On every hand, in every nook and corner, national defense training courses sprang into being, some of which were fair, some indifferent, and some worthless. An attempt was here being made to do the impossible—to make up, in a few short weeks or months, for the neglects—the depreciations—in the discarded ranks of labor for the prior years of disuse of skilled hands and trained minds.

For the first time since the halcyon days of 1928 and 1929, the engineering students, soon to be graduated, were eagerly sought, with few questions asked as to their scholastic attainments. Loud lamentations were heard because double and treble the number of graduates were not available. Proposals to shorten college curricula were urged in some quarters, and in other quarters even outstanding junior students were ill-advisedly enticed away from their engineering courses.

During this extreme urgency of employing all available engineering graduates, the members of the day classes were, for the most part, the ones who were readily available. The prospective employers found these well-prepared young men virtually in the ranks of the unemployed, or at least among the nonproducers. In consequence, their number added tangible substance to the personnel of industry. Now, if you please, let us turn our attention to another source of technical graduates—a supply of considerable and growing numbers, the graduates of evening sessons from the many colleges of engineering throughout the urban areas of our land.

Perhaps in this emergency these young men do not appear to personnel recruiting agencies to stand forth as prospective "knights in shining armor" because they have, long since, been employed in industry and, in consequence, they are already in harness, sharing responsibilities effectively and intelligently with their fellow workers as a result of their educational attainments. Of these graduates from evening sessons in engineering, and of the facilities for their training, we hear much less than of the conventional facilities for the instruction of day students.

Night Courses in the New York City Area

Here in the metropolitan New York area seven colleges of engineering have been offering evening courses leading to baccalaureate degrees. Of these the Polytechnic Institute of Brooklyn has had a long career in its service to the young men of this area, having pioneered in this type of engineering education in America, and having organized itself for this additional sphere of activity as early as 1904. The curricula and the standards of the day and the evening sessions are identical.

In those urban communities where the concentration of industry is relatively dense, obviously large numbers of young technical graduates are found among the personnel who are engaged in engineering supervision, design and planning, of industrial research, and of semi-engineering activities of countless types. The more ambitious of these feel a challenge offered to their abilities by the increasingly complex problems which they encounter in the rapid march of technological progress. They find need for more specific knowledge of engineering science in order better to cope with the growing intricacy of electrical systems with which they are engaged.

It is natural that they should turn to the engineering colleges near them in seeking advanced education to aid them in the more intelligent pursuit of their daily tasks. At this critical period of their professional lives, they know precisely wherein they lack certain educational facilities to cope with their new and advanced problems. It is then they need most the help which graduate study can give them. They have already become oriented in the field of their life's work, and perhaps quite a different one from that which they envisioned upon the day of graduation, so they can now choose more intelligently the character of advanced study which will serve them best.

The Organization of Graduate Courses

The Polytechnic Institute recognized this call for graduate instruction from these young men of industry as far back as 1925. In a community where so many utilities are engaged in the public service of electrical communications, of power and light, and of transportation, a course of basic and common interest to the many engaged in these various fields was that of advanced electrical-circuit analysis, in which the spectrum of frequencies considered was from commercial power values to those then employed in radiobroadcasting. From this small start, with but a handful of students, all eager to learn, appreciative of the opportunities offered,

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Robin Beach is professor of electrical engineering and head of the department with The Polytechnic Institute of Brooklyn, Brooklyn, N. Y.

and hoping for additional facilities in such evening instruction, we launched upon our program of evening graduate study.

The results of our adventure into the then little explored field of evening graduate service have led to the development of the present programs of courses culminating in the master's and doctor's degrees in electrical engineering. It is of passing interest to note that the handful of students of the earlier years has grown into a registration of about 100 students of electrical engineering per year in continuous attendance throughout the period of the past decade. Of more recent years, other engineering colleges here in the New York area have likewise added evening courses of instruction at the graduate level to help in this service of affording the facilities of advanced education to the thousands of engineering graduates employed within our metropolitan area.

The programs of evening graduate study may be readily inspected by reference to the appropriate college catalogue. This listing of courses, citation of their contents, and enumeration of the assigned members of the teaching staff, whereas enlightening and informative, do not, however, give any indication of the problems encountered in the organization, administration, and growth of this graduate service, and of the different emphases in the philosophy of education as applied to this particular type of education.

With the hope that the review of some of these specific problems which confronted us may be of value or assistance to those who are now entering upon enterprises of a similar character, the author, with apologies for the close personal nature of these citations, ventures to describe those elements which played a dominant role in the evolution of our present electrical engineering evening curriculum.

After a brief experience with the instruction of these graduate evening students, several observations served us well in establishing subsequent procedures of organization and administration for this type of instruction. We found that these young men differed from the conventional graduate students of the day session in a number of essential characteristics. Most of them had the advantage of from three to five years, or more, of engineering experience which provided them with the motivating urge to develop further their mental facilities through evening study in advanced courses. These young men were matured, serious, and purposefully minded. They recognized in these opportunities for graduate instruction a possible means of enabling them to rise more rapidly above their present engineering status.

Many of them were engaged in various advanced phases of electrical engineering work, frequently of the industrial research type, wherein they discovered their knowledge to be deficient in meeting effectively, and with necessary dispatch, the problems of the day. They too often realized this disadvantage when attempting to study collateral bibliography because of their unfamiliarity with the various branches of advanced mathematics which they repeatedly encountered. Therefore, they sought a knowledge of advanced mathematics and a facility with its applications to a wide variety of electrical problems, as well as a thorough training in the more theoretical phases of electrical science—all to equip them with the ability to appreciate and to understand the literature in their respective fields. In addition, they expected that their increased knowledge at these higher levels of engineering and scientific attainments would provide them with the facility of technical expression by which they, in turn, would be enabled some day to contribute to engineering literature.

One of the prerequisites, or gratifications, granted to those of us who are in close association with this type of educational enterprise, is to observe the realization of the hopes of these young men to achieve such objectives and to actually contribute technical papers of commendable character to the various engineering and scientific periodicals. In fact, such achievements might well serve as an effective measure of the performance of these young men at the graduate levels of education.

The Instructor

It may appear trite and needless repetition to put into words the personal, industrial, and scholastic qualifications, the specifications, if you please, for instructors who contemplate this type of graduate teaching; but be assured that no phase of organization in this endeavor stands paramount to that of the selection of the most capable and highly qualified engineering educators. For the instructor of evening graduate students to enjoy the fruits of success in his endeavors, he should possess a rich, full educational background in the broader aspects of general engineering and, of course, he should be a master in the restricted confines of his primary interests. This should be supplemented with the invaluable experience to be gained from a period of engineering service in industrial practice in order to become intimately conversant with the varieties of problems of industry relating to his special field. He will then have a clearer perspective of the objectives of his instruction. The educational facilities of his courses will also serve his students better in meeting their urgent day-to-day needs, and they will also help to provide a more technically trained personnel for industry.

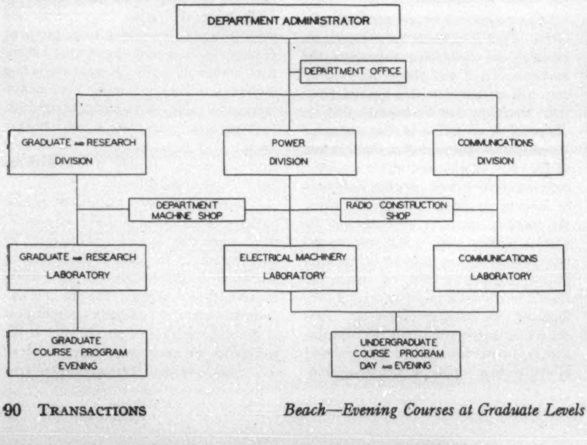
On the personal side, this instructor should have a cordial respect for the aspirations, the motivating influences, and the eagerness for learning of these evening students. His prestige as a scientist, an engineer, and an educator should invite a warmth of response from his class members. He should be their inspiring and cultural leader, and an enthusiastic proponent of this type of cooperative education. Nothing short of these essential attributes of the capable graduate instructor will meet with more than mediocrity in this challenging venture of higher education.

Those individuals in graduate engineering education who possess these requisite qualifications for teaching are indeed rare. This situation exists largely as a result of the natural and inexorable economic law of supply and demand. The prospective young teaching initiates—those exceptional young men of high scholastic attainments who can perform and think effectively at the doctorate level and who possess the many other attractive attributes of a cultured scientist-engineer are urgently importuned to remain in the industry wherein they are serving their internships in acquiring the practical experience prerequisite to teaching. Why shouldn't industry want to retain the services of such promising young men? If the graduate classrooms and research laboratories of our colleges of engineering are to be graced with the capabilities of these industrially groomed protégés of advanced engineering education, obviously such prospective young teachers can only be induced back to the campus by sufficiently attractive rewards. Industry pays them their current market worth. Do we expect them to sacrifice their careers in industry to teaching for less? Some apparently do.

Colleges have been known to accuse industry of unethical practices of competition when they have been frustrated in their attempts to employ these highly educated scientist-engineers by unattractive offers of salary. The problem appears to be one of justifiable competition in the open market of personnel. If we seek gems, we must be prepared to meet their market values. Perhaps the colleges are not overly ethical at times in bartering too low for highly qualified engineering teachers. In our perplexing problems of personnel, of which we have had many, we found industry genuinely co-operative in its willingness to help. Many of the industrial leaders are farsighted in such matters. They envision that their research and design engineers of tomorrow, their future executives as well, are the offspring, let us say, of university adventures in the higher technical educations of today. In consequence, they want these prospective employees taught with utmost thoroughness. They would prefer the colleges of engineering to administer the basic instruction, but because they have not found the graduates well grounded in basic science and engineering, a number of the larger companies have felt impelled to do the job themselves concurrently with their inductive training for specialized branches of service.

As though to lend support and credence to this implied malignment of the incompetence of instruction, how often do we see the graduate instructor who is, figuratively, but a hop, skip, and jump ahead of his class—the instructor who can lay claim to no achievements in his sphere of technical activity? He is restricted in his services by a lack of knowledge and facility with mathematics; he is circumscribed by a "low ceiling" of vision in his specialized field; and he has literally a complete "blackout" of knowledge in the general field of engineering and science. How can the instructor who has achieved no success at the levels of graduate endeavor, lead, inspire, or encourage his students to the attainment of what he, himself, has been unable to attain?

Figure 1. The organization chart for the electrical-engineering department, Polytechnic Institute of Brooklyn, showing the lines of delegated responsibilities


DEPARTMENT ADMINISTRATOR
DEPARTMENT OFFICE
GRADUATE – RESEARCH DIVISION
POWER DIVISION
COMMUNICATIONS DIVISION
DEPARTMENT MACHINE SHOP
RADIO CONSTRUCTION SHOP
GRADUATE – RESEARCH LABORATORY
ELECTRICAL MACHINERY LABORATORY
COMMUNICATIONS LABORATORY
GRADUATE COURSE PROGRAM EVENING
UNDERGRADUATE COURSE PROGRAM DAY – EVENING

The Administrator

The administrator of an active, well-organized department finds himself with a full-time job of attending to its manifold activities. His services to the college and to his department divide themselves into two major lines of responsibility. On the one hand, the dispatch of the minutiae of normal routine—in trying to keep the multiple divisions of the department's activities progressing smoothly and effectively—constitutes his most time-consuming, but lesser important, function, while on the other hand, his major administration, and the one by which his competence is primarily judged, is his leadership in engineering education. Does he promote high standards of instruction; does he stimulate in the students and faculty members inspiring ambitions; and does he encourage in his associates the joy of planning for future developments and achievements?

In the execution of his duties he has probably discovered by experience how much more satisfaction is derived through the sharing of certain groups of these responsibilities with the fellow members of his staff. At the Polytechnic Institute, this plan has been effectuated in the electrical engineering department through the organization of three divisions—the power division, the communications division, and the graduate and research division. The organization and functional chart which shows the lines along which the department has developed over a period of years is portrayed in Figure 1 to indicate the major divisions of delegated responsibility. In each of these, further delegations of duties are carried down even to the grade of assistants and graduate fellows, so that everyone on the staff has his part in the administration of duties commensurate with his position and in addition to his major function of service.

In this paper we are primarily concerned with the organization of the graduate and research division which coordinates and administers all matters pertaining to the evening graduate courses and to programs of research. The organization chart for this division is shown in Figure 2 to illustrate the lines of responsibilities here. Under this plan each member of the staff is charged with definite duties of a restricted scope which he is encouraged to administer within a certain degree of autonomy. His efforts and accomplishments are given full recognition, and his initiative and enterprise are factors which are considered in his advancement. Ideas and plans for improvements in the service of the division are discussed in meetings of its personnel, and when necessary department meetings are held to discuss the broader aspects of those plans and proposals wherein the composite judgment of the staff members is desired.

The department head co-operates to his fullest extent in trying to relieve the staff members of the graduate division from any arduous duties of administration that will unduly distract their attentions from their graduate and research endeavors. He should recognize that needless distractions from their absorbing problems demoralize their interests and dissipate their efforts. Among the administrative problems which he may find at times almost insurmountable, and which have their origin in just such distractions and interruptions, are those of providing required space facilities, of acquiring expensive pieces of necessary specialized apparatus, and of securing adequate and capable mechanician services and laboratory assistants. These problems arise largely, of course, because of inadequate appropriations—a subject mentioned with respect and without comment.

A word of caution which the administrator should impart to all instructors of evening graduate courses is that of urging them to so allocate their time as to provide unfailingly for adequate preparation of all phases of their course notes. The lure of research must not be allowed to unbalance their sense of proportion. Unpreparedness for a session is no less disastrous to the success of the course than is incompetence. The young men of industry who enroll for graduate study are making sacrifices of their marginal time, and, too, they are paying well for the privilege. They have confidence in the integrity of their neighboring engineering college to provide instruction of the highest quality. The administrator of the courses should make doubly certain that he is not inadvertently selling to these ambitious young men "gold bricks" of education.

Table I. Showing the Groups of Courses in the Graduate Program of Electrical Engineering Leading to the Master's and Doctor's Degrees, Polytechnic Institute of Brooklyn

G619	Introduction to the Theory of Functions	(M)
G351	Analysis of Transient Phenomena (Pre G619)	(M)
G625	Vector Analysis	(M)
G353	Electromagnetic Theory (Pre G625)	(M)
G357	Theory of Electrical Measurements*	
	Major Course Groups	
	Power	
G361	Power Transmission and Distribution Theory	
G363	Advanced Alternating-Current Machinery*	
G367	Design of Electrical Machines*	
EEW-254	Protective Relaying of Power Systems*	
G731	Dynamics of Machines and Vibrations*	
G740	Advanced Thermodynamics	
G741	Steam Power Plants*	
G743	Steam and Gas Turbines*	
	Communications	
G371	Theory of Electronic Tubes and Their Circuits	
EEW-293	Design and Application of Electronic Tubes*	
G373	Transmission Network and Filter Theory*	
G377	Advanced Network Theory* (Pre G619, G373, G627, G351)	
G384	Ultra-High Frequency Theory* (Pre G353)	
G355	Conformal Mapping* (Pre G619)	
	Electrophysics	
G837	Introduction to Theoretical Physics	
G627	Fundamentals of Mechanics*	(D)
G845	Statistical Mechanics*	(D)
G381	Fundamentals of Electronics*	
G841	Fundamentals of Radiation*	
G847	Quantum Mechanics** (Pre G845)	
G629	Higher Mathematical Analysis II*	
	Minor Course Groups	
	Physics	
In addition to "Electrophysics"		
G831	Advanced General Physics	
G834	Acoustics**	
G835	Optics**	
G842-3	Physical Measurements	
G844	Conduction of Electricity Through Gases**	
	Mathematics	
In Addition to "Basic Course Group"		
G621	Advanced Calculus	
G623	Differential Equations	(D)
G629	Higher Mathematical Analysis I**	
G631	Matrix and Tensor Analysis*	
	Physical Chemistry	
123	Elementary Physical Chemistry	
G121	Advanced Physical Chemistry	
G123-4	Advanced Physical Chemistry Laboratory	
G125	Chemistry of Colloids*	
G126	Chemical Thermodynamics*	
G127	Advanced Electrochemistry*	
G673	Metallography and Heat Treatment*	
G692	Metallurgical Thermodynamics*	

(Pre) Prerequisite courses.
(M) Courses required for the Master's Degree.
(D) Courses required additionally for the Doctor's Degree.

* Given in alternate years.

** Given every three years.

In the graduate instruction of these young men, almost the entire gamut of engineering practice may be represented in the experiences of the combined membership of the class, so that questions of a wide variety, and of astounding degrees of depth, may enter the discussions of even the first-class session. It is by no means uncommon to have numbered among the class members, those with an enviable array of technical publications pronouncing them authorities in their particular fields of endeavor.

How obvious it appears, then, that these young men should expect in their mentor one thoroughly versed in his field in order that he should stimulate them to their greatest attainments, and inspire them to carry on far beyond the confines of the class lessons. Let the administrator spend more time and more resources to provide his department with a staff of research investigators and graduate teachers who will meet the fullest expectations of these young men.

The problem which confronted us at the Polytechnic Institute in the beginning of this new venture, and one which may perplex others who are contemplating the organization of this form of education, or its counterpart in nonresident instruction, was how to provide this highly qualified instructing personnel. In those early days we were feeling our way along through the shoals of both economic and educational hazards. The "pay as you go" policy which we adopted from the start certainly did not make easy the problems of providing graduate instructors. We solved this problem temporarily by affiliating with us some of the foremost scientists, engineers, and industrial research specialists here in the metropolitan New York area.

We induced a few of these specialists who were especially well qualified through the breadth of their educational background and of their experience, through their love of their work, and through their demonstrated ability to impart their knowledge to others, to join, as part-time members, our graduate staff. They organized and offered instruction in certain prescribed basic courses in which they were pre-eminently qualified to bring to the class a wealth of experience in industrial applications, a broad vision of the frontier problems of present day science, and a richness of enthusiasm for their subjects. By this means we were enabled to establish and maintain high standards in regard to the qualifications of our teaching personnel.

The department was also privileged to have associated with it, during the formative period of this venture, the part-time services for two years of Vladimir Karapetoff, the revered dean of America's electrical engineering teachers, and the full-time services for one year of Bernard Hague of the University of Glasgow, the pre-eminent authority on electrical measurements, as a visiting professor.

The Curriculum—at the Master's Level

The development of a curriculum of courses leading to the master's degree and to the doctor's degree is a subject charged and surcharged with controversy. The adherents of the "free choice" of subjects at the graduate level would probably not condone the curriculum that contains a large percentage of required sub-

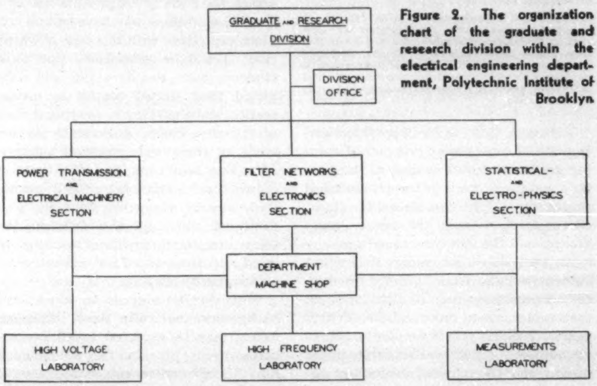

GRADUATE AND RESEARCH DIVISION
DIVISION OFFICE
POWER TRANSMISSION AND ELECTRICAL MACHINERY SECTION
FILTER NETWORKS AND ELECTRONICS SECTION
STATISTICAL- AND ELECTRO-PHYSICS SECTION
DEPARTMENT MACHINE SHOP
HIGH VOLTAGE LABORATORY
HIGH FREQUENCY LABORATORY
MEASUREMENTS LABORATORY

Figure 2. The organization chart of the graduate and research division within the electrical engineering department, Polytechnic Institute of Brooklyn

jects. After lengthy consideration of this important subject, and based upon several years of experience, we are the more convinced that, in the curriculum leading to the master's degree, certain subjects are so basically important that the omission of any one would seriously weaken the foundation upon which the superstructure of the student's subsequent educational endeavors is based.

The subjects which we require of all electrical engineering students are:

1. Vector analysis.
2. Introduction to the theory of functions.
3. Analysis of transient phenomena.
4. Electromagnetic theory.
5. Electrical engineering seminar.
6. Thesis

These courses make a total of twenty semester hours of the minimum of thirty which are required for the degree. The elective subjects, totaling ten or more semester hours, are chosen by the student in conference with the department adviser, and these subjects are generally more or less closely allied to the field in which the student is employed during his day hours.

These lines of specialized study have been formulated into three general "major" classifications in each of which the following subjects are included:

Power Engineering. Electric-power generation, transmission and distribution, system calculations, electrical protection, system stability, design of machines, transient behavior of machines, and a selection from several subjects in mechanical engineering.

Communication Engineering. Electronic tubes, their theory and their circuits, network and filter theory, advanced network theory, fundamentals of electronics, ultrahigh-frequency theory, conformal mapping, theory of electrical measurements, fundamentals of mechanics, and a selection from several basic subjects of physics, including acoustics, introduction to theoretical physics, and fundamentals of radiation.

Electrophysics. Conformal mapping, theory of electrical measurements, fundamentals of electronics, fundamentals of mechanics, and a selection from several basic subjects of physics, including fundamentals of radiation, physical measurements, conduction of electricity through gases, statistical mechanics, quantum mechanics, and physical properties of metals.

Reference to Table I will provide a perspective of our present program of evening graduate courses leading to the master's and the doctor's degrees in electrical engineering. Here are shown the basic, or required, courses, the major course groups, and the minor course groups.

An analysis of our records shows that students who have been granted the master's degrees have been in attendance, on the average, about three and three-fourth years. The first year is devoted largely to the subjects of mathematics, while in the second year the required subjects of circuit analysis and electromagnetic theory are taken; during the third year the student generally completes the elective subjects and starts thesis work, and in the fourth year his time is wholly devoted to the thesis investigation. Occasionally students with less call on their marginal time by outside activities, or home ties, take an extra course or two in their first two years—thereby sometimes completing their thesis work and all other requirements for the degree at the end of the third year. Attendance without credit is required at the electrical engineering seminar during each year of enrollment in electrical courses. The speakers at these seminar sessions, who are invited to address the students and staff members and lead the discussion, are outstanding men of science and engineering here in the New York area.

The Thesis

Of the many aims and objectives in graduate study, the outstanding one is, in a few words, the development in the student of his capabilities to carry on a co-ordinated program of original thinking and independent investigation. We believe that the thesis is an essential means of appraising the student's ability to conduct an advanced and original investigation of an appropriate engineering or scientific subject, of showing his mastery of the subject matter of the courses he has taken, of demonstrating the development of his processes of clear analytical thinking, and of proving his facility in expressing his thoughts and his developments of theoretical conceptions, properly and adequately, in technical language.

The competence of these evening graduate students to undertake and vigorously attack their thesis assignments will be a revelation to those who have had no previous experience with this type of education. Let it be remembered that these students have already taken and completed their formal courses in mathematics, and that they have applied most of it many times subsequently in the study of their basic electrical subjects. After they have completed all of these required courses and most of their specialized subjects, they then have the psychological advantage of undertaking the thesis investigation with an assurance of mind and the educational advantage of an advanced viewpoint.

With this training as an educational background, naturally these thesis enrollees may be expected to achieve superior results, provided they are inducted into this new undertaking with a personal understanding of their possible pitfalls and of their inhibitions to use all they have learned to the best ends. The assignment and guidance of thesis work requires a particularly careful vision as to the probable length and character of the problems, the facilities for laboratory measurements, and the general nature of the theoretical aspects of the problems. It is so easy to extend beyond the bounds of a reasonable thesis, unless proper care is exercised.

Not infrequently students are enabled to carry on their investigational work under the co-operative auspices of their supervisors on their jobs, in which cases a close collaboration is maintained between an assigned member of the graduate division of the department and the direct supervisor in the company with which the graduate student is employed. This co-operative plan for the thesis investigation is especially helpful in certain projects that involve a wealth of specialized apparatus with which the company may be well equipped.

We have been operating under another co-operative plan for the past three years by which employees of the Westinghouse Electric and Manufacturing Company may receive graduate course credits, to a maximum of fourteen, towards the master's degree at the Polytechnic Institute for courses which they have passed with honor grades in the company's graduate program. Reciprocally, our students may elect to take certain courses, for credit, offered by company specialists on the Westinghouse plan. These courses for interchange credit are restricted to the applied types, in particular. New York University and Stevens Institute of Technology also share in this Westinghouse plan in the New York area.

The Doctorate Program

Having organized and administered for several years the curriculum leading to the master's degree, an insistent demand kept growing for the extension of our graduate program into the doctorate level. Several of those, who had made meritorious achievements in their work leading to the acquirement of the master's degree, were engaged in the research activities of various neighboring industries. Most of them had already published papers setting forth the results of these various research investigations, but they wished additional knowledge in the collateral fields of electrochemistry, electrophysics, and mathematics, and in the frontier science of electrical engineering. For their immediate benefit, a number of these young men continued their studies in the various advanced courses which were being offered in mathematics, physics, and electrical engineering, although they expressed their hope that eventually the electrical engineering department would offer a program of study leading to the doctor's degree. This step was formally taken in 1936.

In organizing instruction at this level, we departed basically from our objectives and philosophies underlying the master's program. Here, we held that "freedom" should be allowed the students in choosing broadly from those courses which would focus their attentions most specifically upon the lines of study in which their life's work seemed directed. To assure definiteness of purpose, it seemed desirable that these subjects be classified within a major field and within one, or possibly two, minor fields of concentration, and at least one of the minors be chosen outside of the offerings within the major field. Students who enroll for doctorate study are referred to individual guidance committees whose purposes it is to direct their selection of courses of study and collateral reading, and to advise them in the preparation for qualifying examinations in the subjects of their major and minor fields, in gaining an adequate understanding of two foreign languages, and in undertaking the investigational program that constitutes the dissertation.

Thus far we have graduated three students with the doctor of electrical engineering degree, but an increasing number are aligning themselves for qualification in the near future. The prospects look favorable for the granting of three, or possibly four, doctor's degrees at the end of the current academic year. Actually, about twenty evening students are at various stages of doctoral preparation above the master's level at present. The number of course credits above the master's degree leading to the doctorate is a minimum of twenty-four and the time required for the dissertation is the equivalent of at least sixteen additional credits. The selection of students for this level of educational attainment is rigorously based upon the evidence of ability which they have displayed in prior years in achieving honor grades for courses at the master's level, and in conducting meritorious research work. Students are not allowed to enroll in the doctorate program unless they have conducted thesis investigations for the master's degree.

It is too early in the progress of our venture in this division of graduate service to draw conclusions. Yet from our experiences in the instruction of those who have already obtained the doctor's degree and of the twenty now enrolled in this advanced program, we believe that we are headed in the right direction. If such is possible, our applied philosophy is one of overemphasis on thoroughness, high quality, depth of specialized study, and unquestioned ability to carry on independent studies and investigations.

This process of education, through evening study, imposes an unrelenting and self-administering firmness in maintaining the quality of its attainments by discouraging those who cannot stand its rigors. Those possesssing lesser qualifications, who, in consequence, must devote abnormal time to the preparation of their assignments, or who just cannot orient themselves to originality of thought are self-prompted to withdraw voluntarily. At this level of education, the students are not given the benefit of doubt in the event of their work falling slightly below honor grade. We believe that at the doctorate level the time for the "mollycoddling" of students has definitely passed. They are expected to perform at high levels of excellence in all of their work and at all times.

Decentralized, or Nonresident, Instruction

The basic elements in the organization of nonresident graduate evening courses which must be given careful consideration by those who contemplate embarking upon this type of educational enterprise are

1. The choice of subject to be offered.
2. The selection of a well qualified instructor.
3. The development of the content of the course.
4. The establishment of prerequisite courses, especially in mathematics.
5. The procurement of suitable housing facilities.
6. The qualifying of applicants.
7. The provision for critical supervision.

The ultimate objectives of this decentralized instruction determine to a large degree the specific emphasis of those policies which influence the decisions on some of the above elements. For example, is it contemplated that the courses are to constitute a miscellany of specialized subjects in electrical engineering to provide advanced instruction for employees in industry who seek immediate answers to their urgent problems of design and manufacture? If so, are the courses to be organized at a truly graduate level, with prerequisite courses made available, and with admission allowed only to qualified applicants? In this case, graduate credit for the courses could be allowed by the college, although this is an emolument of doubtful value unless a full program of nonresident courses is planned whereby the applicants may be encouraged to continue for the master's degree.

On the other hand, perhaps the contemplated courses are to form a co-ordinated program of basic and applied subjects in science and engineering which will provide the equivalent of the day instruction leading to the master's degree that may already be in operation at the college. The latter plan, of course, encompasses an elaborate duplication of facilities at the urban center which now constitutes a part of the campus organization. In effect, this plan anticipates the establishment of another division of the department of electrical engineering, which is specifically organized to offer graduate evening instruction within the confines of the neighboring urban area. A plan of this magnitude is, of course, a major undertaking and one which should be subjected to most careful economic analysis and critical study.

The former plan of offering a few specialized engineering courses at graduate level, either with or without college credit, may be readily initiated, and the present time seems to be particularly favorable for starting them. Such courses could be organized to meet the current demands of industry for intensified technical specialization by providing instruction for those employees who find themselves inadequately prepared to cope effectively with the exacting problems of this critical period. This plan, if effectuated now, would possess the dual advantages of meeting the present training needs of a war-time industry in its present emergency and of being organized at a most favorable time to encourage its financial support. With the present dearth of personnel possessing specialized training which now exists in many quarters, financial aid could probably be obtained from the government, but if not, where the need really justifies it, neighboring industries could, most likely, be induced to sponsor the courses and to subsidize small deficits of operation.

The Polytechnic Institute has offered some instruction, by request, on a nonresident basis, wherein a considerable number of employees at a distant plant wished a specific course. In these cases it was possible for a member of our normal staff to conduct the course. Graduate credit was allowed only to those who fulfilled our normal qualifying requirements. We are now finding a most urgent de-

Resistance-Welding Transients

E. E. KIMBERLY
MEMBER AIEE

Synopsis: This paper discusses quantitatively the effect of indiscriminate (random phase-angle) switching in the primary of a resistance-welding transformer supplying a resistance welder. The current and power transients occurring because of "off-angle" switching are investigated through oscillographic records of transients in loads built up to simulate actual welder-head loads as nearly as possible.

In the switching of loads of either high or low power factor and those with or without iron at high-flux densities, the power transients were found to be damped to unimportant values within the time of a very few cycles after the instant of switching. In spot and projection welding, the first few cycles may be extremely important, however, determining the quality of weld even though the total time of the weld period is much greater. This investigation is limited to 60-cycle frequency and to welding periods of two cycles or more.

The General Problem

When a circuit containing resistance and reactance is energized from a source of alternating current, its initial current is likely to be greater than it will be after a few cycles have elapsed.[1,2] The current is also likely to be affected by the momentary unusual resistance voltage drop in the primary winding of the supply transformer. Inasmuch as this paper applies only to heat transients in the weld itself, the surge of current in the supply transformer primary will be considered only as it affects the heat produced in the weld because of its effect on the secondary voltage. The transformer used had an impedance of about 10 per cent.

Because of the great diversity of types of resistance-welding loads in which both ferrous and nonferrous metals are placed in the welding throat, generalization is somewhat difficult, and only a limited number of specific cases can be considered in detail in a paper of this kind. All resistance welders have a common characteristic, however, in that their welding heads carry heavy currents, and the leads to the transformer secondary coil enclose an area which accommodates a flux which produces an inductive reactance of some importance. Whether the metal introduced into the throat be ferrous or nonferrous, the welder head and its transformer leads can be considered theoretically and practically for transient analysis the same as any other circuit consisting of resistance and inductive reactance in series. This investigation was carried out using three types of artificial circuits built up to simulate representative welding loads.

Circuit 1 was of unusually high power factor of 0.85. Circuit 2 was of medium power factor of 0.5. Circuit 3 was of more common power factor of 0.22. The power factor of a welding circuit is, of course, fixed by the ratio of resistance to the reactance caused by the magnetic flux enclosed by the throat. From this fact it is apparent that a large throat or ferrous metal in the throat will be conducive to low power factor. With the shortest possible throat depth in a practical design it is difficult to obtain a power factor when spot welding of more than about 0.6 with a frequency of 60 cycles.

No attempt was made to control the residual flux of either the transformer or of the load, because it was considered reasonable to assume that the stock to be welded has no remanent magnetism and that the transformer residual flux will produce in these tests the same secondary-circuit effects as in an ordinary welder transformer. The welding current used was about two times the permissible continuous current in the transformer.

Method of Testing

To examine the power transient characteristics of the three representative circuits, they were switched on at a great variety of instants on the applied voltage cycle, and many visual observations were made. Only those found to indicate special conditions, such as maximum additive or subtractive power transients were repeated, photographed, and reproduced in this paper. The curve sheets, therefore, show extreme results of "off-angle" switching for the various circuit conditions. The time constant of flux decay as expressed by the ratio of inductance to resistance, L/R, is affected not only by the resistance of the welder and transformer secondary coil and the flux enclosed by the throat but also by the entrapped flux in the transformer core itself. Because of this fact, the rate of decay of transient current is influenced by the size of transformer used relative to the welder requirements; the rate of decay being less as the transformer size is increased.

The energy delivered to the weld for any period was found by planimeter integration of the power curves on the oscillogram. The power oscillograms were taken by a power galvanometer with the current coil in series with the welding circuit and the potential coil connected across the secondary of the welding transformer. While the power transient had not entirely subsided in some cases by the time the end of the oscillogram was reached, the steady-state energy per cycle was available in every case from another oscillogram in the same series with the same load.

Results and Conclusions

1. **Random switching may result in a good weld, a cold weld, or a hot weld when the welding period is less than about 20 cycles.**

2. Random switching may cause as much

Paper 42-25, recommended by the AIEE committee on electric welding, for presentation at the AIEE winter convention, New York, N. Y., January 26-30, 1942. Manuscript submitted May 8, 1941; made available for printing November 25, 1941.

E. E. Kimberly is professor of electrical engineering, Ohio State University, Columbus, Ohio.

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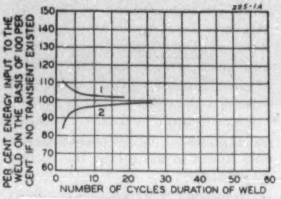
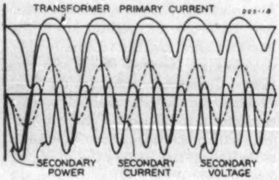
mand from neighboring industries for instruction in the theory of ultrashort waves, their production, transmission, and reception. This course was included in our current curriculum for the second semester, and we are advised to expect a large enrollment. In fact, we have been requested to consider the organization of certain classes in the subject at remotely located plants where a sufficient number might warrant nonresident instruction.

The call of the junior engineers of industry for more and more of this type of graduate evening instruction offers a resounding challenge to the technical schools, in or near the urban centers, to organize their facilities for taking an active part in this growing enterprise. Already a number of colleges of engineering throughout the United States has responded to the opportunities offered in this attractive field, and others are now formulating their plans to inaugurate programs of advanced courses in the near future. These colleges will soon find, too, that the fruits of their new adventures bear to them deep satisfaction and stimulating pride in the achievement of their discovery of a new and broader field of service.

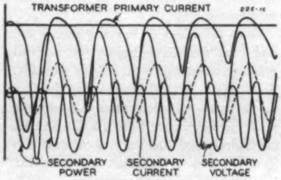


50 per cent power factor

1. Transformer primary closed at zero line voltage
2. Transformer primary closed 45 degrees before zero of line voltage

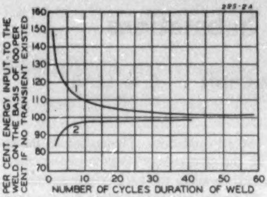


Oscillogram 1



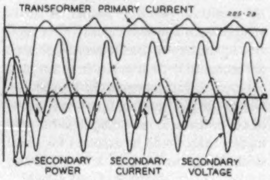
Oscillogram 2

Figure 1. Oscillograms and heat-input curves for circuit 1

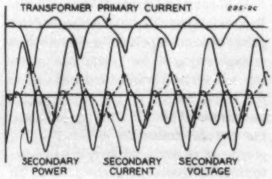


85 per cent power factor

1. Transformer primary closed at zero line voltage
2. Transformer primary closed 68 degrees before zero of primary voltage

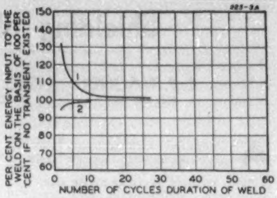


Oscillogram 1



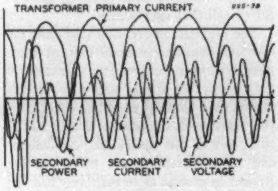
Oscillogram 2

Figure 2. Oscillograms and heat-input curves for circuit 2

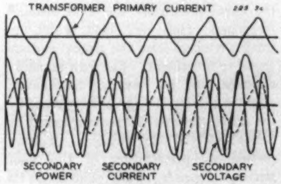


22 per cent power factor

1. Transformer primary closed at zero line voltage
2. Transformer primary closed 85 degrees after zero of line voltage



Oscillogram 1



Oscillogram 2

Figure 3. Oscillograms and heat-input curves for circuit 3

as 50 per cent variation in the heat produced in a weld having an "on" time as short as two cycles, because of unpredictable transients. Such transients may also soften the electrodes and materially shorten their useful lives.

3. Random switching will cause an error not in excess of five per cent in the heat produced in a weld having an "on" time exceeding 20 cycles at a line frequency of 60 cycles. This statement is true only on condition that the welding circuit is opened between welds, or that the time interval between consecutive welds is sufficient to permit the complete dying out of entrapped flux in the throat. Any entrapped flux lingering from a previous weld period will tend to be accumulative in further saturating the transformer core.

In the case of projection welding even when the weld time does exceed 20 seconds, a transient of added energy in the first two or three cycles may be disastrous, because of the exploding away of the projections, and for that reason accurate switching control is imperative.

4. When the welding period is less than about ten cycles the heat delivered to the weld may be appreciably increased or decreased at will within small limits, depending upon the number of cycles of the period by controlling the instant of closing the primary circuit of the transformer. This procedure is permissible, of course, only if the voltage disturbance caused by the transformer primary-current inrush is not excessive when the switching point on the voltage wave departs from that at which the minimum transient occurs. Such practice may, however, result in the softening and extrusion of the electrodes.

References

1. TRANSFORMER ENGINEERING, Blume. John Wiley and Sons, New York. Pages 23–33.

2. INTRODUCTION TO ELECTRIC TRANSIENTS, Kurtz and Corcoran. John Wiley and Sons, New York. Pages 149–59.

Shielding of Substations

C. F. WAGNER
FELLOW AIEE

G. D. McCANN
ASSOCIATE AIEE

C. M. LEAR
NONMEMBER AIEE

As compared to transmission lines, it is more important that overhead ground wires or vertical masts over substations be correctly located so as to provide shielding of the structure against direct strokes of lightning. In a previously published paper[1] the authors discussed the shielding characteristics required for transmission lines. The present paper extends these investigations to the shielding of substations.

The previous paper indicated that the essential characteristics of natural lightning, that must be correctly simulated so that laboratory sparks and scale models can be used to study shielding effects, are the relative development of the initial streamers of the discharge. Schonland[2] and his associates found that lightning strokes to ground or to relatively low objects, such as transmission line towers, are initiated by a streamer propagating from the cloud practically the total distance to ground. Only very short, if any, upward streamers from the ground end are found to be present. The path taken by a stroke and its resulting terminating point on the earthed end is determined by the initial downward streamer, called by Schonland, the pilot streamer. The direction of propagation of the pilot streamer depends, at any point along its path, upon the electric field produced by the charges in the cloud, at the ground, and in the streamer itself, and upon localized conditions of ionization at the tip of the streamer. These localized effects tend to make the path erratic so that, as shown by laboratory tests, for the same configuration of cloud and ground, no two strokes will follow the same path.

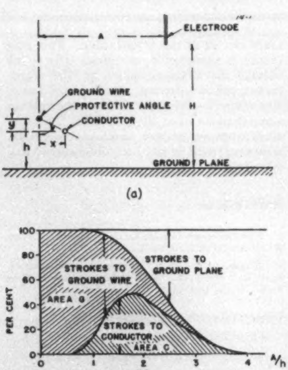


Figure 1. Symbols utilized with stroke-distribution curves for overhead ground wires

It was found that, although natural lightning is predominantly negative in polarity, the relative streamer development is best represented in the laboratory by positive polarity sparks, that is, strokes from a positive cloud. For negative laboratory sparks, upward streamers are more likely to originate from the most exposed object and span the greater portion of the gap spacing, thus giving rise to more optimistic shielding results with models than would be expected for actual strokes in nature.

Description of Model Tests

As in the previous work, this investigation employed $1^1/_2$x40-microsecond impulses of positive polarity at the minimum voltage required for breakdown of the gap. A vertical pointed rod was used as the cloud source of the stroke and a smooth metal plane for the ground plane. For the determination of the shielding properties of overhead ground wires protecting horizontal line conductors, the arrangement shown in Figure 1a was used, in which the symbols are clearly defined. Owing to the localized variations at the tip of the pilot streamer, all strokes from a given position of the cloud electrode do not follow the same path and terminate at the same point at the ground. Thus, the strokes divide between the three possible terminating points: the ground wire, the conductor, and the ground plane. It is found that the probability of the protected object being struck although decreasing as more favorable protection is afforded does not necessarily become zero.

Because the shielding characteristics are the same at any point along the line, they can be determined from a two-dimensional plot such as shown in Figure 1b. This curve was determined by increasing A from zero and counting the proportional distribution between the three possible terminating points until A reaches such value that all strokes strike the ground. The ratio of area C to the sum of areas C and G represents the proportion of the total strokes to the system that strikes the conductor.

The shielding characteristics of a mast involve a three-dimensional problem, as shown in Figure 2a, which defines the symbols used. The origin of the stroke is located by the dimensions A and θ, and the distribution curves now become the three-dimensional surfaces of Figure 2b. The volume C, that resembles a rounded cone, represents the total strokes to the protected mast and the volume G, that resembles an inverted basin with the volume C removed, represents the total strokes to the shielding mast. The ratio of volume C to the sum of the two volumes is the proportion of the total strokes to the system that strikes the protected object. For a given position of the cloud electrode, 50 strokes were found sufficient to determine the percentage distribution of strokes between the three possible terminating points.

Since the effect of varying model size had been found[1] negligible for positive polarity, a fixed ground wire or shielding mast height, h, of 10 inches was used for all the tests. To determine the effect of mast diameter, tests were made with rods having rounded tips varying in diameter from $^1/_{16}$ inch to $^1/_4$ inch. Substantially, the same results were obtained, regard-

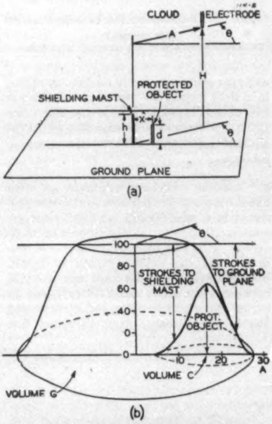


Figure 2. Symbols used with stroke-distribution curves for vertical masts

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C. F. Wagner is manager of the central station section, G. D. McCann is central station engineer in the industry engineering department, and C. M. Lear is with the engineering laboratories and standards department, all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

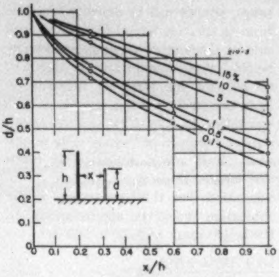


Figure 3. Exposure of an object protected by a single mast

less of the size or combination of rods, as long as positive polarity was used, and the rod sizes were held within this range. From this it can be concluded that the data obtained with one rod size are applicable to practical construction. For subsequent tests $^1/_8$-inch rods were used. Previous tests[1] showed that the same conclusions apply to horizontal ground wires and conductors.

To determine the total effect of cloud height, the relative frequency with which strokes originate from a given height should be known. Sufficient data of this type are not available and considerable variance undoubtedly exists in different regions. It is known that the base of thunderclouds[1] varies in height from a minimum of about 500 feet above ground to as high as 20,000 or 30,000 feet. A common minimum for relatively flat terrain is about 1,000 feet. Since more pessimistic results are obtained with lower cloud heights and ratios of H/h, minimum values should be used. A ratio of H/h of 5 was taken for this study, which for a 200-foot mast results in a 1,000-foot cloud height and for a 100-foot mast in a 500-foot height. The use of this ratio should give conservative results.

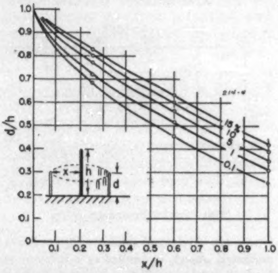


Figure 4. Exposure of a ring of objects protected by a single mast

Shielding of One Mast by Another

In Figure 3 are plotted the results of the tests made with the configuration of masts shown in the insert. The strokes that contact the protected mast, expressed as a percentage of the strokes to the system of masts, are plotted as a function of the ratio of d/h and x/h.

The actual configuration of the equipment to be protected, such as a substation bus structure, may vary widely and it would be very difficult to make a study of all configurations. However, it will be shown that a few fundamental configurations, such as the one discussed, are sufficient for practical purposes. The performance of one mast protecting another is applicable to the case for which a single mast protects a structure having a single prominent projection.

Shielding of a Ring of Masts by One Mast

If a number of points on a structure have equal exposure with respect to a single shielding mast, the probability of at least one being struck is increased. This can be seen with reference to Figure 2b. If there were another mast of the same height, d, and distance, x, from the shielding mast but at an angle θ equal to 180 degrees, there would be then another volume C, and the number of strokes to the two protected masts would be twice that to one. As the number of masts increases, forming a circular ring around the shielding mast, the exposure increases until the limit of an infinite number of such masts or a solid ring is reached. The distribution curves for such a case are independent of θ. A conservative estimate of the shielding performance of such ring can be obtained by assuming that these distribution curves are the same as that of a single protected mast for θ equal to zero.

Thus, the volume C is a single volume of revolution about the shielding mast whose cross section is the heavy curve of Figure 2b. This is somewhat conservative, because, as the number of masts in the ring is increased, they eventually become so close that more than one mast becomes involved in the distribution curve and less than the number indicated by the curve for one mast for θ equal to zero will strike any one of the masts.

It was in this manner that the data for the curves of Figure 4 were obtained. These data are directly applicable to a structure having all points of equal height and equal distance from the shielding mast. Almost all practical cases of shielding by means of a single mast will lie between this condition and that of one protected point.

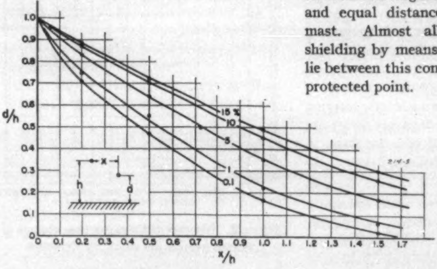


Figure 5. Exposure of two horizontal conductors protected by a single overhead ground wire

Table I. Record of the Number of Times Objects of Varying Heights Are Struck

Object and Location	Height (Feet)	Number of Years	Times Struck	Average (Number Per Year)
Mast at North Wales substation (Philadelphia) of Philadelphia Electric Company	80	4	1	0.25
10 fire towers of the Pennsylvania State Department of Forests and Waters, western Pennsylvania	100	1	2	0.2
Radio tower of WWSW, Pittsburgh	100	3	1	0.33
Radio tower of WHK, Cleveland	300	1	1	1.0
Radio tower of WCLE, Cleveland	300	1	0	0
Radio tower of WADC, Akron	360	3	6	2.0
Cathedral of Learning of University of Pittsburgh	535	3	8	2.7
Anaconda Copper Mining Company smoke stack at Great Falls, Mont.	545	2	1	0.5
Anaconda Copper Mining Company smoke stack at Anaconda, Mont.	565	2	5	2.5
Empire State Building, New York, N. Y.	1,250	3	68	23

These objects are in regions of isoceraunic levels varying from 25 to 45 storm days per year.

Shielding of Two Horizontal Conductors by a Single Overhead Ground Wire

The shielding characteristics of two horizontal conductors protected by a single overhead horizontal conductor are given in Figure 5. These curves can be applied to such cases as an overhead wire shielding a substation or shielding the incoming lines to a substation.

Shielding of Objects Between Two Masts or Ground Wires

Data similar to that given above are presented in Figures 6 and 7 for a protected mast located midway between two shielding masts and for a single horizontal conductor located midway between two parallel overhead ground wires. The increase in shielding, which is obtained when several masts or ground wires are used and placed such that they more or less surround the protected equipment, is not generally realized. The area protected by two masts or two ground wires is considerably greater than twice the area protected by one.

Total Number of Strokes to Substations

The degree of shielding necessary for adequate protection can only be determined after it is known how frequently the system is struck. Data available to the authors on the number of times per year objects of varying heights are struck in regions of isoceraunic levels, varying from 25 to 45 storm days per year, are listed in Table I. All but the data on the Empire State Building[3] were obtained from lightning investigations being conducted by the Westinghouse Electric and Manufacturing Company.[4]

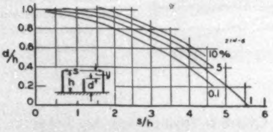


Figure 6. Exposure of a single mast midway between two shielding masts

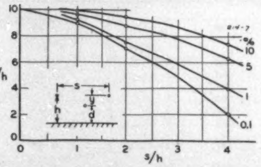


Figure 7. Exposure of a single conductor midway between two ground wires

The curve of Figure 8 was obtained by grouping the data of Table I into mean values of height and averaging the strokes per year for each group. The range of this curve as applied to substations, would fall below 200 feet. A mast of such height can be expected to be struck about once every one and one-half years, and a 100-foot mast about once every three years. Laboratory tests indicate that for strokes that do not have appreciable upward leaders, the strokes attracted to a mast increase linearly with the height of the mast. This relation is indicated by the general shape of the lower part of Figure 8. The upward trend of the curve for high objects is probably due to the upward streamers that occur in nature from objects of such height.

A further estimate of the number of strokes to a substation can be obtained from the data of Waldorf[5] and others[1] on the frequency of strokes to transmission lines. The average figure for lines of from 60 to 100 feet in height is one per mile of line per year. The previous model tests[1] show that, in this height range, strokes will be drawn to the line from an effective lateral distance on each side of the line of about 3.5 times the height. Assuming an average height of 80 feet for the foregoing transmission lines, one stroke per line per year is thus equivalent to 5,280/(2×3.5×80) or 9.5 strokes per year per square mile of sky area. If W and L designate the width and length, respectively, in feet of the substation then the total strokes to the substation should be approximately, in the height range from 60 to 100 feet,

$$\frac{(W+700)(L+700)}{(5{,}280)^2}\,9.5$$

The strokes to a substation for which $W=L=100$ feet, are

$$\frac{(800)(800)}{(5{,}280)^2}\,9.5=0.22 \text{ per year}$$

or once every four and one-half years. This compares favorably with the data of Figure 8.

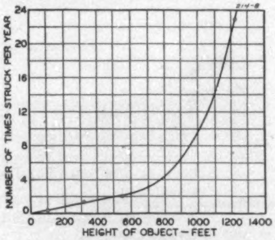


Figure 8. Number of times per year objects of various heights are struck by lightning

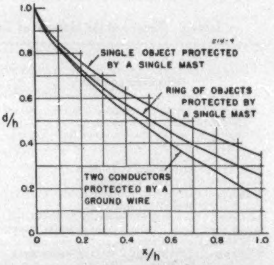
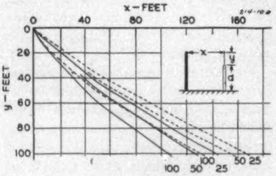
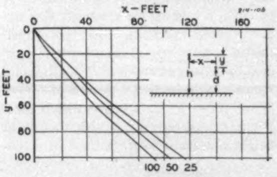


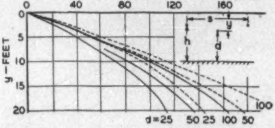
Figure 9. Shielding characteristics of a single mast or ground wire for 0.1 per cent exposure



(a) One shielding mast:
Dotted lines for one exposed object
Full lines for ring of exposed objects



(b) One horizontal ground wire



(c) Two masts or two ground wires:
Dotted lines for masts
Full lines for horizontal wires

Figure 10. Height of shielding object above protected object, y, plotted as a function of the horizontal separation, x, and the height of the protected object, d, for 0.1 per cent exposure

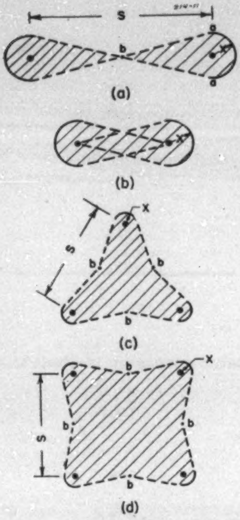


Figure 11. Areas protected by multiple masts for point exposures of 0.1 per cent

When considering a single substation and a figure of one stroke to the structure every two to four years, then, if an exposure of 10 per cent is assumed, the live parts will be subjected to one stroke every 20 to 40 years. One per cent exposure results in one stroke every 200 to 400 years, and 0.1 per cent exposure in one stroke every 2,000 to 4,000 years. However, many systems have a large number of substations, which increases the overall exposure.

Balanced against the desirability of perfect shielding must be considered the increase in cost incident to taller shielding structures. Certainly not over one per cent exposure should be permitted, and, when a comparison between the height of shielding structure required to obtain 0.1 per cent exposure over that for one per cent exposure, as obtained from Figures 3 to 7, is made, it will be seen that, in general, the added height can be obtained with little increase in cost. For this reason, the authors have chosen an exposure figure of 0.1 per cent in discussing the shielding of structures.

Working Curves

Figure 9 shows the relative configurations for a single mast or ground wire required to reduce the exposure to 0.1 per cent and Figures 10a and 10b show the same data in more usable form in which distances are plotted in feet directly, thus eliminating the necessity of using the ratios d/h and x/h. In these figures is introduced the distance y which represents the vertical distance between the protected object and the top of the shielding object. Similar data for two masts and two ground wires are presented in Figure 10c from data given in Figures 6 and 7.

Protection of Substations

To this point, consideration has been given to the protection afforded by one or two infinitely long wires, such as the overhead ground wires on transmission lines, to the protection afforded by one mast, and to the protection afforded to objects located at the mid-point of a line connecting two masts. Substation configurations are so diversified in construction that it becomes impossible to test each type individually. The best alternative is to convert the information already obtained to a form that can be utilized to best advantage.

A single mast protecting a substation offers no particular difficulty, the curves of Figure 10a being used directly. If the structure has a single prominent projection or several projections in a limited region, to be protected, such as a set of disconnects, the dotted curves should be used. On the other hand if live parts are more generally distributed at a given height, then the full-line curves should be used and applied to the most remote object.

For horizontal wires the data of Figure 10b apply to long spans, such as transmission lines. Substations involve much shorter lengths, some being so short that consideration must be given to the end effects. By comparing Figures 3 and 5 and Figures 6 and 7, for which Figures 3 and 6 approximate end conditions and Figures 5 and 7 apply to straight-away conditions, it may be seen that the per cent exposure for a given configuration is always slightly less for the end than for the straight-away Thus, the working curves, Figures 10b and 10c, can be applied directly to ground wires, even if they are short and the per cent exposure figures apply to the total strokes to the substation structure.

If two masts are used to protect an area, the data presented give shielding information only for the point b, midway between the two masts, and for points on the semicircles drawn about the masts as centers as shown in Figure 11a. For given values of d and y, a value of s from Figure 10c and x from Figure 10a can be determined, which will give an exposure of 0.1 per cent. The locus shown in Figure 11a, drawn by the semicircles around the masts as centers and connecting the point b, represents an approximate limit of 0.1 per cent exposure. Any single point falling within the cross-hatched area should have better protection than 0.1 per cent. This arrangement is likely to leave some points of a rectangular substation protected by two masts with higher exposure than desirable. If, however, the distance between the masts is decreased, the protected areas are, at least, as good as the combined areas obtained by superposing those of Figure 11a. For example, if the distance between masts is halved, the resultant protected area is somewhat as shown in Figure 11b.

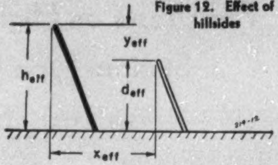


Figure 12. Effect of hillsides

On this basis, to form an approximate idea of the width of the overlap between masts, first obtain a value of y from Figure 10c corresponding to twice the actual distance between the masts. The width of overlap is then equal to x corresponding to this y as obtained from Figure 10a. This undoubtedly gives a conservative width of substation that can be protected.

For three masts located at the points of an equilateral triangle or for four masts located at the points of a square, the protected areas are as shown in Figures 11c and d. The height of the shielding mast should be so chosen that the b points provide 0.1 per cent exposure as obtained from Figure 10c for the mid-point between two masts. The x radii are obtained from the data for a single mast.

Effect of High Earth Resistivity and Terrain

The data presented here apply to stations located in regions of relatively flat terrain and low earth resistivity. As shown in the previous work,[1] high earth resistivity lowers the effective ground plane below the surface of the earth and results in poorer shielding for a given configuration. This effect is appreciable, however, only for very high values of resistivity, and, since most substations are provided with an extensive grounding

Field Tests on High-Capacity Air-Blast Station-Type Circuit Breakers

H. E. STRANG
MEMBER AIEE

W. F. SKEATS
MEMBER AIEE

IN January 1940 there was presented before the Institute a paper[1] describing a new high-capacity air-blast circuit breaker. Since then, breakers of higher interrupting rating have been built, following out the general principles of design and construction disclosed at that time. As part of an organized development program, such breakers have been subjected to extensive interrupting tests under factory laboratory conditions. However, it is recognized that the final proof of the interrupting performance of high-capacity circuit breakers comes as a result of tests made on actual operating systems.

The engineers of the Consolidated Edison Company offered to make such tests up to the full short-circuit capacity available on the bus of the Hell Gate Station in New York.

First Series of Tests

A breaker rated 15 kv, 1,200 amperes, 1,500,000-kva, 8 cycles, mounted in a steel cell, was submitted for field interrupting tests in September 1940. Out of seven tests, up to 1,480,000 kva, the breaker failed to clear on two occasions, resulting in a fault to ground inside the steel cell, which was cleared by the back-up protection. On the first of these, there was no damage whatever to the test breaker, and after the parts were inspected and the insulators cleaned, the tests were resumed. In the second case damage was limited to the upper barrier of the arc chute in the test breaker, and the glaze of some of the porcelain insulators. Adjacent equipment, such as the control equipment and current transformers located in the test cell, and the sand bags and tarpaulin outside and at the top of the test cell, was undamaged. Arc durations were unexpectedly long as compared with those found in factory tests. This caused gas to escape around the blade, and produced the faults to ground. When the breaker was returned to the test laboratory, it was found possible to reproduce this condition, and it was then discovered that alterations inadvertently made just prior to the field tests had interfered with the full flow of air across the contacts and had, therefore, resulted in long arcing time.

This specific difficulty was corrected, and, in addition, further development work, using the improved synthetic method of testing as the basis of study, produced other refinements in the design of the interrupting structure, vastly increasing its margin of safety. A breaker with these refinements performed perfectly in a second series of field tests in May 1941.

Description of Breaker

The breaker which successfully passed this second series is shown in Figure 1. As in the earlier breaker, the mechanism and air tank are mounted on top, the contacts and arc chute of the individual phases are mounted in separate steel compartments immediately below, with provision for conveying the exhaust gases up past the mechanism to openings at the top.

Without any sacrifice in operating performance or efficiency, this arrangement of major components provides adaptability to back or bottom connection equal to that of any oil circuit breaker.

Figure 2 shows one of the arc chutes with one side plate removed. The stationary finger contacts are shown in the bottom of the left-hand chamber. The moving blade passes down through openings in the top walls of this chamber to make contact with the fingers and thus close the circuit. To interrupt the circuit, the blade contact is withdrawn upward, and a blast of air is blown across the arc from the opening visible at the left. This forces the arc against the cross barriers on the right-hand side of the chamber where it is extinguished at current zero.

The arcing chamber through which the blade passes is narrow, and beyond the leading tips of the cross barriers, the path expands rapidly, both horizontally and vertically so as to increase the cross-sectional area available for exhaust of the gases. Beyond the stacks of copper cooling plates all four passages lead into a common chamber whence the gases are carried off by the vertical exhaust tube.

In the full open position the moving blade pulls clear out of the opening in the top of the chute. In order to prevent the escape of gas around the blade at some intermediate position during the inter-

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H. E. STRANG is engineer with Philadelphia Works, General Electric Company, and W. F. SKEATS is technical assistant to the engineer, Philadelphia Works, General Electric Company, Philadelphia, Pa.

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system, this is probably not an important factor. The effective ground plane in regions of high soil resistivity can be raised to the earth's surface by laying counterpoise wires to distances from the shielding masts of two or three times their height. However, in most cases it is probably more economical to increase the height of the masts.

Local terrain conditions are usually more important. The previous investigation[1] showed that, for transmission lines the nominal protective angle, which is defined as the angle between the vertical and a line joining the conductor and ground wire, should be modified for lines constructed on hillsides. The true protective angle for this case should be the angle between the perpendicular to the side slope and the line joining the conductor and ground wire. Similarly for the application of the data presented here to substations on hillsides the dimensions *h*, the shielding mast height, and *d*, the height of the protected object, should be measured perpendicular to the earth's surface. The distance *x* between the object and shielding mast should be measured along the earth's surface. This is illustrated in Figure 12.

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rupting operation, an air lock has been provided in the form of a chamber about the blade opening. This chamber is fed by fresh air bled from the blast stream back of the orifice. There is also a corrugated turbulent seal both between the chamber and the chute and between the chamber and the outside air.

The operating mechanism is of the pneumatic type utilizing air for both the closing and the opening operations. It controls the blast valve in such a way as to insure that the air blast is available prior to contact separation.

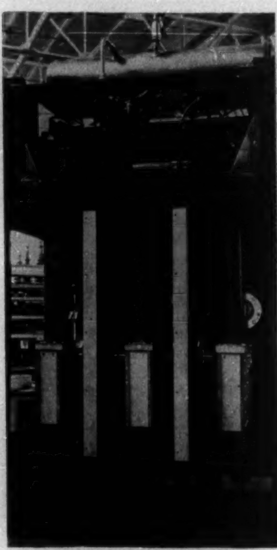

Figure 1. The air-blast breaker

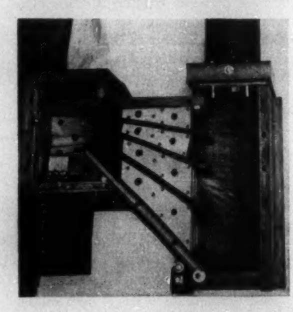

Figure 2. The arc chute with one side plate removed

Factory Tests—the Synthetic Circuit

There has previously been described before the Institute[2] the so-called synthetic method of making short-circuit tests on interrupting devices. This method consists essentially of passing heavy current at low voltage through interrupting contacts and causing high or normal recovery voltage to be applied across the contacts at the instant of interruption at current zero. The new and improved circuit, which differs somewhat from that previously described, is shown in Figure 3. In this case the high-voltage source is connected through to the circuit breaker under test and is subject to practically full excitation at all times. This facilitates proper timing in that it causes the recovery transient from the high-voltage circuit to appear automatically upon momentary extinction of the arc in both the test breaker and the auxiliary breaker rather than making the appearance of this transient dependent upon the breakdown of a gap.

Referring to Figure 3, the heavy lines indicate the high current circuit. This passes from two terminals of the generator through

1. A station breaker for finally de-energizing the circuit.
2. Reactors for controlling the current magnitude.
3. The test breaker and an auxiliary breaker in series.

One terminal of the test breaker is grounded. The auxiliary breaker must be arranged to interrupt at the same time as the test breaker. A second pole of the same breaker or another identical breaker is usually the most convenient thing to use.

The high-potential circuit starts from the third terminal of the generator, passes through a reactor, a step-up transformer and a resistor to a capacitor. By manipulation of the resistance and reactance of this circuit, the magnitude and phase angle of the voltage at the capacitor is subject to close control. The usual phase-angle adjustment is such that the capacitor voltage leads the current through the test breaker by 90 degrees so as to give the same phase relationship between voltage and current as the conventional circuit. The high-voltage terminal of the capacitor is connected through a resistance and in-

Figure 3 (right). The synthetic testing circuit

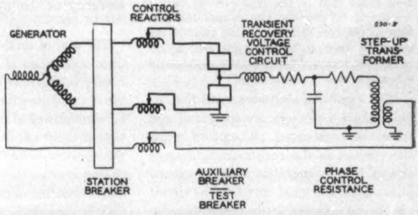

Table I. Summary of Test Results

Test No.	Phase	Type of Test	Currents: First Cycle	Currents: Initial in Arc	Kva	Arc Length: Cycles	Arc Length: Inches Per Break	Trip to Interruption Cycles	Short-Circuit Duration
1A	A	O	41,000	24,000	600,000	0.2	0.4	4¼	6.2
	B		42,000	24,000		*	*		
	C		26,000	23,000		0.2	0.4		
A	A	CO	38,000	23,000	580,000	0.2	0.4	4¼	6.2
	B		28,000	22,000		*	*		
	C		32,000	23,000		0.2	0.4		
3B	A	O	89,000	48,000	1,200,000	0.4	0.9	4½	6.1
	B		64,000	47,000		0.4	0.9		
	C		69,000	47,000		0.2	0.5		
4B	A	CO	67,000	46,000	1,180,000	0.4	0.8	4½	6.2
	B		60,000	47,000		0.2	0.3		
	C		80,000	47,000		0.4	0.8		
5C	A	O	72,000	60,000	1,560,000	0.5	1.0	4½†	3.3†
	B		76,000	61,000		0.5	1.0		
	C		94,000	62,000		0.2	0.5		
6C	A	CO	85,000	58,000	1,480,000	0.1	0.2	4	5.5
	B		111,000	59,000		0.3	0.8		
	C		74,000	58,000		0.3	0.8		
7D	A	O	57,000	26,000	200,000	*	*	4	14.5

*Cleared with contacts separated, but still overlapping.

†Breaker pretripped.

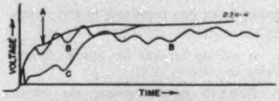

Figure 4. Recovery-voltage transients

A—Synthetic circuit, breaker clearing
B—Conventional circuit
C—Synthetic circuit, breaker failing

ductance to the ungrounded terminal of the test breaker. The magnitude of the resistance in this circuit was usually such as to give a current between 5 and 50 amperes. The inductance was much smaller in ohmic value than the resistance and so had no appreciable effect on the current in this connection. Its purpose was to introduce a controlled amount of oscillation into the recovery transient at the test breaker.

For a circuit-opening test, this circuit operates as follows:

> Both the test breaker and the auxiliary breaker are initially closed. The station breaker closes applying the short circuit. This allows current to flow in the high-current circuit and also energizes the high-voltage circuit, setting up voltage at the capacitor and current in the resistance-inductance circuit between the capacitor and the test-breaker terminal. This current leads that in the high-current circuit by about 90 degrees, and causes the current in the test breaker to lead that in the auxiliary breaker by an angle which, expressed in terms of time, usually amounts to a few tenths of a microsecond.

This condition continues until the circuit breaker contacts are separated and the arc is interrupted. When this occurs the current in the resistance-inductance circuit has its previous path to ground interrupted and must complete the circuit via the capacitance of the connection between the two breakers. Were there no inductance in this circuit the voltage across the test breaker would respond by rising along a logarithmic curve to the voltage of the capacitor, without oscillation or overshoot. By inserting a small amount of inductance, however, a controlled amount of oscillation can be obtained, so that it is possible to obtain a range of recovery characteristics quite representative of a good number of those occurring in actual service. Curve *A* of Figure 4 shows a cathode-ray oscillogram of a voltage-recovery transient actually obtained in this manner for simulation of the duty represented by curve *B*, which was obtained on a conventional test circuit.

In common with other synthetic circuits, the present one gives the breaker an opportunity to extinguish the arc at a given current zero or allow it to reignite. Whether extinction or reignition takes place is indicated on the cathode-ray oscillogram. Extinction is indicated by the rapid establishment of voltage across the breaker contacts and the absence of breakdowns on the voltage-recovery curve. Failure to extinguish is indicated by a delay in voltage establishment or breakdowns on the voltage-recovery curve. Curve *C* of Figure 4 shows a cathode-ray oscillogram indicating failure to extinguish for comparison with curve *A* showing prompt extinction.

Severity of the Synthetic Circuit

The goal of synthetic testing is to produce conditions of arc current and recovery voltage for a circuit breaker which are of equal severity with those that may be encountered in service in an interruption of short-circuit current. Shapes of the recovery-voltage characteristic occurring in service vary widely, but reasonable simulation of a service characteristic may be considered to be achieved in the synthetic test if the voltage rises to the same value in the same number of microseconds without necessarily duplicating all oscillations.

The circuit has been criticized occasionally on the basis that the use of two interrupting units in series reduces the duty on the test breaker. In modern a-c circuit breakers the current is allowed to flow substantially without hindrance until it reaches the normal cyclic current zero. The insertion of a second interrupting unit will increase the effective arc voltage, but this will cause only a slight modification of the current wave, and when the tests are properly evaluated, this modification tends, if anything, to result in a synthetic test which is too severe rather than too lenient. This is explained by the following analysis of the ways in which the modification may manifest itself.

The additional arc-voltage effect may

1. Reduce the crest value of current reached during arcing. Any error from this source can be nullified by evaluating test results in terms of the current actually reached rather than in terms of what would have been reached had only one breaker been present in the circuit.

2. Shorten the current loop. Misleading conclusions from this effect can be avoided by interpreting the test results in terms of the ability of the breaker to interrupt on a current zero occurring at a given contact separation.

3. Cause the current to fall to zero more rapidly at the end of the loop. This will result in a higher average current during the last thousand microseconds or so before current zero. Consequently, it will tend to increase the difficulty of clearing when the current zero occurs.

The duty of the test breaker might be considerably reduced if the auxiliary breaker were in series with it for the recovery voltage. As will be apparent from inspection of the circuit, however, this voltage is applied across the test breaker alone.

Tests made for comparison of the severity of the synthetic testing scheme with that of conventional tests confirm this analysis. The air-blast breaker is well adapted to such tests, for, in the reduction of tank pressure, it offers a convenient means of adjusting the interrupting ability of the breaker to the power available from a conventional circuit. Further tests may then be made with the synthetic circuit set up to simulate this conventional circuit.

Tests of this type have been made on two different breakers. The first of these breakers was rated 500,000 kva at 15,000 volts, so that tests could conveniently be made close to full rating, and the second was very similar to the breaker finally tested in the field and described above.

The first group of tests was made at from about 10,000 to 13,000 amperes with 14,500 volts across a single pole of the breaker. The results are shown in Figure 5. Here the contact separation corre-

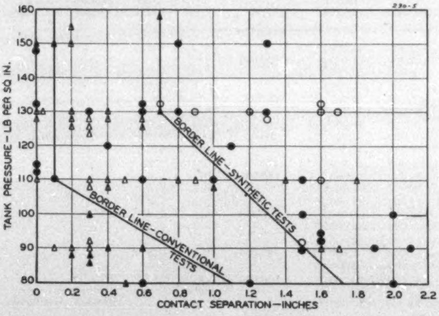

Figure 5. Conventional and synthetic tests on an air-blast circuit breaker

10,000–13,000 amperes 14,500 volts
Conventional circuit
● Arc extinctions
▲ Reignitions
Synthetic circuit
○ Arc extinctions
△ Reignitions

Vol.
1942

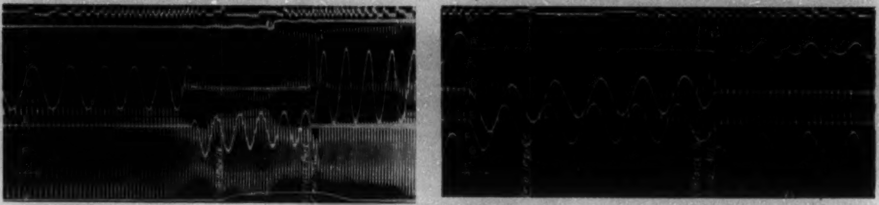

Figure 6. Oscillograms of test 6-C

Curve A—Breaker travel, each step = approximately 0.17 inch
Curve B—Cathode-ray oscillograph trip
Curve C—Pressure in manifold, pole 2
Curve D—Voltage across contacts, pole 2
Curve E—Current, pole 2
Curve F—Pressure in manifold, pole 3
Curve G—Trip-coil current

Curve A—Breaker travel, each step = approximately 0.17 inch
Curve B—Voltage across contacts, pole 3
Curve C—Pressure in throat, pole 3
Curve D—Current, pole 3
Curve E—Current, pole 1
Curve F—Pressure in throat, pole 1
Curve G—Voltage across contacts, pole 1

sponding to a given current zero is plotted as abscissa and the tank pressure used in the test is plotted as ordinate. Points are coded according to whether the arc was extinguished or reignited at the current zero in question and according to whether the test was conventional or synthetic. A few of the points indicate clearing at zero contact separation. This occurs because, with the blade and finger contacts, contact separation is measured from the point where the tip of the blade is flush with the tips of the fingers rather than the point of metallic contact.

On this plot one curve is drawn to indicate the border line between extinction and reignition as shown by the synthetic tests, and another to indicate the corresponding border line for conventional tests. There are not enough test points to determine both curves with high precision throughout their length, so there may be some question as to whether the curves should be exactly as shown. With any reasonable change, however, the border line for the synthetic tests lies to the right of the border line for the conventional tests, showing that at any given pressure more contact separation is required to clear the synthetic circuit than the conventional circuit. These tests, therefore, indicate the synthetic circuit to be somewhat more severe than the conventional.

The second series was made at about the same current and voltage as the first series, but at a higher recovery-voltage rate and on a breaker of higher interrupting rating similar to the breaker tested in the field. In this case the line of demarcation between extinctions and reignitions lay between 70 and 90 pounds per square inch both for the conventional circuit and for the synthetic circuit, indicating approximate equivalence of the two circuits.

These series may be taken as concrete evidence that the synthetic circuit produces test results sufficiently close to those obtained conventionally to serve as an acceptable procedure for determining ability of a circuit breaker to interrupt at the first current zero of arcing.

Synthetic Tests at Breaker Rating

After making conventional tests on this breaker up to the limit of the laboratory both at 14,500 volts and at a reduced voltage with currents extending up to and beyond the rating, the synthetic circuit was applied in order to obtain performance data upon the application of both rated voltage and rated current in the same test.

Tests were made with a restored voltage of about 15 kv rms across a single pole of the breaker and at currents varying for the most part over the range from 50,000 to 62,000 amperes. Under these conditions with a tank pressure of 150 pounds it was found that occasionally the breaker would not clear at the first current zero after contact separation. Increasing this pressure to 200 pounds sharply reduced the probability of carrying over for extra loops of current, and at 250 pounds in all cases the breaker was found to clear at the first zero point after the contacts had separated 0.4 inch.

Other tests made by varying the rate of rise of recovery voltage, keeping all other conditions the same, indicate that the tank pressure required to insure clearing at the first current zero increases as the circuit recovery rate goes up.

The use of 250 pounds of pressure instead of 150 pounds, therefore, provides an increased margin of safety in insuring half-cycle arcing time for high-capacity breakers under conditions of high rate of rise of recovery voltage, and since it imposes no economic or operating burden on this type of breaker, this pressure was selected for the second series of field tests.

Second Series of Field Tests

The results of the field tests on the modified breaker are shown in Table I. The series consisted of an opening and a closing-opening test, three-phase, at each of three steps, at approximately 40, 80, and 100 per cent of full interrupting rating. Root-mean-square currents in the first cycle on the closing-opening tests varied from 28,000 amperes to 111,000

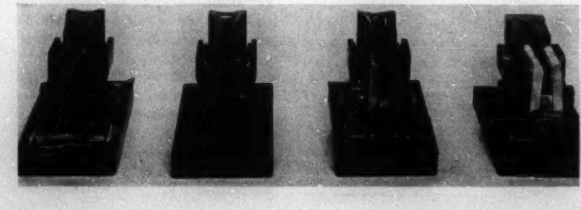

Figure 7. Stationary contacts after test, with new contact

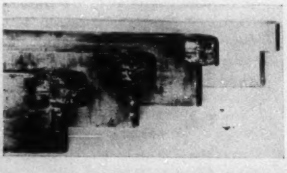

Figure 8. Movable contacts after test, with new contact

amperes. These three-phase tests were followed by one single-phase operation at about 350,000 kva which was a system, and not a breaker test. All of these tests were completely successful. The breaker operating time ranged from 4 to $4^1/_2$ cycles. The arcing time never exceeded one-half cycle, confirming the prediction of the synthetic tests made in the factory. There was no sign of fire throw either from around the contacts or from the vents on either opening or closing-opening tests.

Figure 6 shows the oscillograms of test 6-C.

The condition of the contacts from all three phases (compared with new contacts) is shown in Figures 7 and 8. Burning of the metal is detectable for a short distance up the trailing edge of the blade, at the hump of the arcing fingers, and on the arcing plate at the back of the finger assembly. Loss of material was very light, however, and conservative estimates placed the useful life of the contacts before requiring replacement at several times this amount of service.

Figure 9 shows the interior of one of the arc chutes following the tests. Here also burning is very light. The piece most severely burned is the first fibre barrier, and this could be expected to last approximately as long as the contacts.

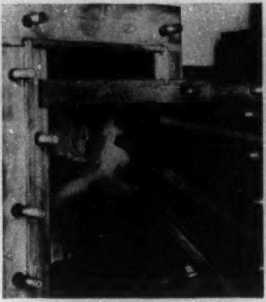

Figure 9. Interrupting chamber after test

Conclusions

1. Although assuredly unpremeditated and unscheduled, and at the moment extremely disappointing, the eventual result of the difficulties on the first series was to build up confidence in the air-blast breaker principle, for there resulted no damage whatever to adjacent equipment, and very minor damage to the test breaker itself.

2. A 15-kv, 1,500,000-kva air-blast breaker has demonstrated its ability to perform satisfactorily over the entire range up to full rating, and to close currents as high as 111,000 amperes, under field conditions, in the first such tests ever made at generator voltage.

3. Close agreement has been demonstrated between the results of factory synthetic tests for high-capacity circuit breakers and field-interrupting performance. Recognizing that such field tests, however desirable, can serve as only an occasional check on laboratory results, this agreement is significant in that it establishes confidence in the present method of developing high-capacity interrupting devices.

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Electric-Power Distribution Systems in Wartime

PHILIP SPORN
FELLOW AIEE

I—Introduction

WE are living today in some of the most critical times in the world's history. Throughout a large portion of the world men and machines are engaged in titanic struggles that may, and undoubtedly will, affect the future of Western civilization as we know it, for a century, perhaps for centuries. We, ourselves, in this country have, against our will, been drawn into this world reaching cataclysm. We are now dedicating a large part of our energies and resources and even our lives to the task of assuring that the democratic idea and way of life will emerge victorious over its counterpart and deadly enemy—the totalitarian idea. We are at war.

I have referred to the dedication of a large part of our energies. But more specifically I meant to emphasize—energy. For truly, as in no other armed clash in the history of civilized man, this is a struggle between opposing horsepower. Consider, for example, the fact that our monthly output (August 1941) of airplane engines alone has reached the figure of four and one-half million horsepower. Add to it, the engine horsepower of tanks, trucks and the numerous auxiliary vehicles going into direct military effort, and the power being built monthly for the propulsion of the numerous varieties of ships being built by, and for, the American navy, and the supreme importance of the horsepower in this particular defense program becomes strikingly clear.

Equally significant are some figures from the electric power industry—public and private. During the 12 months ending September 1941 it generated some 159 billion kilowatt-hours. As of the same date it had an installed capacity of over 42,950,000 kw. Further, it is expected that during the year 1942 there will be added over 3,000,000 kw of capacity and during 1943 close to 2,000,000 kw. One single system alone—the electric system with which the author is associated—has added during the past year over 230,000 kw of steam electric capacity, and it has under construction and will add during 1942 and 1943 over 500,000 kw of additional capacity.

All of the vast reservoir of power is designed to back up the man power of the nation and to make certain that energy, to the fullest extent that energy can be used to complement the human effort, is available to meet the requirements for increased production of all the materials needed to wage modern war.

Most of this energy has to be distributed before it can reach the utilization point. It has, in other words, to utilize the facilities of a distribution system. It is a well-known fact that the distribution system represents the largest single item of plant investment in any well-balanced electric utility system. Hence, it represents the largest aggregation of skilled effort and materials. But materials composing the distribution systems, and particularly items like copper, steel, and electrical assemblies of these, represent some of the most critical materials in the present all-out defense effort. The factories normally engaged in manufacturing these materials into finished usable products to take their place in a distribution system are, in a great majority of the cases, engaged also in manufacturing similar materials, or materials of a sufficiently closely similar nature, so as to require the same human and machine facilities, for direct use in vital defense instruments both on land and in war vessels of various kinds.

Distribution systems, as we know them, and as they exist today, are the products of concepts and ideas developed into plans, and built of materials and labor utilizing the concepts and ideas of human beings, generally engineers. These concepts and ideas, in turn, have usually been based upon knowledge of physical and electrical requirements of a distribution system and of the corresponding abilities of certain materials to meet those requirements. In many cases, however, the requirements themselves, although the result of a great deal of experience and a knowledge of fundamental engineering characteristics, are based upon more or less arbitrary criteria of what is required to give satisfactory performance. For example: It is apparent that, while there is much experience and sound distribution engineering behind the common criterion of designing distribution feeders for two per cent voltage drop to the distribution center or point of primary take-off, it is possible to design, build, and operate distribution systems with higher voltage drops. This is but one example illustrative of the fact that many parts of distribution systems have been built to meet more or less arbitrary standards.

This is not said in derogation of distribution engineering as currently practiced. We all recognize that certain standards of performance are prerequisite to any rational design of any distribution system, but many of the standards are not only based upon judgment as to the level of performance that the standards have to reach, but, in many cases today, are the

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PHILIP SPORN is vice-president and chief engineer of the American Gas and Electric Service Corporation, New York, N. Y.

product of a continuous raising of such levels. Only to the extent that they do not represent the minimum that can be safely used, particularly in times of necessity, do I mean to label them as arbitrary standards. To that same extent, the limitations from a load-carrying standpoint are, in many cases, man-made and they, therefore, offer possibilities of being unmade and their use expanded in times of necessity, by man.

Considering the necessity of utilizing all available effort to the utmost in a war for survival, it is obvious that, to the extent that distribution systems, so vital to the delivery of power and energy to all war industries, have to be expanded in order to meet the increasing need for electric power and energy, it behooves us at this time to re-examine the peacetime ideas and concepts behind the planning and building of our distribution systems. It is necessary to ascertain how far ideas that were perfectly sound only a short time ago are still sound today and how far they can be modified; to what extent new ideas can be used, particularly for the duration of the emergency, so as to reduce to the practical minimum the total efforts expended on expansion of distribution systems.

II—The Object and Scope of This Symposium

The object and scope of the symposium, to which this paper forms an introduction, is to survey, study, and analyze all phases of the distribution system with a view to not only developing new ideas and concepts, but even more so to altering previous concepts of what has to be done on a distribution system under conditions facing the electric supply industry and industry of the country in general, today: to determine means and methods that can be employed for adapting, expanding, and operating existing distribution systems for the maximum broad overall benefit. This will recognize first, that all distribution systems (at least distribution systems that have maintained what might be called normal standards of adequacy) have inherently considerable elasticity, or rubber, or stretch, to use more common terms. A good deal of that stretch, it would appear, can and must be used under present conditions. The exact methods of utilizing such elasticity, the ways and means, will be developed in detail in the principal papers. Again, it will recognize that, in many of its aspects, distribution engineering as practiced today is a result of slowly developed standards and concepts of what is adequate quality of service. Side by side with such development has been an acceptance, during the past two decades at any rate, of the idea that quality of service should be continually improved. In general, these ideas have been sound, at least for peace times. But in making such a statement, it needs to be recognized that service can be rendered on several—perhaps an infinite number of—different quality levels, ranging from the very highest level to one that is definitely below the passable or acceptable. But in between these two limits there is a broad band for latitude. Where a national emergency exists and where the national interest dictates the desirability of modifying such standards of quality, there most certainly is every reason for analyzing *de nova*, determining, and then adopting a newer concept of what is adequate for the emergency period.

Not only in operation, but in maintenance also, a closer examination of the true needs of a distribution system is in order. More than ever in times of a national emergency, the question of indispensability of any proposed program needs to be gone into thoroughly. Not only that, but the long-term concept, even the reasonably long-term concept, may have to be disregarded and be permitted to give way to the concept of immediate or reasonably immediate need. It is true that all this may result in somewhat greater eventual cost. Deferred maintenance, for example, always results in greater ultimate costs. But here again, there is considerable margin of latitude between what might be called clearly deferred maintenance and what is merely anticipatory maintenance. The latter will frequently result in a total saving over a long enough period of time. However, under the situation confronting the country today, a program involving somewhat higher ultimate costs may be the more sound, and therefore, the desirable one, if not the necessary one, to follow.

From the standpoint of the materials situation and the help that can be given here, it is necessary to give consideration to the fact that many defense industries are in direct competition for the same or allied materials used on a distribution system. This is true, for example, in materials like copper, steel, tin, zinc, and any number of others. In other cases, the competition extends beyond the raw material and enters into the manufactured product itself. Typical examples of that are transformers, electric cables, circuit breakers, protective devices, and numerous others that can be cited. Hence, the need to critically examine not only the extent to which existing materials and devices can be more intensely employed, but where new materials or devices are needed, to determine what substitutes for critical materials might be employed. This symposium will concern itself with this also.

The development of the idea of the importance of the distribution system to the task of making available adequate electric energy to successfully prosecute the war leads directly to the thought that the defense or the protection of that system is likewise a most vital matter. But this subject is a difficult one to handle, particularly in a public forum. A discussion of weaknesses of any system is an automatic indication of the logical point of attack by those sufficiently motivated to carry on the work of the saboteur. Hence, such a discussion has to be, of necessity, most limited. But such limitation of the scope of the discussion does not alter the fact that certain plans, ideas and ways and means of providing secondary lines of defense, so to speak, can be developed, and in many cases have been developed and put into effect. To that extent, discussion is not only possible but can be fruitful in bringing about greater safety.

III—The Limitations of the Symposium

Even the brief discussion of the distribution problem that has been given here, should have indicated fairly conclusively that the problem undertaken is a complex one. What is of even greater significance is the fact that the distribution system, more perhaps than any other part of an electric supply system, is a local problem. It is a polygon full of local angles. All of this has been fully recognized. The fact still remains, however, that certain principles are more or less universally applicable. These, in particular, it will be the aim and purpose to explore.

In arranging the series of papers here presented, and in their writing, it has been neither the aim nor the intention to prepare a complete text on the subject, or on any phase of it. On the contrary, it is fully recognized that a task of that sort would be practically impossible, and certainly so, within the time and the space that has been made available for that purpose. Rather has it been the thought that this series of papers will result in a stimulation of thinking along new avenues and that that will lead to further work that will be undertaken. This work will have to be done in the scores, possibly

Underground Distribution Systems in Wartime

L. R. GATY
ASSOCIATE AIEE

Synopsis: This paper reviews the underground distribution system, suggests methods to conserve existing facilities and ways of utilizing latent capacity, and summarizes some of the known information available. Additional information is given on duct-bank load limitations and on time-temperature factors for cable and duct.

THE war program requires that we take every advantage of existing equipment and unused capacity to carry the unusual loads which are being supplied. Unpredictable new loads and load increases must be supplied promptly, although materials and equipment progressively become scarcer. Expedients will be necessary which would not be considered during normal times and all of our combined ingenuity will be required.

The experience from previous operation must be utilized to the greatest extent, but since the conditions at present are becoming so extreme, previous experience under normal conditions may be of value largely as a sign to point the direction. The opportunity to obtain valuable data must not be overlooked. Field studies, investigations, and surveys should be continued or expanded so that the road can be mapped as we travel over it. Information gathered now will be of great value for present operation and future design.

Method of Supply

In those cases where a radial distribution system is used advantage may be gained, both in capacity and in regulation, by changing to a network system. A properly designed network will increase the capacity of existing facilities in a given area and improve the service reliability. If maximum use is made of the capacity increase, there will be very little voltage improvement. However, it may be necessary, for the duration of the emergency, to modify our existing concepts of satisfactory voltage regulation in order to make available the maximum capacity to carry the load. This does not necessarily mean exceeding the existing limits of minimum and maximum voltage, but it may mean a wider band than has been considered good practice. The change from radial to network will only be economical in certain cases. In general, the increase in capacity alone will not justify the expense of changing over, and the material required may be more than to obtain the same capacity increase in other ways.

A radial system usually has a number of dual-service customers supplied from it. Standard practice requires that capacity must be available on each service for the entire load of the customer. This reserves *normal* capacity of twice the load being supplied. More liberal use of the emergency capacity of the system will permit this load to be supplied normally from one feeder without overload, but will not reserve capacity on a second cable so that when the second supply is required the emergency rating of the cable will be used. In some cases, additional load transfers at the time of the emergency can be made to relieve the overload. Emergency service may have to be restricted to the customer's essential load. As capacity limitations become more critical, it may become necessary to make available emergency service only to essential war industries, although this does not appear necessary at this time. Control of the use of emergency service by the utility instead of by the customer will provide greater flexibility for the use of existing capacity. A review should be made of the facilities supplying all dual-service customers to release all possible capacity for new loads and increased loads.

Additional capacity may be made available by the parallel operation of primary feeders. This must be done with care so that a large area will not be without service in case of a cable failure. The interrupting duty on equipment connected to such parallel lines will be materially increased, and fuses and circuit breakers may be inadequate. The same advantage may be gained by transferring customers between feeders, in order to obtain the highest load factor. As an example, if there are two feeders into an area, one industrial and the other commercial and residential, it may be advantageous to change both to combination feeders.

Reliability of Supply

All reasonable precautions should be taken to guard the system against damage to cables and accessories. This damage may be intentional or unintentional and may be mechanical, thermal, or flood. Mechanical includes both external and internal explosions as well as other types of mechanical damage. Thermal includes fire as well as overload.

Damage should be prevented if possible, but when it does occur, the organization must be prepared to repair it promptly and restore the equipment to service. This will require suitable trucks and trailers equipped with splicing materials and pumps. Careful study should be given to the amount and efficiency of this equipment.

Fire fighting equipment mounted on a truck or trailer should be provided which can be quickly moved to any part of the system. More than one may be required for complete coverage of a large property. Mobile fire fighting equipment of the CO_2 type has been designed and is available.[1] This is useful not only for manhole and vault fires, but also for other types of fires on the system. An ample reserve supply of CO_2 should be kept on hand at all times.

Fireproofing of cables in manholes and vaults should be very liberally applied to minimize the danger of a fault being communicated to adjacent cables and to

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L. R. GATY is assistant electrical engineer with the Philadelphia Electric Company, Philadelphia, Pa.

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hundreds, or even thousands, of distribution systems.

The experience in the application of these ideas, and the development of new ideas that effort along this line is bound to bring, will eventually give a great deal more knowledge and information. But there is no time to await the full gathering of that experience. Even though all the facts necessary for a complete solution of all the phases of the problem are not available at the present time, a beginning must be made at once. All engineers, and distribution engineers among them, must pitch in and contribute their share to the common task.

reduce the damage which will result from a manhole or vault fire. In special cases, it may be advisable to install barriers to segregate one group of cables from another. A highly important transmission cable in the same manhole with old distribution cables might justify special precautions.

Spare materials must be checked and watched to assure ourselves that any reasonable emergency can be handled promptly and efficiently. A proper stock of cable in the various lengths required should be given careful study. The stock of reserve cable should be higher than normal, due to the probability of a higher failure rate with heavier loads, the possibility of extensive damage under emergency conditions, and the extended delivery required on new cable. Other spare materials should include complete kits of splicing material, and necessary switches, transformers, fuses, network protectors, potheads, and any special equipment such as oil reservoirs, gas pressure tanks, and various types of fittings. Emergency conditions caused by explosions, washouts or cave-ins, and fires must not be overlooked. Manhole frames and castings should be available. Consideration should be given to using precast manholes for emergency replacement in cases of extended damage. The installation of precast manholes has already been shown to be practical and economical[2] and may meet certain emergency conditions.

Fault-locating equipment and methods should be checked. Fault-locating equipment is available for distribution cable which will generally give a prompt and accurate location. This is necessary before much other work can be done to start repairs.

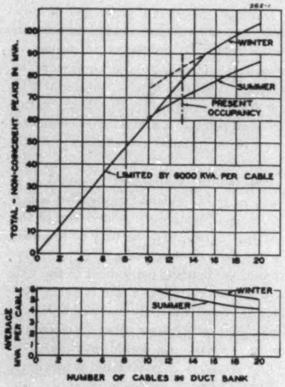

Figure 1. **Duct-bank thermal capacity**

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable

Repairs must be made as quickly as possible. Temporary repairs which have to be remade later are usually not justified. This has been verified by incomplete reports from England. Some experiments have been made with a "cold-setting" material for making a temporary high-voltage cable joint which does not require a lead wipe.[3] This has been found to be practical and usable where conditions require it, such as with very extensive damage requiring prompt repair with minimum skilled labor.

Salvage methods should be reviewed so that maximum advantage is obtained from removed cable and material. Duct splices have been found uneconomical for short lengths of cable but may be necessary to conserve material and maintain emergency stocks. Cable which is unfit for use at its rated voltage may be usable at lower voltage. Material must not be discarded if it can be reused to conserve new material.

A supervisory and alarm system giving temperature and pressure indication and alarm is a tool which can be used to obtain the maximum capacity with safety from a given system. Temperatures and pressures can be watched and the necessary steps taken to relieve any unsafe conditions as they arise. All such systems which have been installed or are being installed should be checked to see that the maximum benefit is being obtained from them. By combining supervisory indication with remote control of switches, a very flexible system will result.

Underground Equipment Limitations

A considerable amount of research has been done on the underground system. Cables, ducts, manholes, transformers, and cable accessories have been studied and related to each other and to the ratings which may be used with safety. Additional work is in progress in many of the companies. In presenting the following material, liberal use has been made of the information now available, some of which has been previously presented to the Institute.

Table I

	Ratings in Amperes (2-Phase 3-Wire—A and C Conductors)		
		Emergency	
	Normal	2-Hour	4-Hour
As received from field	160	240	200
After reconditioning and silver-plating	200	300	250

The above ratings are based on maximum temperatures as follows:

	Temperature—Degrees C.	
	Normal	Emergency
Contacts under oil	70	90
Terminals above oil	85	90

Circuit capacity may be limited by apparatus in the circuit, such as switches. Tests were made at Philadelphia on a subway-type oil switch rated at 200 amperes. These tests revealed that the current-carrying capacity of the switches was relatively low, although their appearance was good. Silver plating of the contact surface was found necessary to put them in satisfactory condition. Table I summarizes these tests.

The *B* phase leg was the limitation in each case. The *B* phase current is approximately 1.4 times the *A* or *C* phase, so that the switch would be rated somewhat higher on a three-phase system.

Equipment of this type, which imposes a capacity limitation on the cable, should be rebuilt, replaced, or removed. These switches are being replaced with a "drop-out" switch in all cases where there is a circuit limitation. The new switch has adequate carrying capacity and may be repaired or replaced without interruption to either cable connected to it.

Transformers and transformer vaults may be a limitation either on a network or on a radial system. In general, underground transformers have been operated at or less than name-plate rating whereas overhead transformers are operated considerably in excess of name plate. The peak load on network transformers averages only 50 per cent of name-plate rating,

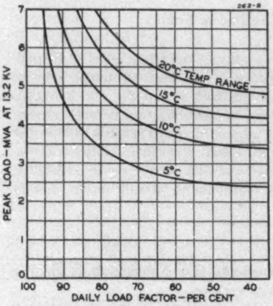

Figure 2. **Allowable daily peak load for various conductor temperature ranges and daily load factors**

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable although individual units may operate close to capacity. This is due to fault capacity requirements, provision for load growth and use of standard sizes. However, individual transformers in a network may become overloaded and require relief. Vaults designed for a single bank may be large enough to install a second bank or a three-phase transformer. Either operating above name plate or operating with excess capacity in the vault will require added ventilation. Natural ventilation may be improved, or forced ventilation controlled by a thermostat may be required. Considerable reduction in ambient temperatures with a corresponding capacity increase may be obtained by ventilation improvement.

Improvement of vault ventilation and subway-transformer cooling has been investigated by the Boston Edison Company. The maximum safe operating copper temperature is established as 100 degrees centigrade at 100 per cent load, which results in a top oil temperature of approximately 80 degrees centigrade. During the summer of 1938 the problem of excess temperature on subway transformers became critical and was temporarily solved by flooding the vaults with water from the city mains. This method of cooling was entirely satisfactory as an emergency expedient, but a more permanent method of correction was necessary.

A fabricated steel grating was designed and used to replace the four-foot-two-inch-by-three-foot-two-inch solid rectangular covers in the roof of the vault. This resulted in a reduction of 14 degrees in the manhole ambient temperature, although the differential between the top oil temperature and the manhole ambient temperature remained at 22 degrees centigrade. A net capacity increase of 15 per cent resulted from the reduced manhole ambient temperature provided by the open grating.

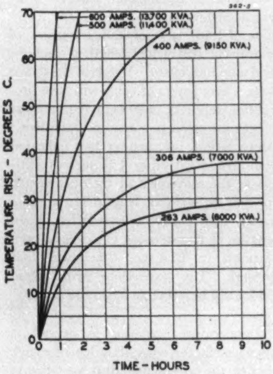

Figure 3. Conductor temperature rise above ambient

Laboratory test data

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable

The transformer loading was so high that this reduction was insufficient to reduce the hot-spot temperature to 100 degrees centigrade. Experiments were then conducted with supplementary external radiators installed on the transformers. Two cast-iron radiators of $26^1/_3$ square feet of rated radiation each were used on a 100-kva three-phase cast-iron-tank transformer. One of two transformers in a vault with the open grating was equipped with these radiators and a ten-day heat run made on each transformer. This resulted in an indicated increase in capacity of 21 per cent for the transformer equipped with radiators when installed in a vault with the ventilating grating.

These tests showed that the combination of an open grating and external radiators on the transformer is equivalent in its cooling effect to flooding the manhole with water, and results in a more permanent remedy.

Insulation Limitations

The problem of cable loading cannot be separated from duct and manhole design. In order to determine the rating of any cable, the number and arrangement of ducts, the other load in the duct bank, the length of the duct run between manholes, the depth of the duct run, the length of the manhole, and the offset in the manhole must be known. Other factors involved are the soil conditions, the earth temperature, and local heating due to steam mains or other subsurface facilities.

Valuable work on the over-all problem has been done by the Commonwealth Edison Company and the results of these investigations presented by Halperin in 1939.[4] Investigations of safe operating temperatures and of the economics of duct loading have been made by the Consolidated Edison Company of New York and reported by Franklin and Thomas in 1939.[5,6] The over-all problem should receive greater attention both in the field and in the laboratory. Present conditions will give a large amount of field experience, and adequate data should be collected. This will supply information for the operation of the system at its maximum capacity and indicate critical conditions which can be corrected before they become serious. It will also add much to our present knowledge of how far we can safely increase ratings of cable and duct. In order to do this with the greatest benefit to the industry, an agreement should be reached on definitions and basic information to be collected.

The great majority of the cable which is in service in the classification being discussed is solid-type paper-insulated cable. Only this type of cable will be considered, although some of the factors may be applied in modified form to other types of insulation.

The maximum allowable copper temperature for solid impregnated paper-insulated cable under the AIEE rule is 90 degrees centigrade minus the rated voltage in kilovolts, but not more than 85 degrees or less than 60 degrees. This rule covers normal operation of the cable. Most users have developed certain working rules for emergency conditions. In some cases, these do not cause any material temperature rise above the basic rule but depend on the heat-storage capacity of the cable and duct. In other cases, the temperature is permitted to go substantially above the recommended level for short times at infrequent intervals. "Emergency operation" is defined in the Association of Edison Illuminating Companies Specification for Impregnated Paper-Insulated Lead-Covered Cable, Solid-Type (July 1941) as operation at temperatures approaching the maximum allowable emergency temperature for one period per year on the average and not for more than four periods in any one

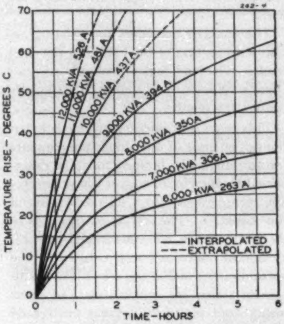

Figure 4. Conductor temperature rise above ambient

Interpolated from laboratory test data

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable twelve consecutive months, each period being not more than 24 hours. The maximum temperatures permitted are 115 degrees centigrade for cables operating up to 1 kv, 102 degrees centigrade for cables operating up to 5 kv and 96 degrees centigrade for cables operating up to 15 kv. These temperatures are being exceeded in some cases, at least one company using 110 degrees centigrade for cables up to 5 kv. Cables up to 15 kv are generally being operated at a maximum emergency temperature of 90 degrees centigrade, although some experimental work has been done at higher temperatures.[4,5]

Duct Limitations

From the above rules, ratings can be developed for cable in various types of ducts, duct formations, soil conditions, and duct occupancy. Complete empty-duct temperature surveys are the most satisfactory method of determining the ratings which should be applied to the cables in any duct bank. In applying the temperature rule literally the hot spot must be used, since no part of the cable should rise above the recommended temperature.

Duct banks have a definite thermal limitation so that many of the old 16-, 20- or larger-duct lines may be thermally loaded, even though there are vacant ducts available. Figure 1 illustrates a typical summary of the analysis of test data for a survey made in Philadelphia on a 20-duct bank containing 13 cables. The survey consists of taking daily temperatures by thermocouples installed in empty ducts and hourly load readings for all cables in the duct bank. The average readings for three consecutive days are used.

The analysis considers the existing actual load factors of the cables in the duct bank. The load factor probably will not change sufficiently in a year to materially change the results.

This chart is predicated on the fact that the upper limit of duct bank temperature (empty-duct air temperature) is determined by the rise of copper above empty-duct temperature for a selected rating of the cable. The cable involved is a three-conductor 350,000-circular-mil 13-kv belted paper and lead cable. At the normal rating of 265 amperes it has a temperature rise of copper over adjacent empty-duct air of 27 degrees centigrade as determined by calculation and confirmed by test. With a maximum allowable normal copper temperature of 77 degrees centigrade for this class of cable, we must limit the empty-duct air temperature to 50 degrees centigrade. This agrees with the findings of Church,[10] Halperin[4] and others on the recommended maximum duct-bank temperature.

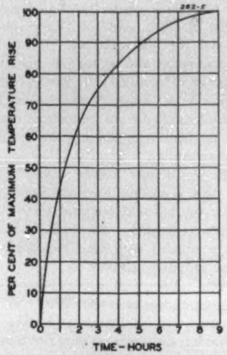

Figure 5. Heating time of cable in terra cotta duct (for all loads)

Three-conductor 350,000-circular-mil 13-kv belted paper and lead cable

Similar charts are used to determine the need for additional duct lines and have been made for many of the critical locations.

Sheath Limitations

Insulation deterioration is not a serious problem with modern distribution cables operated at temperatures well above the AIEE temperature rule.[4–8] It is estimated that the sheath life is from one half to two thirds of the insulation life if recommended temperatures are observed. Temperature variation rather than temperature level causes the major damage to the cable by causing sheath deterioration. The absolute sheath temperature does not materially change the lead characteristics at temperatures below 70 degrees centigrade. Research to extend sheath life is being carried on in the laboratory, but field experience and investigation must come from the cable users to supplement the laboratory information.

Manhole size and design have a very definite relation to cable loading. Investigations have been made and reported,[4] and additional investigations are in progress. The majority of older manholes are too small from the standpoint of cable movement, and inadequate protection is provided at duct mouths. Modern practice provides for a longer and wider manhole and for fewer ducts with improved protection for the cable at the duct mouth. Badly congested manholes in critical locations can be materially improved by a program of rebuilding to more modern standards, which should result in a material reduction in failures due to sheath breaks. Such a rebuilding program would permit higher cable ratings on the rebuilt portion and require very little critical material. Present information is incomplete, but generous manhole construction should be provided wherever the space is available. Halperin[4] has recommended a minimum manhole of 10 by 6 feet for three-conductor 500,000-circular-mil 13-kv cable, and 8 by $4^1/_2$ feet for three-conductor 5-kv cable of 375,000 circular mils or less.

Small cables have less cable movement for the same temperature variation than large cables. Cable conditions can be materially improved by using three single-conductor cables of the same copper cross section in place of one three-conductor cable. This will result in a substantial reduction in the sheath damage without any decrease in the rated loading of the cable. One objection on certain applications is that the cable impedance will be increased approximately ten per cent so that such a cable cannot be operated in parallel with an existing three-conductor cable. However, on radial feeders a substantial increase of the cable rating will result from the use of single-conductor cables.

Cable movement has been related to temperature variation.[4] Cable movement has been related to sheath damage to show that increased movement results in increased damage although present information is incomplete. Factors to be considered include the size of cable, thickness of lead, type of lead, frequency of movement, size of duct, length of manhole, and man-hole offset. Halperin has shown that a long length of cable will have proportionately less movement than a short length for equal temperature variations.[4] More recent information has indicated that up to a variation of 25 degrees centigrade, the movement at the duct mouth for 800- to 900-foot lengths is the same as for 300- to 400-foot lengths. Long lengths of transmission cable have been installed in Chicago, Cincinnati, and Newark. The experience with these cables will be valuable for future installations.

Temperature variation is dependent upon load factor, since, if the load does not change, the only temperature variation is due to the duct-bank temperature change. Figure 2 shows the peak load plotted against the daily load factor for various temperature ranges. The range is the same for a 6,500-kva peak at 95 per cent load factor as for a 3,000-kva peak at 73 per cent load factor. These values are computed but field investigation indicates they are conservative. Sheath damage is a function of cable movement which in turn is a function of temperature variation. A high load factor will reduce the sheath damage and, therefore, permit higher loading of the cable.

Two customers in Philadelphia illustrate the variation that may be expected. One is an oil refinery with a load factor of better than 95 per cent. The second is an electric furnace with a load factor of less than 60 per cent. The latter is full load for approximately three hours and no load for approximately two hours, then repeat. The normal rating assigned to these cables is the same for both customers. No sheath damage is expected from the first but an extreme amount from the second. Unfortunately, the experience has been too short to be of any value.

Emergency Ratings

Ratings have been applied to 13-kv paper-insulated three-conductor belted, 350,000-circular-mil cable in Philadelphia as shown in Table II.

These calculations are based on six cables per duct bank with a 75 per cent load factor, 20 degrees centigrade earth temperature, and result in a normal-load copper temperature of 77 degrees centigrade. The emergency ratings are based on various time-temperature tests to give a maximum copper temperature of 90 degrees centigrade. Figure 3 shows laboratory test data and Figure 4 shows various ratings interpolated from the laboratory test data. The laboratory setup was arranged to simulate field conditions and was later checked in the field and found to be quite accurate. Figure 5 shows the per cent of maximum temperature plotted against time for all loads.

An interesting example of the punishment which can be taken by cable is the Delaware-Richmond 13-kv tie line in Philadelphia. This consists of six cables between two generating stations. Originally in 1926 it was given a rating of 265 amperes per cable, which, due to duct conditions, is about ten per cent above that permitted by the AIEE rule. This was operated at or above its rating for a number of years, and then in 1936 for station economy reasons the rating was increased to 289 amperes. The failure rate doubled following this increase, and in 1937 it was reduced to the original rating. The average failure rate during the period of service has been 28.5 per 100 miles, which is a high rate but, nevertheless, would result in an average cable life of 29 years. Cable-failure rates of this order are extreme and cannot be considered good practice. However, in spite of this high failure rate, the life is so long that obsolescence and system changes have to be given equal weight with service failures. In the fifteen years of operation there have been 95 failures, 28 in the first 10 years and 67 in the last five years. This latter period includes the period of increased rating. During the past $2^1/_2$ years, accurate failure records have been kept and show that 80 per cent of the failures can be attributed to cable movement. Manholes in this entire duct run are small and severely congested, and much of the damage is attributed to this. More modern manholes with adequate space would result in a very substantial reduction in the failure rate. Cable can be punished severely year after year and continue to operate. The economics of this case indicate that it has been good business to operate the cables this way.[9]

Cable rating must be distinguished from cable loading in the operation of a system. The practice in some companies is to establish a rating for the cable but permit the loading to reach only 85 per cent or 90 per cent of the rating. If the system of rating is reliable, the forecasted load should be permitted to equal the rating. In one case[5] the cable rating for 50 cables was raised to a basis of 100 degrees centigrade copper temperature, but later records showed that cable loading had been very little higher than under the old rating. However load growth will gradually change this so that additional capacity will result. Capacity will be made available by working the cable loading right up to the rating. This should be on the basis of average loading and not on short-time swing peaks.

Conclusions

Past investigations and recommendations have been based on restricted experience obtained in the laboratory or on relatively small test installations in the field. There is every reason to believe that the period we are now going through will result in creating a giant laboratory out of our present systems. Our first responsibility is to continue to carry the load and all load which is added. To do this we must search out and eliminate the "hot spots" and "bottlenecks." Material must be used where it will give the greatest return. Secondly, we should get the greatest possible return from this condition to use in future design to build better and more economical systems. An investigation of the following conditions will permit us to do both of these intelligently:

Table II. Emergency Ratings—13-Kv Three-Conductor 350,000-Circular-Mil Belted Cable—Amperes

Normal—265 Amperes

	After Normal Load	After Less Than $^1/_2$ Normal Load
$^1/_2$-hour emergency	460	520
2-hour emergency	365	395
4-hour emergency	330	350

1. Earth temperatures in the vicinity of duct lines.
2. Duct-bank temperatures—studies of various types of duct banks with respect to construction and environment.
3. Cable-sheath temperatures—correlation of cable-sheath, occupied-duct, and empty-duct temperatures.
4. Cable loading—hourly load readings with special consideration to emergency and abnormal conditions. This should be related to above surveys.
5. Cable movement—the relation of cable movement, load changes, section lengths, and load factor.
6. Cable operating performance—careful analysis of cable failures and sheath breaks. Particular attention should be given to failures due to expansion and contraction and to thermal insulation failure.

Summary

1. Provide all reasonable safeguards for the system.
2. Prepare for emergencies and rapid restoration of service.
3. Provide adequate emergency stocks of cables and accessories.
4. Maintain an adequate inspection and maintenance program.
5. Maintain complete records for use in determining present ratings and future revisions.
6. Review ratings of all cables and permit load to reach rating.
7. Rearrange load connected to cable to improve load factor.
8. Provide adequate reliability to all load by alternate routes.

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Power Supply to Distribution Substations in Wartime

H. P. ST. CLAIR
MEMBER AIEE

1. Introduction

THE purposes of this symposium in relating distribution engineering to the present war emergency have been fully introduced and set forth in the opening paper so that further elaboration will be unnecessary here. As indicated by the title, the discussion in this paper will be confined principally to transmission substations and subtransmission lines, all as a means of supplying distribution substations. An attempt will be made to explore ways and means by which more capacity can be obtained from a given amount of material and labor, both in existing facilities as well as in the design and construction of new facilities.

2. Transmission Substations

It might be said that transmission substations are the beginning of distribution, since such substations furnish the supply to subtransmission lines, which in turn supply the distribution substations. The importance of transmission substations in relation to the present war-emergency program is very great. With the exception of generating facilities, the possibilities of their becoming bottlenecks on the system may be more acute than almost any other system element, because of the larger size and more or less special design of equipment required for each particular substation. For this reason it is particularly desirable to explore the possibilities of getting more capacity from existing equipment, and to design new substations in such a way as to use material and labor most efficiently.

Transformer Capacity

While the problem of obtaining larger capacity from transformers is, in many respects, the same, whether these transformers are in a transmission substation or a distribution substation, there are some essential differences. For example, the transmission substation transformers are usually of much larger capacity and more or less "tailored" to fit a particular job, and, hence, there is less possibility of shuffling them around from one substation to another, as is often done to advantage with distribution-substation transformers. Also, because of the greater dependence placed upon such units in supplying not one, but a considerable number of distribution substations, relatively greater care in maintenance and in any procedure used in overloading such capacity should be exercised.

Several possibilities for getting increased capacity from transformers, some of which will also be discussed in a companion paper dealing with distribution substations, will be suggested below.

On existing self-cooled transformers it is possible in practically all cases to obtain an increase of 25 per cent to 33$^1/_3$ per cent in capacity by the application of fans or blowers for forced-air cooling. Where the transformers were originally designed for such cooling, such a procedure would, undoubtedly, be adopted forthwith when increased capacity is needed, and no particular problem would be involved. Permanent blower or fan equipment should be obtainable within a few weeks.

On self-cooled transformers not originally designed for it, the application of forced-air cooling must include a check on the current-carrying capacity of leads, bushings, and so on, with a possible change, if necessary, in some of these to make sure that all parts of the circuit are adequate for the larger transformer output. In extreme emergencies, any type of fan, even a large portable fan, could be used to obtain a quick capacity increase.

In the case of self-cooled transformers already equipped with forced-air cooling, and for water-cooled transformers, an increase in capacity can be obtained by forced oil circulation with external coolers or radiators. These radiators can be air-cooled, or in extreme cases could be cooled by refrigeration.

It should be noted that to get the full cooling effect and resulting maximum capacity out of water-cooled transformers, the cooling coils must be maintained in good condition and free from internal scale or deposit. A thorough cleaning of cooling coils has often resulted in a substantial capacity increase.

The use of external water spray may be found of value in certain emergencies, particularly during hot weather. While this method does not offer a large increase in capacity, at the same time it is inexpensive where water is readily obtainable, and can be applied very quickly when needed.

Referring again to a companion paper on distribution substations, mention is made of the possibility of loading transformers on a temperature basis to values well beyond name-plate ratings, and references are made to material published on this subject. The basis of all of this, of course, is the fact that most transformers possess a certain amount of inherent overload capacity, varying with the load factor and ambient temperature conditions under which the transformers operate. Since we may be forced to use this overload capacity whether we choose to do so or not, it may be possible to do it more safely and intelligently in the light of quantitative analyses as to the effect of overloading on the over-all life of typical transformers, and from these, arrive at

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H. P. St. Clair is system planning engineer of American Gas and Electric Service Corporation, New York, N. Y.

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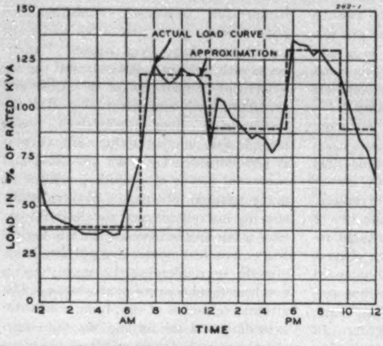

Figure 1. Typical assumed load curve for substation transformers

Load factor 67.5 per cent

Assumed peak load 130 per cent

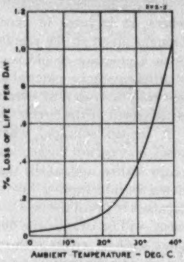

Figure 2. Daily loss-of-life curve for transformers operating on load curve of Figure 1 at various ambient temperatures

some measure of the amount of overloading which can be reasonably tolerated.

As an example of the type of analysis which can be made, Figure 1 gives a typical assumed load curve in which the peak load is 25 per cent above the transformer rating. Based upon this loading, Figure 2 has been prepared showing the loss or consumption of life in this transformer for various levels of ambient temperature. This analysis has been made on the basis of data published by Nichols and others[1,2] and obtained from exhaustive tests on the effect of temperature on the life of insulating materials in oil. It is obvious from Figure 2 that a lowering of the ambient temperature by only a few degrees very greatly reduces the effect of a given overload in consuming the useful life of the transformer.

Actually the data used in the above analysis are believed to be well on the conservative side. For one thing, the ratio between copper and core losses has been taken as 2.75, which is considerably higher than average practice. A lower ratio would obviously mean a smaller increase in heating on overload. This and other margins of safety in the figures used, along with results of field experience, tend to support the conclusion that actual transformer life would be considerably better than indicated by the analysis.

A particular instance where it may be considered entirely permissible to make rather liberal use of the inherent overload capacity of transformers would be in the case of an outage of one unit where two units are normally operated in parallel. While it should be done only on the basis of some knowledge or analysis as to the oss of life involved, nevertheless, a much higher rate of consumption of life could be allowed in this case as against the cost and, particularly during the present emergency, the availability of full spare capacity to carry the entire load. As a matter of fact, this practice, at least to a degree, would be sound economics during normal times.

PORTABLE CAR-MOUNTED TRANSFORMERS

As pointed out above, the transformers used in transmission stations are usually fairly large and more or less "tailored" to fit a particular job, as compared with distribution station transformers. For both of these reasons, there results the possibility of a serious bottleneck to the supply of power in case of

1. A serious transformer failure.
2. A sudden demand for increased power, either at an existing location or at a new location for which it may not be possible to obtain new transformers within the available time.

To take care of such emergencies, primarily on transmission substations, there have been designed and constructed for one system, the system with which the author is associated, portable emergency transformers permanently mounted on specially designed railroad cars and arranged with a considerable number of voltage, tap, and phasing combinations to cover practically all of the transmission-substation requirements on the system. The capacity of these transformers was made as large as railroad clearances and other limitations would permit, and the entire equipment includes lightning arresters and all necessary auxiliaries mounted on the cars. With the exception that lightning arresters must be dismantled during transportation, the entire transformer is carried complete with bushings and oil in place ready to operate as soon as it reaches its destination.

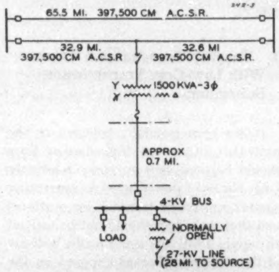

Figure 3. Small-capacity transformer tapped to 132-kv transmission line with minimum protection

Because of the extreme range of requirements to be covered by such transformers, two complementary units of approximately equal capacity and physical size were used. One of these, with a three-phase capacity of 15,000 kva, was designed for stepping down from 132 kv and 110 kv to any of the lower voltages down to and including 11 kv. Transformations from 132 kv to 110, 88, and 66 kv were obtained by autotransformer connections, while other voltages were obtained by two-winding combinations.

The other unit, having a three-phase capacity of 17,000 kva, was designed to cover the intermediate step-down ratios from 88 kv, 66 kv, and 44 kv down to lower voltages from 44 kv to and including 11 kv.

The size and weight of these transformers, approximately 85 tons each, requires each unit to be permanently mounted on a special 100-ton, drop-frame-type railroad car, which is designed, however, to meet all the requirements of the Interstate Commerce Commission and the Association of American Railroads. The cars are registered with the latter

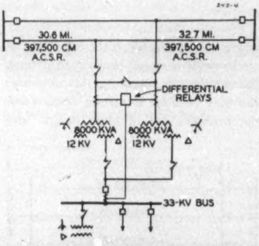

Figure 4. Medium-capacity three-phase transformers tapped to transmission line with automatic air-break switches

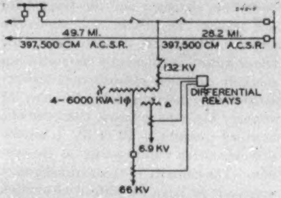

Figure 5. Large-capacity transformer bank tapped to transmission line with automatic line sectionalizing

and subject to the same obligations and privileges as any other railroad freight car.

Inasmuch as railroad sidings are not or cannot be made available at all of the transmission substations or other possible locations where it might be necessary to use these portable "universal" transformers, it will be necessary at such locations to unload a transformer from its car and transport it over highways, using special highway trailers owned by local contractors. For this purpose the weight can be materially reduced by removing the oil and some other equipment from the transformer.

Switching Equipment

As in the case of distribution stations themselves, as pointed out in a companion paper, the rapid expansion of electric systems now taking place, and which will undoubtedly continue to a large extent during the war emergency, creates a serious problem in connection with the interrupting capacity of circuit breakers at transmission stations. In many cases the growth of systems far in advance of expectations has brought about circuit-breaker duties in excess of capacity at stations fairly recently built, or where circuit breakers have recently been modernized. A number of procedures may be suggested for taking care of this circuit-breaker problem.

Probably the most economical solution, where it is applicable, is the rebuilding or modernizing of circuit breakers to increase their interrupting capacity; this, of course, can only be done in certain cases where the breakers are of older design, and where an increase in capacity can be brought about in the rebuilding process. Such a program should result in a substantial saving in material and labor and generally also in cost over the complete replacement of the circuit breakers.

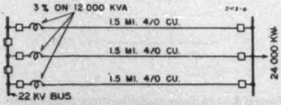

Figure 6. Original layout for 24,000-kw load

A second solution, which, in many cases, may permit the necessary expansion of the station and, at the same time, hold down the interrupting duty on the circuit breakers to avoid the necessity of replacement, will be to split the bus into two or more sections, either with or without reactors to tie the sections together. Unless the system is designed on a well-balanced basis for operating independent bus sections, it will probably be necessary to use reactors to tie the sections together. In some cases it may be necessary to employ both of these procedures, that is, to sectionalize the bus and to rebuild the circuit breakers as well.

The third, and perhaps final alternative, will be to install new and higher-capacity breakers where the existing breakers are hopelessly inadequate. This, of course, does not save either cost or material, except where it may be possible to make use of the existing breakers at other locations for which new breakers otherwise would have to be purchased. This "shuffling" of breakers, with the purchase of only larger sizes wherever possible, and the shifting of smaller breakers to lesser locations, is sound practice from an engineering and economic standpoint, not only during emergencies but during normal times as well.

Disconnecting switches and air-break switches should not ordinarily present a problem in connection with increased loading on transmission substations but should be carefully checked and maintained, along with other equipment in the station, to be sure that adequate carrying capacity is available. Overheating of such switches will lead to progressive weakness and eventual failure if not taken care of.

3. Tapping Transmission Lines With Low-Cost Transmission Substation

It has been generally believed in the past that important transmission lines should be used only to carry relatively large blocks of power between generating sources and major transmission stations, and that such lines should not be tapped to supply relatively small loads, because of the additional hazard imposed on the transmission lines. However, the economies made possible by occasionally tapping transmission lines with a relatively inexpensive step-down transformation have been so great that more and more installations have been made. Experiences with these installations, and the development of methods for installing and protecting such equipment, as well as the transmission lines themselves, have brought this practice to the point where it is now considered a very practical and successful means of providing service at many suitable locations. If a transmission line is available near the desired location, a tap on the line may be the logical and economical means of supplying loads directly, or reinforcing the supply to an overloaded subtransmission system. The alternative means of supplying a load by extending and reinforcing the subtransmission system may be much more expensive both in material and labor.

Many such installations have been made on the systems with which the author is associated, particularly for stepping down from 132,000 volts to various voltages all the way from 66,000 to as low as 4,000 volts. These transformer installations vary from the very small ones as low as 1,500 kva in capacity for serving a 4,000-volt load, to as high as 18,000 kva for reinforcing a 66,000-volt subtransmission system. The smaller installations, for obvious economic reasons, are made as simple as possible with a minimum of protective equipment. For this reason it may not be feasible to provide for clearing the transformer in case of fault in the transformer, so that it is necessary in this case to take the risk of transformer damage. Experience for a number of years, however, has been quite satisfactory in this regard. Figure 3 shows a typical installation of a small 1,500-kva three-phase transformer tapped to a 132-kv transmission line.

For larger stations and loads, more elaborate protective equipment is justified, although not the expense of high-voltage circuit breakers. Figure 4 shows a typical medium-capacity station with automatic air-break switch protection for the high side and with a differential-relay scheme for clearing the transformers in case of trouble. The two main functions of the protective equipment in this case are as follows:

1. For a transmission-line fault, the transformer is disconnected by means of its low-voltage circuit breaker from the subtransmission system, which would, otherwise, feed into the transmission-line fault through the transformer. To provide positive relaying of the transformer switch in case of line-to-ground fault on the transmission line, it is necessary that the transformer be wye-connected on the high side with neutral grounded.
2. For a fault in the transformer itself, the

transmission line is opened by its own relaying and the transformer disconnected from the low-voltage side by its own differential relays. Immediately thereafter, during an interval of approximately one minute while the transmission line remains de-energized, the transformer high-voltage air-break switch is opened up automatically from the operation of the differential relays. Following this the transmission line is restored to service.

The desirability of providing a grounded wye-connection on the high side of such a transformer may add slightly to the cost of installation:

1. By necessitating a delta-winding on the transformer if it results in a wye-wye connection.

2. By requiring the installation of a grounding transformer for the low-voltage bus if it results in a wye-delta connection, and it is also desired to establish a neutral ground at that point for the low-voltage system.

This additional cost, however, is usually considered well-justified to obtain the more complete relay protection which it affords.

While carrier-current relaying is actually employed on the transmission lines to which this station is connected, its use is not essential to the carrying out of the protective functions described above, except with regard to the ultrahigh-speed reclosing on the transmission lines. The operation of the transformer-differential relays provides a carrier impulse which stops the line from reclosing in case of a transformer fault; otherwise, the line would reclose on the transformer fault the same as for a fault on the line itself.

A still larger station with the most complete automatic protection obtainable without using high-voltage circuit breakers is shown in Figure 5. Here the transformer installation is large enough to justify the use of three single-phase units with a fourth as a spare. The manner in which transformer-differential protection, as well as transmission-line sectionalizing, is accomplished in this station, using automatic air-break switches only, is described as follows:

1. For a permanent transmission-line fault on either side of the station, the entire line is cleared, along with the low-voltage transformer breaker, and remains open for an interval of approximately one minute during which the two line-sectionalizing air-break switches are opened. Following this, both line sections reclose, the good one remaining closed, and the faulted one tripping and locking out. The sectionalizing air-break switch, on the line which remains energized, then recloses, restoring service to the transformer bank. The final step is the reclosing of the 66-kv circuit breaker on the low side of the transformer bank.

2. For a fault in the transformer bank, both the line and the transformer low-voltage breaker are cleared, but, in this case, the differential relays act to open up the transformer high-voltage air-break switch instead of the line-sectionalizing switches, after which the line recloses and the transformer bank remains de-energized. As in the station of Figure 4, carrier-current relaying and ultrahigh-speed reclosing are used on the transmission line, and, for a differential-relay operation on the transformer bank, a carrier impulse is sent out to prevent the instantaneous reclosure of the line into a transformer fault.

The satisfactory operation of these stations, and of many others of which they are typical, has fully established the soundness of this method of supplying power directly from transmission lines at relatively low cost. The use of this device wherever favorable opportunities are presented offers one possibility for supplying power to distribution substations, and, in some cases, to distribution loads directly, with the most efficient use of equipment and labor.

4. Subtransmission Lines, Existing and Proposed

The term "subtransmission lines," as used in this paper, refers to the intermediate voltage systems, such as 22 kv up to and including 66 kv, which are generally used to supply distribution substations. Such lines, of course, may be supplied either from transmission substations or direct from generating plants. The rapid growth of load brought about by the present war emergency has posed the problem of getting more capacity out of existing lines, or of providing increased capacity in one way or another with a minimum expenditure of material and labor. While a most favorable solution of the problem will, of course, depend upon the particular factors and limitations in each situation, an attempt will be made to suggest methods of obtaining increased capacity in a number of typical situations that may come up.

Short Lines—Thermal Limits Controlling

For lines that are relatively short with respect to the amount of power and voltage, the capacity limitation may be almost entirely a matter of the thermal limits of the conductors themselves, rather than limitations due to voltage regulation. In many cases, however, both of these limitations must be taken into account. Typical ways of obtaining capacity increases in situations of this kind may be suggested as follows:

1. Where several circuits with conductors of moderate or small size are already operated in parallel, the most economical and efficient method of obtaining an increase in capacity may be the rebuilding of such circuits with a considerably larger conductor, on the assumption, of course, that the existing conductor can be salvaged for needed use elsewhere. This method offers certain substantial savings in that

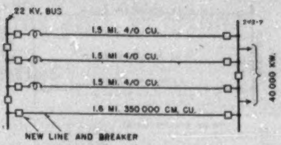

Figure 7. First step to increase capacity of lines to serve 40,000-kw load

1. No additional terminal switching is required.
2. No additional right-of-way is required.
3. In the case of wood-pole lines in good condition few if any structures will have to be replaced.

If the use of larger conductors alone is insufficient to provide the necessary increase in line capacity, it may become necessary to construct additional circuits which, because of right-of-way difficulties, will usually be at least as long if not longer than the existing circuits. In such an event, the use of a larger conductor on the new circuit will be of little advantage, unless the conductors on the original circuits are enlarged at the same time. In other words, if the existing circuits are not changed, any attempts to load up the new circuits in proportion to their larger conductor capacity would result only in exceeding the thermal limits of the existing circuits.

As an illustration, Figure 6 shows a typical case of three parallel 22-kv lines serving a 24,000-kw steel-mill load. The reactors in these feeder circuits were originally installed to limit the interrupting duty imposed on the feeder circuit breakers. Two steps of increased capacity were required, first about a 50 per cent increase to a load of 36,000 kw, and later an increase to more than double, or approximately 70,000 kw. In carrying out these steps, the procedure outlined above was actually reversed because of the presence of the reactors in the original feeder circuits. Figure 7, therefore, shows the first step, which consisted of building a new circuit of 350,000 circular mil of copper to operate without any reactor and in parallel with the three existing circuits. As a result of this arrangement, the new circuit, both from the standpoint of thermal limit as well as actual impedance, had a capacity of approximately $1^{1}/_{2}$ times that of the old circuits, and thus provided the necessary 50 per cent increase in over-all capacity.

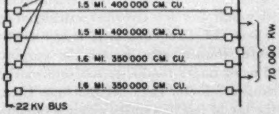

Figure 8. Final layout to serve 70,000-kw load

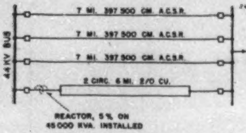
7 MI. 397 500 CM. A.C.S.R.
7 MI. 397 500 CM. A.C.S.R.
7 MI. 397 500 CM. A.C.S.R.
2 CIRC. 6 MI. 2/0 CU.
44 KV BUS
60 000 – 70 000 KW
REACTOR, 5 % ON 45 000 KVA. INSTALLED

Figure 9. Unequal impedances of lines in parallel—resulting in bottleneck

The final arrangement of the setup, to increase the capacity to approximately 70,000 kw, was then obtained as shown in Figure 8. Here the reactors in the original three circuits were removed, new circuit breakers of adequate interrupting capacity were installed, and the existing number 4/0 copper line conductors were replaced with 400,000 circular mil of copper. Both the breakers and line conductors removed in this process were used elsewhere.

In this case at least three new circuits of the same capacity as the original circuits would have been required to handle the final load increase so that the method of changing conductor size resulted in a net saving of two circuits with all the terminal equipment, line construction, and so on, pertaining thereto. At the same time, voltage regulation under the final setup was quite satisfactory.

2. In other cases of relatively short heavily loaded lines, it may not be feasible to change existing conductors; for example, where steel tower lines are already carrying the maximum physical load. Such a situation may be further aggravated by the necessity of taking a longer route for new circuits.

Figure 9 shows a typical instance where one number 2/0-copper double-circuited line constituted a bottleneck when operated in parallel with other circuits of larger conductor size. Here the installation of a reactor in the number 2/0 copper circuits eliminated the bottleneck and allowed all of the circuits to carry load commensurate with their actual thermal limitations. At the same time, with the fairly short lines involved, no voltage regulation problem was introduced by the use of the reactor.

MEDIUM LENGTH LINES—BOTH THERMAL LIMITS AND VOLTAGE REGULATION INVOLVED

In some situations, it may be necessary to increase the capacity of an existing system, consisting of a number of circuits of medium or small conductor size, which cannot readily be changed for physical and economic reasons. Because of increasing right-of-way difficulties, as in the cases mentioned above, it is often necessary that any new circuits constructed to relieve the old circuits be of longer length which immediately defeats the purpose of using larger conductor size or even the same conductor size. At the same time, the use of reactors in the existing circuits would be undesirable from the standpoint of voltage regulation as well as from the standpoint of the number of circuits involved.

For a typical situation of this kind, shown in Figure 10, it was desired to construct two new circuits of relatively high capacity to operate in parallel with four existing circuits of medium capacity, but right-of-way conditions necessitated a longer route for the new circuits. A solution was found in the use of a series booster transformer of simple design connected in each of the two new circuits and operated as part of the line. Using a 60-degree series voltage, obtained from an adjacent phase of a wye-connected transformer as in the zigzag transformer connection, these transformers introduced both a voltage boost and a phase-angle shift to force the line to carry a heavier share of both kilowatts and reactive kilovolt-amperes. The desired results in loading, as shown by Figure 10, were obtained with only $2^{1}/_{2}$ degrees angle shift and the corresponding voltage boost of about $2^{1}/_{2}$ per cent, which required a series winding of only five per cent. Such an installation, even allowing for a maximum angle of five degrees for future contingencies, can be made relatively inexpensively, as it does not require additional switching facilities or a transformer designed for changing taps under load. Taps, of course, should be provided, but can be changed by momentarily de-energizing one circuit at a time. The amount of phase angle required is so small that it should not be necessary to remove it under light load conditions.

LONGER SUBTRANSMISSION LINES—VOLTAGE REGULATION CONTROLLING

In some situations, the length of lines and loading is such that voltage regulation rather than thermal limitations is the determining factor. If the system is laid out with sufficient circuits to give reliable loop service, the use of some means for voltage regulation is probably the most efficient and economical way of increasing system capacity. If not too many stations are involved, individual regulation such as load-ratio-control transformers might be used. Where the need for regulation is quite extensive, it may be more economical and otherwise more advantageous to install one or more synchronous condensers for power factor and voltage control. The beneficial effect of synchronous-condenser operation, particularly if installed at the proper load point on the system, extends over the entire system and even permeates all the way back through the transmission and generating systems. The increase in capacity provided thereby is usually greater than that obtainable from other types of voltage-regulating devices.

If systems such as these are traversed by high-voltage transmission lines, transformer installations tapping directly to the transmission lines may offer the most economical means of increasing the system capacity and at the same time solving the voltage-regulation problem. Even with a synchronous condenser installation, the problem remains of providing the necessary transformer capacity at the transmission substations or other points of supply for the particular system involved.

INCREASE IN CAPACITY BY REVISED CONCEPTS IN OPERATING PRACTICE

In addition to the ways and means discussed above for obtaining increased capacity in subtransmission systems by actual physical changes in these systems, there is also the possibility of getting more out of existing systems merely by a change in prevailing conceptions as to what constitutes feasible operating practice. In other words, during the present emergency situation, it may become necessary to revise existing standards as to the combined requirements of service continuity and voltage regulation. For example, in cases

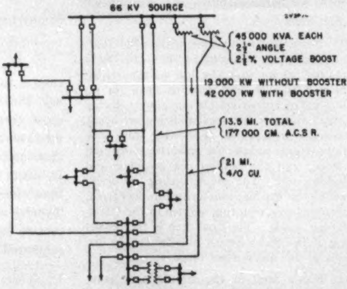

Figure 10. New circuits with larger conductor but greater length compensated to pick up load

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where thermal capacities have not been reached but where voltage regulation is the limiting factor, it may be quite proper to dispense with the common criterion of being able to handle the peak load within prescribed regulation limits with one line out of service, and rather to load all of the lines to the point of permissible regulation and accept an emergency reduced voltage if one line goes out. This, of course, cannot be carried to an extreme such as where the load is supplied by only two lines, since the loss of one such line would result in practically no service at all. It should be feasible, however, where three or four circuits are operating in parallel. Such a situation, of course, is not the most desirable and should be avoided if possible but it may be a necessary sacrifice during the emergency to dispense with the luxury of perfect voltage regulation under all expected line-outage conditions.

In other cases, where thermal capacity limits do enter into the problem, it may be necessary to allow an appreciable encroachment on the normal thermal limits on a short-time basis during the outage of one line, so that a larger loading can be carried with all of the lines in service. In defense of such loading, it may be pointed out that line outages of long duration are relatively infrequent and, furthermore, the simultaneous occurrence of a long-time outage during both a high ambient temperature and a low wind-velocity condition would be relatively improbable.

5. Conclusion

It is realized that only a few of the many possibilities that exist for more efficient handling of the problems of power supply to distribution substations have been touched upon in this paper. It is obvious that for such ideas to be utilized at their fullest potentialities, a substantial background of system planning and studies is needed. But even in such cases knowledge of possibly new but tried methods of approach should help.

It is hoped that the suggestions and examples which have been given in this paper will be of value not only as direct suggestions but also indirectly as an incentive to further thought and discussion on this important subject.

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Distribution Substations and Wartime Necessities

F. C. POAGE
MEMBER AIEE

M. W. REID
NONMEMBER AIEE

Synopsis: Requirements of war will impose unprecedented demands upon many distribution substations. Equipment will be overloaded before extensions can be built, if at all. New construction must be simplified to the bare essentials. Substitute materials must be used. Protection and maintenance must be intensified and operating personnel must be educated to new responsibilities and skills. These are discussed and means are suggested to meet these needs.

LIMITED to that very important part of the whole power system indicated by the title, that is, the "distribution substation," it is the purpose of this paper to make suggestions, to stimulate thought, and to invite discussion of means available under wartime conditions to serve increased loads, to preserve and protect existing equipment, to provide for restoration of service when outages occur, and to employ the available materials, equipment and man power economically in the necessary extensions to existing substations and in the construction of necessary new substations.

The authors do not here presume that they have exhaustively treated the subject, and they confidently expect that in addition to their suggestions many other means will occur to engineers and operating men and will be developed from time to time under the varying requirements of individual situations.

More Intensive Use of Existing Equipment

The three principal functions for which distribution substations are established are: transformation, voltage regulation, and switching. Usually, apparatus for all these three, together with accessories such as line entrances, potheads, cables, lightning arresters, busses, instruments, meters, and control equipment are grouped together in assemblies of varying capacity and complexity, depending upon local needs and the preferences of designers and users.

Bottlenecks limiting the load which may be carried from a given substation may appear in many items of the equipment mentioned. Their correction or removal often can unlock substantial increments of vitally needed capacity.

Transformers

In general, power companies do not load main power transformer banks to the limit of their capabilities. However, as loads continue to increase during this emergency, in advance of the possibility of obtaining equipment, many transformers will have to operate far above manufacturer's rating.

The idea that a transformer name-plate rating is a limit beyond which load must never go dies hard. Transformers may be operated without distress at loads well above kilovolt-ampere ratings upon the basis of temperature. There is considerable material published on this subject which can be re-examined with profit at this time.[1–4] To carry load on transformers beyond the limit fixed by temperature under self-cooling, some form of artificial cooling must be used. For this purpose, artificial air circulation and water jets are most common. Under emergency conditions, almost any form of fan will circulate the moderate volume of air required if there is a sufficient number of these fans properly disposed to reach the greater proportion of radiating surface of the transformer. The use of water jets is more effective than air circulation but is, of course, confined to those locations where supply of water can be made available. Forced circulation of the insulating oil through separately-mounted radiators or water-cooled heat exchangers is also possible. This scheme is particularly adaptable to indoor installations. By use of these methods of cooling, loadings may be increased, often 50 to 200 per cent over name-plate kilovolt-ampere rating, depending upon the daily load cycle, prevailing ambient temperatures, the scheme of artificial cooling adopted, and liberalness of design of the transformers themselves.

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F. C. Poage and M. W. Reid are electrical engineers with Ebasco Services Inc., New York, N. Y.

VOLTAGE REGULATORS

Voltage-regulator loadings may be increased in precisely the same manner as transformer loadings, except that in step-type regulators, the tap-changing switching equipment must be considered. In some instances, induction-type regulators having series parallel windings can be doubled in current capacity by connecting windings in parallel but at a sacrifice of half the range of regulation obtainable when operated in series. It has been too often observed that the "bucking" half of the available regulating range is unused. In such cases, this reduction of range will entail no hardship if the bus voltage is adjusted on a scheduled cycle. Booster transformers may be installed in conjunction with regulators connected in this manner to maintain voltage at locations where its level at peak otherwise would be objectionably low.[5]

CIRCUIT BREAKERS

Increases in current-carrying capacities of circuit breakers can be obtained in some instances through silver-plating of contacts and bushing studs, and by frequent careful inspection and adjustment to assure maintenance of contact pressure. Switching arrangements can be adopted which place two circuit breakers in parallel, but care must be exercised that the impedances of parallel leads are equal, lest the resulting current unbalance overheat contacts and other current-carrying parts of one of the breakers.

When adding to transformer kilovolt-ampere capacity, either by replacement of an existing bank with larger units or installation of additional units, the increase of transformer capacity may increase fault current sufficiently to exceed the interrupting capacity of the circuit breakers. In such a case, if the substation has two or more transformer banks installed, the main bus may be separated into two or more sections, thereby limiting fault current to a value within the capability of the circuit breakers. This method introduces an element of inflexibility into the operation of the substation and to that extent may be objectionable, but it may be the only practical solution to meet the situation at hand. Another solution is to arrange to trip one or more of the transformer breakers or a bus-sectionalizing breaker a few cycles ahead of the feeder breakers in cases of high-current faults, thereby reducing the current to be interrupted by the feeder breakers.

Reactors may be inserted between bus sections normally fed from different transformer banks. A method of limiting short-circuit current applicable where bus sectionalizing is not possible is the insertion of series reactors in the supply circuits. Such reactors will introduce additional regulation and losses into the supply circuits. This effect, where objectionable, may be overcome by normally short-circuiting the reactors through a breaker or fuses co-ordinated with the time characteristics and interrupting capabilities of the feeder breakers.[6] Series reactors may, in emergencies, be "homemade" and constructed from materials that are stock items, that is, bare copper cable and concrete.

If a circuit breaker of adequate current-carrying and interrupting capacity is available, it may be used with appropriate relaying as a group breaker to open a group of feeder circuits when fault current exceeds the interrupting capacity of individual feeder breakers. The analysis of individual situations will often indicate that the impedance of only a few hundred feet of overhead line is required to reduce the fault current to a value which can be safely interrupted by the feeder breaker. By carefully adjusting relay settings, unnecessary operations of the group breaker can be held to a minimum and consequently only a very small percentage of the total tripouts will involve interruptions on feeders in the group which are not in trouble.

Finally, in some cases, the risk will have to be assumed that such faults as do occur will not be of maximum magnitude, and hence breakers will continue to be used at locations where their interrupting ratings are exceeded. This forced decision will unquestionably be reached frequently in the days ahead.

It seems hardly necessary to point out the increased need for thorough and frequent inspection of circuit breakers during this emergency and of keeping them in even higher state of operating efficiency than has been the practice in the past. When apparatus so necessary for protection of vital equipment becomes difficult, if not impossible, to replace, its preservation assumes great importance and reasonable added expense to assure that preservation is warranted.

OTHER SWITCHING EQUIPMENT

Air-break switches and disconnecting switches, particularly those of earlier manufacture, may have capacities increased through the silvering of contact areas and increase of contact pressure. In some cases, this may involve the use of clamps installed over the jaws of contacts. However, it is fortunate that this apparatus as installed is generally of liberal current rating and is seldom a bottleneck in the system.

CABLES AND DUCT LINES

Forced ventilation of cable tunnels, manholes, and duct lines may be required to reduce ambient temperatures and increase capacity of cable circuits. Such

Figures 1–3. Suggestions for release of needed substation equipment

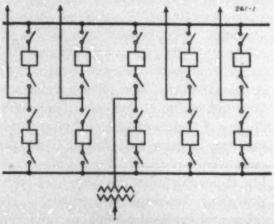

Figure 1. One-line diagram of a double-bus substation

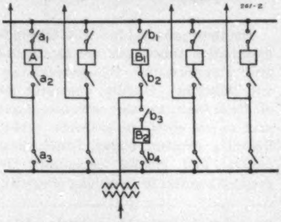

Figure 2. One-line diagram of double-bus substation converted to main- and inspection-bus arrangement releasing four oil circuit breakers and four sets of disconnecting switches

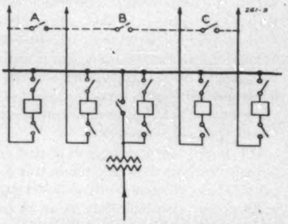

Figure 3. One-line diagram of single-bus substation showing portion of distribution circuits to illustrate sectionalizing and cross-connecting of these circuits to give advantages of inspection bus

Parallel facilities together with such sectionalizing facilities as are usual and desirable for transfer of load and isolation of faulted sections are provided at points A, B, and C ventilation must be directed so that, in case of failures, fires will not be spread to other parts of the substation or operators driven away from their posts of duty.

Busses and Connections

Drastic restrictions in use of copper have been imposed by the Office of Production Management. Copper as a conductor of electricity is recognized as necessary, and power companies undoubtedly will continue to obtain the metal, but in reduced quantities and after much delay. This delay may even become the equivalent of no deliveries at all in the cases of special sizes and shapes of bars and fittings.

Busses and equipment leads are usually of ample current-carrying capacity without exceeding the usually accepted temperature rise of 30 degrees centigrade. This rise, however, may be materially exceeded without deleterious effects on buses, leads or equipment attached to them. Increase of current in the bus of the order of 20 per cent above the limit fixed by present standards seldom will raise bus temperature more than an additional 10 degrees centigrade, if there is reasonably adequate opportunity for dissipation of heat. Higher operating temperatures may require additional provision for expansion, some contact surfaces may require silver-plating to reduce local heating, and soldered joints may need to be replaced by brazed or welded joints. Forced ventilation may be required to protect adjacent apparatus and cables from heat generated within enclosed bus structures. In special cases, temperature of bus bars may be lowered where one section of the bus carries all or a large proportion of the total current, through rearrangement of incoming and outgoing circuits to reduce the current carried in any one section.

Obtaining Additional Circuits and Releasing Switching Equipment

Many substations are in operation which have duplicate equipment not essential to normal operation. Some of this may have to be "borrowed" for more active duty elsewhere. This often can be accomplished without introducing serious operating handicaps. Figure 1 and Figure 2 illustrate one example of this procedure. Figure 3 shows how, in some instances, this may be carried further, still retaining reasonable operating flexibility.

Where duplicate bus and switching facilities are used primarily to permit regular maintenance work on the usual 40-hour-week basis, the rescheduling of this work on a three-shift 24-hour-day basis will often permit these changes and simplifications.

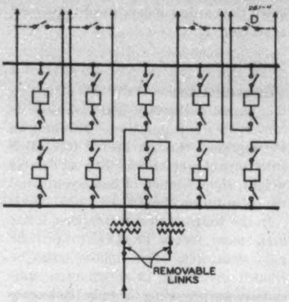

Figure 4. Suggestions for doubling number of circuits and transformer capacity with no additional switching equipment

One-line diagram of double-bus substation of Figure 1 converted to a dual single-bus substation with doubled transformer capacity and number of circuits

Parallel facilities similar to those shown in Figure 3 are provided at point D

Interrupting duty on oil circuit breakers is not increased by this increase in capacity

Figure 4 illustrates a means of doubling the number of circuits and the transformer capacity of the substation of Figure 1 without either increasing the number of breaker positions or the interrupting duty on the breakers.

Automatic high-speed reclosing has been advanced in recent years as a working substitute for duplicate service. Where this would result in saving new construction in the distribution system, the rebuilding of older low-speed breakers may be avoided in substations arranged as in Figure 1 and Figure 2 by the following relay scheme, reference being made to Figure 2. To effect this operation, an auxiliary relay is connected to the trip circuit and auxiliary switches of breaker A, such that completion of the opening cycle of breaker A energizes the closing circuit of breaker B_2. With disconnecting switches a_1, a_2 and a_3 and b_1, b_2, b_3 and b_4 closed, tripping of breaker A will then automatically close breaker B_2 and reenergize feeder A.

Static Capacitors

Where static capacitors are installed on distribution systems, consideration should be given to their proper location on feeder circuits for relief of substation equipment and apparatus that may be severely overloaded.

Installation of capacitors at substations, although not usually as productive of over-all system economies as when installed near the load centers on feeders, may still relieve overloaded condensers, transformers, subtransmission circuits, and system generating equipment. Switching groups of capacitors to control the reactive load and bus voltage can effectively take the place of synchronous condensers or extend the regulating range of existing synchronous condensers.

Customer Co-operation

The co-operation of power customers in scheduling as much of their operations to off-peak periods as practicable offers possibilities for releasing capacity in all current-carrying facilities. There are many industries and commercial enterprises where this off-peak use of power can be effected without difficulty and others where it may be accomplished after careful study and planning. It seems likely, in the emergency, that such co-operation will not be too difficult to obtain where off-peak operation is at all practicable. Co-ordination of the maintenance schedules of customers and power companies should be developed to minimize interference with wartime production and the burden on reserve facilities.

Equipment Capabilities

Operating men will be able to meet load changes and utilize the maximum capabilities of equipment only by keeping themselves more than ever intimately informed of load conditions and the actual capabilities of equipment available, and by using utmost ingenuity in applying that knowledge. Transformers, regulators, circuit breakers or other equipment may become heavily loaded at one substation, and replacement by larger more lightly loaded units from another substation may be indicated. However, only through a thorough knowledge of the equipment itself and what it may be expected to do, can the operating man know whether or not the transfer is justifiable. By keeping information up to date, the need for rearrangement may be anticipated, planned in advance, and changes effected with a minimum of delay.

Equipment and materials in stores should be inspected and, if necessary, reconditioned for immediate use. Thereafter, these should be maintained in readiness in the same manner as those already in service. Many units of equipment and items of material that in more normal times would be looked upon as obsolete or unsuitable for further use can be reused, either by the company, a sister company, or a customer. It is a reasonable prediction that reserve equipment will be depleted and older equipment outmoded or formerly considered inadequate will be found highly useful in the days ahead.

INTERCOMPANY CO-OPERATION

Consideration should be given to the exchange of information among power companies as to the availability of equipment that may no longer be needed for use by the one, but invaluable perhaps for service by another. There should also be merit in a tool-lending and exchange service, similar to that which is understood to be in operation among some of the Canadian utility companies, and in expansion of such service to include replaceable equipment and materials.

Power companies, in the past, have co-operated fully in times of need, and in this emergency period the utmost co-operation is required that all may together better perform a difficult job.

Economies and Conservation in New Work

PLANNING AND DESIGN

In the design of substations to serve wartime loads, a very large "factor of ignorance" must be dealt with in the preliminary stages. Experience has indicated that actual demands may vary up to several hundred per cent from preliminary figures.

As scarcity and priority restrictions both operate to make materials and equipment difficult to obtain, initial orders should cover the full requirements of the work. To run short of materials needed to finish construction may seriously postpone operation of the facilities. To insure against omission of materials required for complete construction, plans must be prepared in greater detail than heretofore considered necessary. This greater detail will also assist erection in view of the lesser skill and experience of the available labor. Extreme care should be exercised in obtaining accurate material lists from the plans. Construction work must be carried out with fidelity to plans, as small changes may call for materials not on order and procurable at best only after long delay.

The scarcity of competent men applies with double force in the engineering and drafting groups, where the detailed planning of new work is done. Simplification of layout, use of standard or semistandard groups of equipment, and elimination of "special" items are all essential to keep down the burden of detail and to prevent mistakes.

STRUCTURES

Structural steel is, at the time of writing this paper, a shortage material, and its conservation is imperative. This may be accomplished through use of substation structures of greater flexibility and lighter weight, simplification of bus layouts and the substitution of wood.

In the design of steel substations, it has been more recent practice to provide rigid structures. This rigidity could be relaxed materially in distribution substations by providing for only slack-span attachments of lines to structures without sacrifice of needed strength or safety. There are many earlier structures of this lighter design in service throughout the country which continue to stand as evidence that these lighter, more flexible structures are adequate.

Wood can be substituted for steel in the majority of jobs. Where insulation is equal to that which would be used on steel structures, and where hardware is thoroughly bonded and grounded, wood structures are electrically equal to steel. Many wood structures long have been in every-day operation. There is no question but that they can be built to operate satisfactorily for all usual voltages, will last through the war period, and will conserve materials sorely needed elsewhere.

BUS AND CIRCUIT ARRANGEMENT

Many of the plants recently established for producing war material provide for power supply to at least half of the load from each of two sources, including separate substations. The practice of providing transfer-bus, inspection-bus, or other duplicate-bus arrangements for new distribution substations should be abandoned for the duration of the conflict in the interest of conserving needed materials and equipment. To serve loads for which separate feeds are already provided, this practice is inexcusable. To serve loads not vital to prosecuting the war, in view of the impelling needs of the times, the omission is amply justified. Substations, if expected to be permanent, may well be designed to permit future elaboration and installation of additional facilities when conditions will permit.

For high-voltage moderate-current bus bars and connections, structural steel shapes or steel pipe may be substituted for copper tubing or bar so long as copper continues to be a shortage material and more difficult to obtain than steel. It may be necessary to use copper cable as bus in lieu of copper tubing or bar, as it seems likely that copper in such form will be more readily obtainable and requires less labor for production and installation. Busses may be reduced in cross section at a saving of material through design to operate at higher current density and through reducing bus current by locating the supply transformer connections as closely as possible to the load center of the substation bus.

FUSES IN LIEU OF BREAKERS

Fuses can be substituted for high-side breakers in substations of simple layout. One precaution may be needed, that is, phase-failure relays may be needed to open the low-side breakers to prevent damage to customers' polyphase equipment in case the supply to the high-voltage side is single-phase due to blowing of one fuse. With this precaution and with proper fuse co-ordination, the installation of high-side breakers may be eliminated, and indeed has been eliminated under normal practice in many situations with satisfactory results.

INSULATION LEVELS

Insulation levels and insulation co-ordination should not be sacrificed in the case of simplified substation layouts. The cost of protecting service by providing adequate insulation strength is very little compared with that of duplicate facilities, throw-over switches and other provisions, which are usually installed to permit continued service in case of apparatus failure. In one case, the cost of the next higher level of impulse insulation in an important unit of substation equipment accounted for only five per cent in the cost of that one item. This might well be considered as representing the insurance necessary to permit elimination of permanently installed duplicate or spare equipment with accompanying switches, bus, and cables.

TRANSFORMERS

The power companies which in the past made a consistent effort to standardize transformer voltages and taps are in position to reap large benefits from this past effort during the present emergency. One of the advantages of this standardization is, of course, that such companies are in position to "play checkers" with their transformers, moving them about between substations to meet the shifting demands. In this connection, it would be well to emphasize the economy of buying new transformers only in the larger sizes in current use by such a company and installing these at locations where smaller transformers are overloaded so that they in turn can be released for installation in new substations.

The three-phase transformer is more economical than three single-phase units of equal rating from the standpoints of material and labor that enter into its manufacture. It requires a single foundation of lesser volume, shorter and more simple bus connections, and fewer man-hours of labor to install. The modern three-phase transformer is reliable in operation and its use in substations of moderate capacity should be carefully considered as a measure of material and labor conservation.

New substation transformers, whether single-phase or three-phase units, should be obtained with blowers or at least with provision for their addition later. Additional capacity may be obtained in this way at a very small cost per kilovolt-ampere, and the economics of losses and regulation can well be neglected "for the duration."

Unit Substations

The limit to which substation capacity may or should be expanded at a given site should be very carefully considered with respect to the cost in dollars, materials and man power of transmission and distribution facilities immediately associated with it. The development of the factory-built single-unit-type substation consisting of transformer, regulator, circuit breaker, and auxiliaries in a single case offers a very attractive means of increasing capacity to serve a given area without adding to either feeder capacities or substation capacities at an existing site. These unit substations are particularly adapted to installations where it is necessary to reinforce feeder circuits by installing such units close to the load and breaking up heavily loaded feeders to keep load within the capacity of existing substations and their outgoing circuits. The fact that such unit substations may be installed quickly and moved quickly to new locations is a decided advantage in meeting unexpected shifts or additions to loads.

Unit substations are economical in material and labor required in manufacture and require less material and labor of erection for foundations, structures, and bus work than equivalent conventional substations. Their use is of advantage also in that labor required for installation may be largely of lesser skill.

During the trying days ahead, the use of unit substations may prove to be worth while even in instances where such use in more normal times might not be considered economical. Many of the substations erected to serve war loads or loads incidental to war activities will be idle after peace returns. Salvage of a conventional substation involves a considerable write-off, whereas the salvage value of a unit substation is high, there being little unrecoverable material and equipment, and a minimum of labor is required to move it to another point of use and to place it in operation there.

Restoration of Service

Reserve Equipment

Difficulty of obtaining priorities for additional equipment and the continuing growth of load will inevitably bring about active use of much of the reserve equipment of power companies. Restoration of service after equipment failure will then present a different problem than when spare facilities were more plentiful. It has been rather general practice to provide a spare transformer at every important substation. With increasing need for transformer capacity to carry the growing load, and the impossibility of obtaining new transformers in any reasonable time, it is likely that more of this reserve equipment will be placed in operation. It may then be that one transformer will have to serve as a spare for several transformer banks, probably at substations located some distance apart. Such a spare unit should be centrally located with respect to the substations where it may be required for service and kept in readiness for transporting and placing in service in minimum time. Skids should be in place and the unit cribbed to a convenient height for loading on the transporting vehicle. Flexible leads connected to the bushing studs and provided with clamps at the free end will save time in placing such a transformer in operation promptly. Other heavy equipment reserved for use as spares should be similarly prepared.

Transportation facilities for moving a spare transformer are, of course, necessary but there seems to be little justification generally for permanent mounting of spare transformers on special trailers. Transformers of modern design are reliable, and it does not appear that the delay occasioned in loading will justify the cost of transportation equipment that would be reserved for a single service and would remain idle over extended periods of time. In these times, we must recast our notions of what is necessary and what may be only desirable.

Portable Substations

In this period of emergency, portable substations in general do not seem to offer outstanding advantages as compared with separate units of spare equipment. Portable substations must be used as an entity and cannot be separated so that each component part can operate apart from the others. With spare equipment at a premium, it would seem to be the better policy to have spare units of equipment so that these units can be used at different locations as needed. For example, a spare transformer can be used in one substation and at the same time, a spare circuit breaker used for emergency service at a second substation. There may be certain systems where portable substations will have advantages over separate units of equipment, but in the majority of instances during the present emergency at least, spare units of reserve equipment offer a greater degree of protection to service in having a wider diversity of use, although the time element for restoring service may, in some cases, be slightly in favor of the portable substation.

Storage of Reserve Materials

Spare parts and materials held as reserves against substation outages may in some instances be stored at the substation of probable use or they may be stored in a central location. In the first case, the number of spare parts and quantities of materials required will be greater than would be necessary if centrally located. Central storage is preferable in offering the maximum availability of minimum quantities of reserve materials and in many cases permitting better maintenance and care. With either plan, spare equipment should be covered by careful up-to-date inventories both as to specifications and locations.

Protection and Preservation of Equipment

Inspection and Maintenance

Greater attention must be given to the operating equipment to observe or anticipate distress and thereby forestall failures in service. Equipment should be inspected more frequently and maintained at a higher degree of precise operating condition than has in many cases heretofore been considered adequate. Moreover, when inspection reveals the need for even minor adjustments, repairs, or replacements, they should receive appropriate attention at once. Failure to act in these minor instances does not necessarily mean that the equipment will fail to continue to operate satisfactorily, but it may and certainly will in some instances shorten the period before major replacements will be required. It should be kept in mind that an ounce of preventive maintenance is worth a ton of replacement, especially when replacement is beset with present uncertainties.

Fire Protection

With the prospect of continuing intensive operation of equipment, it is not only vital to preserve that which we have but to protect it from loss. Fires are not frequent but when they occur in a substation, the damage to the equipment where the fire originates is all too often extensive. Moreover, except as precautions have previously been taken, damage to adjacent equipment is the rule. Any fire, even though it does no major damage, diverts man power from the primary function of operation and absorbs materials needed elsewhere. Often the incidental damage to other equipment and the interruptions to service necessary to clean up and make final repairs are of greater consequence than those involved in the immediate failure. Consequently, careful consideration should be given to protection against fire hazards.

Probably the most economical and effective protection available at this time against fires resulting from oil ignition in outdoor substations is obtained from water spray apparatus. Its use is, of course, limited in application to those locations where water may be made available. Transformers installed at important substations where water can be obtained may be protected by fixed or portable spray nozzles or by a combination of the two. Perhaps the most satisfactory over-all method is the combination of the fixed and portable apparatus. A moderate number of fixed nozzles properly installed will tend to prevent the spread of fire and reduce its intensity until such time as portable apparatus can be brought into action by trained men.

Where water is not available, dry-compound apparatus is quite satisfactory as are carbon-dioxide extinguishing devices, although at present the latter are costly and not always effective for outdoor use. Carbon-dioxide apparatus has its widest application indoors and for this use is most satisfactory in that there can be no deterioration of equipment resulting from its use.

Foam is effective for extinguishing oil fires under some circumstances but its use around electrical equipment is open to objection because of the work required to remove the caked foam after the fire is out.

Arrangements should be made for co-operation with local fire departments in the study of fire-fighting problems, in co-ordinating power company fire-fighting equipment with the department apparatus, and in joint drills and rehearsals. The most important element in minimizing damage is the adequate training and drilling of personnel in the proper use and limitations of use of whatever apparatus may be brought into action.

Lightning Protection

There is considerable divergence of opinion as to the need for lightning arresters, particularly for voltages above 66 kv. Experience of many operating companies in numerous widely separated parts of this country and Canada, under diverse operating and climatic conditions, has established the value of spill-gap protection for substation apparatus.[7] The valve action of the lightning arrester, by limiting the time and magnitude of power-follow current, reduces the number of interruptions from lightning surges, but evidence is lacking that the lightning arrester protects substation equipment more effectively than the spill gap. Conservation of material and labor dictates that serious considerations be given to the use of spill gaps in place of arrester installations at new substations or where replacement of arresters may be necessary.

Personnel Training

Of equal importance to protective equipment in the protection of property is a trained staff of operating men who in case of trouble or incipient trouble know what to do, how to do it, and can act promptly as a well-co-ordinated team. The time taken and money spent to educate and continue the education of these men in this important function will be amply justified. In view of conditions that confront the industry, each one should be induced to feel the added responsibility resting upon him. He should be instilled with an enthusiastic desire to do his full part in providing and maintaining the power supply for war and should be thoroughly trained to do that part well.

Conclusion

Difficult problems will arise in the days ahead due to scarcity of materials, equipment, and suitable man power, which problems must be solved by the engineer and operator in the struggle to serve the rapidly expanding war loads. The manner in which these men meet their more unusual problems will be of interest to all who have similar problems to solve. It will be of benefit if these solutions are passed on to the industry as a whole through publication currently in the technical press.

Experience may indicate that some of the needful practices of rigid conservation established during the present emergency will earn recognition as standard practices in the peaceful days that are to come.

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Overhead Distribution Systems in Wartime

HAROLD COLE
MEMBER AIEE

Synopsis: An overhead electrical system makes use of large quantities of materials which are also very vital to the nation's wartime program. This paper seeks to point out the most practical ways of conserving these vital materials, thereby releasing them in greater quantities for use in wartime production. In general, this can best be accomplished by keeping to a minimum the quantities used to make the line extensions and system reinforcements which will be required to supply electric service to the new industries and military establishments, and to the increased housing facilities which must accompany them. A number of effective tools are available which may be utilized to reduce the quantities of conductor material required to give satisfactory service. There are also possibilities of rearrangement of existing facilities so as to use them more effectively.

Critical Materials Involved

THE critical materials which are used to the greatest extent in the construction of overhead distribution lines are copper, aluminum, steel, and zinc. Of these copper is by far the most important. Aluminum has been making considerable headway in recent years as a conductor material when provided with a steel reinforcement, but, while it has many good features to recommend its use, particularly for long span rural lines, competing materials of copper-steel composite have been made available which can be used to build these lines. The fact that aluminum has been practically unobtainable for some time was not a serious handicap as long as we were still able to get copper-steel composites. These materials, however, are now being greatly restricted in their availability, and the problem, so far as copper is concerned, is, therefore, to find ways to minimize its use, since no substitute material other than aluminum is available. Various possibilities of accomplishing this purpose will be discussed later.

Steel is used in considerable quantities in overhead-distribution-line construction, principally for line hardware and fittings. It would be difficult to find a substitute material which would be nearly so suitable for such important items as bolts and nuts. For certain uses, however, such as insulator-pins, crossarm-braces, platforms, and other fixtures requiring a considerable proportion of the steel tonnage used in overhead-lines construction, wood is quite a satisfactory substitute.

Zinc is important in overhead-lines work, mainly as a coating for steel to prevent rust. The tonnage used for galvanizing of line hardware is not very great, however, and it is to be hoped that its use, particularly for bolts, nuts, and so forth, may be continued. On the materials of larger size it will be possible to use coatings composed of less vital materials which, while less permanent, will be effective through the present emergency and can later be replaced with more lasting materials.

The need for materials, other than metals, used in the construction of distribution lines is not immediately critical but there is a good chance that it may be unless their consumption is curtailed. Poles and crossarms are the most important of these items. Due to increased consumption of these materials in connection with military and emergency housing, stocks of seasoned material have been seriously depleted, and it is reported that unfavorable weather conditions in pole-producing areas have resulted in a subnormal output of new poles, especially in the larger sizes. Conservation in the use of poles and the avoidance of large stocks not immediately required will help keep to a minimum the amount of substandard materials used in the construction of lines which will need early replacement.

Means of Conserving Materials

In general, the conservation of vital materials can best be accomplished in four ways:

1. By using the minimum possible amount of material on all new line extensions.
2. By using the minimum amount of vital material for bolstering up existing lines when overloaded or under-voltage conditions result from increased loads.
3. By rearranging existing facilities so as to use them more effectively.
4. By salvaging needed materials from existing lines where they are not immediately required to give adequate service.

NEW LINE CONSTRUCTION

The most effective way to save material is, of course, not to use it. To the extent, therefore, that restrictions are applied to the extension of lines to serve potential consumers, there will be an almost proportionate reduction in the amount of material used for overhead-line construction. This is for the reason that in periods of expansion the greater proportion of material used goes into the construction of lines to serve new areas. Unless a real effort is made to concentrate housing facilities in compact groups, this will still be the case. It is not within the scope of this paper to discuss the restrictions to be placed on new line construction by those in control of the allocation of material. Of one thing we are certain, and that is that extensions will be made to serve new projects vital to the emergency effort. It would seem almost equally imperative that the necessary extensions be made to serve new housing facilities which are, in a great many cases, essential to the war effort.

In the building of such new lines as are permitted, every effort should be made to keep the material used to a minimum. The most obviously beneficial expedient is to use the least possible amount of copper. The size of the wire used should be the minimum to give reasonably satisfactory service to the initial customers, unless increased loads are known to be coming in the immediate future. For most distribution engineers this will be a reversion to obsolete practices, since it is the way things were done before the adoption of system-planning methods. We can well remember the frequent and costly reconstruction, replacement, and revamping of lines which were necessary in those days. It must be admitted, however, that building to an ideal ultimate plan sometimes results in the installation of line capacities which are much more than adequate for a large part of their useful life when load growth is slow. In the light of present shortages, we would be justified in returning to the old-fashioned method of using the smallest size of wire which would do the initial job, even if, in some instances, it may require changing to a larger size in a year or two.

It may also be possible to justify some modifications of the pole layout so as to conserve material. Temporary short-cut routings of line extensions may be resorted to in some cases to reduce the

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HAROLD COLE is distribution engineer for The Detroit Edison Company, Detroit, Mich.

length of line required. The use of longer spans in the initial stages of residential development will conserve poles, crossarms, hardware, and insulators. The ultimate spacing of poles in residential areas is invariably fairly close, because of the necessity of keeping customers' service drops reasonably short, but, if poles can be so spaced that the later installation of intermediate poles as the area builds up results in a good layout, an initial saving in material can be made. In many cases, where the building-up process is not rapid, an over-all saving in investment results. The use of bare primary wire, if permissible, will favor the adoption of this "double-span" construction.

INCREASED LOADING OF EXISTING FACILITIES

The second objective of the conservation program, namely, the devising of ways and means of carrying increased load on existing lines and equipment with the minimum use of vital material offers the greatest opportunity to the distribution engineer for contributing to the war effort. As loads increase on the electrical distribution system, two limitations operate to cause need for increased capacity in lines or equipment. These are:

(*a*) Excessive heating which may cause damage or possibly failure.

(*b*) Excessive voltage drop which may result in decreased efficiency of utilization equipment and even make its operation impossible.

The current-carrying ability of wires and line devices is affected by many widely variable factors, and, in general, the commonly accepted current ratings are arbitrarily conservative. In arriving at these ratings, a combination of conditions is selected which very rarely occurs in practice, and tests made under actual field conditions show that loads considerably in excess of these ratings may be carried for long periods without resulting in any appreciable damage to overhead lines and associated equipment.

Limitations to Wire Loading. The heating of overhead conductors, for example, is greatly affected by air movement, and yet the limiting ratings are often based on temperature rises in still air with high ambient temperatures. As there is practically always some air movement past conductors in overhead-line construction, the effect is to very materially reduce the actual temperature rise. Furthermore, even at the assumed limiting temperature, the effect on the wire or equipment would be serious only if continued for long periods of time. Very seldom will a condition hazardous to service or persons result.

Limitations to Transformer Loading. The loading of distribution transformers is a good example of the ability of equipment to successfully carry so-called overloads. This subject has been discussed at considerable length in recent technical publications* and will be treated only briefly here. The factors which make it possible to carry peak loads greatly in excess of the name-plate rating are:

1. The thermal capacity of the iron core, the insulating oil, and the tank, absorb heat from the windings and greatly reduce the resulting temperature rise on short-time peaks.
2. The loads of highest peak values and longest duration usually occur in winter when ambient temperatures are low and the resulting temperature in the windings is correspondingly reduced.
3. The limiting temperatures assumed in the rating would cause failure of the transformer only after continuous operation over a period of years.

The greatest problem in the proper loading of distribution transformers, of course, is to know what the character of the loads is on individual transformers, both as to yearly peak value and as to the shape of the load curve throughout the year. However, the general characteristics of residential loads are sufficiently well established that, with any one of several methods of determining transformer peak loads, it will be conservative to allow estimated winter peaks of the order of 150 per cent of name-plate rating. Under this procedure, experience shows that transformer life should be reduced very little, if any, over that which would be obtained by more conservative loading. However, before proceeding to materially increase transformer loading beyond this point, it is particularly important to have adequate information on load characteristics and values, since a large number of damaged transformers on a system would be a serious detriment to good service in later years.

Limitations to Voltage Regulation. Excessive voltage drop, causing wide variation in voltage regulation to customers, will probably be a more frequent cause for system reinforcement than current limitations. Here, again, it would seem that present accepted good practice could be modified somewhat during the emergency. Fairly narrow ranges of voltage regulation may be largely justified by economic considerations in normal times, although by the same consideration we cannot afford to maintain as high standards on rural lines as in thickly settled urban districts. When, in the face of serious material shortages, economics is no longer the ruling consideration, it would seem that standards of voltage regulation which apparently are giving satisfactory service to the farmers should not be too poor for the city dweller to put up with for the period of the emergency. This should apply, not only to the long-time variations, but to the sudden fluctuations commonly known as "flicker."

Improvement of Power Factor. In spite of increased current ratings and more liberal voltage limitations, the time will be reached, as loads increase, when something will have to be done to bolster up the supply. But, this need not always take the form of adding new facilities or replacing old ones with like kind of larger capacity. One of the most promising expedients which is available, not only to improve voltage regulation on overhead lines but to decrease the line current capacity for the same load, is the use of shunt capacitors to improve the power factor of the load. An extensive use of this device has been justified under the normal economic conditions of the past few years, and it offers a very effective means of conserving vital material under present conditions. It is true that capacitors require for their construction an appreciable amount of aluminum foil, but the amount of copper which could be saved by their intelligent use should recommend them very highly to the authorities having to do with the allocation of all of these materials.

The use of shunt capacitors will find its best application in the densely loaded parts of our systems, where the reduction of current will often be as important as the reduction of voltage drop in the circuit. In rural areas where conductors are small, loads are light, and power factors high, shunt capacitors are not quite so effective a remedy, although they may still be justified in many cases. The effect of the capacitors is to cause a constant voltage rise in the circuit which will, if too many capacitors are installed, cause too high voltage at light-load periods unless provisions are made to cut out portions of the capacitance. So far, no economical method has been devised for this purpose which is applicable to small installations.

Series capacitors should also be useful devices for improving the capacity of long lines serving individual customers, although they will not have as wide an application as shunt capacitors by reason of the lack of flexibility in their application.

Voltage Regulation and Boosters. Another very economical expedient available to improve the regulation of long rural circuits is the use of line-type voltage regulators or step-voltage boosters. These are now manufactured in a wide range of sizes and steps of voltage change. The simpler forms of step-voltage boosters are quite satisfactory for rural or suburban line regulation, and their use produces an improvement of voltage regulation which would require many times the amount of material in conductors to produce like results.

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* See list of references at close of paper.

Standard distribution transformers, if they have the proper turn ratio, may be used very effectively as a booster to produce a better range of voltage in a circuit at a very low cost. A small amount of fixed voltage boost at a point fairly well out on the circuit will result in a considerable improvement in peak load voltage to the customers on the tag ends without causing too high voltage at off-peak periods. Booster-transformers connected to give a larger amount of voltage boost, used in connection with automatic regulators to permit the better use of the full range of the regulator, will also prove to be a useful expedient where conditions are favorable. For a slight increase in cost, standard transformers may be purchased with increased secondary-to-ground insulation which will insure their satisfactory operation as booster transformers.

Rearrangement of Existing Facilities

In any well engineered electrical-distribution system there will always be an appreciable amount of wire and equipment which will be capable of carrying much greater loads than they are at the time called upon to carry without exceeding either current or voltage limitations. This is partly the result of the planning of system development to follow a layout of facilities which will be adequate to serve an area after it has become completely built up without too much replacement and rearrangement of the initially installed material. A critical shortage of materials, however, may make it desirable to do a certain amount of rearrangement of these materials at this time, so as to use them more effectively and thus reduce the amount of new materials required. Line wire and transformers are the items of overhead-line construction which offer the best possibilities of such treatment.

REMOVAL OR REPLACEMENT OF WIRE

Expedients to conserve the use of conductor material which should be given most consideration are as follows:

1. Replacement of the larger sizes of wire with smaller in locations where the load has not yet developed or has been greatly reduced by system rearrangements, and the transfer of the larger wire to locations where the load is increasing rapidly. This will in turn release, for the relief of other circuits, an amount of wire nearly equal to that originally used, if proper salvage methods are used to prevent the scrapping of any large part of the material in the process.

2. Building new sections of line which will shorten feeds to increasing loads rather than replace or add wire on existing routes. This will frequently be practical in suburban or rural areas where piecemeal extensions often result in a poor layout. In many cases, when this is done, sections of line of lengths equal to that installed can be salvaged.

3. Removal of wire which is not required to maintain service except in case of the failure of some other source of supply. Throwover connections, ring feeds, stand-by lines are frequently installed to improve reliability of service to customers. Perhaps, if material shortage becomes really critical, a lowering of service reliability standards would be justified, and some of these facilities could be removed in situations where no consideration of public safety is involved.

Since most of these expedients will release more material than they will require, it is hoped that some method will be found to make it possible either to secure blanket approval of their use or to set up some very informal means of securing approval of individual projects. Otherwise, the volume of paper work and the delays entailed in securing approval may be so great as to seriously discourage any attempt to adopt them. This is particularly true in the case of overhead-lines work where individual projects are small and their number very large. The volume of paper work necessary for specific approvals could easily become so great as to seriously interfere with more important projects.

CONSERVATION OF TRANSFORMER CAPACITY

In parts of many overhead-distribution systems, the ratio of demand on a circuit to the total installed transformer capacity on the circuit will be found to be low. This would seem to indicate a large surplus of transformer capacity, which it should be possible to utilize to serve new loads, and reduce, if not eliminate, the necessity of purchasing new transformers for some time. Actually, the possibilities of doing anything very helpful with this situation are not very good. In rural or suburban areas, one of the locations where this ratio of circuit demand to installed capacity is very low, the widespread use of electric ranges, refrigerators and other types of appliances causes high short-time load demands which make it necessary to provide larger transformers than would otherwise be required, in order to give satisfactory voltage conditions. Also, the demands on the individual transformers are not coincidental, and, because of this diversity, the demand on the circuit is much less than the sum of the demands on the individual transformers. The ratio of the circuit demand to the total installed capacity is, therefore, not a proper measure of the loading of the individual transformer and the surplus capacity is more apparent than real.

In urban areas also, during the initial stages of development, this ratio of circuit demand to transformer capacity is low. It might be thought that, in this case, an increased spacing of transformers would reduce the number required with no increase in initial size, but very few liberties may be taken with the spacing of transformers without producing intolerable voltage conditions. Larger secondary wire would be of very little help in this connection and its use would only increase the use of vital material. It would also be unwise to go too heavily into the use of very small transformers for the initial stages of urban area development. The reduction in vital material would not be anywhere near the ratio of the reduction in size, and, in the future, it would probably be impossible to find economical use for them anywhere on the system. Indeed, many of us, with the extensive development of rural lines in our territories, are already having difficulties in finding use for the smaller transformers removed from the farm lines by reason of increased demands of customers on these lines requiring replacement with transformers of larger capacity.

It should be possible to utilize some of the surplus transformer capacity already installed by making interchanges of transformers as some become loaded up and others do not. This will be particularly true if full advantage has not already been taken of the possibilities of loading transformers to the limit of their thermal ability. By this means it should be possible to reduce appreciably the total kilovolt-amperes of transformer capacity required on the system. It will still be necessary to buy new transformers if extensions are made to new areas since, for the reasons given above, it will be difficult to release any large number of transformer units from existing lines.

Salvaging Operations

In addition to conserving new materials for line construction, a great deal can be done toward the same end by the maximum use of material removed from the lines, of which there is usually a considerable amount in any large organization.

Highly profitable salvage operations have already been organized in a number of companies for this purpose, and in the present emergency it is imperative that this practice be adopted by all operating utilities.

Salvage operations which are most productive in the conservation of line materials are the following:

1. Cleaning, straightening, and splicing together into standard lengths all wire suitable for reinstallation.
2. Removing weatherproof covering from wires not suitable for reinstallation, and splicing in lengths suitable for recovering or use as bare wire.
3. Cutting up very short lengths for use as tie wires, ground-rod pigtails, and so forth.
4. Reconditioning hardware, fuse carriers, lightning arresters, and all articles of line equipment not hopelessly obsolete.
5. Cutting off rotted sections of poles and retreating them for use as shorter poles or guy stubs.

Conclusions

It is to be hoped, of course, that some of the expedients suggested in this paper will not be necessary as they involve burdensome operating costs and much additional labor. In some localities now, and probably generally later on, labor will be difficult to obtain. Other expedients involve a lowering of standards of service to our customers and should be adopted only in moderation unless the situation becomes quite critical, and then only with the approval of the authorities charged with regulation of service standards. Failure, however, to adopt some of the practices, which are in the interest of the conservation of materials and are at the same time favorable to the economical design and operation of the electrical system, would be called unprogressive at any time. In the present national emergency, it should be considered unpatriotic.

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Variable-Speed Drive for United States Army Air Corps Wind Tunnel at Wright Field, Dayton, Ohio

A. D. DICKEY
NONMEMBER AIEE

C. M. LAFFOON
MEMBER AIEE

L. A. KILGORE
MEMBER AIEE

THE machinery manufacturers are being required to solve many design and production problems in connection with our national-defense program. All electrical manufacturing companies are exerting maximum effort to produce generators, motors, conversion apparatus, and other electrical equipment which are needed in meeting the requirements of a large and rapidly expanding industrial activity. There is a further need for machines of greater capacity, new combinations of apparatus and control devices, and more information on machine characteristics to accomplish new and difficult objectives.

The new wind tunnel of the United States Army Air Corps at Dayton, Ohio, which is scheduled for operation in the early part of 1942 is an example of this type of problem. Wind velocities of 400 miles per hour and greater are to be obtained for testing large size airplane parts and models. The air is to be circulated by two propeller assemblies mounted on a common shaft and operated in series to produce the necessary high pressure. The propeller assembly is driven by a 40,000-horsepower, variable-speed, wound-rotor-type induction motor, which has a top rotational speed of 300 rpm (327 rpm synchronous speed) and is the largest unit of this type built to date. It is essential in this application to obtain close speed regulation over a wide speed range. This paper discusses the problem associated with the design and operation of the equipment required to meet wind-tunnel specifications. The role which the wind tunnel will play in the race for air supremacy in the present world war is a subject of great interest and national importance, but it will only be discussed briefly in this paper as an introduction to the requirements of wind-tunnel drives.

Wind Tunnels

The principles of aerodynamics and hydrodynamics are neither sufficiently well-known nor subject to sufficiently exact mathematical analysis to permit the accurate calculation of the performance of a body moving through the fluid at high velocities, except for simple cases. It has been the practice of designers for many years to obtain performance characteristic data on equipment and apparatus such as ducts, channels, blowers, waterwheel runners, ship-propeller screws, and ships from the performance of models operated under controlled conditions in laboratories. The same condition is true to an even greater extent in the case of airplanes due to the fact that the plane velocities are extremely high, and materials and structural members must be worked nearer the ultimate limits in order to reduce weights and increase the power per unit of weight. During the early development of the airplane, the Wright brothers used low-power wind tunnels to check plane models rather than resort to the more dangerous and costly method of full-scale flight tests. This condition exists at the present time, and a vast amount of large-scale experimental work will be required to obtain the essential data so that still higher efficiency and flight speed can be reached.

Model tests are quite reliable at low speeds due to the fact that the air-flow lines produced by the full-size unit are similar to those existing in the model test. In some cases, the effects of air viscosity can be determined by varying the density of the air in the tunnel. In other cases, it is necessary to make empirical corrections in extrapolating from the model to the full-size unit. For plane speeds at 400 miles per hour, the air velocity around different parts of the plane may approach the speed of sound. This introduces effects of both the mass and elas-

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A. D. DICKEY is with the United States Army Air Corps, Wright Field, Dayton, Ohio. C. M. LAFFOON is manager of the a-c generator engineering department, and L. A. KILGORE is section engineer, large a-c motor section, transportation and generator department, both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

ticity of the air. In the present state of the art, large-scale models and full-size parts must be checked in wind tunnels at full speed to get reliable data for building high-power superspeed planes. Fortunately, it is not necessary to use full-size planes, due to the fact that basic data can be obtained for large or full-size parts and the constants for the parts combined for application to the entire plane. It is interesting to note, in connection with wind-tunnel operation, that the power required to circulate the necessary large volume of air around a complete wind tunnel is several times greater than the power required to drive the model through the air at the same speed. Since the power required to circulate the air varies as the cube of the speed, it is not surprising to find that a 40,000-horsepower motor rating is required for the largest high-speed wind tunnel.

Wright Field Wind Tunnel

The Wright Field wind tunnel is designed with a cross section sufficiently large to accommodate full-size plane sections or parts, and large-size complete models, with wind velocities of 400 miles per hour or greater. Since the output from the motor is eventually absorbed by the circulating air, it is obvious that the temperature of the air would continue to rise until the rate of heat loss dissipated by the tunnel became equal to the rate of input to the air, if complete recirculation were used. When operating under full-speed conditions with 40,000-horsepower motor output, the air temperatures would become excessive, and it is thus necessary to resort to partial circulation. Sufficiently large inlet and outlet openings are provided at appropriate sections of the tunnel so that approximately 30 per cent make-up air is provided and the gas temperature kept down to tolerable values. Each of the two stage propellers which circulate the air have 16 wooden blades, which are approximately 40 feet in diameter at the tips. The propeller assemblies, including rotating blades, supporting hubs, and stationary guide vanes, were designed by the Air Corps engineers at Wright Field. The general arrangement of the tunnel and propelling equipment is as shown in Figure 1.

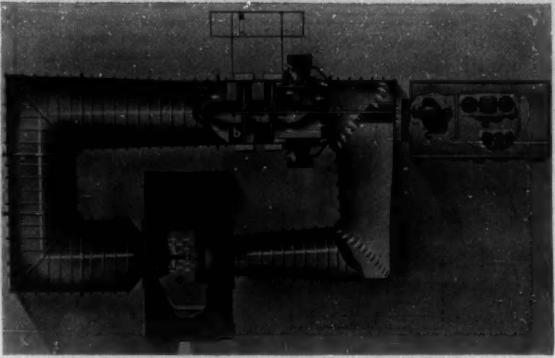

Figure 1. Plan view of wind tunnel and driving apparatus

Driving Equipment

The largest wind tunnel in operation at the time the Wright Field wind tunnel was originally planned, had two 4,000-horsepower driving motors (8,000 horsepower total). A wind tunnel requiring five times as much driving power was a large step forward. It was also desired to use a single driving motor in order to avoid possible difficulties in assembly and complications in the operation of the equipment. Although a 40,000-horsepower wound-rotor induction motor had not been heretofore contemplated, the electrical manufacturers had no misgivings in regard to the feasibility of building it, on account of the fact that synchronous motors on much greater capacity had been built for both 300 and 600 rpm. The motor characteristics—power factor, starting kilovolt-amperes, and speed control—were influenced to an appreciable degree by conditions associated with and requirements of the main power-supply system. In order to maintain speed practically constant at any value over a wide speed range with minimum energy requirements, a speed-control system was chosen which provided for returning the power from the rotor of the main induction motor to the a-c supply system. The main rotating auxiliary equipment required to accomplish this result is shown in Figure 2 and consists of two motor-generator sets. The a-c elements of both sets are salient-pole synchronous units with d-c excitation.

In one set, the a-c motor receives its power supply from the rotor of the main drive motor, and operates as a synchronous machine at the slip frequency over the entire speed range of the main motor. The second set has the a-c generator electrically connected to the 60-cycle main supply system, and operates at constant synchronous speed. Table I shows the speed and frequency for the main motor and auxiliary sets.

A qualitative interpretation of how the main and auxiliary equipment perform can be readily obtained by analyzing the power, voltage, current, torque, and speed relation that exist under given operating conditions.

Starting and Operation of Equipment

It is apparent by reference to Figure 2 that the unit can be started either from the main driving motor or from the a-c element of the constant-speed motor-generator set. When starting from the main motor, no excitation is to be pro-

Table I

Main Driving Motor				Variable-Speed Motor-Generator Set			Constant-Speed Motor-Generator Set		
Supply Frequency	Rotor Speed	Rotor Frequency %	Rotor Frequency Cycles	Stator Supply Frequency	Rotor Speed %	Rotor Speed RPM	Frequency Cycles	Rotor Speed %	Rotor Speed RPM
60	327	0	0	0	0	0	60	100	600
60	*297	9.25	5.55	5.55	9.25	47.5	60	100	600
60	262	20	12	12	20	103	60	100	600
60	196	40	24	24	40	205	60	100	600
60	131	60	36	36	60	308	60	100	600
60	88	80	48	48	80	411	60	100	600
60	** 37.5	89.55	53.8	53.8	89.55	460	60	100	600
60	0	100	60	60	100	514	60	100	600

*Maximum running speed of main driving motor. **Minimum running speed of main driving motor.

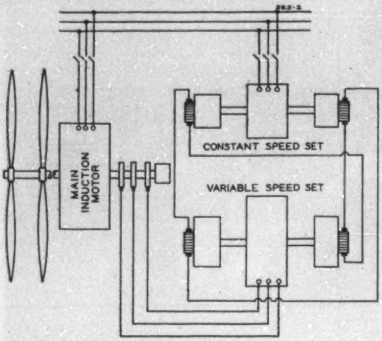

Figure 2 (left). Diagram of equipment and main power connections

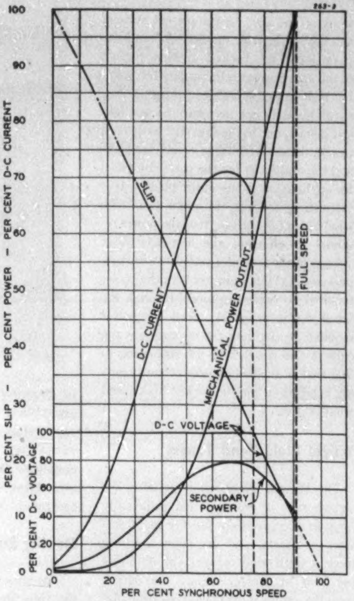

Figure 3 (right). Characteristic curves of a 40,000-horsepower drive

vided for the d-c and a-c elements of the auxiliary sets. When power is applied to the stator of the main motor, the rotors of the main motor and variable-speed set start to roll and accelerate in speed. The speed reached by the rotor of the main motor will be relatively low, due to the fact that the output of the main-motor rotor winding is limited primarily to the losses of the variable-speed set. The speed of the variable-speed set will be relatively high, for its speed is determined by the slip frequency of the main driving motor. If excitation is applied to the field of the a-c element of the variable-speed set, it will pull into synchronism with the rotor of the main motor. The constant-speed set can then be started from either the d-c or the a-c end. If started from the d-c end, it will be necessary to synchronize the a-c end with the main power supply. When the constant-speed set is started from the a-c end, it is necessary to adjust the excitation of the d-c elements of both sets, so that there will be no appreciable interchange of d-c power. When starting the main drive from the a-c end of the constant-speed set, the stator of the main motor should be left on open circuit, the connections for the remainder of the equipment should be normal, and the excitation of the two d-c units adjusted so that the d-c element of the constant-speed set delivers power as a generator to drive the d-c element of the variable-speed set as a motor. Both sets are then brought up to full synchronous speed in the conventional manner. The rotor of the main motor remains at standstill, and the voltage induced in the stator has the same frequency as that supplied to the rotor. The excitation of the a-c and d-c elements of the variable-speed set are then adjusted so that the voltages of the main-motor stator are in synchronism with the voltage of the supply system. When the voltages have the same magnitude, frequency, and have approximately correct phase position, the main-motor supply switch can be closed without the interchange of an appreciable amount of power between the main motor and the supply system.

There is thus no flow of power between the rotor of the main motor and the stator of the a-c element of the variable-speed set, and hence no torque is produced to accelerate the rotor. By increasing the excitation of the d-c end of the variable-speed set, its speed will tend to decrease and its voltage increase so that it functions as a generator and supplies power to the d-c end of the constant-speed set. This power is then returned to the supply system by the a-c element of the constant-speed set. At the instant the speed of the variable-speed set starts to drop, the initial angular displacement between its voltage and the voltage of the main-motor rotor winding produces a secondary current which reacts with the magnetizing flux to produce a motor torque to turn and accelerate the rotor. As the speed of the variable-speed set decreases, the speed of the main rotor increases and both elements maintain synchronism at the slip frequency of the main motor.

Either of the two methods of starting are practical and satisfactory. The method of starting from the relatively small constant-speed set was adopted due to the fact that less complication is involved in the starting equipment and less shock to the supply system occurs.

It is apparent from the above discussion and through a knowledge of the characteristics of a-c and d-c motors and generators that the amount of electric power supplied by the rotor of the main motor and its slip frequency can be determined and controlled by varying the excitation of the d-c elements of the two motor-generator sets and controlling the flow of power between them. The amount of wattless power returned to the supply system can be controlled by varying the excitation of the a-c generator element of the constant-speed set. The amount of wattless kilovolt-amperes supplied to the main motor can be controlled by varying the excitation of the a-c element of the variable-speed set. The curves in Figure 3 show the slip, mechanical-power output, and rotor-electrical-power output for the main driving motor, and the d-c voltage and current of the auxiliary sets as a function of the speed of the main motor.

The elementary power relations in any induction machine are best understood by first considering an ideal machine without loss. In this machine the same torque is developed on the stator and the rotor by the fundamental flux and current. The product of this torque and the rotor speed represents mechanical power output (P_m). The product of the torque and synchronous speed represents power input (P_i), and the secondary power at the slip rings (P_s) is represented by the product of torque and slip speed.

Expressing the slip as a decimal fraction (s) of the synchronous speed, the secondary power can be written in terms of the mechanical power as $P_s = P_m[s/(1-s)]$. For a fixed propeller and a given air density, the power P_m varies as the cube of the speed, which gives a maximum secondary power at $s = {}^1/_3$.

The accurate power relations are obtained by adding the friction and windage losses to the mechanical power (P_m) and subtracting the secondary I^2R from the secondary power P_s to obtain the net power at the slip rings. To trace the power flow from the slip rings through the auxiliary machines back to supply line, it is necessary only to subtract the losses of the various auxiliary machines at each step.

The variable-speed set operating from the slip rings of the induction motor will have a speed proportional to slip. Neglecting the losses, the variable-speed set must handle the secondary power (P_s) which is then equal to the product of its speed and torque. The torque could be written as $P_s/s = P_m/(1-s)$, and from the curves of Figure 3 it will be seen that although the secondary power is maximum at $s = {}^1/_3$, the maximum torque will be at minimum slip. The product of this maximum torque of the variable-speed set and its synchronous speed is approximately equal to the full rating of the main motor, and this determines the size of the variable-speed set. The fact that the maximum current is required only at the minimum speed is of advantage in designing the d-c generators for large capacity.

The constant-speed set must handle the same maximum d-c voltage and current as the variable-speed set, but the maximum voltage occurs at about four times as high a speed requiring a correspondingly smaller machine for the d-c motors. The a-c generator feeding the power back into the line must handle only the maximum secondary power minus the auxiliary machine losses. While the total capacity of the auxiliary sets is great, the speed can be kept high and the physical size is not so large as might be expected.

With the power-speed relations in mind, it becomes apparent that it is desirable to keep full field on the d-c generators over the upper range of speed where the torque is high, and to control the speed by the field of the d-c motors. In the lower range of speed the d-c voltage is held constant and the flux reduced in the generator. The rheostats for the motors and generators are mounted on one shaft, but arranged so that they are changed one at a time.

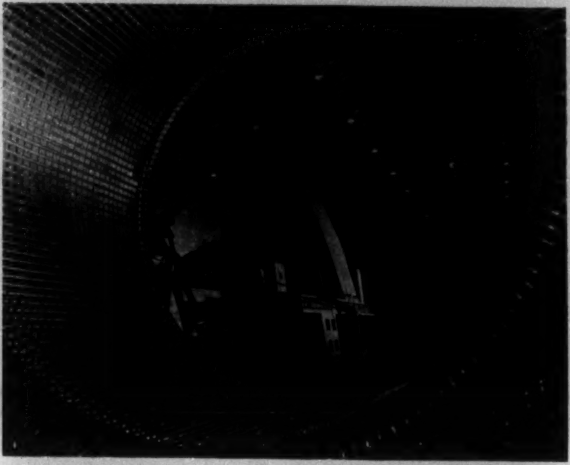

Figure 4. View of main induction motor looking through the stator of the synchronous motor for the variable-speed set

New Problems

In addition to the problems in designing the largest induction motor yet built, there were a number of other problems which had to be solved in the application of this system of speed control. These problems included the steady-state stability, and the dynamic stability or the reaction of the system to oscillating torques either impressed by the propeller or self-excited.

The usual induction-motor criterion of stability that the torque should increase with increased slip is readily met since, even with fair regulation of the d-c machines, the secondary power taken by the variable-speed set, with fixed fields, would increase much faster than the slip frequency. The synchronous motor which is fed from the secondary of the induction motor, must stay in synchronism with the slip frequency, and this presents a stability problem with new conditions. However, it can be shown that the induction motor can be treated as a high reactance transformer with high magnetizing current so that the problem is resolved into the conventional two-machine synchronous-stability problem.

The possibility of self-excited torsional oscillations mentioned above arises from the fact that at low slip frequencies, the reactance-resistance ratio is much lower than in large machines of normal frequency. This tendency to oscillate is the same general phenomenon of hunting encountered in small synchronous machines but is complicated by the addition of the induction motor in circuit. This tendency to establish self-excited oscillations has been termed negative damping, since it is due to a torque in phase with the velocity, or opposite in sign to the usual damping torque. Negative damping may exist in this system for relative angular motion between the induction and synchronous motors. Fortunately, the synchronous-motor damper winding and the speed-torque characteristics of the d-c machines, as well as the fan load, all contribute to the positive damping of the system, so that by proper attention to these factors it is possible to get a net positive damping. Hence it is possible to avoid any tendency to hunt. Methods of analysis of this problem have been written up and proposed for presentation at the winter convention.

A model system was set up using a 100-horsepower wound-rotor motor with two synchronous d-c motor-generator sets. The propeller was represented by a d-c generator with an added flywheel. The starting and control features were tested and the tendency to hunt at low-slip values investigated. With small machines, it was not possible to represent the large units exactly, but the test did serve to prove that the methods of analysis were adequate.

Since there are torsional impulses on the propeller resulting from slight irregularities in the air flow caused by guide vanes, it was necessary to determine their effects on the entire system. The fan, the main induction motor, and the various auxiliary machines, all represent inertias tied together either by shafts or electrical ties. This complicated system has many modes of vibration and many natural frequencies. These were solved by setting up a complete equivalent circuit representing the electrical analogue of the mechanical system, along the well-known principles of representing torques by voltages, velocities by currents, inertia by inductance, torsional flexibility by a capacity, and damping by a resistance. Solving for the natural frequencies of this electrical analogue was difficult due to its many branches. The a-c calculating board was used to represent the system and the approximate frequencies found which could then be checked by direct calculations in complex numbers. The torsional impulses are low, and the net damping is found to be adequate to keep any oscillations low.

The same electrical analogue was found to be very useful in calculating the stability of the speed-regulating system. Since the speed variations must be measured on the motor and the corrections applied through the d-c auxiliary machine fields, the regulating problem is not simple, and careful consideration had to be given to the amplification and antihunting features of the speed regulator.

Conclusions

The type of drive adopted for this largest wind-tunnel application is found to be very efficient to give low starting currents. Also this system lends itself to full automatic control and accurate speed adjustment.

The solution of the many problems associated with this drive have led to some advances in the analysis of machines and in the design of very large motors. This knowledge should prove valuable in the future, on other variable-speed drives, on ship drives and other applications involving systems of machines.

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Progress in Design of Electrical Equipment for Large Diesel-Electric Locomotives

GERALD F. SMITH
ASSOCIATE AIEE

DURING the last eight years, as the result of development of the Diesel engine, Diesel-electric locomotives have come into wide use for heavy switching, passenger, and freight service. There has also been a rapid development in the electrical equipment going back about 15 years.

The primary function of the electrical equipment is to transmit the power from the Diesel engine to the rail, and also change the relation of engine to locomotive speed. In modern high-speed equipments, the weight of electrical equipment somewhat exceeds the weight of the Diesel engine with radiators, and so forth, and is of the order of 20 per cent to 25 per cent of the locomotive weight. This may appear to be out of proportion, but it must be realized that the propulsion output of the Diesel engine is all converted to electrical energy and then converted back to mechanical output at the rail over a very wide range of speed.

Modern high-speed passenger and freight locomotives operate under the widest range of conditions. They traverse the plains, rolling country, and mountainous ranges.

Definitions

In order to understand clearly the operating conditions affecting equipment design, it is well to define several items:

1. *Maximum Test Speed.* This is the speed in rpm defined by the rules of the A.S.A. for a stand test of the motor for two minutes and is 120 per cent of the rpm corresponding to:

2. *Maximum Safe Service Speed.* This is the highest locomotive speed permitted in service with minimum size wheels (worn wheels) on down grades.

3. *Balancing Speed on Straight, Level Track.* With normal trains, this is generally somewhat less than the maximum safe service speed, and is of interest chiefly for comparative purposes since stretches of straight, level track long enough to permit reaching balancing speeds are not frequently encountered.

4. *Balancing Speed on Prevailing up Grades.* In rolling country, these grades may be of the order of 0.2 per cent to 0.4 per cent. In hilly country, they may be in the neighborhood of one per cent. In mountainous country, long grades of 1.8 to 2.2 are common and helper grades up to 3.5 exist

5. *Unloading Speed.* This is the speed above which the full power of the Diesel engine is not utilized. It should be sufficiently high so that the full power of the Diesel engine can be used on the prevailing up grades in *each* district.

6. *Continuous Rated Speed.* This is the lowest speed which can be maintained with full engine power for prolonged periods with unworn wheels without exceeding the temperature limits of the motors and generators, and should closely correspond to the speed with maximum train weight on the ruling and helper grades unless the time spent on these grades is fairly short and is preceded by a run at light load or a very definite and effective cooling period.

7. *Maximum Tractive Effort.* This is the maximum tractive effort which can be developed for short periods during accelerations. In high-speed passenger locomotives it may be limited by the commutating capacity of the generator, otherwise by the slipping point of the driving wheels. In very powerful freight locomotives, driver slippage is the usual limit, but the strength of freight car draft gear may make the practical limit lower than the slipping point.

8. *Maximum Weight Per Driving Axle at Rail.* This is the weight as limited by stress in rail and depends on the minimum (worn) wheel size and maximum safe service speed.

Chronology

Prior to 1927, standard 600-volt self-ventilated railway trolley-type motors, designed for suburban and subway service, were used. About 1928, motors designed primarily for operation from limited power supply were introduced. These power plants were of 300- to 400-horsepower capacity, with one generator and two motors. In 1932, 600-horsepower equipments were introduced using

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Gerald F. Smith is engineer in the transportation engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

semiforced ventilated motors, class B insulation; with multiple windings and four brush holders instead of the conventional two-circuit winding and two brush holders; with wedges on the core, nonmagnetic bands on the end windings and with roller bearings. In 1936 a further increase to 900-engine horsepower was made. At present, motors are available which can use 1,500-engine horsepower per pair of motors. These motors are only 17 per cent heavier than the motors used with a 400-horsepower engine in 1928.

Motors

Design

This increase in capacity has resulted from improvements which may be listed:

1. Increased peripheral speed of armature. The armature peripheral speed has been increased from 8,500 to 12,500 feet per minute. This has been the result of improved methods of insulating and wedging the coils in the armature slots and better methods of banding the end windings with predetermined band tension and multilayer nonmagnetic bands of high-tensile strength.

2. Increased peripheral speed of commutator; from 7,000 to 10,000 feet per minute. This has resulted from the more detailed study of stresses in the commutator copper and steel parts and of the characteristics of mica, the use of an increased number of brush holders to keep down the over-all length, the seasoning of the commutator and the use of better grades of carbon brushes.

3. Forced ventilation. A definite supply of ventilating air from a source relatively free from snow, water, oil, brake-shoe dust and dirt of all kinds is provided.

4. Better insulation and processes in applying the insulation. Mica tapes and wrappers with a lower per cent of combustible material, glass, and asbestos are used. Coils pressed to exact size exclude air and give high percentage of copper in the coil with good heat conducting characteristics and avoid shrinkage and consequent loosening in service. Also improved sealing compounds exclude oil, moisture and air, thus permitting higher temperatures. It is also good practice to connect all motor and generator field coils on the negative side to reduce the voltage stress in the field coils.

5. Micarta wedges which are capable of standing higher temperatures.

6. Nonmagnetic high-strength multilayer bands on the end windings. In addition to permitting higher peripheral speeds, the elimination of magnetic bands on the armature core and end windings reduces the commutating voltage materially.

7. Multiple armature windings permit higher speeds with low commutating voltage.

8. Armature coils are designed to reduce eddy current loss.

9. Roller bearings.

10. Better gearing.

Overload and Commutating Capacity

With the increase in capacity, the loss in per cent of input is lower; however, with better ventilation the loss in watts per motor is increased. Since the motor weight has not materially increased, the thermal capacity in proportion to motor capacity has materially decreased. Hence much greater care must be used in applying motors to a given service to avoid overloads that produce high temperatures, which by forcing expansion and contraction will damage insulation. This is particularly true of armature windings which are much less free than field windings to expand and contract. When the motor is operating at its highest ampere load, the speed is low and also the commutating voltage is low. Its overload capacity is determined by heating. When the motor is operating on weakest field with full engine power and highest voltage, its commutating voltage is highest.

Generators

Main

The capacity and speed of the main generators is determined by the capacity and speed of the engine. The generator differs from the motor in its speed, mounting and overload capacity. When the generator is operating at its highest ampere load, full speed is obtained, hence its maximum capacity is determined by its ability to commutate heavy ampere loads as well as by heating. Compensating windings, often used on high-capacity generators, are not generally used on railway Diesel-engine generators. This is due to the space taken and the higher resistance of such windings. The main-pole air gap is tapered to reduce the maximum volts between bars and also to reduce the load core loss.

Mechanical connection of the generator armature direct to the engine shaft gives a substantial rigid connection using the weight of the generator armature in place of an engine flywheel. The generator stator is also usually direct connected to the engine frame.

Auxiliary

It has become general practice to drive auxiliary machines such as compressors, radiator fans and main motor fans, and so on, from the engine. An auxiliary generator need be of only small capacity, sufficient to charge battery and supply power for headlights, cab lights and miscellaneous small auxiliaries.

Control

The differential exciter of special design has been developed to give full engine loading over a wide range of locomotive speed under usual conditions. To provide for unusual conditions and to secure the very maximum utilization of engine power, the field of the exciter is put under control of the engine governor. When fuel rack exceeds a predetermined setting, the exciter field is decreased, and when the fuel rack goes below this setting the field is returned to its normal maximum value. This avoids overload on the engine and keeps the fuel rack at full load without sacrificing the inherent advantages of the simple differential exciter. By operating on the exciter field, the physical size of the device is kept low. Since the time constant of the exciter field is much more rapid than that of the main generator field, this does not slow up the operation.

Conclusion

For present high-speed passenger locomotives, electrical equipment is available with a weight between 20 and 21 pounds per engine horsepower, with continuous rated speed 33 per cent of maximum service speed of above 100 miles per hour and with sufficient maximum tractive effort to slip wheels at 33 per cent adhesion. For freight locomotives with double the number of motors per power plant, the weight is between 30 and 31 pounds per engine horsepower, and the continuous rated speed 16 per cent of maximum rated speed.

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Distribution-Type Lightning-Arrester Performance Characteristics

AIEE COMMITTEE ON PROTECTIVE DEVICES
Lightning Arrester Subcommittee*

THIS report presents the results of work by the lightning arrester subcommittee to bring up to date and make available to the industry the performance characteristics of present day valve-type distribution arresters having a maximum rating of 3 kv to 15 kv. Distribution arrester characteristics were given in a report by the lightning arrester subcommittee in ELECTRICAL ENGINEERING, volume 56, May 1937, page 576. Since that date there have been changes in the arrester. The subcommittee felt it desirable to present up-to-date values and also to include values at higher discharge currents than those previously given.

Table I includes the arrester gap breakdown values—maximum, average, and minimum—from tests on the arresters using the rates of voltage rise specified in the Lightning Arrester Standards Bulletin 28, ASA Standard *C-62*. Table I also shows maximum, average, and minimum *IR* discharge voltages for all arresters at discharge currents of 1,500, 3,000, 5,000, 10,000, and 20,000 amperes, all on a 10x20 current wave.

To enable rapid interpretation, the data have been plotted in curve form.

Figure 1 shows the relationship of arrester gap breakdown for the AIEE rate of voltage rise versus arrester maximum rating, with maximum, average, and minimum values.

Figure 2 shows arrester gap breakdown for other rates of voltage rise as well as for the AIEE standard rates. The breakdown values cover a range of 0.25 microsecond to 6 microseconds.

Figure 3 shows the spread found in *IR* discharge voltage values for a discharge current of 1,500 amperes, for the several voltage ratings.

Figure 4 shows average *IR* discharge voltage for the different discharge currents from 1,500 amperes to 20,000 amperes and the different voltage ratings 3 to 15 kv.

Four lightning-arrester manufacturers supplied the data from which the characteristics in the report were tabulated and plotted. The values are for arresters of present day manufacture and do not necessarily apply for older types.

While the total spread for all manufacturers is of the order of plus or minus 40 per cent, the tolerance permitted by any one manufacturer does not exceed plus or minus 25 per cent for arrester gap breakdown voltage, or plus or minus 20 per cent for arrester *IR* discharge voltage from the average performance values which are published in his literature.

The 60-cycle spark potential of the arresters referred to in this report will not be less than 150 per cent of the arrester maximum line-to-ground rating.

By comparing the volt-time characteristics of the insulation to be protected with the volt-time characteristics of the arrester, an evaluation can be made of the margin of protection.

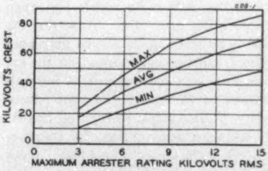

Figure 1. Arrester impulse-gap breakdown, AIEE wave

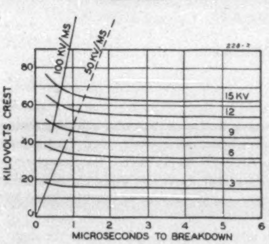

Figure 2. Arrester impulse-gap breakdown (average values)

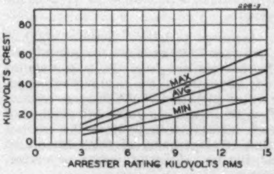

Figure 3. Arrester discharge voltage at 1,500 amperes

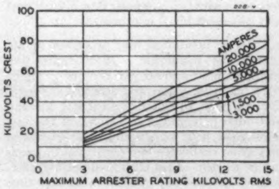

Figure 4. Arrester discharge voltage on 10x 20 current wave (average values)

Table I. Performance Characteristics Distribution-Type Arresters (3–15 Kv)

IR Discharge Voltage on 10x20 Current Wave

Arrester Maximum Rating (Kv)	Arrester Breakdown AIEE Wave†			1,500 Amperes			3,000 Amperes			5,000 Amperes			10,000 Amperes			20,000 Amperes		
	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.	Min.	Avg.	Max.
3	10.5	17.4	23	7	10.7	14	7.9	12.5	16	9.4	13.7	18	10	15.5	21	11.7	18	24
6	22.5	35	46	12.7	21	27	14.5	23.6	31	15.9	26.1	34	19	30	38	22.5	34.4	44
9	32.2	48.7	66	19.7	31	39	22.3	35.5	45	24.5	39	51	28	43.9	57	33	50.5	66
12	41.3	60.6	78	25.9	39.4	51	29.4	44.7	58	32	48.8	62	37.5	54.9	69	43.5	62	77
15	49	69.1	86	31.5	49.5	63	35.6	56.4	71.3	39.2	61	77	45	69	85	53	78.6	99

†The AIEE wave is 50 kv per microsecond for arresters rated 3 and 6 kv and 100 kv per microsecond for arresters rated 9, 12, and 15 kv.

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* Personnel of the AIEE lightning arrester subcommittee: H. W. Collins, *chairman;* H. N. Ekvall, I. W. Gross, Herman Halperin, F. M. Defandorf, Edward Beck, W. J. Rudge, J. R. McFarlin, A. H. Schirmer, J. M. Towner, H. R. Stewart, E. R. Whitehead.

Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers

F. J. VOGEL
MEMBER AIEE

PAUL NARBUTOVSKIH
ASSOCIATE AIEE

In recent years operators have wanted to know how much overload a transformer would carry for a given length of time without exceeding a safe temperature. This knowledge would enable them to obtain the most from available equipment or to install equipment which would provide reasonable overload capacity in case of emergency. In order to do this, it is necessary to be able to calculate the temperatures within windings, and to know safe operating temperatures.

It is not easy to calculate the temperatures within windings, and rough rules which are extremely conservative have been used. For example, it was recognized that there were places in the windings hotter than the average temperature, and an arbitrary 10-degree hot-spot allowance was made.

It is shown in this paper, that the hot-spot allowance for many transformers is less than five or six degrees centigrade, and that the gradients do not increase as fast as previously supposed, due to the effect of decreased oil viscosity at higher oil temperatures. This makes it possible to recommend much higher emergency overloads than in the past. In an example it is shown that 200 per cent load could be carried for one hour, without exceeding what appears to be a conservative temperature limit, as against previous recommendations of 138 per cent load for the same duration of time.

Variation of Temperature With Load

In presenting this subject, it is thought that it will be easier to follow if we take a specific transformer as an example and carry through calculations for a given overload. Comparison of the calculations and actual test results will show whether the method is accurate or not.

Test Data

Full-load heat run, 2,000-kva single-phase transformer, 44,000 high voltage, 2,300 low voltage, 60 cycles.

Actual copper loss	9,450 watts
Iron loss	5,200 watts
Total losses	14,650 watts
Ambient temperature	27.5 C
Rise by resistance,	
High voltage, winding	34.8 C
Low voltage, winding	33.8 C
Top oil rise	30 C

An overload run was made at 135 per cent load. The copper losses at the 100 per cent run were at an average copper temperature of approximately 34 degrees centigrade rise, plus $27^{1}/_{2}$ degrees centigrade ambient, or $61^{1}/_{2}$ degrees centigrade. If the winding on the 135 per cent run was at about 75 degrees centigrade, the copper losses would be:

$$\frac{9{,}450\times1.35^2\times(234.5+75)}{234.5+61.5}=18{,}000\text{ watts}$$

The total losses would then be 18,000+5,200 or 23,200 watts. If the oil rise at full load was 30 degrees, the oil rise at 135 per cent load should be $30\times(23{,}200+14{,}650)^{0.8}$ or 43.4. This is based on the assumption that the hot oil rise above ambient varies as the 0.8 power of the total losses. The gradient between the high-voltage average copper and the hottest oil is 4.8 degrees centigrade, at full load. If the gradient varies as the 0.8 power of the copper losses, the new gradient should be $4.8\times(18{,}000/9{,}450)^{0.8}$ or 8 degrees centigrade. This would give an average copper rise of 43.4+8 or 51.4 degrees centigrade.

The actual temperatures at 135 per cent load were 43 degrees centigrade oil rise, and 46.6 degrees centigrade copper rise, compared to 43.4 degrees and 51.4 degrees centigrade, respectively for calculated values.

In order to check this further, several transformer test records were examined where overload runs had been made, and the results are tabulated in Table I, below.

The interesting part of this tabulation is found in the fact that the oil rises seem to follow, within a few degrees, the values obtained by calculation, but it will be noted that there is a considerable discrepancy in the gradients. In general, they change much less than might be expected. It will be noted that so far as the temperature rises at overloads are concerned, values calculated in this way from full-load data are apt to be higher than those obtained in actual runs.

There are several reasons why these approximate methods give the results they do. The laws of convection and radiation of heat losses from the tank surface and radiators seem to be well established, and the average temperature rise above ambient air varies very closely as the 0.8 power of the losses. This would apply with a small error to both the hottest and the average oil, if the difference between top and average oil is not great.

For example, suppose we have an average-oil rise of 26 degrees centigrade, and a top-oil temperature rise of 30 degrees centigrade, at full load. We can refer

Table I. Overload Heat-Run Data

								Gradients Average Copper to Top Oil			
				Copper Rise by Resistance		Top Oil Rise		Actual		Calculated	
Transformer Test	Load	Copper Losses	Total Losses	H.V.	L.V.	Actual	Calculated	H.V.	L.V.	H.V.	L.V
A	100	9,450	14,650	34.8	33.8	30		4.8	3.8		
	135	18,000	23,200	46.6	48.1	43	43.4	3.6	5.1	8	6.4
B	100	5,300	7,565	25	25.5	20		5	5.5		
	125	8,550	10,815	31.9	34.9	26.5	26.7	5.4	8.4	7.3	8.1
	135	10,180	12,445	36	40	32	29.8	4	8	8.4	9.3
C	100	7,120	10,404	31.3	34.5	26		5.3	8.5		
	125	11,400	14,684	40	41.9	33.3	34.2	6.7	8.6	7.7	12.4
	135	13,530	16,714	43.9	44	37.5	38	6.4	6.5	8.9	14.2
D	100	10,800	14,508	31.5	33.3	25		6.5	8.3		
	125	17,620	20,840	41.5	43.3	34.5	33.4	7	8.8	9.6	12.3
	135	20,450	23,670	47.2	47.9	39	37	8.2	8.9	10.8	13.8
E	100	6,650	9,350	30	15.5	22.5		7.5	—7.5		
	125	10,520	13,220	40.7	28.4	29	29.6	11.7	—0.6	10.8	*
	135	12,900	15,600	45	34.7	34	34	11	+0.7	12.7	*

*Not calculated because of apparent "negative" gradient.

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F. J. VOGEL is division engineer, and PAUL NARBUTOVSKIH is engineer in the transformer department, both with Westinghouse Electric and Manufacturing Company, Sharon, Pa.

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back to the first example given. The calculated top-oil temperature rise using the 0.8-power rule at 135 per cent load is 43.4 degrees centigrade, and actually was 43 degrees centigrade. If the same rule is applied to the average-oil rise, which is 26 degrees centigrade, at full load, the calculated value would be 37.6 degrees at 135 per cent load. The difference between the top-oil and average- or effective-oil temperature is assumed to be 4 degrees at full load. Suppose it varied anywhere from 4 to 6 degrees at the overload. The calculated top-oil temperature rise would range from 41.6 to 43.6 degrees versus 43 degrees test. This indicates that where the difference between top oil and average oil is not great, both of these rises may be assumed to vary as the 0.8 power of the losses.

If we follow along this reasoning, the difference between the average copper and average oil adjacent to the coils will be 8.8 degrees centigrade at full load, and 14.7 degrees centigrade at 135 per cent load. This assumes that the gradient, copper to oil, varies as the 0.8 power of the losses. The difference between the top oil and average copper might then be, at 135 per cent load, 14.7−6.0 degrees or 8.7 degrees centigrade, compared to 3.6 degrees centigrade measured. This indicates that there are other factors than those considered, or that the factors used are in error. These factors are oil flow or convection, and the effect of changes in oil temperature on the gradients between the copper and oil.

Effect of Convection Currents

The convection currents within windings and in the radiators are not easily estimated from the dimensions, and no effort to provide methods for such calculations is to be made in this discussion. However, it is possible to consider the variation of convection currents and their effect on temperature rise. Table I contains some interesting material. For example, there are variations in the test gradients of the low-voltage and high-voltage windings at different overloads which do not look consistent. Part of this may be due to experimental error. The corrections for "time to shut down" were made according to AIEE test standards. The largest factors in these variations are undoubtedly the difference in length and area of the oil paths through different windings and the variation in the watts loss per-unit area in the windings. This is particularly true in the case of the last unit. The low-voltage winding has a relatively large area of ducts, and a very low value of watts per square inch, compared to the high-voltage winding. The result is that the oil flows more easily through the low-voltage winding and is relatively cooler when it leaves the low-voltage coils than the oil leaving the high-voltage winding. If the watts per square inch of coil surface on both windings were the same and the length and area of the oil paths were the same, then both windings would behave more nearly alike.

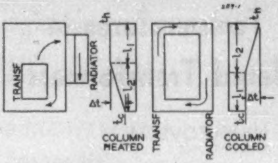


Figure 1. Sketch showing main convection current in transformer and equivalent circuit, with oil temperatures at various points

If both windings are about alike, it is possible to replace them thermally with a heating and cooling circuit as shown in Figure 1.

The density of the oil at $t_h = D_{t_h} =$

$$\frac{1}{(1+0.00074\,\Delta t)}\,D_{tc}$$

The pressure of the liquid in column 1 will be

$$l_1\left(\frac{D_{t_c}}{1+0.00074\,\Delta t}\right)+ l_2\left(\frac{D_{t_c}+\dfrac{D_{t_c}}{1+0.00074\,\Delta t}}{2}\right)$$

and in column 2

$$l_2\left(\frac{D_{t_c}+\dfrac{D_{t_c}}{1+0.00074\,\Delta t}}{2}\right)+l_1 D_{t_c}$$

The difference between these quantities represents the "head" available to cause oil flow, or

$$l_1 D_{t_c} - l_1\left(\frac{D_{t_c}}{1+0.00074\,\Delta t}\right)=H$$

$$H = l_1 D_{t_c}\left(\frac{1+0.00074\,\Delta t-1}{1+0.00074\,\Delta t}\right)\alpha K\,\Delta t$$

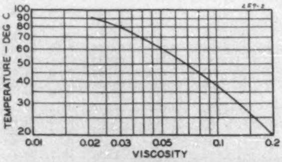


Figure 2. Variation of kinematic viscosity of transformer oil with temperature

The conclusion can be drawn that the pressure head causing thermosyphon flow is practically proportional to the difference between the hot and cold oil temperatures. Although this is derived for a specific case, it is generally true as long as the temperature gradients in each leg remain proportional.

If the flow is slow enough, and is streamline in character, not turbulent, the rate of flow is proportional to the head, and inversely proportional to the viscosity. This can be expressed as—

$$\text{Rate of flow} = K\frac{\Delta t}{v}$$

Also the losses which are dissipated are equal to the rate of flow times the specific heat (S.H.) times the temperature difference—

$$\text{Rate of flow}\times K_2\times(\text{S.H.})\times(\Delta t)=\text{losses}$$

$$\therefore\ \text{Losses}=K\,\frac{\Delta t}{v}\times K_2\times(\text{S.H.})\times\Delta t$$

$$\therefore\ (\Delta t)=K_3\sqrt{\text{losses}\times\text{viscosity}}$$

Figure 2 shows the relationship between oil temperature, in degrees centigrade, and the kinematic viscosity of a transformer oil. In order to show the effect of these factors, a transformer was tested at overload, and top and bottom temperatures noted, as shown in Table II.

Table II

Transformer Rating—1,667-Kva Single-Phase 60-Cycle, 13.2 to 2.3 Kv. Copper Loss at 75 Degrees Centigrade—11,308 Watts. Iron Loss 3,850 Watts

	80% Load	100% Load	120% Load
High-voltage rise	42.1	56.7	68.5
Low-voltage rise	42.4	56.9	69.8
Ambient	27	31	29.5
Top-oil rise	36.5	49	60
Top of radiator, rise	34	45	56.5
Bottom of radiator, rise	22	34	43.5
Difference in radiator temperatures	12	11	13
Bottom-oil temperature	49°	65°	73°
Viscosity	0.070	0.045	0.036
Copper losses	7,100	11,750	17,500
Total losses	10,950	15,600	21,350
$\sqrt{\text{Losses}\times\text{Viscosity}}$	27.7	26.5	27.6

The following conclusion may be reached:

The difference between the top oil and average oil does not change greatly with load at constant ambient temperature, due to the nature of the oil flow and viscosity changes. If the velocity is sufficient to cause turbulence, there is the possibility that some increase in temperature difference will occur.

Effect of Oil Temperature on Gradient Between Copper and Oil

Tests have been made on circular coils wound with 0.009 thickness of paper insulation at various oil temperatures, both in a vertical and horizontal position. In the case of the vertical coils, the oil temperature and copper temperature were measured at the top of the coil. In the case of the horizontal coils, the oil temperatures were measured both 2 inches from the coil horizontally, and also, in the duct between the coils and the tube inside the assembly. The oil temperature was found to be essentially the same in both places. The results of these tests are shown on Figures 3 and 4. It is of interest that the gradient for the vertical coils varies very closely to the 0.8 power of the watts per square inch for a given oil temperature, and that the gradient is higher for lower oil temperatures. It will be noted that low oil temperatures affect the gradient for horizontal coils very much, but at 80 degrees, the curves for both horizontal and vertical coils are substantially the same. This is very reasonable, as the viscosity of the oil at 80 degrees centigrade is very low, and permits nearly as easy flow into the horizontal ducts as in the vertical ducts.

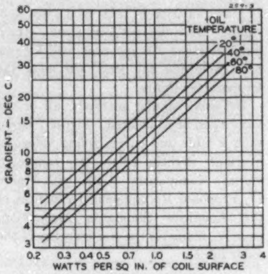


Figure 3. Gradients between oil and copper for model transformer coils—coils in vertical plane

There is one interesting fact that can be derived from Figure 4. If a transformer had an average of one watt per square inch dissipated on the coil surface at 60 degrees centigrade, the gradient would be 15 degrees. At 80 degrees centigrade, the watts per square inch would be increased to be approximately (234.5+95)/(234.5+75)×1, or 329.5/309.5 equals 1.07. With 80 degrees centigrade oil, the gradient would be about 12 degrees. A change in ambient temperature of 20 degrees centigrade, therefore, would make the temperature rise 3 degrees centigrade less. This effect is less pronounced for transformers with vertical coils.

Temperature Rises Under Transient Conditions

In order to determine how the temperatures varied during short-time overloads, a test was made at varying loads for different lengths of time on a 600-kva, three-phase transformer. The hottest spot temperatures were also measured by thermocouples in the second coil from the top.

Figure 5 shows the transient conditions between the oil and the copper, and since this is a fairly representative case, shows that this transient for most transformers will be completed in fifteen minutes, more or less, depending on the design and the current density in the copper. After this transient has ended, the determination of the hottest spot would be reduced to determining the difference between the average and top oil, and the gradient between the top oil and the hottest copper.

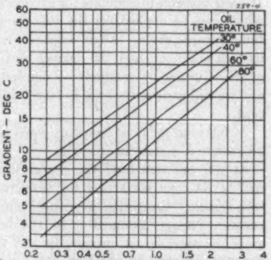


Figure 4. Gradients between oil and copper for model transformer coils—coils in horizontal plane

Since the top oil and the average oil have the same difference, or gradient, between them on overloads of long duration, it would follow that this might also be true on shorter-time overloads. To satisfy this condition, the difference between the average rise by resistance and the hottest copper rise should also be constant, since the gradient between the copper and oil, adjacent to it at any place in the column of coils, should be substantially a constant. This would not be true if the conductor insulation, ventilation, and so forth, were not the same for the top coils as for the body coils. Figure 6 shows the temperature gradients found between the top oil and both the hottest copper and the average copper. On the average, the difference between the hottest copper and the average copper was 8 degrees. The variation is within the limits of reasonable experimental error, and indicates that there would be no serious error in calculating hottest-spot temperatures using this assumption.

Table III

Time—Hours	Hot-Spot Temperature—Degrees Centigrade
1/4	140
1/2	135
1	130
2	125
4	120
8	115
24	110

Application of Calculated Hot-Spot Temperatures

The data obtained above are of interest because they can be applied to practical transformer design and operation. The most interesting application is in the determination of safe short-time overloads for transformers. In order to do this, a table of safe temperatures must be established. To illustrate the point, assume the temperature-time table, Table III.

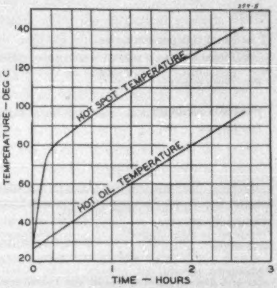


Figure 5. Transient temperatures in 600-kva transformer at 300 per cent load, starting cold

The values for hot-spot temperature and the times associated with them are a matter of judgment, and are based on tests of insulation under oil at various temperatures.[1] These values are very conservative, and higher values probably could be used when the present operating recommendations are revised. If we are to make a general application of these values, it is next necessary to establish average transformer characteristics. Such a study

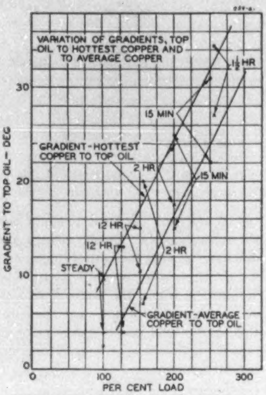


Figure 6. Variation of gradients, top oil to hottest copper, and to average copper

has led to the tabulation for power transformers shown in Table IV.

From other authors,[2] the approximate formula for transient oil rises has been derived as follows:

$$T_t = T_f(1 - {}^{-}\epsilon^{t/B}) \qquad (1)$$

where

T_f = Final temperature rise
t = Time, hours
B = Time constant $= \dfrac{T_f \times C}{L}$
C = Heat capacity
L = Losses

Table IV

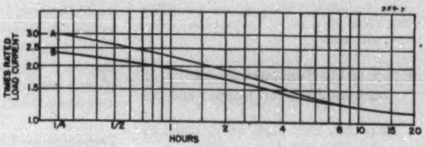
Ratio of losses, copper to iron, full-load	2:1
Time constant	4
Temperature rise—copper*	55 degrees
Hot-oil rise*	50 degrees

*Maximum allowed, not average values. Gradients of 5 degrees between copper and hot-oil rises are common.

The formula 1 can be shown to hold for any abrupt change in loading, so that if the transformer is fully loaded and at a steady state, a sudden increase in load will result in a transient determined by the difference between the final condition, under the overload, and the full load condition.

It appears that for many transformers the actual hottest-spot temperatures are in the order of 6 degrees or less above the average copper temperature. In this case, the actual hottest spot should not exceed 61 degrees. Also, the average oil rise will be 50−6 degrees or 44 degrees centigrade. It is now possible to calculate the overloads which can be applied for the times and temperatures given. As an example, let us pick one hour, and 130 degrees centigrade. Cut and try methods are simplest, and we will assume the load to be 200 per cent.

Figure 7. Emergency overloads for various times



Power Transformers
A—Following no load
B—Following full load

Assumptions at Rated Load
Ratio of losses = 2 to 1
Time constant = 4
Oil rise = 50°
Hot spot = 61°

Time in Hours	Times Rated Load Current Following: Full-Load	Times Rated Load Current Following: No-Load	Hot-Spot Temperature in 30° Ambient
¼	2.35	3.00	140
½	2.10	2.60	135
1	1.90	2.25	130
2	1.71	1.9	125
4	1.45	1.55	120
8	1.33	1.33	115
24	1.2	1.2	110

At full load the total losses are equal to $3X$, where X equals the iron loss.

At 200 per cent load the copper loss will be $8X$, not correcting for temperature change. Correcting for copper temperature change, assumed to be 124 degrees centigrade, it would be $8X \times (234.5+124)/(234.5+85) = 9X$. The total losses would be $10X$. An ambient of 30 degrees centigrade is assumed.

Since the average oil rise is 44 degrees at 100 per cent load, at 200 per cent load, and with losses at 124 degrees centigrade, the ultimate rise would be $(10/3)^{0.8} \times 44$, or 115.0 degrees centigrade.

If the original rise was 44 degrees centigrade, the change in temperature rise is 115−44 or 71 degrees centigrade.

In one hour, the oil rise will be:

$$71(1-\epsilon^{-1/4}) = 71 \times 0.223 = 15.9 \text{ degrees centigrade}$$

The actual top-oil temperature will be $15.9+50+30 = 95.9$ degrees centigrade.

If no allowance for the reduced oil viscosity is made, the winding gradient is obtained by multiplying the gradient at 100 per cent load (=11 degrees centigrade) by the ratio $(9/2)^{0.8}$, which takes in account the increased losses, due to the change in copper temperature. There is an appreciable change in oil temperature, however, and Figure 4 and the previous discussion indicate that it should be safe to assume that the increase in resistance loss at higher temperatures is compensated for by the reduction in gradient, due to reduced oil viscosity. On this basis, the computed gradient for this case will be $(8/2)^{0.8} \times 11 = 33.3$ degrees centigrade.

The total rise is then 95.9 plus 33.3, equals 129.2 degrees centigrade, which indicates that 200 per cent load for one hour following full load will meet the temperature limits.

By using similar methods, all of the values can be calculated. Figure 7 shows such a table and curve, giving values of overload slightly below the computed values. Comparison of this with Figure 17, Proposed Recommended Practices of the American Standards Association, page 86, indicates the conservatism of the older table.

Table V. Table of Safe Emergency Loads for Different Times After Full Load

Time—Hours	Times Rated Load Current: Values From Figure 7	Times Rated Load Current: Values From ASA, Figure 17
½	2.10	1.6
1	1.90	1.38
2	1.71	1.25

Summary

A more accurate method than previously used for calculating temperature rises in transformers has been derived, and applied both to steady and transient conditions. This latter application, along with the recognition that newer transformers have both lower gradients and better oil protection, give the possibility of recommending much higher emergency loads than previously given to such apparatus.

References

1. Temperature Limits Set by Oil and Cellulose Insulation, Charles F. Hill. AIEE Transactions, volume 58, 1939, September section, page 484.

2. Transformer Engineering (book), L. F. Blume, C. Camilli, A. Boyajian, and V. M. Montsinger. Pages 300–03.

Electropneumatic Brakes for High-Speed Trains With Particular Reference to Their Electrical Features

JOSEPH C. McCUNE
NONMEMBER AIEE

ELECTROPNEUMATIC brakes for main-line high-speed passenger trains are a recent innovation. Electropneumatic brakes have, however, been known and used for many years. The first steam road tests were made in 1887. The results were quite gratifying, but improvements in the pneumatic apparatus resulted in such satisfactory performance that the added expense and complication of electropneumatic control was clearly not warranted. Electropneumatic brakes have been used, however, in subway and elevated service, and in considerable quantity, for more than 30 years.

To understand why electropneumatic brakes have so generally been adopted for modern high-speed trains requires some comprehension of the limitations of a strictly pneumatic system and some grasp of the part played by the brake in modern transportation. The importance of the brake, as a factor in the expeditious movement of trains, is not generally appreciated. The brake system suffers, in a consideration of its influence upon the maintenance of high schedule speed, because its work seems entirely negative. That is, the purpose of the brake is to retard or stop trains, a result which can be attained, after a fashion, without any brake at all. Since the brake appears to be only assisting in an event which would take place anyway, its potentiality as a means to move trains faster over the road is not apparent at first glance. In fact, it seems anomalous to assert that a brake, which stops a train, has anything to do with covering ground, which obviously implies motion, but that this is actually the case will become apparent later.

Because the work done by the brake is of a negative character, it frequently happens that the technical difficulties involved in producing a satisfactory brake and the power embodied in it are not fully visualized by those who are not directly in contact with the braking situation. Many engineers are confronted with the problem of transferring heat energy into mechanical energy, as in steam or gas engines. Electrical engineers change, in motors, electrical energy into mechanical energy. Engineers, in general, are mainly preoccupied with obtaining mechanical energy from some other form of energy, whereas brake engineers, as a very special group, deal with the transformation of mechanical energy into heat. Although this transformation is the reverse of that ordinarily encountered, it is evident that many technical problems must be met with, a consideration of which is not pertinent to this paper.

It is believed, however, that some mention should be made of the power requirements of the modern brake. Neglecting friction, wind resistance, and so forth, it is obvious that, measured at the rail, the motor torque and the brake torque must be equal if a train is to be accelerated or decelerated at the same rate. But in actual practice, the brake torque is greater than the motor torque since the rate of deceleration exceeds the rate of acceleration. Furthermore, this higher brake torque is produced at the highest speeds. Consequently, since horsepower is proportional to the product of torque and velocity, the horsepower involved in deceleration is many times as great as the horsepower involved in acceleration. Thus, to decelerate a 1,000-ton train, at 100 miles per hour, at the rate of 2.2 miles per hour per second, involves the dissipation of 53,500 horsepower.

Horsepower may be used to indicate the rate at which a brake dissipates energy, since it is a less cumbersome expression than foot-pounds or Btu's per second and is also a concept familiar to engineers. But it should be noted that, from the heat viewpoint, the horsepower of a motor is a very different thing from the horsepower of a brake. The heating of the motor, caused by I^2R loss, friction, and so on, has a low value, expressed as horsepower, since it is but a small percentage of the output horsepower. The entire output of the brake, on the other hand, is heat. When expressing the energy dissipated by a brake in terms of horsepower, therefore, it must be remembered that the quantity of heat involved is many times the heat found in a motor of similar horsepower.

It is clear that in braking trains, mechanical energy is transformed into heat at a very rapid rate and that very quickly tremendous quantities of heat are generated. Fortunately, since a brake can be installed at each wheel, this heat does not have to be dissipated in some localized area, but instead can be dissipated at each wheel in the train. The problem of heat dissipation is thereby made much easier of solution. In addition, a brake at each wheel makes possible the shortest stop, because it enables the maximum retarding force to act upon the train. It is evident that since a brake has to be provided at each wheel, both in order to handle heat satisfactorily and to produce the necessary retardation, the air brake must provide for remote control of all these brakes from the head end.

Electrical engineers will recognize a certain similarity between a multiple-unit control system and an air-brake control system. In the electrical system, each car is fitted with a control group or box; in the pneumatic system, each car is fitted with a triple valve or its equivalent of universal or control valve. In the electrical system, the controller is moved from its off to its on position by remote control from the head end; in the pneumatic system, the triple valve is moved from its release to its applied position, again by remote control from the head end. In the electrical system, the controller progresses or notches up under remote control from the head end; in the pneumatic system, the triple valve graduates on or applies the brake fully, also

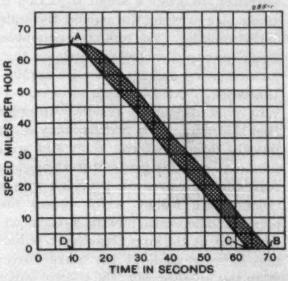


Figure 1

Paper 42-55, recommended by the AIEE committee on land transportation for presentation at the AIEE winter convention, New York, N. Y., January 26-30, 1942. Manuscript submitted November 12, 1941; made available for printing December 19, 1941.

JOSEPH C. McCUNE is director of research with the Westinghouse Air Brake Company, Pittsburgh, Pa.

under remote control from the head end.

If there are certain similarities between a multiple-unit control system and an air-brake control system, there are three striking dissimilarities. The first of these is that the electrical system employs several control wires, whereas the pneumatic system employs but a single control pipe, the familiar brake pipe. The second dissimilarity is that the control wires of the electrical system transmit control current only and do not carry the main motor current; the single pipe of the pneumatic system handles not only air for control purposes, but also the air for charging the brake cylinders, which involves substantial volumes of air. The third dissimilarity is that current flows through the control wires with the speed of light, whereas air flows through the brake pipe with the velocity of sound as the limit.

It is well-known that car brakes are caused to be applied by reducing the pressure in the brake pipe, but this operation should be examined in some detail in order that the reasons for electropneumatic control may be better understood. It is a comparatively simple matter to move a car controller from its off to its on position by remote control from the head end, but a somewhat more difficult operation to move a control valve from its release to its applied position, the equivalent of from off to on with the controller. This difficulty arises because the brake pipe is used, not only as the control medium, but also as the means of transporting large quantities of air to the car supply reservoirs, from which the brake cylinders are charged. Because of this function, which means that the feed grooves are open, the control valves, in release position, are not in a condition to respond to slight reductions in brake-pipe pressure occurring at a relatively slow rate.

To move the control valves from their off to their on position, it is necessary, therefore, to make a slight reduction in brake-pipe pressure throughout the train rather rapidly. This reduction cannot be made too rapidly, however, because then an emergency and not a service application would develop, not to mention certain other undesirable results. In order that this reduction in pressure may occur quickly throughout the train and with minimum pressure gradient from head to rear, control valves are provided with a quick-service feature which causes the pressure to be vented locally, at each control valve, at the same time that a reduction is being made at the brake valve. With this arrangement, the control valve on the last car of a long train can be moved from its off to its on position in a few seconds. It has been found that the time is roughly proportional to the linear length of the brake pipe; that is, in long trains about one second is required for each 300 feet of brake pipe. In other words, if a train were of such length that the control valve on the last car was 1,000 feet from the brake valve, this control valve would move from its off to its on position about 3.3 seconds after the brake valve had been placed in service position.

It should be noted that 300 feet per second applies to the last car only. The combined action of the quick-service feature and the flow of air from the rear is such that the first car does not apply until shortly before the last car. In consequence, at the expiration of a brief interval proportional to the length of the train, control valves, from head to rear, move from their off to their on positions with a fair approach to simultaneity.

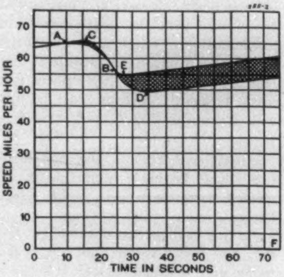


Figure 2

The operation just described covers the movement of the control valves throughout the train from release to application position, in which position the brake cylinders are charged with compressed air from the car reservoirs. In order that the brake cylinders may be charged to full-service pressure, the brake-pipe pressure must be reduced, on each car, 24 pounds or, say, from 110 to 86 pounds. It is clear that the pressure in a long pipe, commencing at the head end, cannot be lowered 24 pounds throughout its length, except when an appreciable time is available, and particularly when the gradient in pressure, from head to rear, must be held to a minimum. With the most modern pneumatic equipment, including continuous quick-service valves, it takes roughly 15 seconds, after placing the brake valve in service position, to build up full-service cylinder pressure on the last car of an 18-car train.

The release of the brakes, or the movement of the car control valves back to off position, is accomplished by building up pressure in the brake pipe. After a full service application, as stated above, the brake-pipe pressure throughout the train, is roughly 86 pounds, and this pressure must be restored to about 110 pounds before all pressure can be completely released from the brake cylinders. Since the brake-pipe pressure is restored from the head end and against the drain of car reservoirs which are, at this time, recharging themselves from the brake pipe, a substantial time element is involved, although this time is still a matter of seconds and not greatly different from the time to apply all brakes fully.

It is clear that the operations just described involve transient pneumatic phenomena. Electrical engineers appreciate the difficulty attached to understanding and controlling transient electrical phenomena. They probably will not dissent from the statement that transient phenomena in a gas, or elastic medium, present technical problems at least as difficult. Many of these problems can be circumvented by utilizing electrical control means.

Since 1887 at least, it has been understood that the time element involved in applying or releasing brakes could be greatly reduced by the use of electricity and since that time many systems of electropneumatic control have been devised, and several types have been placed in actual service. The type used on the modern high-speed train may be called a two-pipe system. That is, a second pipe, termed the straight air pipe, is employed as well as the familiar brake pipe. The straight air pipe only is used during electropneumatic operation.

Each car is equipped with electropneumatic valves. When these valves are energized from the head end, pressure is built up locally on each car, in the straight air pipe, so that the pressure in the straight air pipe throughout the train can be brought up quickly to full service value. Suitable pneumatic relays, controlled by straight air-pipe pressure, charge the car-brake cylinders to the pressure existing in the straight air pipe and in the same time. With this system of control, full-service cylinder pressure can be secured in about four seconds from brake-valve movement on all cars of a train of any length. To release the cylinder pressure, the electropneumatic valves are de-energized, which vents the straight air-pipe pressure locally throughout the train. Brake-cylinder pressure is consequently rapidly released.

With this system, it is evident that in service operation, three results, which cannot be obtained with pneumatic control alone, are accomplished.

1. Pressure commences to appear in all brake cylinders throughout a train of any length at substantially the instant the brake valve is placed in service position.
2. The cylinders throughout the train can be charged simultaneously to full pressure in a time fixed by the designer's option.
3. Pressure in the cylinders throughout the train can be completely exhausted simultaneously in a time again fixed by the designer's option.

It will be noted that the time saved by electropneumatic control is a matter of seconds. The question arises as to why a few seconds is an item of any great moment in the operation of high-speed main-line trains, which make few stops and few slowdowns. If one of these trains, however, traveling at 100 miles per hour or about 150 feet per second, has to make an emergency stop to save lives and avoid property damage, the importance of saving a few seconds in brake-application time is self-evident. This aspect of electropneumatic control is so well understood that no particular comment seems called for. But since the effect of electropneumatic control on service operation is less widely understood, some comment in this connection may be of value.

Figure 1 illustrates a situation which arises more or less frequently in actual service. A train is moving along the track, still accelerating, when it encounters, at *A*, an unfavorable signal aspect and under circumstances which indicate that a service-application stop must be made at once. Power is immediately shut off and the brake valve placed in service position. With pneumatic brakes, the train decelerates along the line *AB;* with electropneumatic brakes, along the line *AC*. The stop distance with the pneumatic train is represented by the area *ABD;* with the electropneumatic train, by the area *ACD*. The cross-hatched area is the saving in stop distance, which, obviously, is far from negligible.

This chart has been prepared to show the gain brought about by a single factor, that is, the use of electricity, and does not refer to any specific train. It assumes that a full service application, with either brake, produces a retardation of 1.25 miles per hour per second, and that this retardation is constant, irrespective of speed or temperature. When the brake is partially applied, the retardation is taken as directly proportional to the cylinder pressure. The cylinder-pressure build-up is assumed to duplicate laboratory tests of an 18-car train. The retardation is supposed to depend entirely upon the car-cylinder pressures, that is, the locomotive brake blends harmoniously with either system of car brakes. Internal and wind resistance is neglected.

None of these assumptions corresponds precisely with the conditions found in actual operation. Notwithstanding, it is believed that the chart fairly indicates the order of the shortening of service-stop distances, on a comparative basis, accomplished by adding electricity to the pneumatic-braking system. The saving reduces as the number of cars in the train decreases but never entirely vanishes, because the pneumatic-application time is always greater than the electropneumatic time, even for a one-car train.

Since signal spacing is based upon service-stopping distances, the situation described above is of considerable practical importance. Another aspect of service-stopping distances was mentioned by R. P. Johnson, chief engineer, Baldwin Locomotive Works (*Transactions* of the American Society of Mechanical Engineers, October 1941, page 614) before the society, namely:

> "Modern high-speed operation presents problems in deceleration as well as acceleration, but the paper does not say much about this. On some railroads, such as the New Haven, station stops may be rather close. If, on such a road, the decelerating force were relatively low, the train could not, within the distance, be permitted to attain the maximum speed of which it is capable."

Figure 2, which is based on the same assumptions as Figure 1, illustrates a situation frequently encountered in regular service. The train is proceeding along the track, still accelerating. Suddenly a condition arises which may call for a brake application. This condition may be an unfavorable signal indication which is not clearing as expected, an automobile crossing the track, a track-repair gang somewhat slow to move, or any unexpected interference, of a temporary nature, which may call for a stop. A brake application is deferred as long as possible but with pneumatic brakes, in order to stop short of the interference, the brake valve has to be placed in service position at *A* and the train decelerates along the line *AB*. To stop at the same point with faster-acting electropneumatic brakes, the brake valve does not have to be placed in service position until several seconds later or at *C*. The train then decelerates along line *CB*.

At *C*, the temporary interference clears; that is, the signal aspect becomes favorable, the automobile completes its passage across the track, the track gang gets out of the way, and so on. The brake valve is, therefore, immediately placed in a position to release the brakes. The train continues to decelerate, of course, until the brakes are completely released, which occurs at *D* with the pneumatic brakes and at *E* with the electropneumatic. Power is then applied and the train accelerates. The cross-hatched area indicates the greater distance covered by the electropneumatic train in the time *OF*. It will be noted also that at *F*, the electropneumatic train is at a higher speed than the pneumatic train and is consequently still gaining distance.

It has been stated that in Figures 1 and 2, it is assumed that a full-service application of either the pneumatic or the electropneumatic brake produces the same retardation. The savings discussed above

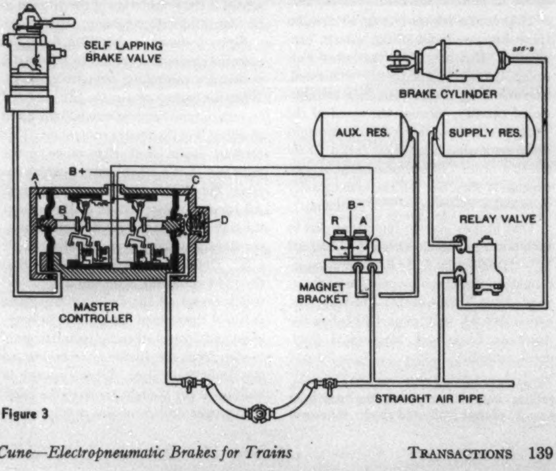


Figure 3

are, therefore, those to be credited entirely to the use of electricity. It will be observed that these savings are obtained in situations where the brake valve has to be placed in service or release position without any prior warning At regular station stops or slow-order curves, the need for a brake application or release can be anticipated, in which event the savings under discussion, with skillful handling, largely disappear. But there are so many occasions in actual operation, apart from emergency stops, when the brake must be applied or released without advance notice that electropneumatic brakes contribute materially to the expeditious movement of high-speed trains.

In this connection, the release function is of particular interest. Since a long time and a considerable distance are needed to accelerate a train to the high-speed zone, it is important that the high speed, once attained, be held as long as possible; that is, that slowdowns and stops be reduced to the absolute minimum. The urgent necessity of such a course can be readily appreciated by anyone who has noted the effect of one or two stops, or a long detour, on the mileage covered in an all-day automobile trip. The railroads, from the beginning of high-speed operation, have fully appreciated this necessity. Large sums of money have been spent to eliminate speed restrictions at curves, track intersections, grade crossings, and so on. Meets have been arranged so that high-speed trains would not be called upon to reduce speed. Steps have even been taken with track-repair gangs so that their presence would not interfere with the maintenance of high speed.

That brake release has an effect upon these improvements is not always considered. But it is clear that since slow orders cannot be completely eliminated, slow-releasing brakes introduce an additional penalty, because the speed of the train, due to slow-brake release, is brought down somewhat below that called for by the slow order. Electropneumatic brakes, because of their fast release, assist greatly in reducing this penalty to a minimum.

That brakes play an important part in maintaining schedule speeds is evidenced by a statement by A. A. Raymond, superintendent of fuel and locomotive performance, New York Central (*Railway Age*, September 20, 1941, page 446) before the American Society of Mechanical Engineers, namely:

> **"While acceleration only has been shown, getting over the road in the minimum amount of time is affected also by the rate of deceleration, although, in general, more time is lost accelerating than decelerating."**

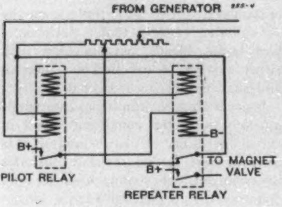


Figure 4

A detailed description of the operation of the complete electropneumatic brake does not come within the scope of this paper. In fact, it appears that such a description would possess only a very limited appeal. It is thought, however, that certain features of the brake involving problems of electrical control, which are not widely, nor ordinarily encountered, will interest electrical engineers in general.

It has been emphasized that electropneumatic brakes can be quickly brought into action, or as quickly taken out of action. In addition, it has been previously pointed out in this paper that a train-braking system comprises a large number of individual brakes, or one per wheel, and that they must all be controlled remotely from the leading unit. The retarding force at each wheel is created by brake-shoe friction; the brake-shoe friction is proportional to the brake-shoe pressure; and the brake-shoe pressure is proportional to the brake-cylinder pressure. It follows that regulation of the cylinder pressure within close limits is essential if the retardation of the train is to be controlled with maximum satisfaction.

Figure 3 shows, in simplified form, the essential elements of the electropneumatic system for controlling cylinder pressure. When the brake valve at the left is moved to service position, pressure is built up in chamber *A* of the master controller. This pressure causes shaft *B* to move to the right and close a set of contacts which energize the release wire from the battery. All release-magnet valves *R* throughout the train are then energized, thereby closing all exhaust ports from the straight air pipe. Continued movement of shaft *B* to the right closes the application contacts, which causes all the application-magnet valves *A* throughout the train to be energized and opened, thereby permitting air to flow from the auxiliary reservoirs to the straight air pipe. When pressure is built up in the straight air pipe, the relay valves open and from the supply reservoirs, charge the brake cylinders to the pressure existing in the straight air pipe. Meantime, pressure from the straight air pipe has been building up in chamber *C* of the master controller. When this pressure almost equals that in chamber *A*, spring *D* moves shaft *B* to the left and opens the application contacts. The application magnets close and the cylinder pressure remains equal to that in chamber *A*.

To release the cylinder pressure, the brake valve is moved to release position which releases the pressure in chamber *A*. Shaft *B* then moves to the left and opens the release contacts. All release-magnet valves are then de-energized and straight air-pipe pressure is exhausted to atmosphere at each magnet bracket. Exhausting the straight air-pipe pressure causes the relay valves to move to a position in which the brake cylinders are connected to atmosphere. The brakes are thereby released.

The very simplicity of this control system tends to obscure the many advantages it possesses. These advantages represent such an advance in the brake art that it is believed attention should be directed to them.

The brake valve employed with this system retains an old name but actually the mode of operation is entirely changed. The self-lapping brake valve is essentially a two-way regulating valve; that is, it regulates the pressure admitted to or discharged from chamber *A*. When the brake-valve handle is moved to the right, out of its normal or release position, each position of the handle calls for a definite pressure. In other words, when the handle is placed in a certain position, the pressure in chamber *A* is increased or decreased, depending upon whether it is less or greater than the pressure called for by the position of the brake-valve handle. The pressure can be increased or decreased at will in very small steps. It is independent of the volume of chamber *A* or the pipe attached thereto and is not affected by any reasonable leakage from volume or pipe. Those who are acquainted with old-style brake valves will appreciate what an advance in the art this self-lapping brake valve represents.

For practical purposes, the master controller moves to its application position, the application-magnet valves are energized and the relay valves are opened coincident with placing the brake valve in an application position. It has been previously explained that several seconds are required to move the control valves of the purely pneumatic system from their off to their on position with trains of customary length. All this time is eliminated by electropneumatic control.

Furthermore, the time of charging the brake cylinders to full pressure is not contingent upon dropping the pressure throughout a long brake pipe. Instead, the application-magnet valves on each car can charge the small straight air-pipe volume of each car very quickly and the brake-cylinder pressure, through the action of the relay valves, keeps pace therewith. Each application-magnet valve is provided with a choke so that the time of fully charging the straight air pipe is prefixed at about four seconds. It is not necessary to change the size of this choke to compensate for variations in car length, and so on, because the straight air pipe itself averages out variations since it is continuous throughout the train. If a magnet valve should become inoperative, this continuous pipe supplies the necessary air from front and rear. If, during an application, the pressure in the straight air pipe should reduce through leakage, which is probable since its volume is small, the master controller promptly moves to the right, re-energizes the application-magnet valves, and restores the loss in pressure.

Electropneumatic brakes can be applied to one-half their full-service effectiveness in about two seconds. It is clear that if the pressure build-up had to be shut off by moving a brake valve back from service to lap, very considerable skill would be required, if indeed it could be done with reasonable accuracy. With the system shown in Figure 3, the brake valve is moved half way toward full-service position, which produces one-half the full-service pressure in chamber *A*. As soon as this pressure is attained in the straight air pipe, and hence the brake cylinder, the master controller de-energizes the application-magnet valves. It is plain that this system makes it possible to apply and release brakes partially, despite the very short times involved, easily, positively, and uniformly.

It is apparent that with this system, the control of cylinder pressure is not influenced by the number of cars that may be in the train. This is in decided contrast to a purely pneumatic system in which the time to obtain a certain cylinder pressure changes with the number of cars in the train. That brake action should be independent of train length is obviously so desirable as to call for no extended comment.

The action of the relay valve, which directly charges the brake cylinder, is not changed by variable brake-cylinder volume. Variations in brake-cylinder pressure caused by variations in piston travel, a troublesome problem with older pneumatic brakes, are entirely overcome. Moreover, the relay valve automatically maintains the brake-cylinder pressure against leakage.

The net result of this control system is that any cylinder pressure desired, requiring either an increase or a decrease over the pressure previously existing, may be easily, positively, and uniformly obtained in a very short time which is always the same irrespective of train length; and independent of variations in pipe volumes, changes in brake cylinder piston travel, or brake cylinder leakage. Similar results, particularly with respect to time, cannot be attained with a one-pipe pneumatic system.

It is well known that the friction of cast-iron brake shoes, for a given pressure, decreases as the speed increases. If the same retardation is to be obtained in the high-speed brackets as in the low, a greater brake-shoe pressure must be employed at the higher speeds. It has been found empirically that satisfactory results are obtained if 100 per cent of this higher pressure is employed at speeds above 65 miles per hour, 80 per cent between 65 and 40 miles per hour, 60 per cent between 40 and 20 miles per hour, and 40 per cent at speeds below 20 miles per hour. To make these changes automatically, it is evident that a speed-recognizing device is required. For this purpose, a generator, usually axle-mounted, is employed.

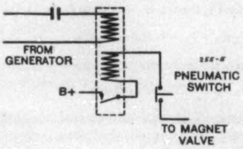


Figure 5

A generator to measure speed is a familiar arrangement but such generators ordinarily provide energy only sufficient to operate indicating instruments. The generator here considered has to supply enough energy to operate a system of relays, which means that at 100 miles per hour the generator output is about six watts. Since the relays operate on direct current, a d-c generator is used, and operation in either direction is taken care of by a directional relay.

One problem with relays, of course, is that they pull in with one voltage and drop out with an appreciably lower voltage. This characteristic has to be overcome with the speed-control system, since it is desired that speed recognition be as sharp and definite as possible. The method used is illustrated by Figure 4.

When the generator has built up sufficient ampere turns in the lower coil of the pilot relay to pull in the armature, the closing of the contacts energizes the lower coil of the repeater relay. The flux thereby built up induces a current in the upper circuit which adds to the ampere turns of the pilot relay, so that its armature is pulled in without hesitation or fluttering. Opening of the upper contacts of the repeater relay cuts additional resistance into the circuit of the lower coil of the pilot valve. It consequently drops out on a higher voltage than would otherwise be the case.

The result achieved with this combination is that if the pilot relay pulls in at a speed of 69 miles per hour, it drops out at no less than 65 miles per hour and ordinarily at a somewhat higher speed. This four-mile-per-hour maximum range represents a drop of 5.8 per cent. Despite the fact that this device must be sufficiently rugged to withstand the shocks of railway service, its range approaches that of a household thermostat.

This generator can also be used to prevent wheel sliding. When used for this purpose, the generators are termed "Decelostats" and one is required per axle.

It is obvious that not very much time is available to prevent wheel sliding. When the wheel is rolling freely with brakes applied, a very small portion of the braking torque is devoted to destroying the angular velocity of the wheel and axle assembly, but the greater portion is balanced by the counter torque set up at the rail. But the retarding force set up at the rail cannot exceed the product of the coefficient of adhesion and the pressure actually excrted by the wheel on the rail. If the retarding force is near the adhesion limit, therefore, it is clear that the wheel will slip if

1. The braking torque, for any reason, increases abnormally.
2. The pressure of the wheel on the rail is reduced because of oscillations, and so on.
3. The coefficient of adhesion decreases because a section of bad rail is encountered.

If one or more of these conditions arise, the wheel and axle assembly loses angular velocity with great rapidity because the braking torque is directed almost entirely to arresting the rotary motion of the wheel and axle assembly. The wheels may cease revolving completely in a time which may not exceed 1 second.

A mechanism to prevent wheel sliding, therefore, must recognize wheel slippage promptly and must act quickly to release brake cylinder pressure. How this can be accomplished with a generator is illustrated by Figure 5. The generator charges a condenser through a unidirectional relay. If the wheel is losing angular velocity at an abnormal rate, as it does when the wheel slips, the generator voltage drops at an abnormal rate. The condenser then discharges a current sufficient to trip the sensitive relay, which causes a magnet valve to be energized and pressure to be released very rapidly from the brake cylinder.

In conclusion, it should be pointed out that electropneumatic brakes involve control of the brake-cylinder pressure from the brake valve and do not necessarily include speed governor or "Decelostat" control. That is, speed governors and "Decelostats" may or may not be used with electropneumatic brakes.

A Control System for Modern Multiple-Unit Rapid-Transit Cars

H. G. MOORE
ASSOCIATE AIEE

MUCH has been accomplished in recent years in the development of refined systems of control for traction motors as applied to buses, trolley coaches and street cars. Chief among the innovations is the extensive use of some form of electric braking—either as a holding brake for limiting speed when descending a grade or as a service brake for retarding the speed of the vehicle each time a slowdown or stop is made.

The latter type of brake is used as the principal means of braking PCC (Presidents' Conference Committee) cars, being supplemented by magnetic track brakes to obtain rates of retardation beyond the adhesive limit of wheels to rail and complemented by some form of friction brake to provide means of retarding the car at speeds at which the electric brake is not effective and to hold the car at standstill on a grade. The electric service brake is obtained by connecting the motors so that they operate as series-excited generators, the output being absorbed by the same resistors used for acceleration. To control this output under widely varying and continually changing conditions of speed and load requires the use of a novel switching and regulating scheme.

The success with which the equipment for PCC cars has met in street railway service has led to its adaptation to multiple-unit service. A notable example of such adaptation is the "compartment car", a number of which have been placed in operation by the New York City Transit System (Brooklyn-Manhattan Transit division). Because of conditions peculiar to operation in multiple-unit and from third-rail supply in subway and elevated service, various modifications and additional features (as compared to PCC car equipment) are provided.

Characteristics of Dynamic Braking

Methods of determining the accelerating and braking characteristics of d-c series-excited machines and control are well known. For the elementary circuits illustrated by Figure 1 the following relations apply:

In Figure 1a,

$$2I_MR_A = V_L - 2nE_M - 2I_MR_M \qquad (1)$$

and, in Figure 1b,

$$2I_MR_B = 2nE_M - 2I_MR_M \qquad (2)$$

where V_L is line voltage and R_M is the motor resistance (including effective brush-contact resistance).

Equation 2 is of particular interest, because it illustrates that, since E_M is a function of I_M, the braking resistance is a linear function of speed for any constant value of current I_M. In fact, from equation 2

$$\frac{n}{R_C} = \frac{I_M}{E_M} \qquad (3)$$

where $R_C = R_B + R_M$ = total circuit resistance.

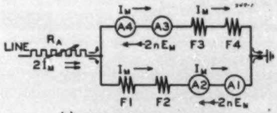
If, therefore, the ratio I_M/E_M for each value of field strength used in braking be plotted as a function of I_M as shown in Figure 2, very useful curves for determining braking characteristics of the machine are provided. The curves are the basic form of all constant-resistance braking curves of the machine as, by multiplying the ordinate scale by any value of R_C, the curves become the current-speed characteristics for that circuit resistance. For example, with a circuit resistance of three ohms, the GE-1198 motor (full-field) will generate 200 amperes at 3,000 rpm. By further multiplying the ordinate scale by the proper factor determined by gear ratio and wheel diameter, the speed can be expressed in the more familiar unit of miles per hour car speed.

The curves also clearly show, for each field strength, the critical ratio of speed to circuit resistance below which the machines will not generate appreciable current. At this critical ratio the machines are very sensitive to slight changes in speed and resistance, this condition representing operation in the unsaturated portion of the magnetization curve.

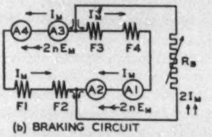
Switching and Spotting

Obviously, in the interests of safety and efficient operation, the response of dynamic braking to the control of the operator or to automatic application for an emergency stop must be prompt but free of surges which might cause discomfort or injury to passengers.

Switching from accelerating to braking connections is facilitated by the use of motors connected permanently in parallel relation as it is necessary only to transfer the location of the resistors in the circuit.



(a) ACCELERATING CIRCUIT



(b) BRAKING CIRCUIT

Figure 1. Elementary motor-circuit connections showing relations of current and electromotive force

I_M—Motor current
E_M—Motor electromotive force per unit speed
n—Speed
R_A—Variable accelerating resistance
R_B—Variable braking resistance

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H. G. Moore is of the transportation control engineering department, General Electric Company, Erie, Pa.

It is not necessary to operate the motor reversers and, in the braking connection, the exciting fields are "cross-connected" to insure equal division of load between the two parallel groups of machines.

It is not enough to merely establish the braking connections, but the circuit resistance must also be quickly adjusted to the value which will give the desired value of braking current at any speed at which the brake may be applied. This operation has been termed "spotting" the control, and various schemes have been used to accomplish the desired results.

One scheme which has been successfully used is to arrange that, regardless of car and motor speed, the braking resistance be at (or near) its maximum value at the time of switching to the braking connections, and then to rapidly reduce the resistance until the current builds up to the desired value. This requires, regardless of the means for controlling the resistance, that the control be capable of rapid operation and that its current-responsive regulating system be capable of quickly stopping operation before the resistance has been reduced to too low a value. The principal advantage of this scheme is that the braking sequence is practically independent of the accelerating sequence, and a "switching step" of acceleration control can be provided wherein the accelerating resistance can be retained at maximum value.

The principal factor governing the correct value of braking resistance is, however, the speed of the car and motors. This suggests, if the acceleration sequence be suitably arranged and controlled in relation to motor speed, that braking current can be obtained immediately upon switching to the braking connection without requiring that the resistance control operate to change the load resistance. Such ideal operation cannot be attained in practice because of the wide ranges of accelerating and braking currents used, but is approximated in the control systems now generally used for PCC cars. Such systems permit the use of a less powerful drive for the resistance controller but requires, even on the "switching step", that the acceleration sequence progress so as to maintain the required relation of resistance to car speed.

Coasting Operation

A refinement of the spotting operation can be provided by utilizing the interval in which the car may be allowed to coast before the application of braking. This is done by automatically switching the motor circuit to the braking connection and allowing the braking sequence to progress immediately after the operator shuts off accelerating power. However, under this condition, the braking current is held to a very low value and the exciting fields are partially shunted to maintain the resulting retardation at a negligible value. The control thereupon operates to establish the circuit resistance at the critical value corresponding to that particular field strength and car speed.

By proper choice of field strength and value of current used for coasting, the equipment may be made to provide immediate response to the application of dynamic braking simply by the operation of unshunting the exciting fields. As an example, from Figure 2, if the coasting current is maintained at 20 amperes with fields partially shunted, a current of approximately 95 amperes will be obtained by full-field operation without change in either motor speed or circuit resistance. Thereafter this or higher values of braking current are obtained by the resistanee control operating automatically in response to a current-sensitive relay to maintain the speed-resistance ratio at the value corresponding to the desired braking current.

Specific Connections and Operating Sequence

Figure 3 illustrates, with some modifications described later as being incident to multiple-unit operation and a particular type of service, a complete motor circuit for a PCC car. The resistor section *R*1-*R*2 indicated as having a tap which can be moved from position *A* to position *B* takes the form of a rheostatic, or "commutator", controller as shown by Figure 4. This device is essentially a stationary commutator with 137 active bars on about 330 degrees of the periphery, each connected by copper straps to resistor elements. Each active commutator bar, therefore, represents a tap on the whole resistor section *R*1-*R*2. Six carbon-tipped fingers or "brushes" are carried in constant contact with the surface of the commutator by an arm driven by a small reversible motor so as to rotate in either direction over the active bars. Mechanical stops are provided to prevent rotating the brushes over the inactive commutator bars which, with their slot insulation, serve to insulate resistor terminals *R*1 and *R*2 from each other over the remaining 30 degrees of periphery.

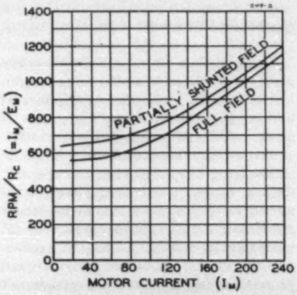

Figure 2. Basic characteristics of GE-1198 motor operating as a series-excited generator

The direction of rotation of the pilot motor and brush arm is controlled solely by the combinations of contactors which may be closed, and the speed is controlled primarily by the operation of accelerating and braking relay *ABR*.

For an acceleration from standstill to maximum speed, line contactors *LB*1, *LB*2 and *LB*3 close to complete the power circuit with all of rheostatic controller resistance *R*1-*R*2 and resistor sections *R*2-*R*3 and *R*3-*R*4 in series with the motors. The initial value of current and tractive effort so obtained is equivalent to a rate of only about 0.4 mile per hour per second for the usual weight of car.

Contactors *C*1 and *C*2 close in sequence to leave only the rheostatic controller in series with the motors and thus, in two steps, increase the current and tractive effort to approximately the value required for acceleration at the minimum service rate. The rheostatic controller then operates to move its brush arm from position *A* towards position *B* under control of relay *ABR* which, in turn, is responsive to current in its operating coil *ABR* (*a*). If a high rate of acceleration is required, relay *ABR* is made responsive to a corresponding value of current and the rheostatic-controller brush arm is permitted to move rapidly until this current is obtained. Thereafter, the speed of brush-arm movement is closely regulated to maintain constant current through the motors as the car speed increases.

When the brush arm reaches position *B*, the motors are operating at full voltage and contactor *LB*4 closes to prevent the re-insertion of resistor *R*1-*R*2 when the brush arm subsequently moves back towards position *A*. At this point in the sequence, transfer switch *TS* operates to open its contact (*a*) and close contact (*b*). This has no immediate effect in the motor circuit but is in anticipation of a following coasting or braking sequence. As the brush arm returns to position *A*, field-shunting contactors *FS*1, *FS*2, *FS*4 and *FS*3 are closed in sequence by circuits

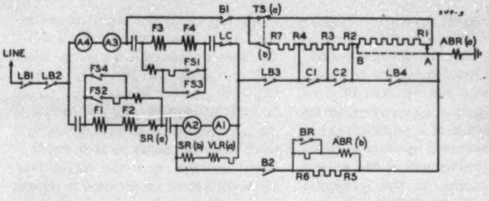

Figure 3. Motor-circuit connections for accelerating and braking

Figure 4. Rheostatic motor controller

controlled by cams mounted on the brush-arm shaft of the rheostatic controller.

The motor circuit may be switched to the braking connections at any time in the accelerating sequence by opening all contactors which may have closed and immediately closing braking contactors *B*1 and *B*2. As transfer switch *TS* and the rheostatic-controller brush arm do not change position during this operation, the value of braking resistance initially obtained in the braking circuit is determined by the status of the accelerating sequence just preceding the switching operation. Thus, if the acceleration sequence had been completed with rheostatic-controller brush arm returned to position *A*, the initial braking resistance would be at a maximum value, or the sum of all resistances shown. On the other hand, if the circuit were switched to braking connections at some low speed such that the brush arm was still moving from position *A* toward position *B*, the initial braking resistance would be only that part of section *R*1-*R*2 which had been cut out during the partial acceleration, plus the resistance of section *R*5-*R*6. This latter section is never cut out of the braking circuit, because it has been found impractical—except by means otherwise detrimental—to automatically regulate the braking current at very low speeds where the response of the motor to changes in speed-resistance ratio is very sluggish.

While either coasting or braking, the speed of brush-arm movement is again controlled primarily by the operation of relay *ABR* which is now responsive to an operating coil *ABR* (*b*) connected so as to be energized by the voltage drop across resistor section *R*5-*R*6. By use of a suitable control resistance in series with this operating coil, the range of operation of the relay in respect to braking current through resistor *R*5-*R*6 can be readily changed from a very low value, corresponding to coasting current, to high values, corresponding to normal service and emergency braking, by allowing contact *BR* to open.

The complete coasting and braking sequence from high speed consists of first moving the rheostatic controller brush arm from position *A* towards position *B*, transfer switch contact *TS* (*b*) having been closed and contact *TS* (*a*) having been opened in the preceding acceleration sequence. When the brush arm reaches position *B*, the transfer switch operates to open contact *TS* (*b*) and close contact *TS* (*a*). Since the resistance of sections *R*2-*R*3-*R*4-*R*7 is essentially the same as the resistance of the rheostatic controller resistor, the operation of the transfer switch re-inserts the resistor *R*1-*R*2 in the braking circuit without surge due to abrupt change of load resistance. As the car speed continues to decrease, the rheostatic-controller brush arm moves back to position *A* until, finally, only resistor *R*5-*R*6 is left in the circuit and the dynamic braking "fades out" at very low car speeds.

As previously indicated, the braking connections may be established at any speed and, therefore, the braking sequence described may be foreshortened by any amount.

The relation of braking-sequence progression to speed of the car also permits the switching to accelerating connections and the reapplication of power quickly and smoothly at any speed. A typical diagram showing the relation of sequence progression to car speed for extreme rates of acceleration and braking is shown in Figure 5.

Cushioned Shutoff

Emphasis has often been given, in the design of accelerating and braking control, to the smooth application of power and brakes, particularly where high rates of acceleration and braking are provided. It is quite as important that if, while accelerating at a high rate, the operator finds it necessary to coast, power be removed in easy steps to avoid passenger discomfort. This is accomplished by introducing sequential opening of contactors *C*2, *C*1, the field-shunting contactors (if accelerating sequence has progressed that far) and, finally, the line contactors. If it is necessary to make a brake application, however, the sequential operation is automatically nullified and the switching of the motor circuit to braking connections is obtained without delay.

A cushioned release of high rates of dynamic braking is obtained by the normal operation of partially shunting the motor fields when coasting. The braking current does not immediately drop to the normal coasting value because of the relative characteristics of braking with full and partially shunted fields.

Control of Rheostatic Motor Controller

Figure 6 shows the complete circuit for speed and directional control of the pilot motor which drives the rheostatic controller brush arm. The motor is provided with separate series-exciting fields for each direction of rotation so as to simplify the reversing operation by contactor interlocks. It should be noted that the direction of rotation is always such as to reduce traction-motor circuit resistance, whether accelerating or braking. No voltage, therefore, appears between the commutator bars of the rheostatic controller and the trailing edges of the carbon-tipped fingers, and the voltage at the leading edge is so low as to preclude any possibility of burning or sparking.

Control of the speed of the pilot motor in response to accelerating or coasting and braking currents is provided through the medium of relay *ABR* which, at a predetermined current determined by tension of a restraining spring, picks up to short-circuit the pilot-motor armature. Besides the operating coils (*a*) used during acceleration, and (*b*) used during coasting and braking, a third coil (*c*) is in series with the circuit controlled by *ABR*. This coil provides a powerful anticipatory response to increasing current in the traction-motor circuit to prevent "overshooting" and, at the regulated value of traction-motor current, causes the relay to operate on one or more of the sequentially closed contacts with a vibrating action such that the armature voltage and field excitation of the pilot motor and, therefore, the speed of brush-arm movement is regulated to maintain constant current through the traction motors.

Where service is such that the cars may be operated frequently at maximum speed it is desirable to prevent the repeated use of dynamic braking at loads beyond safe operating limits of the traction motors. As these limits are characterized by an almost constant value of armature voltage, a voltage-limit relay *VLR* may be provided to control the rheostatic-controller pilot motor in a manner similar to relay *ABR*, but responsive to a predetermined traction-motor overvoltage. The effect of voltage-limit operation on braking sequence is indicated by the broken line at the top of Figure 5.

Multiple-Unit Operation

Figure 7 shows one of a number of multiple-unit rapid-transit "compartment" cars to which the electric equipment of a PCC car has been adapted. Each compartment car is equivalent to two PCC cars in electrical equipment, as each car has four two-axle trucks supporting three articulated body sections designated *A*, *B* and *A*1. The outer sections, *A* and *A*1, are identical and carry the major portion of the electrical equipment. The car weight per motor is approximately 15 per cent greater than that of a standard PCC car but, as the car operates in less frequent stop service, is well within the capacity of the motors.

Multiple-unit operation requires that certain devices (controlled mechanically by the operator of a PCC car) be arranged for remote control. The hand-operated reverser of the PCC car is replaced by a conventional electropneumatic reverser, controlled by the energization of either one of two train wires. On a PCC car, the accelerating and braking currents are controlled by varying the tension of the accelerating- and braking-relay restraining spring. For remote control, the spring is fixed at a value which will normally cause the relay to operate at the desired maximum values of current. Lower operating currents are obtained by energizing a separate coil section, *ABR* (*d*) of Figure 6, at various currents to assist the other coils in closing the relay contacts. A variable voltage is supplied the train wire to which the rate coils are connected by a potentiometer incorporated in the master controllers.

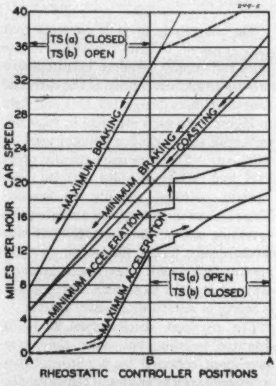

Figure 5. Typical sequence diagram

It is neither practicable nor safe to control all of the circuits directly through train wires from the master controllers, because

(*a*) Some circuits must be energized to obtain dynamic braking sequence.

(*b*) Some circuits associated with undervoltage protection are controlled independently of the master controller.

(*c*) The capacity and number of master controller fingers and train wires would be increased appreciably by direct control.

The control circuits of each equipment are supplied, so far as is practical, from the local battery and its charging generator on each *A* and *A*1 section, through contacts on auxiliary relays which, in turn, are controlled through train wires from the master controllers. Because of the possibility of open circuits or short circuits on these train wires, the circuits are arranged so that it is necessary to energize two or more train wires to release all types of brakes with which the cars are equipped (electric, magnetic track and friction brakes), and also it is necessary to energize two or more train wires to accelerate the cars. Failure of any essential train wire therefore cannot prevent the removal of power and the application of brakes.

Low-Voltage Bus

All control-voltage storage batteries in a train are connected in parallel to provide a reasonable division of load and to insure a reliable supply of power for the master control and train-wire circuits. The battery positives are each connected to the positive bus line through a limiting resistor, which serves the dual purpose of diminishing the effects of inequalities in the setting of the battery-charging generator-voltage regulators and, in case of a fault between positive and negative bus lines, prevents quick depletion of battery charge by limiting the fault current to the full-load capacity of the charging generators.

It is very desirable to ground the battery system to prevent or minimize damage to low-voltage control devices and insulation if a fault should occur between the control circuits and the high-voltage power circuits. Grounding one of the battery bus lines at more than one point in a train, however, invites the diversion of power current from its normal return path through the running rails, if the train should pass over a high-resistance rail bond. In such circumstances the battery negatives, while connected directly to the train wire used as the negative bus line, are also connected to "ground" (car structure) through a resistance such as will limit the current diverted from the rail to the capacity of the bus line and yet pass power-to-control fault currents which may occur.

Remote Battery Disconnect Contactors

To prevent back circuits through contacts in master controllers which are not being operated, all such contacts are usually required to be open when the master controller is nonoperative. But this does not de-energize control circuits local to each equipment which are essential to the operation of brakes. In particular, the magnetic track brakes are fully energized from local batteries and charging generators when all control circuits are opened preparatory to changing control stations or laying the car up in yard or shop. To automatically remove these power loads which are no longer required after the car or train is at standstill, the circuits are supplied through contactors which are closed by energizing a train wire whenever any master controller is operative. Each contactor mechanically latches in the closed position so that the local circuits will not be interrupted if the train wire is subsequently and unintentionally open-circuited. The contactors are automatically tripped open by momentarily energizing another train wire in the sequence of movements the operator must make in making a master controller nonoperative.

Cutting Out Bad-Order Cars

It is of extreme importance that means be provided to permit the continued operation in multiple unit of any equipment in the train which may not be func-

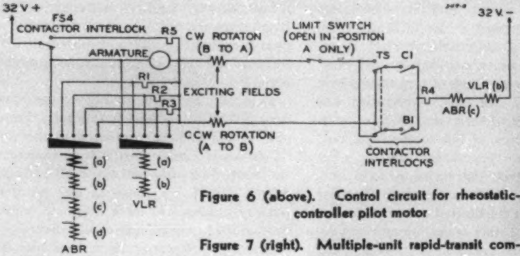

Figure 6 (above). Control circuit for rheostatic-controller pilot motor

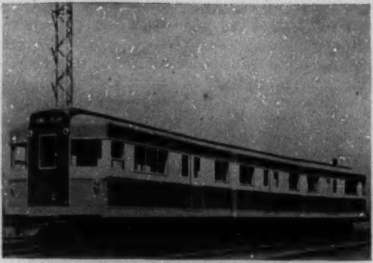

Figure 7 (right). Multiple-unit rapid-transit compartment car

tioning properly. This is particularly true of equipments having motors connected permanently in parallel or having provision for the type of electric braking here described. Failure of a motor reverser to throw to a position in agreement with direction of train movement will cause the motors to build up as short-circuited series generators, the current flowing around the "loop" circuit formed by the motor interconnections. Failure of the braking-circuit contactors to open or the sequence to progress will cause abnormal braking currents to be generated while the rest of the train is being accelerated.

To guard against such contingencies a train-wire circuit is provided which, energized at the rear end of the train, can be completed to the front or operating end to operate a control relay and permit the acceleration of the train, only if all reversers in the train have assumed a position in agreement with the desired direction of train movement, and only if the braking connections of all motor circuits have been interrupted.

Failure to accelerate a train because of operation of this protective circuit requires that the motor "loop" circuit of the defective or "bad-order" car be interrupted by some means. This is accomplished by the operator momentarily energizing a train wire which causes cutout relays in each equipment to operate contacts which, in turn, de-energize the operating coil of a "loop contactor" in the motor circuit of that equipment. The opening of this contactor (designated *LC* in Figure 3) effectively prevents the motors acting as series generators under any condition and, by electrical interlocking, prevents the closing of line contactors on that equipment. The operating coil of contactor *LC* is normally energized directly from the battery-charging generator. As this generator is driven by the motor which also drives a blower for forced ventilation of the resistors and rheostatic controller, loss of ventilation will also cause that equipment to automatically cut out.

The cutout relays, when operated to open all loop contactors, latch in this position. As soon as the operator attempts another acceleration, however, all cutout relays associated with equipments which are functioning properly are tripped out, the loop contactors on all but defective equipments close, and a normal acceleration is made. A cutout relay which remains latched in on a bad-order car also causes a light to burn continuously to assist in location of that car so that it can be readily removed from service for correction of the defect.

Overspeed and Braking Overload Protection

Protection against braking overloads is not desirable or necessary on a PCC car, because loss of dynamic braking due to the operation of an overload relay would mean complete loss of the principal braking means, and, in any case, the current is limited by the loss of adhesion of wheels to the rail.

In the case of multiple-unit compartment cars the loss of dynamic braking on one equipment in a train is not so serious; the greater weight per axle may cause overloads of greater magnitude before the wheels slide on the rail, and the operation of a whole train should not be handicapped by the possibility of repeated overload in one equipment. Accordingly, a braking overload relay is provided which operates to open the loop contactor if a braking current in excess of normal maximum value is obtained. The relay latches in position and may be reset only by hand. The circuits are arranged so that the loop contactor can reclose during each acceleration sequence, and the operation of the braking overload relay, therefore, does not result in loss of accelerating ability.

The possibility of running at sustained high speeds is much more prevalent in rapid-transit service than in street railway service. It is, therefore, desirable to limit (or at least to make less likely) the operation of a train above a predetermined speed which may be determined by any one of several factors. To this end a speed relay is provided which operates when a speed of 40 miles per hour is attained in acceleration. The relay is of the differential type, having a balanced beam attracted to one position by a coil which carries motor-exciting field current, and attracted to another position by a coil which is excited in proportion to motor armature voltage. See Figure 3. At the high motor speed and low current at which the relay is required to operate, the motor is unsaturated, and the ratio of armature voltage to field current is essentially proportional to speed. At the ratio corresponding to 40 miles per hour the relay operates to open the field-shunting contactors and thus reduce the motor tractive effort to a value below the train resistance.

Line Breakers and Overload Protection

The magnitude of fault currents that may occur on circuits connected to the third-rail supply requires extraordinary provisions for interruption of the currents. Line contactors designated *LB*1 and *LB*2 in Figure 3 are each identical in most details with the single line breaker used on PCC cars. They are magnetically operated, and each is provided with an independent overload tripping mechanism. This mechanism not only de-energizes the operating coil of the contactor but also mechanically forces the main contacts open at high speed. Two line contactors are used in series to provide a high factor of safety in interrupting power faults.

The overload mechanisms latch in operated position, but, since they may have been operated by a nonrepetitive occurrence such as an abrupt rise in supply voltage, they may be remotely reset by the operator momentarily closing a control switch provided for this purpose. Operation of an overload relay does not prevent obtaining normal dynamic braking.

Summary

Operating the series motors of a street railway car as series generators is a convenient way of providing electric braking, but, since the motors are self-excited while braking, they are very sensitive to changing speed and load conditions. This is particularly true of modern small high-speed motors which operate at relatively low-flux densities. Control systems are available and in use, however, which provide a smooth, responsive and flexible electric brake which has replaced the air brake as the principal means of braking modern cars in street railway service.

The use of such a control system with electric braking has been extended to multiple-unit rapid-transit cars by adapting the equipment of the PCC car to conform to the requirements of remote control without sacrifice in safety or reliability, and by providing special features in recognition of the distinctive operating conditions encountered in rapid-transit service.

Utilization Voltages

HOWARD P. SEELYE
MEMBER AIEE

UTILIZATION voltage is usually thought of in terms of the voltage rating of the lamps used, or of a nominal standard voltage announced by the company rendering service. It is probably quite generally understood that the voltage actually appearing at the outlets to which lamps or appliances are connected varies somewhat from time to time. What is not so well recognized, however, is the fact that the voltages delivered at customers' outlets on any power system are normally distributed in a characteristic pattern through a band of voltage of considerable width. It is the purpose of this paper to point out that this voltage band or spread is an inherent characteristic of the system, to discuss its source and its nature, and to indicate the connection between it and the design and rating of utilization equipment.

The voltage which will be treated in this discussion is the "utilization voltage" as distinguished from "service voltage." That is, it is the voltage appearing at wall outlets and lamp sockets rather than that which the utility supplies at the service entrance to the building. The difference between these voltages is the voltage drop in the building wiring, which is an appreciable amount, as will be shown.

While utilization equipment is built for various voltage classes, the major portion is applied at voltages in the 120-volt group. Attention will be given to these first, therefore, but mention will be made later of the special problems encountered with the higher voltages.

Voltage Spread

It would be preferable if the general conception of utilization voltage could be that of a band or spread of voltages between defined limits, rather than of a single voltage such as 115 volts or 120 volts. It is, of course, commonly accepted that a piece of commercial utilization equipment may be called upon to operate at other voltages than the rating stamped on its name plate. There seems to be a tendency, however, to consider such variations as deviations from a normal voltage rather than as themselves normal voltages within a normal voltage band. It should be clearly understood that on any ordinary distribution system such a voltage spread is inherent in its operation. At heavy-load periods, the voltage delivered at customers' outlets will, of necessity, be distributed throughout a band which is rarely less than 6 or 8 volts where close regulation is feasible, and may be as wide as 15 volts in some areas.

With reference to utilization equipment, this means that the equipment is expected to operate satisfactorily to the users through this band of voltage on any individual system, and through a still wider spread of voltage in general when the variations between systems are taken into account. Only a part of this equipment will actually operate regularly at its rated voltage, the rest being used as voltages ranging from the lowest to the highest limits of the spread. It follows, therefore, that, in the design of such equipment, the emphasis must be placed as far as possible on having good operating characteristics over a specified band of voltage rather than at some one voltage. In order that this voltage band be so placed as to be most advantageous to the users, it is essential that a generally accepted standard voltage spread for the industry be fixed and defined.

A considerable amount of work has been done by the transmission and distribution committee and the electrical equipment committee of the Edison Electric Institute during the past few years toward the establishment of standards of utilization voltage, particularly this voltage spread for satisfactory operation. Surveys were made of 45 operating companies to obtain data in regard to the actual operating voltages in use. The data from these surveys, and other information on this subject which has been presented to these committees, have been made available to the author for use in the following discussion.

The reasons for the normal voltage spread will first be described. The numerical order of existing spreads and their limits and the standards which have been proposed will then be discussed. Following that, there will be pointed out for some of the commoner types of equipment, the conditions of operation to which weight should be given in choosing their "best voltage."

Causes of Voltage Spread

There are, in general, three causes for the existence of a "spread" in utilization voltage. The first two pertain to the variation between different power systems throughout the country. The third is responsible for the deviations in any one system or circuit. All three should be given careful consideration in setting a voltage standard or an apparatus rating.

1. Lamp-Voltage Ratings

It is well known, of course, that lamps are made in several standard-voltage ratings, 110, 115, 120, 125, 130 volts, and so on. This and an even greater variety of ratings came into existence in the early growth of the industry. At first this was largely on account of the inability of the makers of carbon lamps to standardize their product to definite voltage ratings.

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Howard P. Seelye is senior engineer, engineering division, The Detroit Edison Company, Detroit, Mich.

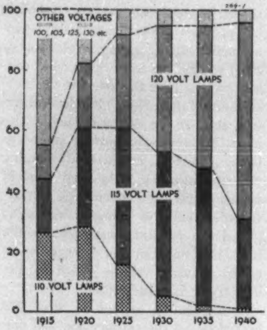

Figure 1. Proportion of incandescent-lamp sales, according to voltage rating

Later, as manufacturing control improved, it was continued on account of the same sort of voltage spread on operating systems which is being discussed here. As time went on there was a tendency toward concentration on a few definite ratings. This was facilitated by the fact that the negative resistance characteristic of tungsten lamps permitted a lamp to be used satisfactorily through a wider spread of applied voltage than was allowable with the positive characteristics of carbon lamps. Figure 1 shows how the lamp sales have varied in proportion to voltage rating since 1915. It is of interest that, whereas 115-volt lamps were in the majority from 1920 to 1930, they are now only 30 per cent of the total, while 120-volt lamps now constitute 65 per cent; 110-volt lamps have become a negligible factor; 125-volt and 130-volt lamps are still insignificant, being more or less special. It is evident, however, that there are a sufficient number of both 115- and 120-volt lamps in use to account for a spread of voltage between different systems which use them, even if the other ratings are not considered.

The shift from 115- to 120-volt lamps has been due to several causes, among them the desire to gain the advantages of higher voltage in distribution and the introduction in recent years of the 120/208-volt three-phase four-wire secondary network. Motor ratings have remained at 110 volts until recently when the standard for single-phase motors has been raised to 115 volts in the National Electrical Manufacturers' Association Standards and in the proposed revision of American Standards Association Standard *C*50. Other utilization appliances have been variously rated at voltages from 110 to 120.

It is believed desirable to discourage a future repetition of this shift in voltage preference to an eventual predominance of one of the higher-voltage lamps. There is now considerable confusion in the ratings and applications of both lamps and appliances. A continued rise in lamp voltages would add to the confusion, particularly in regard to the appliances. These must attempt to cover the whole field of voltages in use but cannot follow changes so readily, their useful life being much longer than that of lamps. The establishment of a standard voltage spread would tend to restrict further extension of that spread upward, unless the advantages were to become obvious and generally acceptable.

2. Relation of Rating to Outlet Voltage

In addition to the differences in lamp- (and system-) voltage ratings, there exists in the industry well founded differences of opinion in regard to the relation which the utilization voltage should bear to the rating of the lamp used. Figure 2 shows the characteristic variation in lamp lumens, energy consumption, and lamp life with variation in applied voltage. Some operators prefer to have the lamps operate as nearly as possible at their voltage rating. Others believe that the increased life resulting from voltages a little under rating is of more practicable value than the higher illumination, and shorter life which results from operation at rated voltage or higher. Others take the opposite view, that the increased lumens per watt at over-voltage operation are preferable. The question is basically one of economy, involving the relation between the cost of lamps and the cost of electrical energy. It must be considered, however, that the nuisance of too frequent lamp replacement may outweigh economy in the minds of the general run of customers.

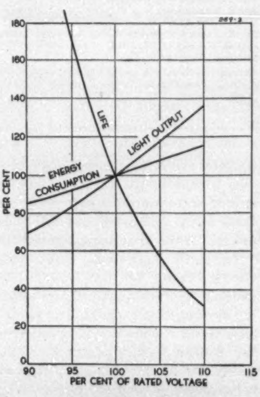

Figure 2. Variations in incandescent-lamp characteristics with voltage

These preferences must, of course, take into account the inherent voltage spread that exists on every system, which will be discussed in detail later. Where, for example, there is a 10 per cent total spread, and this is not uncommon, there will be some customers, even though a small proportion of the total, getting five per cent above the average. If this "average" is held at lamp rating, say 120 volts, these high customers will be getting 126 volts with a lamp life of 55 per cent. If the average is above rating, say 124 volts, the high customers will be getting 130 volts, with lamp life at 37 per cent. The performance of ordinary appliances, such as toasters, waffle irons, and the like, at such voltages must also be considered. Of course, where a smaller spread such as six per cent is feasible, an average of 124 would result in a high of only 127.6.

On the other hand, if the average voltage is low, the customers at the bottom of the spread may be getting inferior illumination and indifferent operation of appliances. It will not be attempted here to state the proper relation between rating and average operating voltage. It is merely pointed out that the voltage spread, including the top and bottom limits, should be taken into account as well as the mid-point or average. The differences in opinion in regard to this relation of lamp rating to voltage leads to a voltage difference even between systems using the same lamp rating. One such system will carry its voltage spread at a higher level than will another.

A confusion in system ratings also results from this source, since these ratings are generally 115-volt or 120-volt, according to the lamp rating used. Some so-called 115-volt systems, with average of outlet voltages above the lamp rating, actually have practically the same voltage spread and at the same voltage level as other systems normally designated at 120 volts but having average outlet voltages below the lamp rating. System rating is, therefore, not a definite indication of the existing utilization voltage.

3. Inherent Voltage Spread in Distribution System

The normal, inherent spread of utilization voltage on an operating system is probably the least understood of the three causes of voltage deviation. Its basis is simple, lying in the elementary fact that when electric current passes over a wire, there is a corresponding drop in voltage. The energy delivered to customers on a power system passes over many miles of wire in

(*a*) Transmission.

(*b*) Step-up and step-down transformers.

(*c*) Distribution circuit feeders and primaries.

(*d*) Distribution transformers.

(*e*) Distribution secondaries and service drops.

(*f*) Customer's house wiring.

The voltage drop in transmission is usually controlled to considerable extent by variation of generator voltage, transformer taps, and regulators. Since any of these can regulate in bulk only at one point, there will be a variation in the voltage between that regulating point and other points on the system at which load is supplied. This variation can be compensated for at the substation by voltage regulators on the substation bus or on individual distribution circuit feeders.

These substation regulators and supplementary regulators and boosters out on the circuits, can also compensate for *part* of the drop in the outgoing distribution circuits. A fairly constant voltage can be held at *some point* out on such a circuit. Since the circuit may consist of several miles of line, however, it is impossible to have the voltage constant at all points. Some customers will be connected near the regulated point and some far from it. Some will be connected through heavily loaded distribution transformers and some through transformers only lightly loaded. The impedance of the transformers themselves will vary with size, type, and make. Some customers on a secondary main will be near the transformer and some will be at the end of long secondaries. Figure 3 is a schematic diagram indicating these conditions. The result is that, even though the voltage at the circuit feeding point is held practically constant throughout the day and night, utilization voltage at the customer's outlets will vary through a band which is of appreciable width.

In Figure 3 the station bus is, for convenience, assumed constant at 122 volts at all loads. The circuit regulator is compensated to hold 122 volts at an intermediate point on the primary mains, indicated by the middle distribution transformer (*C*). The voltage drops indicated for primaries, transformers, secondaries, and service and house wiring are typical, rather than extreme cases, of those in ordinary practice. It is evident that customers' outlet voltages at time of heavy load will be distributed through a band whose maximum is about 122 volts at customer (*A*), and whose minimum is something like 112 volts at customer (*B*). At light-load periods, the voltages lie in a much narrower band, of perhaps 119 to 121, while at no load, which seldom occurs, all would be 122, the voltage at the regulating point (*C*).

Table I

	Maximum Voltage Drop, Per Cent	
	Urban Customers	Rural Customers
In primaries	1.3 to 3.5	3.3 to 10.0
In distribution transformers	2.5 to 4.65	2.0 to 4.4
In secondaries	2.0 to 4.5	No secondaries
In service drops	0.5 to 1.25	0.5 to 1.8
In customer's building wiring	1.0 to 5.0	1.0 to 2.0
Total utilization-voltage spread	7.5 to 12.0	6.87to15.0

There are many sorts of distribution circuits, ranging from those in concentrated urban areas where the distances are short, and regulation can be reasonably close, to rural lines with long distances and correspondingly poorer regulation. In many of the latter, the desirability and practicability of giving reasonably good regulation, similar to that in more densely built suburban territory, is recognized, but, on the other hand, there are others where regulating equipment is omitted in the interest of lower first cost. Also, there are various types of circuits, such as networks, ring-main, and feeder and branch. In any case, however, there will be an unavoidable spread in the delivered utilization voltage similar to that indicated in Figure 3. It may, of course, be considerably less in some areas than the amount shown here (networks for example); and in others it will be greater. It will be located at several different voltage levels on different systems.

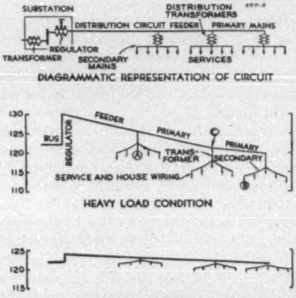

Figure 3. Diagrammatic illustration of voltage spread on a typical distribution circuit

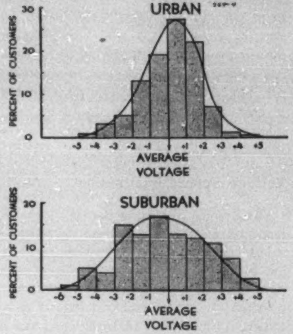

Figure 4. Distribution of customers' outlet voltage

From actual sampling survey of a large system

A survey of 14 companies made by the transmission and distribution committee, Edison Electric Institute, produced results which shed some light on the magnitude of the various elements in this voltage spread. The figures in Table I show the range of values reported. Since these figures were mostly based on design values rather than field measurements, they should be considered only as indicative.

Pattern of Voltage Spread

The outlet voltages on a system are, of course, not distributed uniformly over the voltage spread. The pattern of this distribution is shown in Figure 4. The blocked graphs were obtained from a sampling survey on a large diversified system. While the urban graph is somewhat different from the suburban, their general shape is similar as shown by the smooth, generalized curves. This pattern has been confirmed in essentials by data from several companies and is believed to be typical.

It is evident that comparatively few customers get either the highest or the lowest voltage in the spread, but on the other hand, there are appreciable numbers getting a volt or two higher than the minimum and a volt or two lower than the maximum. The largest number getting any one voltage is about 27 per cent in the urban curve and only about 17 per cent in the suburban.

The average voltage is determined by the shape of the curve and this may vary somewhat between different systems.

Field surveys in several companies have shown, however, that the average as in Figure 4 is near the mid-point, or in some cases even a little higher, up to about 60 per cent of the spread. This distribution of customers' voltage is useful in considering the range for "best operation" of equipment.

Voltage Spread in Practice

Surveys of the industry in general, made by the EEI committees, have indicated existing values for voltage spreads and other related quantities. The surveys covered 45 companies, serving approximately ten million residential customers. Since most of these companies did not have available figures on actual field surveys of voltage conditions, design values were reported. Some allowance should perhaps be made for discrepancy between these and voltages which really exist.

No extensive field data on the voltage drop in building wiring is available. Some estimates are given in Table I. A sampling survey on the Detroit Edison system covering 300 customers, mostly residential, showed a range from about $^{1}/_{2}$ volt to 5 volts, with an average of about $1^{1}/_{4}$ volts to lamps in the most used room under evening load conditions. There are, of course, some outlets which have less drop than these, and there will be some cases with more. It is believed that a 3-volt drop will cover the maximum for the great majority of customers.

If 3 volts is added to the spread at service entrance resulting from the survey as listed above the following deductions can be drawn:

1. The maximum voltage spread at *utilization outlets* for the industry as a whole is of the order of 20 volts, from 107 volts (110−3) to 127 volts.

2. This whole spread will not occur ordinarily in any one system but takes account also of the variation between systems which has been previously discussed. It is the spread for which utilization equipment in general must be designed.

3. There will no doubt be some outlets outside of this spread, both above the upper limit and below the lower limit. It is believed, however, that the percentage of these will be very small, and that they may be considered as unusual or temporary conditions, for which operation of equipment cannot be expected to be as good as it should be within the normal range.

4. The greater bulk of customers have outlet voltages within the band between 110 (113−3) and 125 volts.

5. On individual systems the voltage spread at utilization outlets ranges from 7 volts to 18 volts, with nearly two thirds lying between 10 and 13 volts, very few above that, and the remaining one third lying between 7 and 9 volts.

Table II. Service Voltage

Drop in building wiring should be deducted

Minimum Service Voltage Reported	110 volts
Maximum Service Voltage Reported	127 volts
Minimum Service voltage for companies serving 77 per cent of the total customers	113 volts
Maximum Service voltage for companies serving 80 per cent of the total customers	125 volts
Voltage spread at service	
Minimum reported	4 volts
Maximum reported	15 volts
Classification of service voltage spreads	
For companies reporting 33 per cent of total customers	4 to 6 volts
For companies reporting 62 per cent of total customers	7 to 10 volts
For companies reporting 5 per cent of total customers	11 to 15 volts

It should be clearly understood that these figures refer to voltage spreads existing on systems as a whole and are not intended to refer to the voltage variations at any individual customer's service. The voltage at such a customer's service will vary from his voltage at heavy-load period to that at light-load. Referring to Figure 3, in the case there represented the maximum individual variation occurs at the customer's service having the lowest heavy-load voltage and is equal to nearly the full spread of some 10 volts. The minimum is at the customer's service having the highest heavy-load voltage and is very small. Referring to the customer distribution curve of Figure 4, it is evident that few customers have the maximum or minimum variation. Most of them get more nearly the average, which in the case of Figure 3 would be about 5 volts.

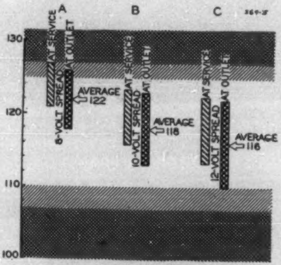

Figure 5. Typical voltage spreads within the proposed standard

A—Urban service, closely regulated, 120-volt lamps

B—Urban service, general, 120-volt lamps

C—Rural service, 115-volt lamps

The figures which have been cited lead to a suggestion of a general standard maximum utilization voltage spread of 107 to 127 volts, with a *preferred* spread of 110 to 125 volts. Figure 5 illustrates how several different typical service conditions can fall within this spread. There are, of course, many other variations which can and will be used on operating systems and still lie within the total proposed spread.

Higher Voltages

The discussion so far has referred only to voltages in the 120-volt group. The same problem exists, however, with the higher utilization voltages, 240, 480, 2,400, and so on. In the 240-volt group, the complication of the "odd-ratio" system is introduced. Network distribution in dense urban territory is now commonly four-wire, at 120/208 volts. The voltage supplied to 240-volt equipment on such a system is only 87 per cent of what it would be on a 120/240-volt even-ratio system, with the same voltage for the 120-volt equipment in both cases.

For the most part, the same standard 240-volt equipment has been used satisfactorily on these odd-ratio systems as on those of even-ratio. Special 208-volt motors are available but have not been widely adopted. This practice has been allowable, largely because the odd-ratio systems usually are found in densely loaded areas where service voltage tends to be relatively high and the voltage spread relatively small, making the minimum voltage high enough for 240-volt equipment. There are several advantages in having one standard line of apparatus so designed as to serve both purposes if feasible. Manufacturing and stocking are obviously simpler for one line than for two; interchangeability is provided between locations where service voltage is odd-ratio and locations where it is even-ratio.

In setting up a standard voltage spread for the industry, it is desirable that it be broad enough to be adequate for both even-ratio and odd-ratio systems, and yet not too broad to allow one line of apparatus to cover it. Taking into consideration the probability that the voltage drop to 240-volt equipment would be somewhat greater than to that at 120 volts, and that the service voltage in general to 208-volt services would be relatively high and well-regulated, a spread of 193 to 250 volts maximum, 197 to 245 volts *preferred* has been recommended.

At the still higher voltages, 480, 600, etc., similar problems of "odd-ratios" are also found, and it is probable that the voltage spreads for these levels should be multiples of those at the 240-volt level.

Equipment Design

One of the most important objectives in trying to establish standard voltage spreads is the simplification of the design and rating of utilization equipment. If the voltage spread which must be met in practice is fixed between reasonable limits, there will be more assurance that equipment which operates satisfactorily within those limits will be generally satisfactory for the industry. Also, as further knowledge is gained of the characteristics of service voltage within those limits, the designs of specific items of apparatus can be more definitely directed toward the actual voltages at which they will most commonly be operated.

This paper has pointed out the fact that a voltage spread of from 7 to 15 volts is a normal condition on an operating system. The utilization equipment supplied should be expected to operate at any voltage throughout that spread, with satisfactory results to the user. If and when a standard spread for the industry, such as that which has been suggested, is generally adopted, it should be expected that equipment sold would operate satisfactorily *throughout* that spread. Since the bulk of the customers will have voltages within a somewhat smaller spread than the maximum, the preferred voltage spread indicates the range through which *good characteristics* of operation should be maintained. On the other hand, it should be kept in mind that there will be occasional customers on any system where voltage will fall somewhat above or below a recognized spread, and their equipment should still operate, even if not with characteristics which would be considered generally satisfactory.

It would be desirable, if practicable, to eliminate all the variety of voltage ratings on different equipment, reducing them to a uniform rating of, perhaps, the nominal 120-volt designation, with the understanding that they would meet the requirements of the standard-voltage spread. It is realized that there are difficulties in carrying out such a simplification, one of them being the different ideas which are prevalent concerning the proper relation between lamp rating and average voltage supplied, or in other words, between lamp life and illumination. Another is the probability that all types of equipment are not adaptable to as wide a range of voltage as the spreads proposed. These considerations will probably require the retention of more than one rating for some equipment. Other items, such as motors, are probably already pretty well adapted to such a standard range and could be uniformly rated accordingly.

While it is not the province of this paper to attempt to specify the best voltage for which any piece of equipment shall be designed, some of the considerations which could affect such a choice within a standard voltage spread will be suggested.

LAMPS

There is probably a distinct need for both "long-life" lamps, and for "high-illumination" lamps. The former would be used where efficient illumination is not the prime requirement, such as for low-wattage lamps for indicating or ornamental purposes. The design voltage should be high in relation to the voltage spread so that they would be operated at undervoltage most of the time. "Long-life" lamps in larger sizes would also be preferred for general use by some operators. For this purpose, however, the design should give somewhat more regard to illumination and less to extreme length of life. "High-illumination" lamps would have a demand where illumination intensity is closely measured, and the application is strictly on a "lumens-per-dollar" basis. The design voltage should be nearer the middle of the spread or below it, giving "normal" voltage or "over-voltage" operation to the majority of lamps.

FLUORESCENT LAMPS

A recent publication states, "The lamps with 'low'-voltage ballast equipment are designed for operation on circuit voltages of from 110 to 125 volts inclusive and, in some cases, may operate satisfactorily on circuits as low as 105 or as high as 130 volts."

These figures correspond well with those of the proposed standard-voltage spread.

ELECTRIC-RANGE ELEMENTS

Ordinarily the voltage drop to a major appliance, such as a range, will be fairly large, due to the current it draws. Much of its operation will be during hours which are off-peak for the lighting load, but it will also have to operate on peak. Its maximum, will, therefore be several volts lower than the top of the voltage spread and its minimum near the bottom of it. The suggested standards, published in 1938 by a joint EEI–NEMA committee, have a "preferred standard rating" of 115–120 volts, based on a design voltage of 118 volts, with a maximum operating voltage of 124 volts. There are, in addition, two optional ratings, one at 125 volts with maximum operating voltage of 129 volts, and the other at 110 volts with a 116-volt maximum. The preferred rating corresponds fairly well with the proposed 110–125-volt preferred spread.

TOASTERS, WAFFLE IRONS, FLATIRONS, AND SIMILAR APPLIANCES

These produce a considerable voltage drop, due to their own relatively large current, but operate mostly offpeak, when supply voltage is relatively high. Their maximum will, therefore, be several volts below the top of the voltage spread and their minimum several volts above the bottom of it.

REFRIGERATOR MOTORS, OIL-BURNER MOTORS, AND SIMILAR MOTORS

These must operate throughout the day and hence throughout the voltage spread, but most of the operation during 24 hours will be at voltages in the top part of the spread, since such voltages exist during the greater number of hours.

It is not intended to infer that the existence of normal voltage spread or of the factors involved in "best voltage" has been unknown to the makers of utilization equipment or has been ignored by them. It is believed, however, that the material which has been presented here will be helpful to both makers and users of such equipment in promoting a better understanding of the nature of the voltage spread and the related numerical values of utilization voltage.

Current Loci for the Capacitor Motor

THOMAS C. McFARLAND
MEMBER AIEE

THE basic theory of the capacitor motor from the cross-field point of view has been recently presented by Puchstein and Lloyd.[1] It is the purpose of this paper to extend the basic relations developed by them so as to demonstrate certain current-loci characteristics of the capacitor motor.

The circuit to be considered is shown in Figure 1. A variable condenser of impedance Z_C is connected in series with the auxiliary winding. The rotor squirrel-cage winding is considered equivalent to a d-c winding with the commutator brushes arranged to short circuit the winding along the main and auxiliary axes of the stator winding. A common voltage is impressed on the two windings.

The flux relations are illustrated in Figure 2. There is a mutual flux linking the stator and rotor windings along each axis of the machine. Considered as a transformer there are leakage fluxes linking with each of the stator windings, and also leakage fluxes which link the rotor winding on the main and auxiliary axes. Each of these fluxes is considered to be stationary in space and variable in time. As a consequence the mutual fluxes and rotor-leakage fluxes are cut by the rotor conductors as they rotate.

Current and voltage relations are indicated by the vector diagrams of Figure 3. The vector diagram for the auxiliary axis is drawn so that the mutual flux ϕ_{mb} is in space quadrature with the main-axis mutual flux ϕ_{ma}, and is lagging. This makes it possible to represent each of the speed voltages in its proper phase relation. In each stator winding there is a local-impedance drop and a component of impressed voltage equal and opposite to the rotor-induced voltage, assuming a ratio of transformation of unity for each axis. In each rotor circuit there is an induced voltage, a local-impedance drop, a generated voltage due to cutting the mutual flux of the other axis, and a generated voltage resulting from cutting the rotor-leakage flux of the other axis. Applying Kirchoff's law of voltages to each circuit on the main and auxiliary axes the following equations can be written:

Main Axis (Figure 3a)

(Stator) $$V=I_aZ_a+(I_a-I_{2a})Z_{ma} \tag{1}$$

(Rotor) $$O=-(I_a-I_{2a})Z_{ma}+I_{2a}Z_2+ jS\left[-\frac{Z_{mb}}{k}(I_b-I_{2b})+jkX_2I_{2b}\right] \tag{2}$$

Auxiliary Axis (Figure 3b)

(Stator) $$V=I_b(Z_b+Z_c)+(I_b-I_{2b})Z_{mb} \tag{3}$$

(Rotor) $$O=-(I_b-I_{2b})\frac{Z_{mb}}{k}+kZ_2I_{2b}- jS[-Z_{ma}(I_a-I_{2a})+jX_2I_{2a}] \tag{4}$$

wherein

V = applied or line voltage
I_a = current in main stator winding
I_b = current in auxiliary stator winding
I_{2a} = rotor current in main-axis circuit
I_{2b} = rotor current in auxiliary-axis circuit
$Z_a=r_a+jX_a$ = local impedance of main stator winding
$Z_b=r_b+jX_b$ = local impedance of auxiliary stator winding
$Z_{ma}=jX_{ma}$ = mutual inductive impedance of main-axis stator and rotor windings
$Z_{mb}=jX_{mb}$ = mutual inductive impedance of auxiliary-axis stator and rotor windings
$Z_2=r_2+jX_2$ = local impedance of each rotor circuit
$Z_c=r_c-jX_c$ = impedance of capacitance in series with auxiliary winding
k = ratio of auxiliary-winding turns to main-winding turns
S = speed as a decimal fraction of synchronous speed

From equations 1 and 3

$$\left.\begin{aligned} I_{2a}&=\frac{I_a(Z_a+Z_{ma})-V}{Z_{ma}}\\ I_{2b}&=\frac{I_b(Z_b+Z_c+Z_{mb})-V}{Z_{mb}}\end{aligned}\right\} \tag{5}$$

Substituting these relations into equations 2 and 4 find

$$\left.\begin{aligned} A_aI_a+B_aI_b&=K_aV\\ A_bI_a+B_bI_b&=K_bV\end{aligned}\right\} \tag{6}$$

wherein,

$$A_a=Z_a+Z_b+\frac{Z_aZ_2}{Z_{ma}} \tag{7}$$

$$A_b=S\left[X_2\left(1+\frac{Z_a}{Z_{ma}}\right)-jZ_a\right] \tag{8}$$

$$B_a=S\Big[(Z_b+Z_c)\left(j\frac{1}{k}-k\frac{X_2}{Z_{mb}}\right)-kX_2 \tag{9}$$

$$B_b=kZ_2+(Z_b+Z_c)\left(\frac{1}{k}+k\frac{Z_2}{Z_{mb}}\right) \tag{10}$$

$$K_a=1+\frac{Z_2}{Z_{ma}}+S\left(j\frac{1}{k}-k\frac{X_2}{Z_{mb}}\right) \tag{11}$$

$$K_b=S\left(-j+\frac{X_2}{Z_{ma}}\right)+\left(\frac{1}{k}+k\frac{Z_2}{Z_{mb}}\right) \tag{12}$$

In each of these equations the complex expressions for the impedances must be substituted (see nomenclature). When the substitutions have been made find

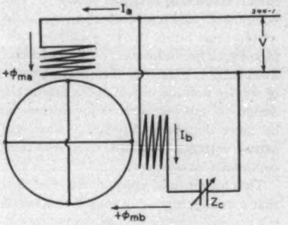

Figure 1. The circuit

$$A_a=\left(r_a+r_2+\frac{r_aX_2+r_2X_a}{X_{ma}}\right)+ j\left(X_a+X_2+\frac{X_aX_2-r_ar_2}{X_{ma}}\right) \tag{13}$$

$$A_b=S\left[X_2+X_a\left(1+\frac{X_2}{X_{ma}}\right)\right]- jSr_a\left(1+\frac{X_2}{X_{ma}}\right) \tag{14}$$

$$B_a=-S\left[kX_2+(X_b-X_c)\left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)\right]+ jS(r_b+r_c)\left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right) \tag{15}$$

$$B_b=\left[kr_2\left(1+\frac{X_b-X_c}{X_{mb}}\right)+(r_b+r_c)\times \left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)+j\left[kX_2+(X_b-X_c)\times \left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)-\frac{kr_2}{X_{mb}}(r_b+r_c)\right]\right. \tag{16}$$

$$K_a=\left(1+\frac{X_2}{X_{ma}}\right)+j\left(\frac{S}{k}+\frac{kSX_2}{X_{mb}}-\frac{r_2}{X_{ma}}\right) \tag{17}$$

$$K_b=\left(\frac{1}{k}+\frac{kX_2}{X_{mb}}\right)-j\left[\frac{kr_2}{X_{mb}}+S\left(1+\frac{X_2}{X_{ma}}\right)\right] \tag{18}$$

Solving the equations of (6) simultaneously,

$$\left.\begin{aligned} I_a&=\frac{B_bK_a-B_aK_b}{A_aB_b-A_bB_a}\cdot V\\ I_b&=\frac{A_aK_b-A_bK_a}{A_aB_b-A_bB_a}\cdot V\end{aligned}\right\} \tag{19}$$

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THOMAS C. McFARLAND is associate professor of electrical engineering, University of California, Berkeley, Calif.

Substitution in the equations of 19 from equations 13–18 inclusive yields

$$\left.\begin{aligned} I_a&=\frac{M+jN}{U+jW}\cdot V\\ I_b&=\frac{P+jQ}{U+jW}\cdot V\end{aligned}\right\} \qquad (20)$$

wherein,

$$M=\frac{1}{k}\left[(r_b+r_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)+\frac{r_2(X_b-X_c)}{X_{ma}}\right]+k\left[r_2\left(1+\frac{2X_2}{X_{ma}}\right)+\right.$$
$$\left.\frac{X_2}{X_{mb}}(r_b+r_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)+\frac{r_2}{X_{mb}}(X_b-X_c)\left(1+\frac{2X_2}{X_{ma}}\right)-\frac{(r_b+r_c)r_2^2}{X_{ma}X_{mb}}\right] \qquad (21)$$

$$N=\frac{1}{k}\left[(X_b-X_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)-\frac{r_2(r_b+r_c)}{X_{ma}}\right]+Sr_2+k\left[X_2\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)-\frac{r_2^2}{X_{ma}}-\right.$$
$$\left.\frac{r_2}{X_{mb}}(r_b+r_c)\left(1+\frac{2X_2}{X_{ma}}\right)+\frac{X_2}{X_{mb}}(X_b-X_c)\left(1+\frac{X_2}{X_{ma}}\right)(1-S^2)-\frac{(X_b-X_c)r_2^2}{X_{ma}X_{mb}}\right] \qquad (22)$$

$$P=\frac{1}{k}\left[r_2+r_a(1-S^2)+\frac{r_2X_a+r_aX_2(1-S^2)}{X_{ma}}\right]+k\left[\frac{r_aX_2(1-S^2)+r_2(X_a+2X_2)}{X_{mb}}+\right.$$
$$\left.\frac{r_aX_2^2(1-S^2)+r_2(2X_aX_2-r_ar_2)}{X_{ma}X_{mb}}\right] \qquad (23)$$

$$Q=\frac{1}{k}\left[(X_a+X_2)(1-S^2)+\frac{X_aX_2(1-S^2)-r_ar_2}{X_{ma}}\right]-Sr_2+$$
$$k\left[\frac{X_2(X_a+X_2)(1-S^2)-r_2(r_a+r_2)}{X_{mb}}+\frac{X_aX_2^2(1-S^2)-r_2(2r_aX_2-r_2X_a)}{X_{ma}\,X_{mb}}\right] \qquad (24)$$

$$U=\frac{1}{k}\left[(r_b+r_c)\left\{r_2+r_a(1-S^2)+\frac{r_2X_a+r_a(X_2-S^2)}{X_{ma}}\right\}+\right.$$
$$\left.(X_b-X_c)\left\{\frac{r_ar_2-X_a(X_2-S^2)}{X_{ma}}-(X_a+X_2)(1-S^2)\right\}\right]+$$
$$k\left[r_2(r_a+r_2)-X_2(X_a+X_2)(1-S^2)+\frac{r_2(r_2X_a+2r_aX_2)-X_aX_2(X_2-S^2)}{X_{ma}}+\right.$$
$$(r_b+r_c)\left\{\frac{r_aX_2(1-S^2)+r_2(X_a+2X_2)}{X_{mb}}+\frac{r_2(2X_2X_a-r_ar_2)+r_aX_2(X_2-S^2)}{X_{ma}X_{mb}}\right\}+$$
$$\left.(X_b-X_c)\left\{\frac{r_2(r_a+r_2)-X_2(X_a+X_2)(1-S^2)}{X_{mb}}+\frac{r_2(r_2X_a+2r_aX_2)-X_aX_2(X_2-S^2)}{X_{ma}X_{mb}}\right\}\right] \qquad (25)$$

$$W=\frac{1}{k}\left[(r_b+r_c)\left\{(X_a+X_2)(1-S^2)+\frac{X_a(X_2-S^2)-r_ar_2}{X_{ma}}\right\}+\right.$$
$$\left.(X_b-X_c)\left\{r_2+r_a(1-S^2)+\frac{r_2X_a-r_a(X_2-S^2)}{X_{ma}}\right\}\right]+$$
$$k\left[r_2(X_a+2X_2)+r_aX_2(1-S^2)-\frac{r_2(r_ar_2-2X_aX_2)-r_aX_2(X_2-S^2)}{X_{ma}}+\right.$$
$$(r_b+r_c)\left\{\frac{X_2(X_a+X_2)(1-S^2)-r_2(r_a+r_2)}{X_{mb}}+\frac{X_aX_2(X_2-S^2)-r_2(r_2X_a+2r_aX_2)}{X_{ma}X_{mb}}\right\}+$$
$$\left.(X_b-X_c)\left\{\frac{r_2(X_a+2X_2)+r_aX_2(1-S^2)}{X_{mb}}+\frac{r_aX_2(X_2-S^2)+r_2(2X_aX_2-r_ar_2)}{X_{ma}X_{mb}}\right\}\right] \qquad (26)$$

Substitution of the complex expressions for the impedances in the equations of 5 results in

$$\left.\begin{aligned} I_{1a}&=I_a\left[1+\frac{X_a}{X_{ma}}\right]+j\frac{V-I_ar_a}{X_{ma}}\\ I_{1b}&=I_b\left[1+\frac{X_b-X_c}{X_{mb}}\right]+j\frac{V-I_b(r_b+r_c)}{X_{mb}}\end{aligned}\right\} \qquad (27)$$

The equations of 20 and 27 are better for purposes of extended computation than are those of 19 and 5 because they require a minimum number of operations involving complex numbers. Although most engineers can handle the complex algebra without difficulty, it is generally admitted that the chances of error are reduced if the equations are such as to minimize the number of manipulations requiring complex algebra.

To illustrate the application of the preceding relations, computations have been made for the four-pole one-fourth-horsepower 110-volt single-phase motor for which constants are given in Morrill's[2] paper of April 1929. In Morrill's paper corresponding values of resistance and reactance for the capacitive impedance are given for only two different values of impedance. Here the interpolation curve of Figure 4, which includes the two values, is assumed to represent the variable capacitance used. Other values of the constants for this machine, as recorded by Puchstein and Lloyd,[1] are

$r_a=2.02$ (ohms) $\quad X_a=2.79$ (ohms)
$r_b=7.13$ " $\quad X_b=3.22$ "
$r_2=4.12$ " $\quad X_2=2.12$ "
$X_{ma}=66.8$ (ohms)
$X_{mb}=92.9$ "
$k=1.18$ "

Substitution of these constants into the equations 21–26 inclusive yields

$$\left.\begin{aligned} M&=12.17+0.912r_c-0.109X_c-\\ &\qquad S^2(6.589+0.914r_c)\\ N&=4.425-0.109r_c-0.903X_c+\\ &\qquad 4.12S+S^2(0.906X_c-5.448)\\ P&=5.853-1.826S^2\\ Q&=4.167-4.12S-4.38S^2\\ U&=47.0+5.88r_c+3.95X_c+\\ &\qquad S^2(13.52-1.795r_c-4.333X_c)\\ W&=86.2+3.93r_c-5.73X_c-\\ &\qquad S^2(41.68+4.34r_c-1.79X_c\end{aligned}\right\} \qquad (28)$$

By using these relations in conjunction with the data of Figure 4, computations were made for several values of S. The results of these computations are shown in Figures 5, 6, and 7. It is seen that P and Q are independent of the capacitance, and plot as linear functions of speed over the operating range of speeds. The constants M, N, U and W are seen to be approximately linear functions of the capacity reactance for all speeds over the operating range of capacitances. The curves may be extrapolated linearly. For the practical range of operation P and U are always positive and M, N, Q and W are always negative.

For several constant values of S computations of I_a and I_b were made as functions of X_c using the equations of 20. A plot of these values is shown in Figures 8 and 9. It will be observed that

(*a*) The locus of each current for constant S is a circle.
(*b*) The locus of each current for constant X_c is a circle.
(*c*) The centers of each circular locus lie on the arc of a circle.
(*d*) For I_a the locus of centers of circles of constant S passes through the blocked rotor value of current (Figure 8).
(*e*) For I_b the locus of centers of circles of constant X_c passes through the origin of co-ordinates (Figure 9).
(*f*) For the loci of I_a with constant S the triangles ABC remain similar. A is the blocked-rotor point, B is the extremity of a diameter which passes through A, and C is a point on the locus corresponding to a fixed value of X_c.
(*g*) For the loci of I_b with constant S, the angle δ between a line joining the point on the locus corresponding to a fixed value of X_c to the origin and the diameter which passes through the origin is constant.
(*h*) Over the whole range of speeds ($S=1.0$ to $S=0.0$) the radius of a circular locus of constant S for I_a is very closely equal to S^2

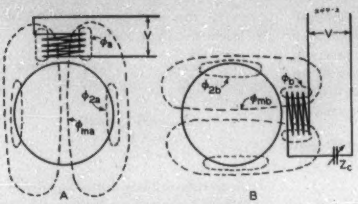
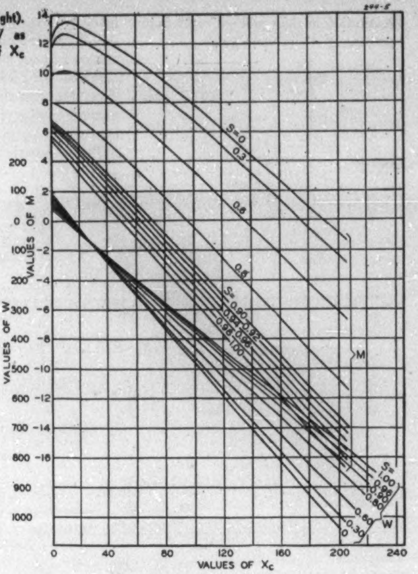

244-2
V
ϕ_a
ϕ_{2a}
ϕ_{ma}
A
V
ϕ_{2b}
ϕ_{mb}
ϕ_b
Z_c
B

Figure 2. The flux relations

A—Main-winding fluxes
B—Auxiliary-winding fluxes

Figure 5 (right). M and W as functions of X_c



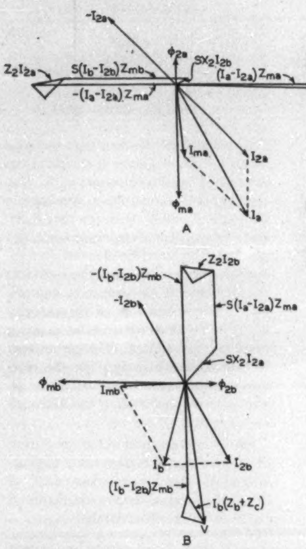

244-3
$-I_{2a}$
ϕ_{2a}
SX_2I_{2b}
Z_2I_{2a}
$S(I_b-I_{2b})Z_{mb}$
$(I_a-I_{2a})Z_{ma}$
I_aZ_a
V
$-(I_a-I_{2a})Z_{ma}$
I_{ma}
I_{2a}
ϕ_{ma}
I_a
A
Z_2I_{2b}
$-(I_b-I_{2b})Z_{mb}$
$-I_{2b}$
$S(I_a-I_{2a})Z_{ma}$
SX_2I_{2a}
ϕ_{mb}
I_{mb}
ϕ_{2b}
I_b
I_{2b}
$(I_b-I_{2b})Z_{mb}$
$I_b(Z_b+Z_c)$
V
B

Figure 3 (left). Vector diagrams

A—Main axis
B—Auxiliary axis

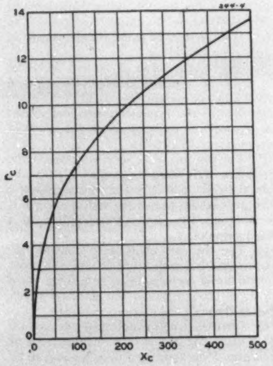


Figure 4 (left). Relation between X_c and r_c

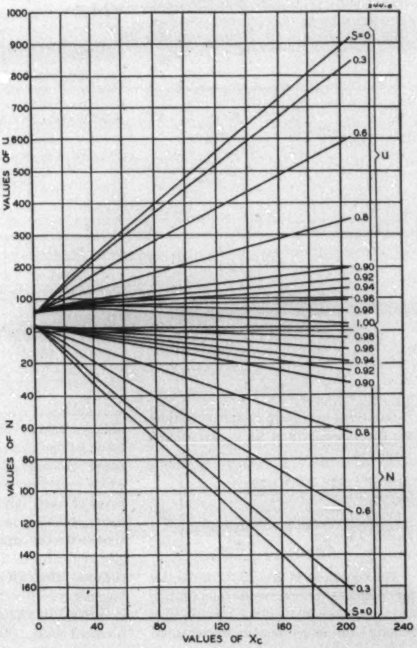


Figure 6 (right). N and U as functions of X_c

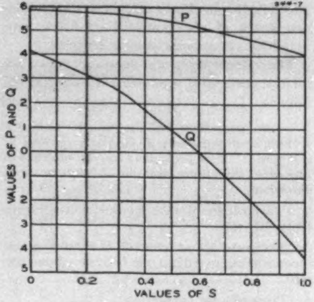


Figure 7. P and Q as functions of S

times the radius of the circle for $S=1$. For I_b the radius is approximately S^3 times the radius of the circle for $S=1$, over the operating range of speeds ($S=1.0$ to $S=0.9$).

As a consequence of these observations the following procedure is suggested for the determination of loci:

(*a*) Compute constants M, N, P, Q, U and W for three widely separated values of X_c, such as $X_c=20$, $X_c=40$, and $X_c=200$, at speeds of $S=1$, $S=0.8$, and $S=0$.

(*b*) Using the constants thus determined find corresponding values of I_a and Ib (any one computation for I_a with $S=0$ will suffice).

(*c*) Plot the computed currents as shown in Figure 8 and Figure 9.

(*d*) Using perpendicular bisectors locate the centers of circles for $S=1$, $S=0.8$ and $S=0$.

(*e*) Using perpendicular bisectors locate the center of the circular arc passing through the centers of circular loci found in (*d*), and draw the arc.

(*f*) On the plot of I_a draw the locus for $S=1$, and then draw the diameter of this circle which, if extended, passes through the current value for $S=0$.

(*g*) Connect the end of the diameter thus drawn, and the blocked-rotor point, to the point on the locus corresponding to the largest value of X_c, and determine the angle β.

(*h*) On the plot of I_b draw the locus for $S=1$, and the diameter which passes through the origin.

(*i*) Measure the angle δ between this diameter and the vector I_b corresponding to the largest value of X_c.

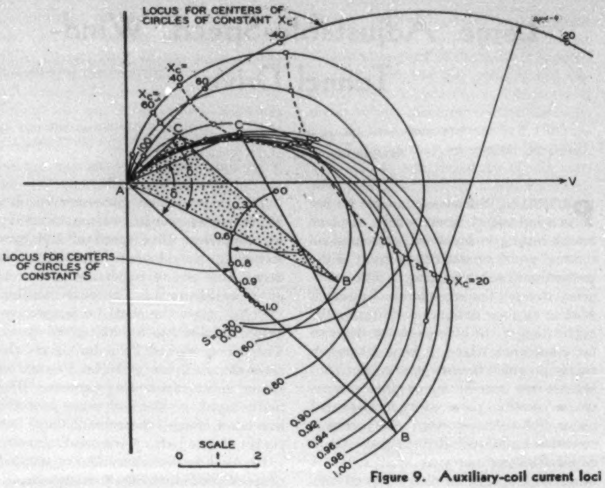

LOCUS FOR CENTERS OF CIRCLES OF CONSTANT X_c
40
20
X_c= 40
60
X_c= 60
100
C
C
A
δ
δ
V
0.3
0.6
0.8
0.9
1.0
LOCUS FOR CENTERS OF CIRCLES OF CONSTANT S
B
B
X_c=20
S=0.30
0.00
0.60
0.80
0.90
0.92
0.94
0.96
0.98
1.00
SCALE
0 1 2

Figure 9. Auxiliary-coil current loci

(*j*) For I_a compute the radii of other circles of constant S between $S=1$ and $S=0.9$ as S^2 times the radius for $S=1$. For I_b use S^3 intead of S^2.

(*k*) By trial find the centers of these circles subject to the conditions that the centers lie on the locus of centers, and triangles ABC remain similar. (The first condition is sufficient for I_b.) Draw the circles.

(*l*) From the loci for $S=1$ determine and plot β and δ as functions of X_c. (Three points are sufficient to determine the curve if it is borne in mind that β changes very slowly with large values of X_c.) From this curve any desired value of X_c on the loci can be found, and any locus of I_a and I_b for constant X_c is readily determined.

(*m*) For each of the values of I_a and I_b found in item (*b*) compute corresponding values of I_{2a} and I_{2b} by the equations of 27.

(*n*) Plot the values of I_{2a} and I_{2b}. The loci are similar to those of I_a and I_b, respectively, but displaced from them by the

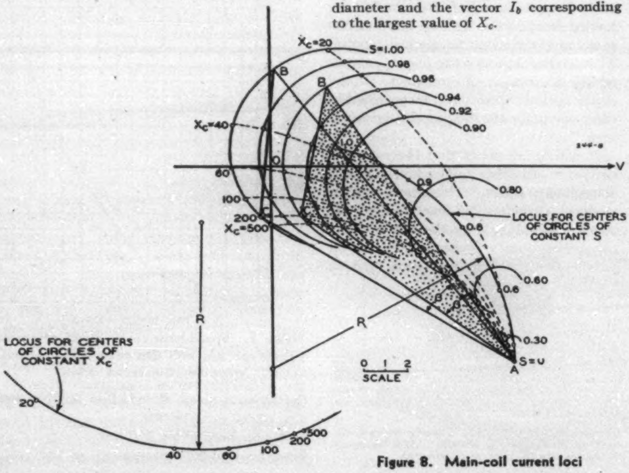

X_c=20
S=1.00
0.98
0.96
0.94
0.92
0.90
B
B
X_c=40
V
60
100
200
X_c=500
0.9
0.80
0.8
LOCUS FOR CENTERS OF CIRCLES OF CONSTANT S
0.60
0.6
0.30
S=0
A
R
R
β
β
0 1 2
SCALE
LOCUS FOR CENTERS OF CIRCLES OF CONSTANT X_c
20
40
60
100
200
500

Figure 8. Main-coil current loci

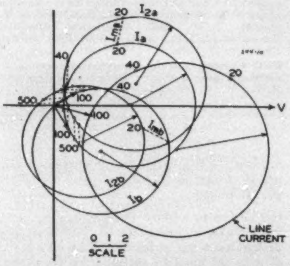

20
I_{2a}
I_{ma}
I_a
20
40
40
20
500
100
100
V
20
I_{mb}
100
500
I_{2b}
I_b
LINE CURRENT
0 1 2
SCALE

Figure 10. Current loci for S=0.96

Large Adjustable-Speed Wind-Tunnel Drive

C. C. CLYMER
MEMBERSHIP APPLICATION PENDING

PROPELLER-drive equipment for use in wind-tunnel work, where airplane model testing is involved, presented no unusual problems until the advent of the present national emergency. The emergency stressed the importance of research work in airplane design, necessitating the application of the largest motor drive so far considered where a fixed frequency supply provided the energy source. Tunnels are now in operation or in the process of construction, powered by drives rated up to 40,000 horsepower. The primary condition for all such drives is variable or adjustable speed over at least a 6 to 1 range, with a large number of speed-control points and accurate speed regulation.

The accepted method of controlling wind-tunnel motors heretofore was by means of adjustable-voltage control using conventional apparatus, or multispeed wound-rotor induction motors with slip regulators. The conventional adjustable-voltage or generator-field-control system is out of the question, for either single or double units in the above capacities. Two-speed wound-rotor induction motors using slip regulators presented formidable design problems, and offered such questionable operating characteristics as to discourage their consideration. Aside from the questionable practice of starting such large motors directly from existing power systems, the problem of disposing of the slip energy is quite serious. It appears that the problem of the dissipation of energy in large quantities is almost as difficult as the problem of producing it.

The drive under discussion requires 40,000 horsepower in two counterrevolving propellers at a speed of 300 rpm. Assuming wound-rotor induction-motor drive, the power requirements are as given in Figure 1. The curve marked "fan hp" gives the shaft horsepower required by the fan for any given speed. The curve marked "slip hp" gives the rotor electrical energy to be disposed of in the most satisfactory manner. The motor input, or the load taken from the line is, of course, the sum of these two curves at any particular operating point.

Needless to say, every known method of speed control was analyzed and its advantages and disadvantages tabulated before the described system was selected. None of the existing systems completely met the conditions; hence, this combination of machines. This arrangement is shown schematically in Figure 2. The two d-c machines were used on each set simply because of design consideration. A smaller drive would use but one such d-c machine.

Obviously the main drive motor can not operate with zero slip except with an unwarranted complication. Accordingly, the main drive motors were selected with a synchronous speed of 327 rpm, but intended to operate only to a speed of 300 rpm, as the maximum operating point. The determination of the proper amount of slip is a matter of economics. If the slip is too low, means must be provided to compensate for the *IR* drop in the windings.

It will be observed that the rotors of the two main drive motors are, in effect, connected in series. This connection introduces a synchronous machine damping into the circuit. Were the rotors of the two main motors connected directly in parallel with the stators connected in parallel, the machines could oscillate independently of the restraining force provided by the synchronous machine.

The characteristics deemed essential for the successful operation of the tunnel follow:

1. Two propellers will be used, each absorbing approximately 20,000 horsepower at 300 rpm. These propellers are to operate in opposite directions but in absolute synchronism.
2. Variable speed with vernier control from 50 to 300 rpm.
3. Accurate speed control for any given speed setting to within one fourth of one per cent.
4. Power-factor correction.
5. Low-starting kilovolt-amperes.
6. Control of the rate of change of power.

Desirable but not necessarily cardinal characteristics may be listed as follows:

1. High efficiency.
2. Low maintenance cost.
3. Ease of speed control.
4. Negligible line disturbance.

The motors under discussion could be located in the tunnel proper, mounted in a streamlined nacelle or mounted outside the tunnel and the fans operated through shafting. The total input to the stator minus the output from the rotor is dissipated in the tunnel air stream. The motor losses are, of course, dissipated in the nacelle if the motors are tunnel-mounted. Since the tunnel air reaches a rather high temperature, it is not satis-

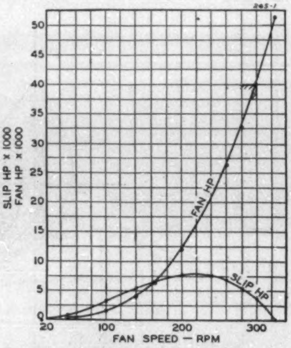


Figure 1. Speed-horsepower chart of a propeller-type fan, with slip energy for wound-rotor induction-motor drive

Synchronous speed of induction motors 327 rpm

Maximum speed of fan 300 rpm

Rated load 40,000 horsepower at 300 rpm

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C. C. CLYMER is in the industrial engineering department, General Electric Company, Schenectady, N. Y.

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amount of the magnetizing current. Figure 10 shows the loci for $S=0.96$ plotted to the same scale.

(*o*) By a procedure paralleling that outlined above in items (*d*)–(*l*), inclusive, any desired locus can be found. Both conditions of item (*k*) must be satisfied for I_{2b} as well as for I_{2a}.

References

1. THE CROSS-FIELD THEORY OF THE CAPACITOR MOTOR, A. F. Puchstein and T. C. Lloyd. AIEE TRANSACTIONS, volume 60, 1941, February section, pages 58–61.

2. THE REVOLVING-FIELD THEORY OF THE CAPACITOR MOTOR, W. J. Morrill. AIEE TRANSACTIONS, volume 48, 1929, pages 614–629.

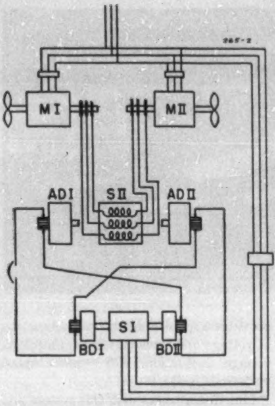

Figure 2. Schematic connection diagram with double motor drive using counterrevolving propeller

MI and MII = 22,000-horsepower slip-ring 327-rpm fan motors
ADI and ADII = 2,500-kw d-c generators
BDI and BDII = 3,200-horsepower d-c motors
SII = 20,000-kva 540-rpm synchronous motor
SI = 6,000-kva 514-rpm synchronous generator

factory as a cooling medium. It is, therefore, necessary to remove the heat from the nacelle by other means. Air-to-water heat exchangers could be used for this purpose, the coolers being so arranged that blowers located in the nacelle draw the hot air through the cooler, where it gives up its heat, delivering the cool air back to the motor. Water for the coolers is obtained from the sump of a cooling tower. The use of the coolers will reduce the ambient appreciably over the tunnel ambient, thus permitting higher temperature rise with consequently smaller and less expensive equipment.

Referring again to Figure 2, induction motors operated in this manner are doubly fed. The speed at any instance is proportional to the difference between the stator and rotor frequency. The machines, therefore, operate without slip in the sense that that term is usually applied, and, in effect, operate substantially as a synchronous machine. In fact, if we arrange to compensate for the *IR* drop in the machine windings, they can be made to operate exactly in synchronism.

Speed variation is accomplished by varying the fields of the d-c machines used in the circuit. This provides the equivalent of full adjustable-voltage or generator field control, and this control is accomplished through the use of d-c machines having approximately one-sixth the capacity of the total power absorbed by the propellers.

Accuracy of speed control is obtained through the use of high-speed excitation on the d-c machines. The excitation is controlled by matching a tachometer voltage against a standard, amplifying the difference that exists and applying this difference to the field of a high-speed exciter (Amplidyne generator).

The voltage standard is a 250-volt excitation bus whose voltage is maintained within $^1/_{10}$ of a volt. Speed variation is obtained by positioning a potentiometer rheostat having approximately 800 positions for the speed range of 50 to 300 rpm. This rheostat need be no more formidable than the rheostat used to control the tuning of a radio. However, since the problem of controlling this much power extends back to the central station serving the project, it is necessary to impose restrictions on the operators in order not to build up the load too rapidly, on the one hand, lest the power system be unnecessarily disturbed, and on the other, to prevent operators whose attention might be engrossed at the moment from blowing low-speed models to pieces in a high-speed tunnel.

Power-factor correction is obtained by varying the excitation on the variable-speed synchronous machine connected across the slip rings. In order to relieve the operator of the responsibility of holding the proper power factor, amplidyne control was applied to the field of the synchronous machines and adjusted to provide a power factor of 90 per cent at all loads and speeds. This is quite an important feature, since induction motors ordinarily applied to an operation of this kind would draw approximately 10,000 kva lagging at the lower speed range where a large percentage of the operation will take place.

Low-starting kilovolt-amperes is obtained by starting the constant-speed motor-generator set. After this set is started, the variable-speed set is brought up to provide 60 cycles, and with proper excitation applied at the rotor, it is possible to match the line voltage and frequency very accurately on the stator of the induction machine. When the voltage and frequency are matched, the running breakers may, of course, be closed. The voltage and frequency of the variable-speed synchronous machine are controlled through automatic synchronizing devices. Through this system, a 40,000 horsepower drive may be started with an inrush under 6,000 kva.

The rate of power change is controlled by using a motor-driven potentiometer rheostat. This rheostat may, of course, be driven at any desired speed. The rheostat may, if desired, be tapered so as to provide a high rate of change over the lower speed range of the drive with corresponding low rate of change at the higher speed range and, consequently, higher power demand point. On the other hand, it may not be desirable to change the speed of the machine too quickly at any part of the range, as this might make it difficult to obtain accurate data. It is conceivable that it might be desirable to change the speed very slowly in order to observe the effect of this speed change on the model. Suffice it is to say that practically any type of speed-versus-time characteristic could be obtained very easily within the design limitations of the associated apparatus.

Concerning the desirable though not necessarily mandatory characteristics, it is well to mention that the load factor on an operation of this kind is low, perhaps not more than 10 per cent. Power consumption will, therefore, be the least expense associated with the operation of the tunnel, and the economics of the problem do not justify any appreciable lay-

Figure 3. Complete assembly of two 8,700-horsepower 327-rpm wound-rotor induction motors and surface air coolers for wind-tunnel drive

Motors are not coupled together

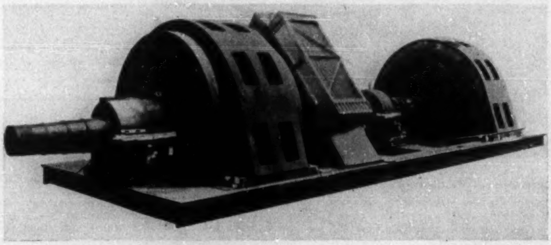

Figure 4. Close-up view of collector end of one 8,700 horsepower 327-rpm wound-rotor induction motor, of assembly shown in Figure 3

Figure 5. Variable-speed motor-generator set for speed adjustment of two 15,000-horsepower 327-rpm wound-rotor induction motors on wind-tunnel drive

A-c synchronous machine rated 13,500 kva, 0.9 power factor, each d-c machine rated 1,750 kw

out for increased efficiency. It is well to point out in this connection, however, that while it is necessary to pay for this energy on the one hand, the expense of disposing of it may more than equal its cost in the first place. The maximum demand on such an installation is often of greater importance than the power consumed. Therefore, the higher the efficiency, the lower will be the demand, and at the maximum loss point which occurs around 70 per cent speed, the maximum demand may be reduced 27 per cent over another system which would waste this slip energy.

The efficiency of the system described here is necessarily high since all slip energy is returned to the line minus machine losses. In the case of slip-regulator operation, it is necessary to operate with a higher slip than is provided by an induction motor operating with a shorted secondary. The electrodes of slip regulators can not be brought in too close proximity, otherwise erratic operation is obtained. Therefore, the full load efficiency of the two systems is comparable. At any reduced operating-speed point, however, the efficiency of the doubly fed motor will be much higher than the efficiency of any system where the slip energy is dissipated.

Ease of speed control is, of course, a by-product of the system used. Speed with this system is a function of frequency rather than voltage, and frequencies of present day distribution systems are maintained with a high degree of accuracy. Temperature changes are ineffectual except in the loop circuit of the d-c machine. Changes here are very gradual and are easily taken care of by the regulating equipment. Any type of induction-motor drive which is not doubly fed provides a speed which is both a function of voltage and frequency, and, therefore, voltage fluctuations will produce speed variations in the tunnel.

Line disturbances with this system are, of course, reduced to an absolute minimum, since there is no high-voltage switching under power, and after the machines are once started, the only change is a gradual build-up or decay of the load. Since the rate of change of this load may be very accurately and completely controlled, the generating system will be enabled to pick up and drop the load at a uniform rate, permitting the distribution system regulating devices to function and maintain normal service.

Low maintenance should result where only the rotating apparatus is employed and where there is no switching of dynamic current. In fact, the entire control operation is performed by handling current in the order of a fraction of an ampere. Maintenance is always appreciable where dynamic currents are switched regardless of the means provided to accomplish a change in operating conditions.

The system is, of course, new, and it is particularly adapted to the operation of fan loads, centrifugal pumps, frequency-converter systems, where the two frequencies do not necessarily match exactly, and where it is desired to accurately control the flow of power.

The Influence of Towers and Conductor Sag on Transmission-Line Shielding

ROYAL W. SORENSEN
FELLOW AIEE

ROBERT C. McMASTER
ASSOCIATE AIEE

THIS paper is the third of a sequence of papers intended to present data which may be used in determining the degree of protection from lightning obtainable by shielding transmission lines and structures with grounded overhead wires and masts. The first two papers of the sequence are: "Shielding of Transmission Lines,"[1] and "Shielding of Substations."[2]

Results Based on Earlier Model Tests

One sentence in the synopsis of a paper, "Lightning Protection for Oil Storage Tanks and Reservoirs,"[3] presented at the 1927 Pacific Coast convention of the AIEE, reads as follows:

> "Tests show that excellent protection can be obtained by towers properly installed, but they do not indicate absolute immunity against hits."

The tests described in that sentence were laboratory tests on small-scale models. Using data obtained from those and other tests, a plan for protecting reservoirs by means of masts was developed. The integrity of those tests in indicating the protective value of shielding by grounded masts is well demonstrated. All oil reservoirs equipped with masts designed according to the data obtained from the tests described in that paper have been free from damage by lightning since the masts were erected during 1926 and 1927. In some installations, well-grounded masts only were used, and in others, where conditions indicated it advisable, the masts were supplemented by interconnecting overhead conductors.

In the 14 years which have elapsed since that method of protection was adopted for the oil reservoirs in question, many field data and many model tests have added to our knowledge of lightning and ways to guard against lightning damage. A bibliography of the reported work relating to this particular discussion was presented in the paper, "Shielding of Transmission Lines." One dictate of the knowledge obtained is that lightning protection for electrical transmission lines and other structures can be obtained most economically in any scheme of protection by taking advantage of the shielding effect of overhead ground wires and grounded masts located in proper juxtaposition to the objects to be protected. Since absolute protection for such structures is not usually economically practical, engineers desire statistical data which will enable them to determine the degree of protection provided by particular arrangements of overhead ground wires and masts. Such data must be obtained by many observations of actual lightning strokes and by careful model tests in the laboratory. The model tests must show valid correlation with field observations, and duplicate those characteristics of natural lightning which determine the paths taken by strokes. A full discussion of this topic appears in the first paper of the sequence.

Recent Model Tests

The first paper presented data showing the value of ground wire protection for a laboratory model representing a section of transmission line with a tightly-stretched conductor protected by a parallel tightly-stretched overhead ground wire. There was no appreciable sag in the section of model line tested, and no supporting towers were included within the test area.

The present paper gives the results of two check tests made in the California Institute of Technology high-voltage laboratory upon models identical with those used for two of the tests made at Trafford, and of further tests which determine the shielding effect, additional to that of overhead ground wires, provided by the transmission towers and conductor sag. Correction factors are given by means of which shielding data for parallel wires can be modified to account for the additional protection resulting from the presence of transmission towers.

Table I. Distribution of Stroke Terminations to a Typical 1,000-Foot Transmission-Line Span (Per Cent)

Model Arrangement	Conductor	Ground Wire	Tower	Ground Plane
(*a*) Taut wires only	23.4	58.5		18.1
(*b*) Tower, taut wires	18.7	50.1	12.7	18.5
(*c*) Tower, sagged wires	8.9	44.6	15.8	30.6

The large amount of published data relating to lightning, surge testing with models, and the correlation between field observations and model tests under various conditions, permits the writing of this paper without including any matter relating to the "mechanism of natural lightning," and with little reference to the "fundamentals of model tests."

In keeping with conclusions of other experimenters, and the experience of the authors, it was considered justifiable for these tests to duplicate as closely as possible the test conditions used in obtaining the data for the first paper.

Laboratory Model and Test Conditions

The 2,000,000-volt 0.065-microfarad surge generator, built by graduate students at Pasadena, was used as the voltage source for the work of this report.

The conventional surge-generator circuit, with a resistance in parallel with the

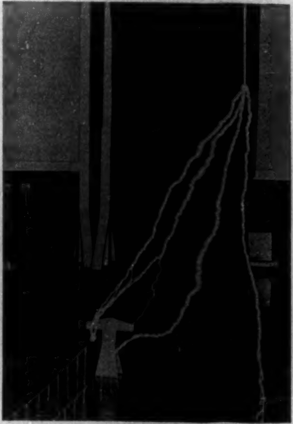

Figure 1. Model transmission line with wires sagged

Four strokes are shown

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ROYAL W. SORENSEN is professor and head of the department of electrical engineering, and ROBERT C. McMASTER is instructor in the department of electrical engineering, California Institute of Technology, Pasadena, Calif.

The authors acknowledge the assistance of E. A. Bureau, University of Kentucky, the several graduate students who assisted in making observations, and the National Youth Administration.

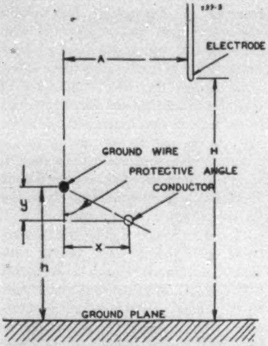

Figure 2. Taut ground wire and conductor parallel

Used to obtain data for Figures 3 and 4

test gap, was used. The wave form was approximately a $1\frac{1}{2}$- by 40-wave; and all strokes were fired at the minimum arc-over voltage of the test gap, with cloud electrode polarity positive. The discharge electrode was a $\frac{3}{8}$-inch diameter rod with a rounded end, mounted vertically with its lower end 50 inches above the ground plane of the model. The ground plane was a large salt water basin in which was submerged a grid of ground wires, covering the entire area of the basin and providing a 12-inch mesh over the test area. Conductors and tower used in the model were solidly grounded.

The model represented 100-foot transmission towers supporting 1,000-foot spans, built to a scale of 10 inches = 100 feet (see Figure 1). Only one mode lscale was used, because experience has demonstrated that change in model scale does not change the relations of test results.[1] For test purposes, number 14 bare copper wire was used for overhead ground wire and conductor. For tests with sagged wires, the proper catenary was maintained by use of nonconducting anchor cords kept dry by having their lower ends attached to metal hooks just above the water.

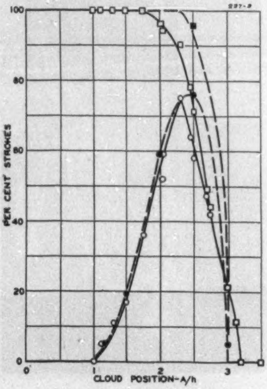

Figure 3. Check tests

$y/h = 0.1$. Solid curves Pasadena test data. Dotted curves Trafford data

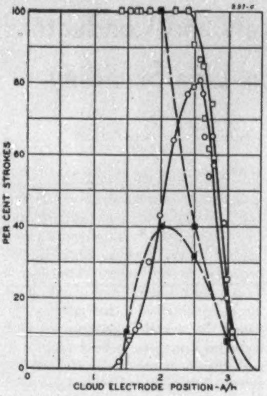

Figure 4. Check tests

$y/h = 0.2$. Solid curves Pasadena test data, Dotted curves Trafford data

To the scale of 10 inches = 100 feet, the equivalent height of the cloud electrode was 500 feet. This is the generally recognized minimum height of cloud from which lightning develops. This arrangement gives conservative results for shielding conditions. The first paper included a study of the effect of protective angle between ground wire and conductor. One set of data was for an angle of 64 degrees. In that test, a measurable percentage of strokes terminated on the "protected line conductor." This 64-degree angle was chosen as a fixed reference angle and used throughout the tests reported in this paper.

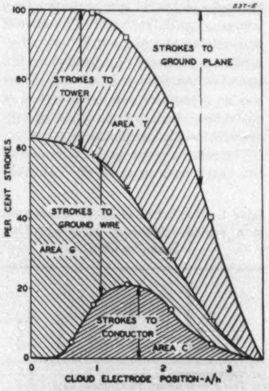

Figure 5. Explanation of distribution curves

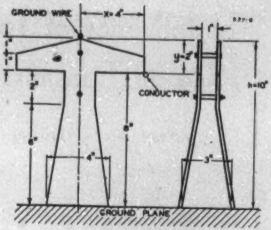

Figure 6. Detail of model tower

Scale 10 inches = 100 feet

Methods of Observation

In reporting the tests, the total number of strokes for any condition was divided into groups and classified according to the points of stroke termination. The strokes were designated as strokes to conductor, strokes to overhead ground wire, strokes to tower, and strokes to ground plane. At least 100 strokes were "fired" for each position of the cloud electrode. The points of stroke termination were recorded by two observers viewing the model from positions such as to have their lines of vision intersect perpendicularly at the point subject to test. This made it possible to determine accurately and without any difficulty the point of stroke termination. The observers exchanged positions after each 25 strokes. If results of the first 50 strokes were not consistent with those of the following 50 strokes, additional strokes were fired. The lack of dependence of results upon any unique skill or opinion of an observer is well established by the agreement in the reports of the 16 observers used during the series of tests.

Model Arrangements

Tests were made under three conditions:

1. As shown in Figure 2, using taut ground wire and conductor parallel to each other, without tower in place or conductor sagged. These were check tests to correlate this work with that reported in the first paper.

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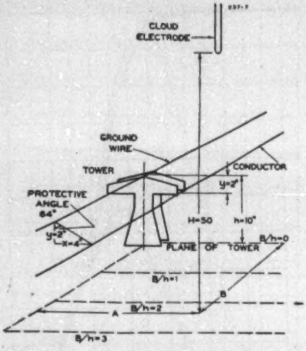

Figure 7. Taut ground wire and conductor parallel

Tower in place. For Figures 8–12, 22, and 24

2. As shown in Figure 7, using taut ground wire and conductor with model tower installed to determine added shielding effect of tower.

3. As shown in Figure 13, using ground wire and conductor under less tension and suspended from model tower to simulate the sag that is always present for actual transmission-line conditions. Tests were made with this arrangement to evaluate the effect of line sag on shielding.

Results of Check Tests

In the check tests, the wires representing the line conductor and the overhead ground wire were tightly-stretched copper wires supported outside the test area and without towers or line sag within the test area. The first check test was made with $h=10.0$ inches; $y/h=0.10$; $H/h=5$; protective angle 64 degrees (see Figure 2); and the second check test was made with the same arrangement except for a change in the ratio y/h from 0.10 to 0.20. Figures 3 and 4 show the comparative results of the tests made in the two laboratories. The Pasadena test results are plotted as solid-line test curves, and the Trafford test results as dotted-line test curves.

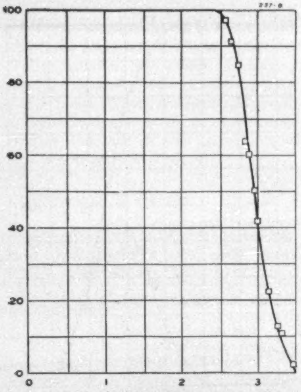

Figure 8. For cloud electrode in plane of tower

$B/h=0$

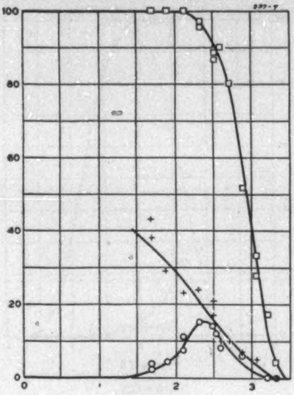

Figure 9

$B/h=0.5$

The results of the two check tests, while not in exact agreement with the Trafford tests, are as much in accord as one could expect for tests of this character made in different laboratories by different experimenters who have no agreement as to number of "shots" for each condition, and other plans of procedure.

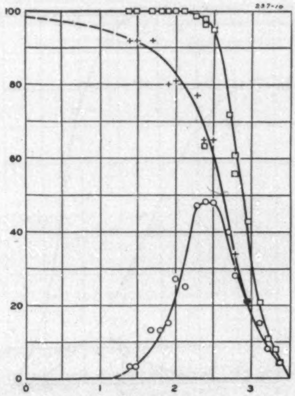

Figure 10

$B/h=1.0$

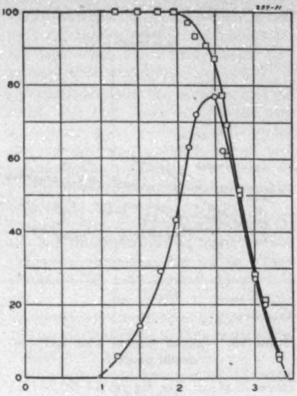

Figure 11

$B/h=2.0$

The differences which are to be noted in the curves may be due to the difference in the number of strokes made to each point. The solid curves represent more than 1,500 strokes for each curve and a minimum of 100 strokes for each A/h value used in plotting the curve. However, in considering actual lines, the effect of line sag and towers is such as to more than make up for the difference in the two

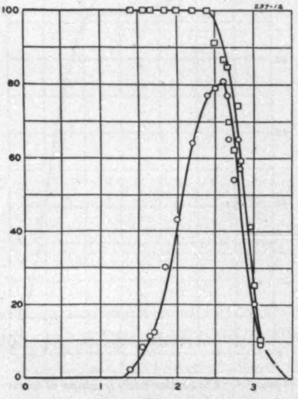

Figure 12. Tower removed

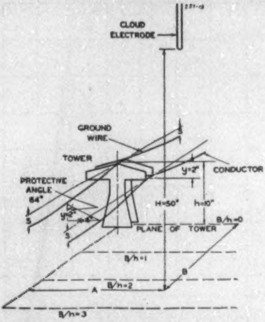

Figure 13. Sagged ground wire and conductor parallel

Tower in place. For Figures 14–20 and 23

curves. One result of the comparison just made is the indication that it is well in making surge tests of the character under discussion to base findings on results obtained by making at least 100 "strokes" for each plotted point. See Table II.

Shielding Effect of Towers

The data obtained with towers in the model line added to our general realization of their value as aids in line shielding by providing data which show that, for a cloud electrode at five times tower height above ground, a line conductor suspended below a metallic cross arm is completely protected from strokes originating from a cloud point in a plane through the tower and perpendicular to the line. The data also show that the protection furnished by the tower decreases with distance from the tower measured along the line, as follows:

With 100 per cent protection at the tower, there is only 85 per cent protection with the cloud point moved parallel to the line a distance of one-half tower height from the tower. If the distance from the plane of the tower to the point of cloud discharge is equal to a full tower height, the protection is only 50 per cent. The towers offered no protection to the line conductor against strokes originating from a cloud point more than two times tower height along the line from the plane of the tower (see Figure 22). Towers 100 feet high and spaced 1,000 feet apart in a transmission line reduce the number of probable strokes to the conductor for the over-all line to 80 per cent of the number that would be expected for a section of line without towers. The line conductor and overhead ground wire were kept taut without appreciable sag in these tests (see Figure 7).

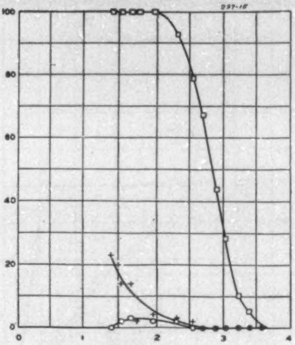

Figure 15

B/*h* = 0.5

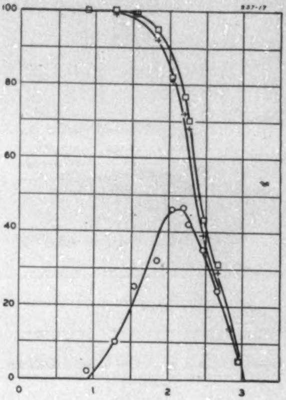

Figure 17

B/*h* = 2.0

Shielding Effect of Line Sag

The third set of tests on the model line was made with the tower in place and with line conductor and overhead ground wire kept parallel to each other, both being sagged 40 per cent of the tower height at the center of the span (see Figure 13). These tests show that with 100-foot towers 1,000 feet apart, the number of strokes that hit the conductor for the cloud

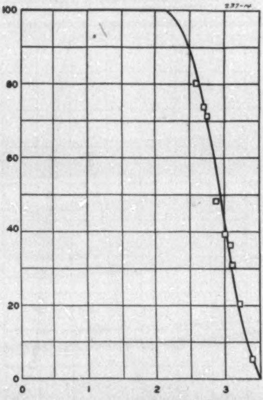

Figure 14. Cloud electrode in plane of tower

B/*h* = 0

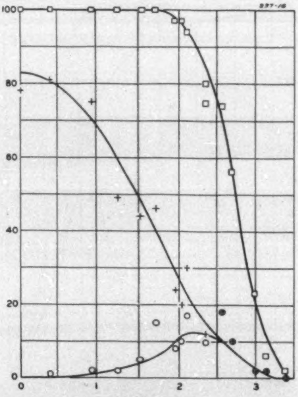

Figure 16

B/*h* = 1.0

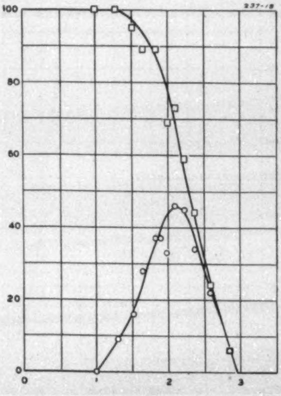

Figure 18

B/*h* = 3.0

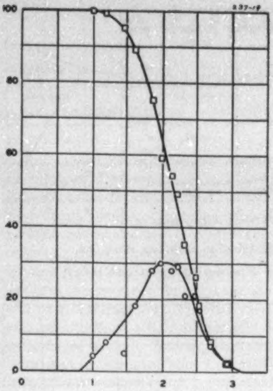

Figure 19

$B/h = 4.0$

electrode opposite the center of the span was only 38 per cent of the number observed when the lines were not sagged. For a uniform distribution of strokes originating from clouds along the line, the line sag reduced the number of strokes to conductor to 52 per cent of the number obtained when towers were in place but there was no line sag (see Figure 23).

Distribution of Strokes

Tests involving thousands of strokes to the model-line area from a minimum cloud height of five times the tower height showed that the line is immune to strokes that originate along the line, outside a band, the width of which is about seven times the tower height. Table I presents the distribution for strokes originating from the cloud electrode located above the line at points uniformly distributed within a band having a width of seven tower heights and with its axis parallel to the axis of the transmission line.

Explanation of Figures

Figures 2 to 24, showing model arrangement and results of tests, enable ready comparison of results. In these diagrams:

h = Height of tower and ground wire at point of tower support. In tests this model was always 10 inches high.
y = Vertical component of displacement of conductor below ground wire.
x = Horizontal component of displacement of conductor from ground wire.
$\underline{/P}$ = Protective angle = $\tan^{-1} x/y$.
H = Height of lower end of cloud electrode above ground plane = 50 inches.

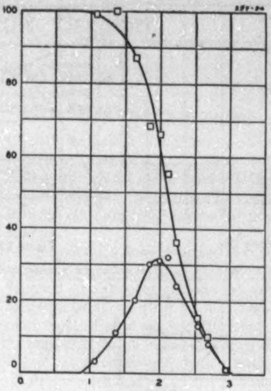

Figure 20

$B/h = 5.0$

A = Horizontal component of displacement of cloud electrode *normal* to the line, measured from ground wire.
B = Horizontal component of displacement of cloud electrode *parallel* to the line, measured from center line of tower.
s = Sag of ground wire and of conductor at center of span, from heights at tower support points.

For each of the three tests, all model dimensions were fixed, except the horizontal displacement of the cloud electrode, given by A and B, which was varied throughout the region from which strokes might terminate on the model line.

Figure 5 explains the curves of test results. For each value of A/h, the percentage strokes to conductor are shown by the ordinate to the lowest curve, test points shown by ⊙; the percentage to ground wire by the difference between the ordinate of the curves whose points are (+) and the curve whose points are ⊙; the percentage strokes to tower by the differences between the curves whose points are (☐) and the curve whose points are (+). Strokes to ground plane are represented by the difference between the 100 per cent ordinate and the curve whose ordinates are marked by (☐).

Figure 2, shows arrangement of model line with taut wires only.

Figure 3 shows results for $y/h = 0.1$, and Figure 4 results for $y/h = 0.2$ for tests at both laboratories, with wires only as shown in Figure 2. Figures 5 and 6 are the explanatory curve and detail of model tower, respectively.

Figure 7, shows arrangement of model line with tower in place and taut wires.

Figure 8 shows stroke distribution for electrode at various positions (A/h) in the plane of tower ($B/h = 0$).

Figure 9 shows stroke distribution for electrode at various positions (A/h) in the

Table II. Typical Test Observation Data

	Test 89—Tower, Taut Wires 5 Strokes in Each Group $y/h = 0.2$; $B/h = 1.0$; $A/h = 2.40$					Test 149—Tower, Sagged Wires 5 Strokes in Each Group $y/h = 0.2$; $B/h = 1.0$; $A/h = 2.36$			
	Stroke Distribution					Stroke Distribution			
	Tower	Ground Wire	Conductor	Ground Plane		Tower	Ground Wire	Conductor	Ground Plane
	1	3	1	0		4	0	0	1
	2	1	2	0		3	0	0	2
	3	1	1	0		3	0	0	2
	0	0	5	0		4	0	0	1
	1	1	3	0		5	0	0	0
	2	0	3	0		2	0	1	2
	1	0	4	0		3	0	1	1
	1	2	2	0		3	0	1	1
	0	2	3	0		3	0	2	0
	3	0	1	1		2	0	2	1
First 50 Strokes	28%	20%	50%	2%	First 50 Strokes	64%	0%	14%	22%
	4	0	1	0		3	0	2	0
	1	1	3	0		2	1	1	1
	2	1	2	0		3	0	0	2
	2	1	2	0		4	0	0	1
	1	2	2	0		3	0	1	1
	1	0	4	0		1	0	2	2
	1	1	2	1		4	0	1	0
	2	0	3	0		2	0	1	2
	3	0	1	1		5	0	0	0
	1	1	3	0		4	0	1	0
Second 50 Strokes	36%	14%	46%	4%	Second 50 Strokes	62%	2%	18%	18%
100 Strokes	32%	17%	48%	3%	100 Strokes	63%	1%	16%	20%

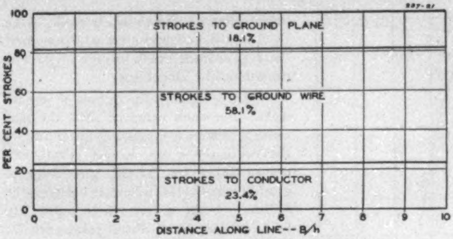

Figure 21. Distribution of strokes along line

Taut parallel ground wire and conductor only

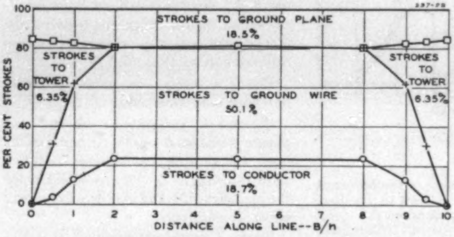

Figure 22. Distribution of strokes along line

Taut parallel ground wire and conductor with towers in place

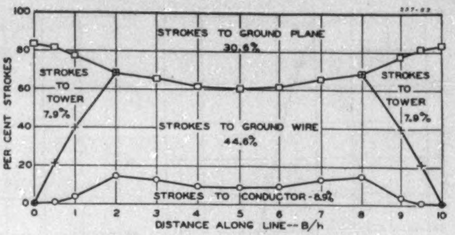

Figure 23. Distribution of strokes along line

Sagged parallel ground wire and conductor with towers in place

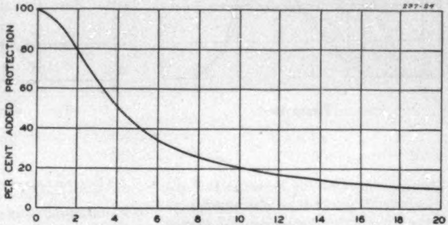

Figure 24. Added protection resulting from presence of towers, for various lengths of span

plane one-half tower height away from tower ($B/h=0.5$).

Figures 10, 11, and 12, show stroke distribution for $B/h=1.0$; for $B/h=2.0$; and with tower removed, respectively.

Figure 13, shows arrangement of model line with tower in place and 40 per cent sag in wires. Figures 14 through 20 show stroke distribution for electrode at various positions A/h in planes for values $B/h=0.0$; 0.5; 1.0; 2.0; 3.0; 4.0; and 5.0.

Interpretation of Test Data

The areas C, G, and T, bounded by the curves, as illustrated in Figure 5, show values of stroke distribution to conductor, to ground wire, and to tower, integrated for all values of A/h. The remaining area shows strokes to ground plane for integrated values of A/h up to about 3.5 times tower height.

Figures 21, 22, and 23, in columns 1, 2, and 3, respectively, show the distribution of strokes along the line, where B/h measures distance from tower in terms of tower height (h). The ordinates of these curves for each (B/h) value were obtained from the areas of the preceding curves. The areas of Figures 21, 22, and 23 show the distribution for stroke terminations for an entire span. The data for Table I were obtained by measuring these areas.

Figure 24, derived from data for Figure 22, shows as a function of span length the percentage of added shielding provided by towers. For a 40 per cent sag in a 1,000-foot span, a similar curve could be drawn showing the added shielding effect of towers and sag. This has not been done because in actual practice sag changes with tower spacing and conductor tension.

Conclusions

The tests on which this report is based show that the Wagner, McCann, and MacLane protection values are conservative, because they do not include the protection provided by towers and line sag. The preceding curves can be used to determine the additional protection due to towers and conductor sag for a protective angle of 64 degrees. For other protective angles estimates of the added protection can be based on reasonable interpretation of these results. The magnitude of this added protection is important, since for typical spans it shows an increased protection of 20 to 80 per cent.

Appendix

Figure 25 is included in this paper to call attention to the pattern formed in the grounding pool, apparently on the surface of the water. At the time this picture was made, the water had a somewhat higher resistance than was used during the regular testing program. Figure 1 was made after the addition of more salt to the water. The "crowfoot" figures were then less noticeable. Figure 8 in the paper, "Lightning Protection for Oil Storage Tanks and Reservoirs"[9] showed the spread of discharge current over a concrete floor. The "crowfoot" travel of current over that floor made a photograph much like the ones shown in this paper. These pictures are of interest in showing what may happen when large currents arrive at a point of sudden change in resistance. The resistance of the concrete floor was very high.

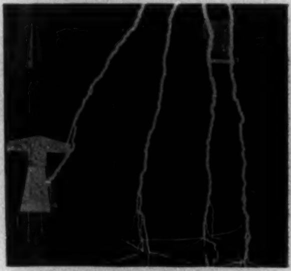

Figure 25. "Crowfooting" or spreading of streamers on surface of abnormally high-resistance water plane

A Fast Circuit Breaker

D. I. BOHN
MEMBER AIEE

OTTO JENSEN
ASSOCIATE AIEE

DURING the past few years the rapid expansion of mercury-arc rectifier installations at 600 volts d-c, both as to the total kilowatts installed and the relatively large number of units operated in parallel, has emphasized the importance of switchgear in providing suitable rectifier operation. In the event of a backfire, the rates of current rise through a rectifier and its transformer windings lie, in general, between three million and six million amperes per second. With large installations, such as shown in Figure 1 where 60 units of 5,000 amperes each are operated in parallel, the ceiling value of these currents is far above a figure which could be tolerated both from the standpoint of continuity of operation and safety to equipment.

High-speed breakers having a time of approximately 0.5 cycle from backfire initiation to current limitation may permit, in some installations, peak currents in the neighborhood of 60,000 amperes. While such a value is appreciably below that which would cause equipment damage, it is nevertheless undesirably high, in that surges and "sympathetic" backfires on other units often result. Breaker duty and maintenance are higher than would be the case with lower values of backfire current, and the factor of safety is not so great as is desirable.

Figure 2 shows the current path during a backfire in a six-phase rectifier. It will be noted that the current flowing through the cathode breaker is supplied by other rectifiers on the bus, and that the current flowing through the anode breaker is the same current plus the additional current from the other anodes of the faulted rectifier.

Figures 3 and 4 show two accepted connection schemes for rectifier stations. In Figure 3 a high-speed cathode breaker opens the backfire current fed from other units on the bus of the backfiring unit. At some later period, such as six or eight cycles, the rectifier-transformer primary oil switch opens and clears the backfire current supplied by anodes in the backfiring unit. Since an oil circuit breaker ahead of the transformer may be called upon to interrupt the full capacity of the system, its economical disadvantage is obvious. Furthermore, its slow clearance of a transformer secondary short circuit, caused by a backfire, is undesirable.

Figure 4 shows an alternative connection scheme in which the individual trip-free poles of a six-pole anode breaker clear backfire currents fed from the other rectifier operating in parallel with the backfiring unit, and also interrupt the backfire current supplied by the anodes in the unit. The semihigh-speed cathode breaker is used for backup protection and to provide, through gang tripping, means of dropping a large unit of load fed from a group of rectifiers.

This paper discusses a six-pole anode breaker primarily designed to provide reduced operating time on reverse current and thus to cut to a minimum the undesirable effects of backfires. The same principles of design incorporated in this circuit breaker could apply equally well to a high-speed cathode breaker.

High-speed breakers may be of either the latched-in or nonlatched-in type. If of the former design, the latch-releasing mechanism may be in the form of a simple trip, presumably designed with a maximum force-to-weight plunger ratio when energized with reverse current, or it may have some form of bucking-bar latch-releasing mechanism, or the latch may consist of a friction clutch. With all of these types of breaker, a relatively large

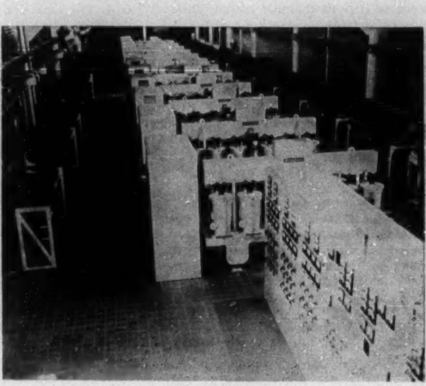

Figure 1. View of large rectifier installation

Paper 42-38, recommended by the AIEE committee on protective devices for presentation at the AIEE winter convention, New York, N. Y., January 26–30, 1942. Manuscript submitted November 3, 1941; made available for printing December 12, 1941.

D. I. BOHN is with the engineering department of the Aluminum Company of America, Pittsburgh, Pa., and OTTO JENSEN is with the I-T-E Circuit Breaker Company, Philadelphia, Pa.

The discharge currents from the arc path into the water forming the ground plane for Figures 1 and 25 were estimated to be of the order of 10,000 amperes. No effort was made at the time of the experiments to measure the resistance of the ground circuit including the arc during the stroke. To get some idea of the probable resistance of the arc path to ground, a brass rod was substituted for the arc path. With the end of the rod dipped one inch into the water, the ground circuit resistance was found with an ohmmeter to vary from 390 ohms to 250 ohms, depending upon the amount of salt added to the water. With one-half inch of electrode immersed in the water, the resistance was 800 ohms. In the tests made with the tower in place, the resistance of tower to ground was three ohms.

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4. The reader will find bibliographies in reference 1 and in the following series of three articles: LIGHTNING PHENOMENA, C. F. Wagner and G. D. McCann. ELECTRICAL ENGINEERING, volume 60, 1941. (a) I—General Characteristics, August, pages 374–84. (b) II—Instruments for the Measurement of Lightning Surges, September, pages 438–43. (c) III—Field Studies, October, pages 483–500.

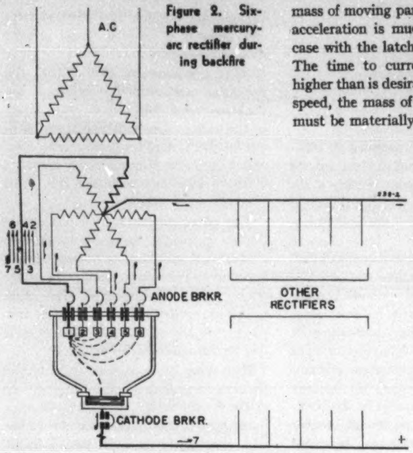
A.C
6 4 2
7 5 3
ANODE BRKR
OTHER RECTIFIERS
CATHODE BRKR.
+

Figure 2. Six-phase mercury-arc rectifier during backfire

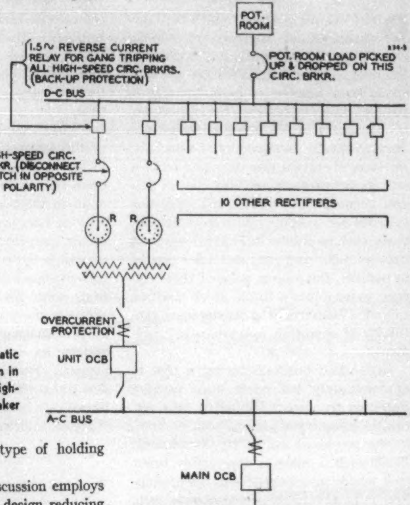
POT. ROOM
1.5~ REVERSE CURRENT RELAY FOR GANG TRIPPING ALL HIGH-SPEED CIRC. BRKRS. (BACK-UP PROTECTION)
POT. ROOM LOAD PICKED UP & DROPPED ON THIS CIRC. BRKR.
D-C BUS
HIGH-SPEED CIRC. BRKR. (DISCONNECT SWITCH IN OPPOSITE POLARITY)
10 OTHER RECTIFIERS
R
R
OVERCURRENT PROTECTION
UNIT OCB
A-C BUS
MAIN OCB

Figure 3 (right). Schematic diagram of main connection in rectifier station, using high-speed cathode circuit breaker

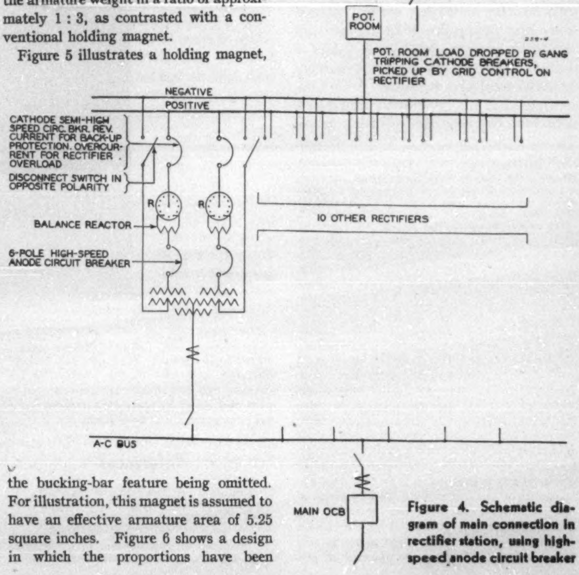
POT. ROOM
POT. ROOM LOAD DROPPED BY GANG TRIPPING CATHODE BREAKERS, PICKED UP BY GRID CONTROL ON RECTIFIER
NEGATIVE
POSITIVE
CATHODE SEMI-HIGH SPEED CIRC. BKR. REV. CURRENT FOR BACK-UP PROTECTION. OVERCURRENT FOR RECTIFIER OVERLOAD
DISCONNECT SWITCH IN OPPOSITE POLARITY
R
R
BALANCE REACTOR
6-POLE HIGH-SPEED ANODE CIRCUIT BREAKER
10 OTHER RECTIFIERS
A-C BUS
MAIN OCB

Figure 4. Schematic diagram of main connection in rectifier station, using high-speed anode circuit breaker

proportion of the total operating time is absorbed in operating the releasing mechanism.

For example, a predecessor of the breaker herein discussed has a polarized solenoid-trip mechanism whose plunger releases a latch engaging a ball-bearing-mounted latch pin on the main arm. This breaker trips on about 1,500 amperes reverse current and about 6,000 amperes forward current, the polarizing providing a bias. A typical time study of this circuit breaker, based on oscillograms simulating backfires, shows the following:

Completion of trip plunger stroke and release of latch	0.17 cycle
Total time to parting of arcing contacts	0.38 cycle
Total time to current peak	0.50 cycle
Total time to end of arcing	0.7 cycle

It will be observed that of these various times the first value, 0.17 cycle, is completely wasted in that the breaker mechanism proper is not released until the expiration of this time. This indicates that, even if the succeeding times were appreciably reduced, no great reduction in the total time to current peak would be obtained.

This initial time can be reduced to nearly zero by using a bucking-bar holding magnet with the armature fastened directly to the main operating arm. With this design, the main contact arm starts to move in 0.03 or 0.04 cycle (60-cycle basis) after the backfire is initiated.

Unfortunately, the difference between these latter figures and the previous figure of 0.17 cycle is not a net gain, as the total mass of moving parts is so great that the acceleration is much slower than is the case with the latched-in type of breaker. The time to current limitation is still higher than is desirable. In order to gain speed, the mass of the holding armature must be materially reduced as compared with the conventional type of holding magnet armature.

The breaker under discussion employs a magnet and armature design reducing the armature weight in a ratio of approximately 1 : 3, as contrasted with a conventional holding magnet.

Figure 5 illustrates a holding magnet, the bucking-bar feature being omitted. For illustration, this magnet is assumed to have an effective armature area of 5.25 square inches. Figure 6 shows a design in which the proportions have been greatly altered, although the armature area and hence its pull are the same as shown in Figure 5.

It will be observed that the design in Figure 6 shows a weight reduction of

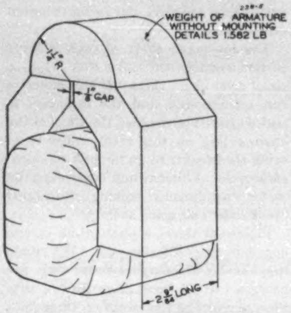
239-5
WEIGHT OF ARMATURE WITHOUT MOUNTING DETAILS 1.582 LB
1 1/4" R
1/8" GAP
2 9/64" LONG

Figure 5. Conventional design of holding magnet

about 3:1, as compared with that in Figure 5, but quite obviously such a design is entirely impractical from a mechanical and stiffness standpoint, so its theoretical advantages cannot be realized.

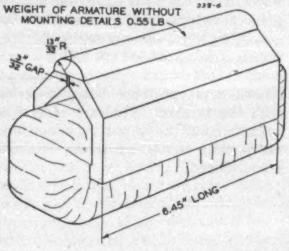
WEIGHT OF ARMATURE WITHOUT MOUNTING DETAILS 0.55 LB
239-6
13/32" R
3/32" GAP
6.45" LONG

Figure 6. Elongated design of holding magnet

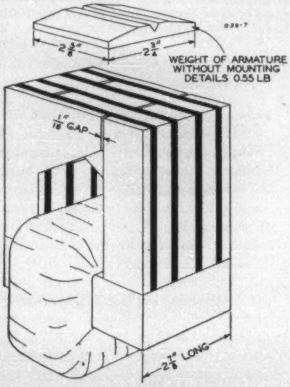
239-7
2 3/8"
2 1/4"
WEIGHT OF ARMATURE WITHOUT MOUNTING DETAILS 0.55 LB
1/16" GAP
2 7/8" LONG

Figure 7. Design of holding magnet used in one-quarter-cycle breaker

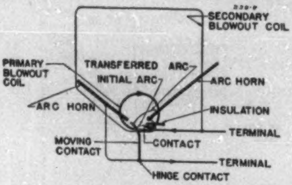
239-8
SECONDARY BLOWOUT COIL
PRIMARY BLOWOUT COIL
TRANSFERRED ARC
INITIAL ARC
ARC HORN
ARC HORN
INSULATION
TERMINAL
MOVING CONTACT
CONTACT
TERMINAL
HINGE CONTACT

Figure 8. Schematic diagram of circuit-breaker magnetic-blowout structure

Figure 7 shows the design employed in the breaker under discussion. It carries out the same general principle of weight reduction as does the design in Figure 6, but provides a suitable armature and magnet design from a practical and mechanical standpoint. The armature weight is 0.55 pound, the armature pull being 400 pounds.

Actually, the weight ratios between the magnets shown in Figures 5 and 7 are even more favorable to the new design,

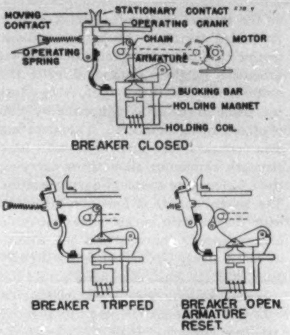
MOVING CONTACT
STATIONARY CONTACT
OPERATING CRANK
CHAIN
MOTOR
OPERATING SPRING
ARMATURE
BUCKING BAR
HOLDING MAGNET
HOLDING COIL
BREAKER CLOSED
BREAKER TRIPPED
BREAKER OPEN ARMATURE RESET

Figure 9 (above). Mechanical operation of one-quarter-cycle breaker

Figure 10 (below). Typical one-quarter-cycle breaker

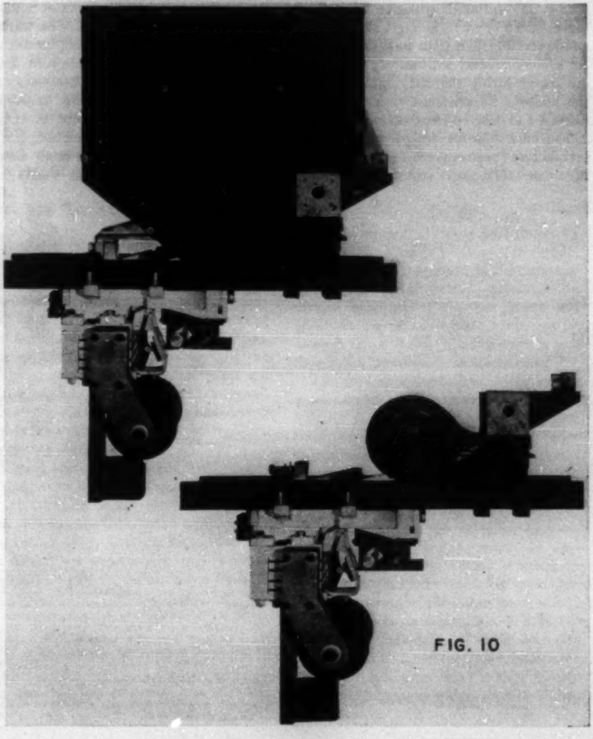
FIG. 10

as the magnet in Figure 5 must have a laminated armature which with suitable mounting details would increase the total armature weight. Combined with the reduction in armature weight, every effort was made to similarly reduce the weights of other moving members. The operating arm consists of furnace-brazed high strength aluminum alloy tubes carrying the 1,600-ampere contact finger assembly. The latter consists of four one-half-inch-wide fingers with nickel silver movable and stationary contacts and one Elkonite-faced arcing tip, which opens slightly later, after the main fingers part.

Initial experiments were conducted with this mechanism, using the blowout structure employed with the previously mentioned latched-in anode breaker. This blowout structure, employed very satisfactorily with the latched-in anode breaker, proved unsatisfactory on the faster breaker. The reason is that with the previous breaker the current reached a value under a certain set of test conditions of 30,000 or 35,000 amperes when arcing was initiated, whereas, with this higher-speed breaker, arcing was initiated at appreciably less than half this value. As a consequence, satisfactory speed of arc travel during the early formation of the arc was unsatisfactory, resulting in distress and delayed current limitation.

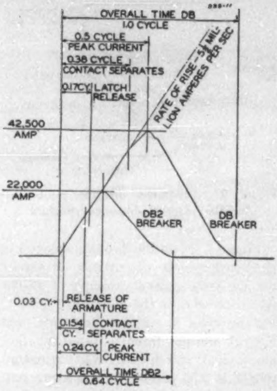

Figure 11. Composite current oscillogram comparing one-half-cycle and one-quarter-cycle anode circuit breakers

The final blowout design, which gave satisfactory performance, is shown in Figure 8. The same general principles, previously used, were maintained, but the primary blowout responsible for initial arc motion was strengthened, and the transfer to the secondary blowout was made much earlier.

The method of obtaining trip-free operation is shown in Figure 9. One compound wound motor is used to operate a six-pole unit, driving, through insulated shafts, the high-speed shafts of small speed reducers, one being mounted on each pole.

The low-speed shaft of each reducer carries a crank and roller over which a small steel chain rides. If the motor is run continuously and the armature is sealed against its magnet, the effect of the chain riding on this crank roller is to cause the breaker to go through an open-close cycle. A limit switch, controlling the motor with dynamic braking, insures this crank roller's stopping at the proper point.

Figure 10 shows a photograph of one complete single-breaker pole. For anode use, the holding-magnet shunt coils are energized at all times from the a-c auxiliary power for its rectifier through a small dry-type rectifier. The six holding coils of the six poles are connected in series, the breaker being tripped from the switchboard by means of the motor mechanism.

Figure 11 is a composite current oscillogram comparing the new breaker with its predecessor. The various time intervals are indicated for the two breakers. The information shown was taken from oscillograms made when testing the breakers by connecting them directly across the terminals of two 3,000-kw 600-volt 300-rpm compensated d-c generators in parallel, no reactance or resistance being in series with the breaker. The rate of rise is approximately the same as experienced in the rectifier station where the breakers are employed.

Recent Developments in Burying Telephone Cables

DONALD FISHER
ASSOCIATE AIEE

TEMPLE C. SMITH
NONMEMBER AIEE

THE term "buried cables" has come to mean those underground cables which have no conduit protection. Due to the accelerated demand for such construction in recent years, much effort has been expended in devising methods and developing machinery for burying cables. One of the earlier methods used in this and some foreign countries was to dig a trench by hand alongside the road; unreel the cable from a moving truck, thus laying it beside the trench; work the cable over into the trench by having 30 to 50 men handle it in relays; splice the cable in the trench, and finally backfill the spoil and tamp it by hand. Later variations of this method introduced one or more of the following units of machine equipment:

Power trenching machines.

Caterpillar tractors with trailers to straddle the trench, laying the cable directly from the reel into the trench.

Drag-line or other types of power backfillers.

Power tampers or rollers.

In order to further reduce the number of operations involved, speed up the installation, and reduce the cost, large plow trains have recently been developed which, except for splicing, in ordinary soil complete the job of burying a cable in one pass over the route. The idea of plowing cable into the ground is not new. In fact the great grandfather of all the cable plows was designed by Ezra Cornell long before he established the university. His "ponderous machine" drawn by a "long line of horses" was designed for laying telegraph cable in the early 1840's, but the development was dropped when the simple expedient of carrying wires on poles and insulators was conceived.

The large plow trains recently developed for installing telephone cable are capable of burying either a single cable or a pair of cables together with as many as three properly spaced lightning-protection wires, and of cutting a slot for them as much as 50 inches deep where such a depth is required. To provide the complete plow train has required the design of many pieces of equipment which the word "plow" does not suggest to one's mind. The plows and some of this equipment are discussed in the following paragraphs.

The Plow Train

Arrangement of the equipment in the train varies with the conditions. In Figure 1 two identical plows are included; the front one roots a trench $3^3/_4$ inches wide by 30 to 50 inches deep, thus insuring uninterrupted passage of the following plow which deposits two cables and the lightning shield wires, all properly spaced in the ground. This 100-ton train moves forward at the rate of a brisk walk, laying the cables and wires as it goes, with pauses for reel changes.

Where the ground is not hard and is free of rocks, and thus there is no danger of interruption to the plow from buried obstructions, the train make-up may omit the rooter plow, leaving three tractors, the cable plow, and the two trailers carrying reels of cable. On the other hand, it frequently happens that more difficult plowing conditions exist, or underground obstructions are known to be present, as, for example, with nests of heavy boulders (Figure 2). Here, two or three tractors and one plow will first go over the line one or more times to root the trench. As a separate operation, there will follow one or two tractors pulling a plow followed by a number of cable-reel trailers corresponding to the number of cables to be placed in the trench.

In rooting or plowing, occasionally the train may become stalled. The front tractor will then run ahead with its 76,000-pound single-line capacity winch and "winch out" the train with a two-to-one pull (Figure 3). One of the caterpillar tractors, weighing with full equipment more than 20 tons, has a maximum drawbar pull of 30,000 pounds on the level.

Burying Cable on Steep Grades

Unfortunately, when pulling up hills the available tractor drawbar pull decreases in proportion to the steepness of the grade, since a part of the power is consumed in raising the tractor weight. However, by careful handling of the equipment and generous use of the winch, cable can be plowed up or down hillsides even where the grades are as great as 60 per cent.

The preferred method of operation on mountainsides is to root down the grades, leaving a tractor on top to steady the heavy train with its winch line. In order to obviate any possibility of buried stones wedging at the side of the plowshare when placing the cable, the last pass of the plow doing the rooting work is in the same direction as the cable-laying plow will take. The trusty winch is also located on the hilltop when it is necessary to plow uphill. In contrast with the rooting operation, the cable must be laid uphill as well as downhill. This is because the reel lengths of cable normally used (1,500 to 3,000 feet) will span more than one hill, and it is undesirable to introduce any more splices than necessary in the cable.

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DONALD FISHER is engineer of outside plant, American Telephone and Telegraph Company, and TEMPLE C. SMITH is also with American Telephone and Telegraph Company, both in New York, N. Y.

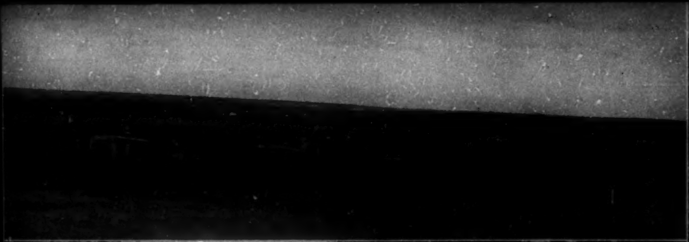

Figure 1. Plow train crossing great plains

The train consists of a 100-horsepower Diesel Caterpillar tractor, a traction-loading cable-reel trailer, two more tractors, a rooter plow, a fourth tractor, a cable-laying plow, and finally two winch-loading cable-reel trailers. Entire train is connected into one unit weighing 100 tons

Figure 2. Rooter train without plow has just passed

Plow comes later and is operated independently where soil obstructions are particularly bad

Figure 3. Pulling plow train out of soft ravine

The heavy steel cables from wire-rope block attached to front of tractor lead ahead to the winch and towing bar of head tractor

Figure 4. Airplane view

Used in selection of cable route

Figure 5. Bulldozer at work

Leveling off sharp dips in uneven ground ahead of plow train

Figure 6. Creek crossing

A roadway for plow train was cut by bulldozer to permit passage through creek banks

Selecting and Preparing the Right of Way

Now that we have seen something of what the plow train is like and how it can winch itself out of difficult situations, let us go back in the sequence of the operations and see how the route for the buried cable is selected, surveyed and explored so that the work of preparing the right of way can be started.

When it has been determined that a buried cable will be required between two points, possible routes are explored to establish the best location, such factors being kept in mind as accessibility, estimated cost of the construction, nature of the terrain, plant of other utilities, cost of right of way, future developments, and so on. In rugged country, this initial survey of the route may be made with the assistance of aerial photography. The airplane survey pictures are carefully studied through special lenses which give a three-dimensional effect in viewing them, and remarkable detail is afforded by the present-day photographic and viewing equipment (Figure 4). The relative heights of trees and buildings stand out with all the clarity of the old-time stereoscope. Since private right of way is generally followed, the use of this ideal method of route selection is often found to be worthwhile.

The tentative route laid out on maps or on the aerial-survey picture is now explored on the ground by engineers. As has been mentioned, the route ordinarily goes across fields, woodlands, mountains, and streams, but always consideration is given to accessibility from the highways and to the other factors which have been discussed. This is important both from the viewpoint of the ease of installing the

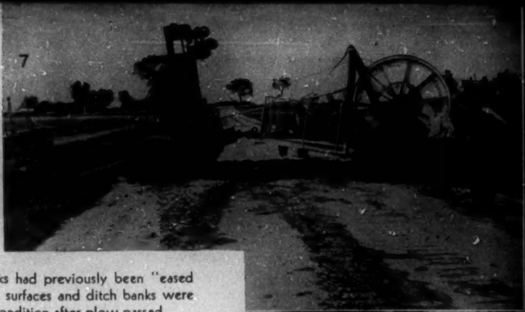

Figure 7. Square crossing of road

Sharp drop of ditch banks had previously been "eased off" by bulldozer. Road surfaces and ditch banks were replaced to original condition after plow passed

Figure 8. "Swamp grousers"

Caterpillar tracks augmented by oak cleats

Figure 9. Steel skid supporting plow tongue in marsh

Because of heavy down thrust on plow tongue a skid $3^1/_2$ by 11 feet is required to support it

Figure 10. Burying cable in mud

The problem is one of density and depth of mud, weight of equipment, the bearing-up area, and the winch power available

Figure 11. Burying cable in river bed

Power winch on tractor anchored in opposite bank pulls plow train across, depositing cable as it goes

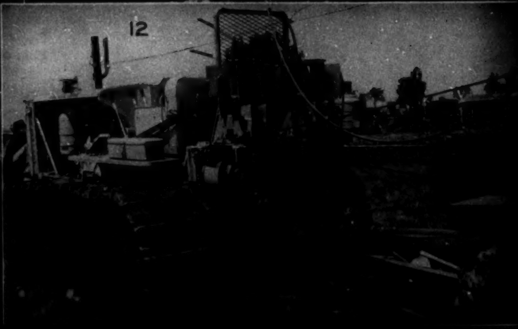

Figure 12. Tractor with four-drum winch

Multidrum winch used for adjusting plow depth, loading cable reels upon trailers, and moving trailers

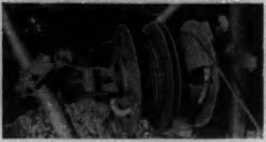

Figure 13. Relay winch

This winch pulls tongue end of rear trailer into special keeper which acts as a coupling. Paying out this rope permits rear trailer to lay behind the train

cable and that of maintaining it in the future.

Information regarding soil conditions and underground obstructions is very valuable in planning the route. Sometimes oil- and gas-pipe lines are encountered. The plow is sufficiently rugged, and the tractors have ample power to snap a good sized underground pipe in two. This method of striking oil is not to be recommended.

By the use of suitable apparatus, underground pipes can be readily located without excavating. There is commercially available a radio-tube type of underground-pipe locator which indicates the route but not the depth of the buried pipe. To determine its exact location, another electrical detector, developed by the Bell Telephone Laboratories, can be used. This device is so accurate that an underground pipe or cable can be located within less than an inch both laterally and in depth below the surface.

Through use of the information accumulated by the methods mentioned, the line of the proposed buried cable can now be staked out ready for the work crews. Buried boulders, ditches, and other obstacles interfere with the cable-plowing operation. Whatever preparatory work can be done to anticipate delays to the rooting and cable-plowing crews helps to "keep the train moving." At sharp ravines, road ditches, or creeks, the crossings become quite simple if a bulldozer on the front of a tractor (Figure 5) has first cut a roadway through the banks. Creek and river banks particularly require this treatment (Figure 6). It might be noted in passing that at such locations, if there is any danger of the cable being disturbed later by road-construction work or earth washing, the cable is plowed in at full 50-inch depth to afford maximum protection. The sides of steep road ditches are sloped off, not only to facilitate pulling the train across, but also to minimize the tilting of the plow, with the resulting tendency to raise the bottom of the share, thus laying the cable too shallow (Figure 7).

The plow operates satisfactorily across gravel and macadam roads as well as those which are not surfaced. Of course, the roads and ditch banks are restored to their former condition.

Experience indicates that it is not safe to operate along hillsides where the grades are more than about 10 per cent. In attempting to pull around the side of the hill, there is always a tendency for the train to work down grade. For such conditions, the bulldozer is used to good effect cutting out a level roadway.

Of course, in wooded country, a roadway must be prepared for moving the cable reels and equipment along the right of way as well as for clearing a place to plow.

On private right of way an easement on a strip of ground about one rod wide is ordinarily secured, anticipating the possible future need of a second buried cable. A passageway at least 10 feet wide is cleared, and at reel-change points additional width is required to maneuver the equipment. The 10-foot width will permit passage of the train in rooting and plowing. However, unless the cost of clearing an extra 3 feet of right of way is excessive, it is very desirable to have a 13-foot passageway so that the $8^1/_2$-foot-wide tractor can be used for tamping, as will be discussed later. Where practicable, the trees within the proposed passageway are pulled out by the roots in order to eliminate the interference with plowing which the roots would cause.

Crossing Swamps and Streams

Swamps and very soft ground present a difficult problem. For such work the tractor-caterpillar tracks are supplemented with "swamp grousers" (Figure 8), constructed of overhanging, bolted-on oak cleats. The plow tongue is supported by a special steel skid (Figure 9) $3^1/_2$ feet wide and 11 feet long. To provide increased bearing surface for carrying the load of the plow, skids may be added under the 12.75- by 24-inch pneumatic tires. These are in addition to the mud-bearing plates under the plow frame. The cable-reel trailers carry large mud-bearing plates under their frames.

By such expedients as these, the unit bearing pressure on the mud is so reduced for each member of the train that it can be slid over the soft surface placing at the same time the buried cables and wires at the proper distances beneath the surface (Figure 10).

The technique followed in crossing streams is similar in some respects to that used in negotiating marshy ground (Figure 11). If the water is deep, the tractor may find it necessary to detour to shallower water or by the nearest bridge, and it may be found desirable to root across before laying the cable under the stream, whereas, in the soft marsh the rooting may not be necessary.

Important Features in Operation of Plow Train

The caterpillar tractor placed next to the plow in the train is equipped with a four-drum winch (Figure 12) with independent lever controls conveniently located for the tractor driver so that he can exert a pull up to about 6,000 pounds in

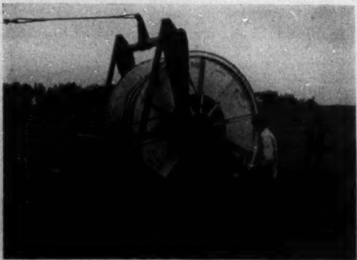

Figure 14 (left). Yoke of winch loading trailer lifting a reel of cable

Yoke is pulled up by winch rope from one drum of multi-drum tractor winch. Spindle has one foot to go before latches will catch it

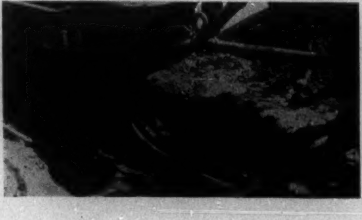

Figure 15 (above). Cables emerging from plowshare

In starting a job cable ends are pulled through share and staked before plow moves ahead any one or more of the four winch ropes.

Two of the ropes are used to raise and lower the plowshare, thus adjusting the cable depth. The third rope feeds to a new-type "relay winch" (Figure 13) mounted in the front trailer. On dual-cable jobs this winch pulls the rear trailer up to its coupling under the axle of the front trailer in the operation of changing reels.

The fourth rope from the multidrum winch leads to the arm of the reel-lifting yoke on either trailer as shown in Figure 14. Pulling the yoke forward raises the (possibly 10,000-pound) loaded cable reel into position where the reel spindle is locked in the traveling position. Reversing the operations and slowly paying out the winch line lowers the empty reel from the trailer.

Feeding Cables and Wires Into Plow

In starting a dual-cable job the plowshare is raised to the top position and the cable ends fed in until they emerge from the exit opening (Figure 15). The cable ends are secured against movement along the ground, the share is gradually lowered to depth in the first few feet of travel, and the plow train is moved forward, thus laying the two cables at the desired depth until the reels are empty. After the empty reels are exchanged for full ones, the new cable ends must be fed through the plowshare. This is done by connecting each new length of cable to the one which has just been placed by overlapping the ends and binding them together (Figure 16). As the plow train now starts on the next installation, the spliced ends are guided into the share by two men riding on a platform carried on the front trailer tongue, and a cycle of operations has been completed.

Where lightning-shield wires are required, the reels of wire are carried on the front trailer or on the plow tower as in Figure 7, and they are fed through special ducts in the share from which they are emitted at the desired locations in the ground.

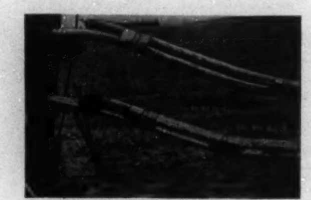

Figure 16. Cable ends clamped ready to enter plow

Each succeeding cable end is clamped to end of cable ahead, in order to pull it into plowshare

Figure 17. Changing reels on front trailer

Train must be broken in order to get new full reel to the front trailer

Changing Reels

Changing the reels as mentioned above is necessary for every cable length. Where one cable is being laid, this is a simple matter, but, where two reels are involved, the changing of the front one presents some difficulties, as can be visualized by referring to Figure 3. The rear trailer completely blocks the operation. In order to leave space between the two trailers, thus permitting the front one to be loaded, the rear trailer is dropped from the train as it moves along, about 50 feet before the cable end leaves the rear reel. This is accomplished by releasing the relay winch, thus disconnecting the rear trailer from the train, just as the engineer of a switching locomotive might drop a car from his train. The front trailer is now accessible from the rear so that the reel can be lowered from it (Figure 17). A tractor with a third trailer now moves a full reel to the front trailer and it is loaded. The relay winch comes into action at this point to pull the rear trailer up to its working position where it is automatically coupled in the train, after which it receives a new full reel of cable.

Lubricating the Cable

The buried cables ordinarily used, range in size from 1 to 2$^1/_2$ inches outside diameter. However, cables as large as 3.2 inches in diameter may be used by employing a wider share which the plow is designed to accommodate. Usually they are covered with an asphalt-impregnated jute wrapping under which, in gopher-infested territories, there will be steel gopher tape surrounding the conventional lead sheath. In some cases a thermoplastic rubber and burlap covering is used instead of jute.

The asphalt-impregnated coverings develop high coefficients of friction against the steel walls of the rectangular tube through which they pass while in the plowshare. This results in tensions as high as 5,000 pounds in the cable, which is objectionable for electrical as well as mechanical reasons. The tension is reduced to a safe maximum of less than 1,000 pounds by directing a steady oil spray on the cable as it enters the share. This is accomplished by an adaptation of a paint spray gun using compressed air or nitrogen supplied from a cylinder carried on the plow.

Safety Shear Pin

With the powerful tractors pulling in series formation, what happens if the extremely rugged plow hits, let us say, a buried ledge of solid rock? The plow is an integral part of the very heavy briskly moving rooter train with its combined tractor drawbar pull of 200 or 300 horsepower. Under such a condition a safety shear pin of 72,000-pound strength, in the plow tongue, releases the load.

Backfilling and Tamping

In plowing, some soils may be considerably disturbed by the passage of the 3$^3/_4$-inch-wide share with the extra ducts, familiarly known as "blisters," on the sides to carry the lightning shield wires. As the rear trailer passes over the trench, it drags a V-shaped device which mounds the loose earth over the trench (Figure

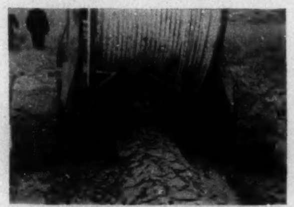

Figure 18. Backfiller

A V-shaped device under rear trailer mounds up disturbed earth over trench in which cable has been deposited

Figure 19. Where buried-cable right of way crosses the high Sierras

In this solid granite, a shallow narrow trench will be prepared by chain blasting

18). Then a good job of tamping is done by running a caterpillar-tractor track along the mound. This is done by the tractor which is used for handling reels.

Signals

The clatter of the Diesel engines makes vocal signals on the job unreliable. A tractor exhaust whistle or an electric horn is connected to a long control rope extending back along the train and so located that it can be reached from convenient points at either side of the train to give signals.

Performance of Cable-Burying Gangs

Under ordinary conditions it is possible to place about 17 trench miles of cable per five-day week with this equipment. This would mean that a foreman with his crew of about eight men, on a dual-cable job, might, in a week, bury 34 miles of cable together with whatever lightning-shield wires are required.

Conditions vary widely from the prairies with their black loam and clay to the steep mountains, or the soft marshes, or to soil sown thick with boulder. The mileage of cable buried daily naturally will correspond to the conditions encountered.

Burying Cable by Other Than Plow Methods

Because of its speed and economy the plow train is used wherever practicable for burying cables. In extremely rough mountainous territory some right of way may be too steep or rocky or too inaccessible, but it is surprising how small relatively is the footage even here which cannot be economically plowed.

For instance, a location on the proposed transcontinental cable line, where there has been some question whether the plow could be used, is in the ten miles of hard salt right of way along the highway adjoining and parallel to the Bonneville automobile-racing flats in Utah. However, in exploratory trials at this location, the plow rooted through the hard salt beds very satisfactorily.

There are some locations in the high Sierras where a relatively small portion of the proposed Sacramento-Reno section of the transcontinental cable run must cross areas of practically solid granite (Figure 19). Here the engineers plan to prepare by chain-blasting a narrow shallow trench in which the cables will be laid and the trench filled with an asphaltic material which will hold its position, keep water out of the trench, and protect the cable.

In a paper such as this, only the major operations and the principal items of equipment can be mentioned. Many others have had to be developed in order to make the use of buried cable broadly applicable. There are the jobs of passing under concrete arterial highways where the pavement cannot be disturbed and the soil may be either earth or rock. There is the matter of finding a way to cross under rivers too swift and full of boulders for submarine cable, having solid granite beds which, of course, cannot be plowed. There is the matter of avoiding buried pipes and other obstructions. These are no small matters. On one 83-mile run there were 91 crossings of oil-pipe lines at the time of the survey. Before the work was done, in a few months, four new pipe lines had been gained and three old ones lost.

All these and many other operations must be planned and executed in an extensive program of burying telephone cables, such as is now being carried on by the Bell system.

A New Instrument for Recording Transient Phenomena

S. J. BEGUN
NONMEMBER AIEE

Synopsis: In many cases it is of great importance to study phenomena which do not occur periodically. Such phenomena, called transients, arise, for example, at the time a short circuit appears in a power line, during the discharge of the condenser of a spot-welding machine, during the starting of electric machinery, and on very many other occasions.

Most of the instruments available thus far for investigation of transients employ the method of film recording, which has the disadvantage of requiring a developing process.

A new transient recorder has been developed, employing magnetic-tape recording as a means of preserving the record of a transient and steadily repeating this record on the screen of an oscilloscope. This method has the advantage that it requires no processing and that the same magnetic carrier can be used continuously without loss of material.

THE investigation of transient phenomena has always been of great interest to physicists and engineers. The observing of transient phenomena presents a major problem, mainly when the time the transient occurs cannot be anticipated, and where the duration of the transient is so short that visual inspection of it is impossible.

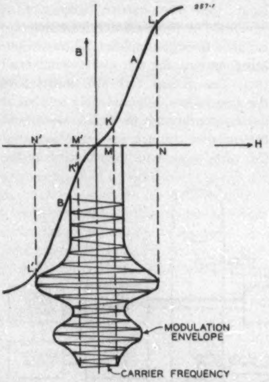

Figure 1. The modulated carrier acting on the magnetization curves of magnetic-recording medium

In most methods known heretofore, recording of the transient phenomena is employed. Usually a permanent record of all occurrences, including the transient under investigation, is made, and the interesting part of the recording is analyzed. Where transient phenomena occur rarely, it seems obviously a waste of material to record continuously the steady-state conditions, only for the benefit of an occasional transient. To meet this difficulty instruments have been developed which will start recording only when the transient occurs. Such instruments require very specialized equipment, particularly since it is usually of great importance to obtain a picture of the complete transient instead of merely one part of it.

Film recording is used in most cases. Mechanical recording, as employed in high speed level—or pen recorders, has not found any wide application, mainly because of the limited frequency range due to the mechanical parts involved.

Different film-recording instruments have been successfully marketed and are now being used in field and laboratory work. The use of film has the great disadvantage that it requires a developing process, and in many cases this requirement makes such an instrument impractical.

These and other difficulties have been overcome by a new transient recording apparatus to be described. This new instrument uses magnetic recording. The principle of magnetic recording and reproduction has been widely discussed in technical literature.[1] The method as used in this instrument, however, involves some novel features which are of interest.

In the magnetic-recording process, small electromagnets known as magnetic heads are subjected to the signal current and produce a magnetic pattern on the recording medium moving between their pole pieces. This magnetic pattern varies in intensity from point to point on the medium, and this variation of the magnetic intensity corresponds to the signal being recorded.

In the reproduction process, this varying magnetic pattern moves past the pole pieces and induces in the pole-piece coils a voltage correlated to the originally recorded signal.

To obliterate the recorded signal it is only necessary to remove the varying magnetic pattern and establish a uniform magnetic state. This is usually accomplished by saturating the magnetic carrier. An alternative way of obliterating is to demagnetize and return the magnetic carrier to its virgin unmagnetized state.

There is, however, one difficulty inherent in the magnetic recording system: it is not possible to reproduce very low frequencies, since the voltage generated in the reproducing head is a function of frequency and becomes small indeed for very low frequencies. On the other hand, transient phenomena often require the recording of not only very low frequencies, but even direct current. Therefore, transient signals in many cases cannot be used directly to actuate the recording head, since it is difficult, if not impossible, in reproducing such recordings to compensate sufficiently for the fall-off at these low frequencies. This difficulty has been overcome by utilizing a carrier frequency which is modulated by the signal to be recorded.

In conjunction with this carrier-recording system, it has been found preferable to obliterate the record by returning the tape to an unmagnetized state. This gives two advantages:

1. It makes unnecessary the use of a d-c polarizing current which normally must be superimposed on the signal to be recorded when obliteration has been obtained by magnetic saturation. This polarizing current, on breaking, would set up its own transient which is unrelated to the investigated transient.

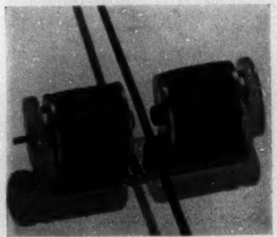

Figure 2. View of obliterating head

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S. J. Begun is with the engineering department of the Brush Development Company, Cleveland, Ohio.

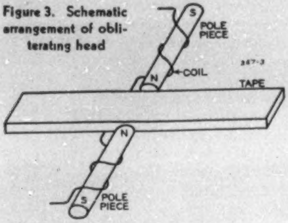

Figure 3. Schematic arrangement of obliterating head

2. When used in conjunction with the carrier system, this obliteration method produces a higher signal-to-noise ratio.

The carrier-recording process may now be analyzed by reference to the two branches A and B of the magnetization curve as shown in Figure 1. These magnetization curves are relatively linear between points K and L and points K^1 and L^1. The unmodulated carrier current is so adjusted that the extreme values of the envelope produced by the modulating signal lie within this linear portion. One half of the modulated carrier acts on magnetization curve A and the other on curve B. The nonlinearity of the magnetization curve near the origin is of little effect since not more than 70 per cent modulation is used. Therefore, the peak values of magnetic induction recorded on the passing medium will be linearly proportional to the peak values of the carrier frequency, thus producing a magnetic facsimile of the envelope.

To obtain a proper reproduction, the reproducing head must be able to take care of the frequency range which is determined by the carrier frequency and the side bands. This, however, can be done without any great difficulty.

The present instrument uses a thin magnetic steel tape as recording medium.

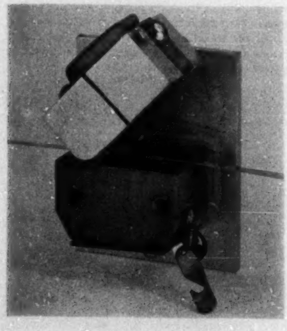

Figure 4. View of recording–reproducing head

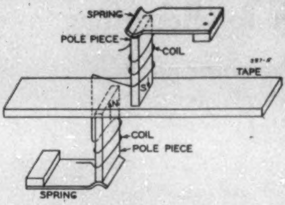

Figure 5. Schematic arrangement of recording–reproducing head

The obliterating is done by a head of novel design. This head consists of two electromagnets oppositely mounted across the width of the tape and polarized so that the opposing ends of the pole pieces have the same polarity, thus producing a diffused magnetic field. This diffused field allows the magnetic-field strength to decrease slowly and gradually as the tape passes by, and since a-c is supplied to the obliterating coil, the magnetic tape emerges completely demagnetized. The obliterating head is shown in Figure 2 photographically and in Figure 3 diagrammatically.

For recording and reproducing, a single standard magnetic head may be used. Such a head is shown photographically in Figure 4 and diagrammatically in Figure 5.

The magnetic-recording method makes it possible to automatically obliterate any previous record and to substitute a new one for it. This unique characteristic of magnetic recording permits continuous carrying on of the recording and obliterating process until a transient phenomenon occurs. As long as steady-state conditions prevail, the record made of them is continuously obliterated, since this can be done without loss of material. The new instrument makes use of an endless magnetic tape on which the signal is continuously recorded and after a short time interval again obliterated. The recording and obliterating process is stopped only when a record of a transient occurrence has been made on the moving tape. The obliterating and recording heads are placed close together in such a way that each portion of the moving tape passes at first through the obliterating head and shortly thereafter through the recording head. The time interval which is required for the tape to complete one cycle, namely to leave the recording head and reach the obliterating head is the available recording time, and this recording time has to be somewhat longer than the maximum duration of the transient to be investigated.

As soon as a transient occurs, a trigger system is actuated which automatically stops the recording process as soon as the tape completes the cycle, as has been just described. The time length of a cycle can be increased by increasing the length of the tape loop. The timing of the trigger circuit is adjustable, in order to conform with this increased time length. Such a trigger circuit is made an integral part of the instrument.

After the transient has thus been recorded on the tape, it will now be periodically reproduced, since the tape is endless. This regular repetition makes it possible to synchronize the horizontal sweep of an oscilloscope with the tape cycle, thus giving a steadily repeating picture on the screen of the oscilloscope, and facilitating observation of the transient.

Figure 6 shows a block diagram of the system. The signal to be investigated is applied to terminals a and b of the modulating circuit (1). The carrier frequency modulated by the signal is amplified by the amplifier (2) and passes then through a filter (3) and switch (4) to the recording head (5). The carrier frequency is generated by the oscillator (6) which also supplies through amplifier (7) an obliterating current for the obliterating head (8) The endless tape (9) moves over the two rollers (10) and (11) so that it passes first through the obliterating head (8) and then through the recording head (5). Any signal which is recorded by the

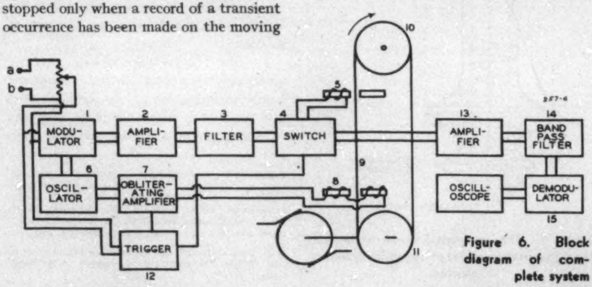

Figure 6. Block diagram of complete system

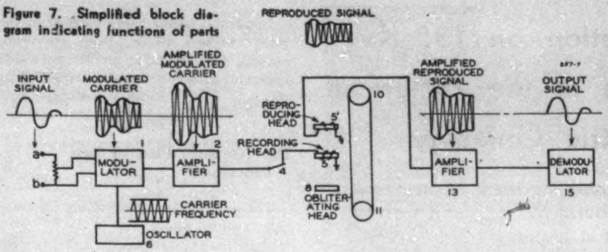

Figure 7. Simplified block diagram indicating functions of parts

Figure 8. View of the instrument

recording head remains on a given portion of the tape until this portion reaches the obliterating head, which eliminates any previous signal from the tape and prepares it to accept a new recording. The transient signal is also supplied to a trigger circuit (12) which operates with a time delay. As soon as a transient occurs, the trigger circuit becomes energized, and after a short time interval, slightly shorter than one complete tape cycle, it blocks the obliterating amplifier. Simultaneously, the trigger circuit disconnects, by means of switch (4), the recording circuit from the recording head (5). The system is now ready for reproduction. The reproducing head, which is in this apparatus the same as the recording head, supplies the induced signal to the reproducing amplifier (13) which supplies the signal through a band pass filter (14) to a demodulator (15). The demodulated signal is now reproduced on the oscilloscope.

A simplified block diagram is shown in Figure 7 and illustrates graphically the electric functions of the essential parts of the system.

The different filters serve to eliminate spurious signals.

Great care has to be taken to eliminate phase distortion and amplitude distortion. For proper analysis of a transient phenomenon, the visual picture of the reproduced transient must be identical with the original. Because of this requirement, such an instrument is subject to much more severe restrictions than a sound-recording apparatus, where phase distortions are usually considered unimportant. By proper design of the filter circuit and the other circuit components, amplitude and phase distortion is reduced to an inconsequential minimum for a given frequency range.

The transient recorder in its practical form comprises two units as shown in Figure 8. Unit A contains the modulator, amplifier, and the trigger system. Unit *B* contains the drive mechanism for the tape loop, the magnetic heads (5) and (8), and the power supply. In the drive mechanism a synchronous motor propels by way of a belt the drive roller (11). A flywheel associated with the drive roller assures a uniform speed of the tape. The idler roller (10) can be moved in a slot (16) so as to tighten the tape and also make adjustment possible for tapes of different lengths. The recording-reproducing head may be opened for removal of the tape loops. If it is desirable to preserve an interesting transient on the steel tape, the tape may be removed and kept for any length of time with the signal recorded on it.

Unit *A* with the modulator, amplifier, and trigger has only electronic parts. To assure satisfactory operation independent of line-voltage fluctuation, the oscillator for supplying the carrier frequency and the obliterating signal has been stabilized with respect to frequency and amplitude. The carrier frequency which has been selected for this particular unit is 2,200 cycles. The band pass filters are so designed as to permit the transmission of the carrier frequency and side bands necessary to obtain the desired frequency range, namely 0 to 500 cycles, without phase and amplitude distortion.

The new transient recorder was originally developed to meet the difficulties in investigating transients occurring in the different welding processes, particularly in the condenser-discharge type of spot welding. The frequency range of the instrument has been chosen for this particular application, and the limit of 500 cycles takes care of the frequency components to be expected. The maximum recording time is about 0.2 second and the required input transient voltage is about 2 volts.

Much work has been done of late with such an instrument in the investigation of welding processes, and it has proven to be very valuable as a laboratory tool. Undoubtedly it will also prove useful for production processes. The instrument may be easily adapted to many other applications. Among these are transient occurrences in power lines, transient phenomena in loud speakers, illumination patterns of photo flash bulbs, shock-excited vibrations of mechanical parts. Many other applications will undoubtedly be found.

The instrument may be designed for different frequency ranges, depending upon the requirements, but still utilizing the same fundamental principle.

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Lightning Investigation on 132-Kv Transmission System of the American Gas and Electric Company

Field Study of Natural Lightning Currents Measured in Towers, Line Wires, and Ground Wires; and Currents and Voltages at Stations

I. W. GROSS
MEMBER AIEE

G. D. LIPPERT
ASSOCIATE AIEE

I. Introduction

THE field study of the characteristics of natural lightning on transmission systems, started some 15 years ago, has been carried on with continued persistence by various investigators up to the present time. On the American Gas and Electric Company 132-kv interconnected system, investigation was started in 1927, and the results obtained have been reported in papers[1-9] before the Institute from time to time, the last appearing in 1937. This present paper presents largely the data obtained from the field work which we have carried on since that time on the above system in an attempt to learn more about the characteristics of natural lightning, particularly lightning currents and rates of voltage rise, as they affect transmission lines and equipment. It includes field data for the past four years, combined in some cases with previous field data in order to make the records complete and inclusive.

II. Purpose and Scope of Investigation

Without attempting to summarize the various phases of the lightning problem to which field research has been directed in past years, it is sufficient to state that the past four years' work in our system has been confined to furthering information along the following general lines of attack:

1. A study of the distribution of lightning currents in parts of the transmission system such as ground wires, line wires, towers, counterpoises, and ground rods.
2. The effect of different methods and technique in grounding, such as with counterpoises and ground rods.
3. Study of lightning voltage, magnitude and wave shapes appearing at stations, as an aid in co-ordination of station insulation and protection.

Incidental to the main purpose of the work outlined above, there are other aspects of the lightning problem on which information has been made available from the field data. These will also be presented and discussed as the data warrant.

Table I. Lightning-Measuring Instruments—Location—Number and Magnitude of Records Obtained on 132-Kv System—1933 to 1941 Inclusive

Ref.	Instrument Location	Instrument Years	Number of Records	Record Magnitude		
				Maximum	Median*	Minimum†
1	Tower legs above ground	3,163	991	25,000	5,300	1,000 Amperes
2	Tower legs below ground	344	54	12,000	2,100	1,000 Amperes
3	Tower-leg sections	244	49	24,000	3,500	1,000 Amperes
4	Tower-leg braces	180	24	6,900	1,250	700 Amperes
5	Tower arms	1,977	372	26,000	2,800	1,000 Amperes
6	Tower-arm braces	237	48	13,600	2,000	600 Amperes
7	Tower lightning rods	560	70	82,000	22,000	12,000 Amperes
8	Counterpoises	937	662	24,300	4,000	200 Amperes
9	Counterpoise tie between tower legs	57	40	10,200	1,250	200 Amperes
10	Ground rods at towers	169	31	11,900	3,000	200 Amperes
11	Ground wires at towers	545	643	70,000	5,000	1,000 Amperes
12	Ground wires at stations	56	53	11,700	2,800	1,000 Amperes
13	Conds. at towers	274	261	29,000	1,800	1,000 Amperes
14	Conds. at stations	117	170	11,400	1,800	1,000 Amperes
15	Wave-slope indicator	77	539	600	200	10 Kv per μsec
16	Surge-voltage recorder at stations	18	151	525	160	120 Kv
	Totals	8,955	4,158			

*50 per cent of records at or above this value †Min. recording sensitivity of instruments.

III. General Plan of Test Procedure

The general plan of obtaining information outlined above was to install lightning-measuring instruments such as surge-crest ammeters, surge-voltage recorders, and wave-slope indicators on some lightning-infested parts of our 132-kv interconnected system. The lines on which the major part of the data presented and discussed here were obtained are the 132-kv lines of the Appalachian Electric Power Company (Glenlyn-Roanoke), the Indiana and Michigan Electric Company (South Bend-Michigan City), The Ohio Power Company (Philo-Canton), and the Atlantic City Electric Company (Deepwater-Pleasantville).

Instruments

The surge crest ammeter[10] using magnetic links was used to determine lightning currents. At stations the surge-voltage recorder[11] was used for determining lightning voltages and the wave-slope indicator rather extensively to determine the lightning-voltage rates of rise.

The wave-slope indicator is based on the following theory, using the well known elements of the electric circuit q, i, and e:

$$q = ec = \int idt$$

$$cde = idt \text{ and } de/dt = i/c$$

Maximum i thus gives maximum de/dt. A wiring diagram of the instrument as used in service is shown in Figure 1A; and field installations in Figures 1B and 1C.

A typical field setup of magnetic links to determine lightning currents at various points on the transmission system is

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I. W. Gross is electrical research engineer and G. D. Lippert is with the engineering department of American Gas and Electric Service Corporation, New York, N. Y.

The field investigation work on which this paper is based was undertaken and planned in co-operation with engineers of the General Electric Company, who furnished the instruments and aided in correlating the data. The authors acknowledge the assistance of the field organizations of the Appalachian Electric Power Company, The Ohio Power Company, Indiana and Michigan Electric Company, and Atlantic City Electric Company, in installing and servicing the instruments in the field.

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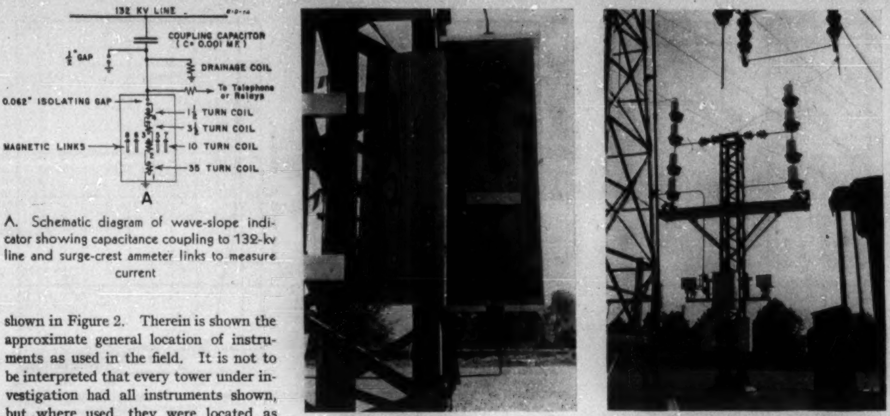


A. Schematic diagram of wave-slope indicator showing capacitance coupling to 132-kv line and surge-crest ammeter links to measure current

B. Wave-slope indicator installed in weatherproof housing in the field

C. Two wave-slope indicators installed on the Roanoke–Reusens line near base of steel structure which carries two 132-kv coupling capacitors for communication service

Figure 1. Wave-slope indicator for determining rates of voltage change

shown in Figure 2. Therein is shown the approximate general location of instruments as used in the field. It is not to be interpreted that every tower under investigation had all instruments shown, but where used, they were located as indicated in the figure.

A concentration of instruments was made at several sections of the line where counterpoises (500 feet and continuous) were installed on the Glenlyn-Roanoke line, and also at some five substations to determine currents in line wires and ground wires, and the wave slope of incoming voltages to which equipment installed in the stations would be subjected.

The data given and discussed below have been obtained from field records as indicated in Table I. Therein it is shown that some 8,955 instrument years have been involved during the nine years in this investigation and have produced a total of 4,158 records. The distribution of these records among various parts of the tower structure, line wires, ground wires, counterpoises, and so forth, as well as maximum, minimum, and median values is clearly shown in the table.

Throughout the investigation, all instruments were serviced approximately once each two weeks to enable the best possible segregation of records without burdensome field servicing. In addition, instruments were frequently serviced after severe lightning storms which were known to have taken place in the general vicinity of the test stations.

IV. Lightning Records and Data

Current Magnitudes

Lightning-Stroke Current

A determination of current in the lightning stroke has been arrived at in four ways:

1. By multiplying the current measured in one leg of a tower, where only single-leg measurements were attempted, by four (these towers have four legs).
2. By adding the separate currents measured in all four legs of a tower.
3. By direct measurement of the current in a lightning rod placed at the top of the tower.
4. By adding the measured ground-wire currents flowing toward a stroke in midspan.

These data have been plotted in Figure 3 as a magnitude-frequency curve. The measured tower-leg values have been increased by 50 per cent to account for the unmeasured cross-brace current, a procedure previously justified.

Comparing these data with those previously published,[9] the range of indicated stroke currents is in substantial agreement. The maximum current here indicated is, however, 150,000 amperes by the single-leg-measurement method compared with the previously reported maximum of 220,000 amperes by the method of adding adjacent tower currents. Direct measurement of current in the lightning rod showed a maximum of 82,000 amperes and measurement in ground wires 110,000 amperes. As both of these methods of measurement resulted in partially saturated links at high currents, it is believed the maximum stroke current is slightly higher than the above 110,000 but not much in excess of 130,000 amperes, which in turn is only slightly higher than the current measured in a single tower.

It is rather interesting to note that the method of measuring currents in one tower leg and multiplying by four gives results quite comparable to the other methods except that it does show a slightly higher maximum current.

Ground-Wire Currents

Measurements of ground-wire currents were made in the line sections some distance out from the station, as well as at the first tower out from the station, and at the station itself. Some 700 separate current records were obtained on ground wires, 643 being of sufficient magnitude and certainty to warrant analysis.

Figure 4 shows the magnitude and frequency of these currents at the three locations (line, near station, and at station). In the line sections the maximum current was 70,000 amperes; at the first tower from a station, 36,000 amperes, and on the ground wire coming into the station, 12,000 amperes. Where measurements were made directly at the station, they were in all cases on one of three fanned-out ground wires extending from the station to the first tower, where single ground-wire construction over the line started.

That the single-ground-wire and three-ground-wire current curves (Figure 4) cross at approximately 7,500 amperes has no significance and is caused by the fact that the data do not attempt to indicate simultaneous current records on the ground wires, but rather the general magnitude and distribution which may be

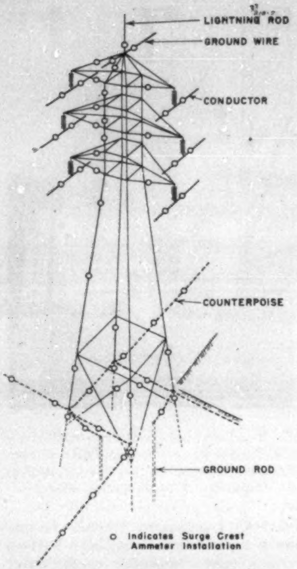


Figure 2. Schematic diagram of tower showing location of magnetic links on various parts of the tower and conductors

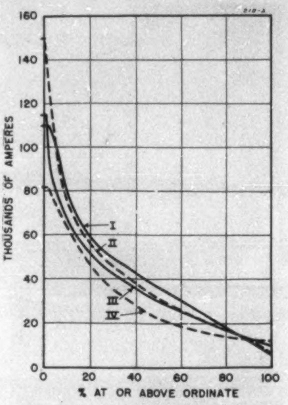


Figure 3. Lightning-stroke currents

Magnitude and frequency determined as follows:

I. Summation of ground-wire currents—51 records

II. Total tower current by multiplying single-leg readings by four—104 tower records

III. Total tower current by addition of four-leg currents—142 tower records

IV. Currents measured directly in tower-top lightning rods—52 records

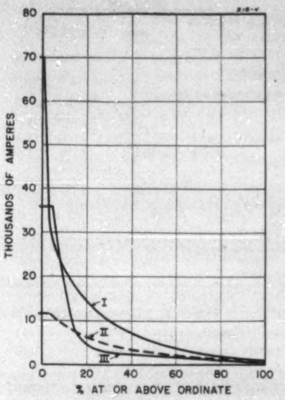


Figure 4. Lightning currents in overhead ground wires

I. At line towers—591 records

II. Currents in one of three ground wires between station and first tower out—30 records

III. In single ground wire on the line side of the first tower from station—22 records

expected where this type of ground-wire construction is employed near a station.

Lightning Currents in Line Wires

The determination of currents in line wires was undertaken for two reasons:

1. In an attempt to correlate the lightning currents and surge impedance of the line with the lightning voltage expected with such currents.

2. To determine the magnitude of lightning currents which might enter a station from the line and would have to be handled by the protective devices.

Over 300 records in line conductors have been obtained, 154 measured at the station and 172 on line towers some distance away. The magnitude and frequency of these currents are shown in Figure 5. The maximum current out on the line is 29,000 amperes and at the station 11,500 amperes. Ten per cent of the measured currents are 6,500 amperes or less at the line towers and 3,000 or less at the station.

Table A

	Amperes		Corresponding Kv of Line Wire	
Instrument Location	Maximum	10% of Values at or Above	Maximum	10% of Values at or Above
On the line	29,000	6,500	11,600	2,400
At stations	11,500	3,000	6,600	1,200

If it is assumed that these measured lightning currents can be simply multiplied by the line-surge impedance (taken as 400 ohms), to obtain the conductor voltage, then the voltages of the line conductors would be as shown in Table A.

The line-current data, analyzed as above, give indicated conductor voltages which seem rather fantastic under the conditions of operation. The magnitude of currents in the line conductors at stations, however, correlates quite closely with the measured currents in lightning arresters as has been reported previously.[12]

Currents in Towers, Counterpoises and Ground Rods

Currents in counterpoises as measured at the point where they attach to tower legs were measured in five different types or lengths of counterpoises, namely, 40, 150, 250, 500 feet, and continuous. The continuous counterpoises were installed between towers where the tower spacing was approximately 1,500 feet or less.

The counterpoise currents recorded on the above basis are shown graphically in Figure 6, from which it will be noted that the currents carried by the counterpoise increase with length, although not in direct proportion to the length. The most searching type of test would, of course, be one where all these counterpoises of various lengths were attached to the same tower and therefore affected by the same stroke current, but since a setup of this kind was not practical, the comparison of data has been made on actual measured currents. This method does not evaluate the variation of stroke current which initiates current in the counterpoise. However, since the data cover some eight years of field tests, the varying magnitude of stroke currents has probably been averaged out to some extent so that the data given indicate to a large degree the relative benefits of the counterpoise length.

Comparative data on a selected group of towers where 500-foot and continuous counterpoises were installed, and, in addition, tie connections between the four tower legs, are given in Figure 7. The tower currents have been obtained by four-leg readings increased by 50 per cent for the shunting effect of tower bracing, and show a maximum of 79,000 amperes. The 500-foot and continuous counterpoises show maximum currents

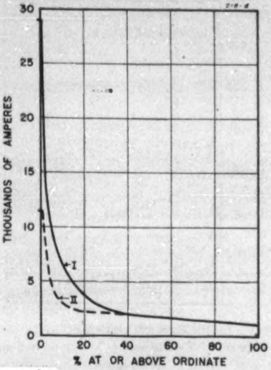


Figure 5. Lightning currents in conductors

I. Conductor currents at line towers—172 records

II. Conductor currents at station entrance—154 records

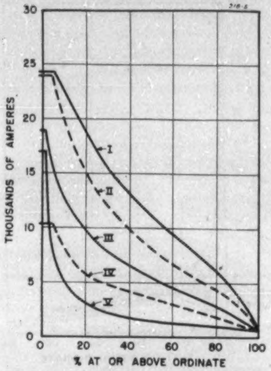


Figure 6. Lightning currents in counterpoises

Measured at point of junction to tower leg

I. In continuous counterpoises—22 records

II. In 500-foot counterpoises—23 records

III. In 150-foot counterpoises—79 records

IV. In 250-foot counterpoises—22 records

V. In 40-foot counterpoises—67 records

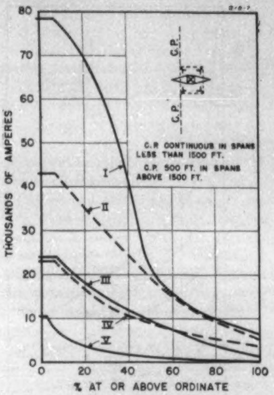


Figure 7. Lightning currents in towers and counterpoises

I. Total tower currents—15 records

II. Summation of currents in counterpoises both sides of tower—15 records

III. In single 500-foot counterpoises—14 records

IV. In single countinuous counterpoises—16 records

V. In buried cross-tie connections between tower legs at towers—40 records

and frequency distribution of approximately the same magnitude. This would be expected if the continuous counterpoise can be considered as effective only for about half the span (or 400 to 750 feet). In fact, the agreement of curves III and IV seems to indicate that increasing the counterpoise much beyond 500 feet is not particularly effective.

One important feature brought out by these data is the records of currents obtained in the buried ground or counterpoise wire which ties the four tower legs together at the base of the tower. This connection consisted of a $^1/_{16}$-inch by 2-inch flat iron strap buried 18 inches in the ground and extending around the base of the tower, as shown in the sketch of Figure 2. The current carried by these tie connections is shown in curve V of Figure 7, a maximum of 10,200 amperes being indicated. While it might seem, in tying a counterpoise direct to one leg of a conventional four-leg tower, that the tower cross bracing would be effective in quickly distributing the lightning currents to the other legs of the tower, such does not seem to be the case (see also Figure 11). It is strongly indicated that, where counterpoises are used, means should be taken to make direct underground connections to all tower legs to obtain the maximum benefits from an even, uniform, and quick distribution of current to the various tower structural members.

A comparison of tower and counterpoise currents with 40-foot and 150-foot counterpoises attached is shown in Figure 8. Here, the benefits of a number of short counterpoises are indicated as compared with using the same length of buried wire as a single counterpoise. Taking the median values in Figure 8, 80 feet of counterpoise in two short sections carry 5,000 amperes, and 300 feet of the 150-foot counterpoise in two sections, 17,000 amperes, or 3.8 times the length, carry 3.2 times the current. When comparing maximum currents, the results show still greater efficiency for the short counterpoise. Again comparing median values for the 40-foot counterpoise (Figure 8) with the 500-foot counterpoises of Figure 7, the ratio of increase in length is 5.9, and the ratio of currents 2, thus indicating considerably less effectiveness with the single long counterpoise. Although these records were not obtained at the same tower under the same lightning condition, the comparison of counterpoise efficiency given above seems justified, as the median tower currents of 30,000 for the short counterpoise and 22,000 for the long single one, and similar maximum currents are reasonably comparable.

Table II*

Station	Line Amperes	Lightning-Arrester Location and Amperes: Line Entrance	On Bus	Transformer Terminal	Bus Kv	Apparent Line-Surge Impedance (Ohms)	Rate of Voltage Rise at Station Entrance (Kilovolts per Microsecond)
Roanoke	2,000	—	500	None	472	236	212
	3,200	—	None	350	525	164	—
	2,200	—	1,050	300	397	180	172
	2,400	—	450	None	300	125	92
	2,000	—	None	None	233	117	164
Claytor	2,000	—	None	—	224	112	260
Fieldale	None	—	450	None	428	—	240
	3,000	—	None	None	428	143	275
	2,000	—	None	None	330	115	350
Glenlyn	2,800	350	—	—	—	—	300

* Dashes (—) in table indicate no installation of equipment nor measuring devices to obtain records.

Tower-Arm and Arm-Brace Currents

The question has been raised[13] regarding the magnitude of stray currents in the tower such as in braces, arms,

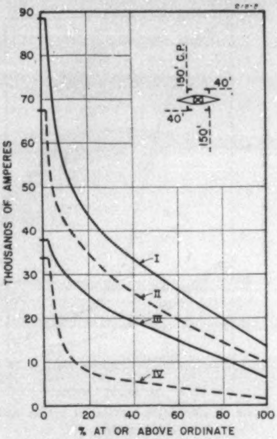


Figure 8. Lightning currents in towers and radial counterpoises—42 records

I. Total current in tower including legs and braces
II. Total current in two 40- and two 150-foot counterpoises
III. Total current in two 150-foot counterpoises
IV. Total current in two 40-foot counterpoises

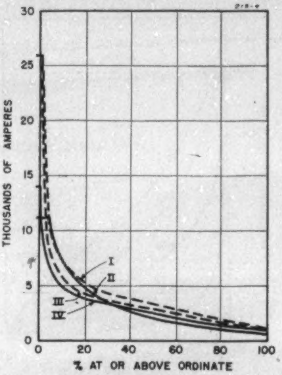


Figure 9. Lightning currents in tower structural members

I. In arms where line flashover occurred—113 records
II. In arm braces—48 records
III. In all arms—353 records
IV. In tower arms without line flashover—240 records

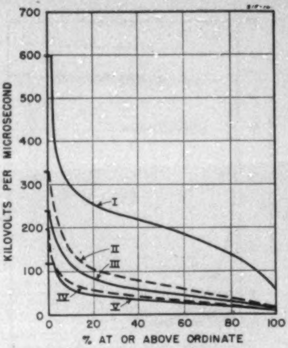


Figure 10. Rate of voltage change at stations

Measured at station entrance

I. Pure lightning surges (link 5)—45 records
II. Determinations from link 4—267 records
III. Determination from link 3—434 records
IV. Determinations from link 2—492 records
V. Determinations from link 1—41 records
Records from links 1 to 4 inclusive include lightning and switching surge voltages

hanger bars, and so on. Some data on this question are given in Figure 9 where maximum currents in tower arms are shown as 26,000 amperes and the median currents in this structural member from 2,000 to 3,500 amperes. Detailed analysis of the records shows that most of these currents, except possibly the highest, are leg currents shunted by the bracing members.

Lightning Voltages at Stations

Magnitudes

In an attempt to correlate lightning effects at stations, surge recorders were located on the three phases at three 132-kv stations and recorded the station bus voltages to ground. Lightning currents on the lines and through station lightning arresters were also recorded. A summary of ten records correlating this information is given in Table II. It will be noted that the highest station voltage recorded was 525 kv, the lowest in this group 224 kv, and the average value 334 kv. At all of these stations, arresters having a gap breakdown and *IR* discharge at 3,000 amperes of approximately 388 kv and 346 kv were installed.

It will be noted that correlation between recorded bus voltage and arrester characteristics for the current measured is rather good, thus indicating, not only that the arresters are affording protection, but also that they are operating with their expected performance characteristics.

In this table are also shown the maximum recorded incoming lightning currents on the line wires. An attempt, however, to use line-surge impedance and current, say 3,000 amperes by 400 ohms, to determine the bus voltage indicates 1,200 kv which requires some further study or interpretation to properly correlate with other records.

Using the recorded bus voltage and line currents in an attempt to determine the line-surge impedance, an equivalent value is given in the seventh column of Table II, ranging from 112 to 236 ohms, with an average of approximately 150 ohms.

Rates of Voltage Rise

The wave-slope indicator used at 26 locations, and coupled to the 132-kv telephone and relay capacitors, in five different 132-kv stations yielded records of some 492 separate voltage occurrences from which the rate of rise of voltage at the station was determined. The data are summarized in Figure 10. The five separate curves shown are based on the rate of voltage rise determined by the individual calibration of magnetic links in the first five positions of the wave-slope indicator (see Figure 1).

. After the first records were obtained from this test setup, it was believed that some of the results were affected by switching surges. A field check verified this point, and it should, therefore, be pointed out that links 1, 2, 3, and 4 are affected by switching surges so that the data presented from link readings 1 to 4 inclusive are a combination of characteristics of lightning and switching-surge voltages. The data from link 5, however, appear to be confined solely to lightning-voltage surges entering the station and, therefore, may be taken as indicative of lightning voltages only, coming in from the transmission lines.

It will be noted that the maximum rate of rise of pure lightning surges is 600 kv per microsecond, the minimum 60 kv per microsecond, and the median or 50 per cent value approximately 200 kv per microsecond. While these data in Figure 5 were obtained from only 45 records, it is believed they are the first of their kind giving any actually measured rates of lightning-voltage rise at the stations where protective devices are used extensively to protect the station equipment. It is worth-while in passing to note that the recorded rates of lightning-voltage rise are somewhat less than the generally accepted 1,000 kv per microsecond on which some present-day test work is often predicated.

The rates of voltage rise for switching surges are considerably lower than for lightning voltages and show a maximum of 325 kv per microsecond, a minimum of 10 kv, and a median value of approximately 50 kv.

V. Discussion of Special Cases

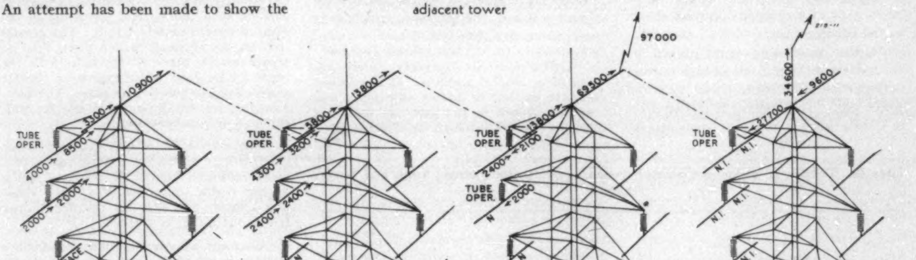
Having obtained some 4,000 individual records of lightning currents and voltages, it has been impossible to present each one separately and discuss it. For this reason, most of the data already presented here have been in curve form, showing the magnitude and frequency of currents which, in many cases, of course, have not been simultaneously recorded with the other correlated data presented. It is, therefore, of interest to discuss a few special cases where readings were obtained simultaneously in various parts of the circuit considered.

Currents in Counterpoises, Ground Rods, and Tower Legs

In Table III are presented the simultaneous readings recorded in counterpoise, ground-rod, and single-tower leg. An attempt has been made to show the comparative value of these three elements in carrying lightning current. The resistances recorded were measured with the customary d-c ground-measuring instrument. (The tower-leg resistance was taken as four times the measured ground resistance of the four-leg tower.) If these three elements are equally effective in carrying current, there should be recorded in each a current in exact proportion to its conductivity. The last column of the table showing the ratio of current to conductance shows the deviation from this law. It will be noted that the counterpoise carries an average current 74.5 per cent of that expected, and deviations from this value in the four cases are not great. The ground rod is very much more erratic, carrying current ranging from 15.6 per cent to 180 per cent of the value based on conductivity alone. This, however, may be accounted for by the lower value being recorded in a 10-foot rod, and the higher value in a 60-foot rod where the earth was not so much affected by the presence of the counterpoise.

In the case of tower legs, omitting the two extreme values of 6.10 and 0.76, the general average is approximately 2.25 times that expected.

The general conclusion to be drawn from these data appears to be that the tower leg on account of the large extent of its exposure to ground is much more effective in carrying lightning current than either the ground rod or the counterpoise. Further, it appears that a counterpoise of 250-foot length, at least, does not carry current in proportion to its conductivity. This is completely in accord with past observations and theory, which indicate that when the counterpoise much exceeds 200 feet or so, its effectiveness does not increase anywhere near in direct proportion to length.


97000
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3300
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4000
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2000
TRACE
N
TUBE OPER.
13800
3800
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69500
13800
2100
2400
2000
N
N
N
34600
9600
27700
N.I.
Tower 66R
Tower 67R
Tower 68R
Tower 69R
N
N.I.
8000
11800
15500
17300
3500 To Tower 67R
200
8600 To Tower 68R
10200
5300 500 Ft C.P.
3600
1101'
1285'
1200'
1500
2600
9700
6000 500 Ft C.P.
11000 To Tower 66R
24300 To Tower 67R
Note:—
N Indicates no record
N.I. " " instrument installed

Figure 11. Typical record of lightning strokes to overhead ground wire on 132-kv Glenlyn–Roanoke line showing distribution of lightning currents in grounded structure, counterpoises, and in line conductors

Note that two strokes have occurred; one to the ground wire in mid-span, and one to an adjacent tower

CURRENTS IN TOWERS, GROUND WIRES, LINE WIRES, AND COUNTERPOISES

A typical case of a severe lightning stroke to the overhead ground wire between towers, and involving a stroke of smaller magnitude to an adjacent tower, is shown in Figure 11. The lightning-stroke current has been determined as 97,000 amperes from the sum of the two ground-wire currents. This is a particularly interesting stroke as it shows currents in the ground-wire on one side of the stroke of 69,500 amperes and on the other side of 27,700 amperes. Assuming that the smaller stroke to tower 69*R* occurred after that to 68*R*, the 69,500 amperes in the ground wire might be approximately half the total stroke current, indicated above as 97,000 amperes, or in other words, the stroke current might have been twice 69,500 or 139,000 amperes. The smaller ground-wire current of 27,700 amperes from tower 69*R* may be accounted for by the demagnetizing effect of ground-wire current flowing into the stroke at tower 69*R*. This sequence of events seems to be borne out by the reversal of current shown in the counterpoise.

Another interesting point shown by this record is the transfer of high current between buried counterpoises between tower legs. A current of 10,200 amperes is shown flowing from one counterpoise tower leg to one non-counterpoise leg. So far as currents in adjacent towers are concerned, these seem to indicate that, in this case at least, the stroke current was drawn through only the tower structures quite local to the point of the stroke.

This record also shows the current in the line wires of some 2,000 to 8,500 amperes. If this is analyzed on the basis of line-surge impedance, extremely high voltage on the line conductors are indicated. The fact that protector tube operations occurred at the four towers further confirms the high voltage occurring at these consecutive towers. As a matter of fact, tube operation occurred in this case at six consecutive towers.

Other strokes to the ground wire similar to the above have been observed showing the same general trend as discussed in the typical case given.

VI. Summary and Conclusions

Based on the data obtained during the past four years in combination with other similar data, much of which has already been published, the following conclusions seem to be warranted:

1. Lightning-stroke currents rarely exceed some 150,000 amperes. It is believed that the method of adding adjacent tower currents (which in the past has indicated stroke currents as high as 220,000 amperes) to determine the stroke current gives results which are probably too high.

Table III. Division of Lightning Currents Between Counterpoises, Ground Rods, and Tower Legs

Structure	Tower and Leg Number	Length (Feet)	Resistance (Ohms)	Measured Amperes	Per Cent Conductivity	Per Cent Current	Ratio of Per Cent Current to Per Cent Conductivity
Case 1							
Counterpoise	25-4	250	15	7,800	82.3	64.5	0.78
Ground rod		60	110	2,500	11.4	20.5	1.80
Tower leg below ground		10*	200	1,800	6.3	15.	2.37
Case 2							
Counterpoise	29-2	250	14	3,300	69.5	51.	0.73
Ground rod		10	100	200	9.8	3.	0.31
Tower leg below ground		10*	48	3,000	20.7	46.	2.23
Case 3							
Counterpoise	29-3	250	10	5,100	61.0	43.5	0.72
Ground rod		40	25	3,300	26.2	28.3	1.08
Tower leg below ground		10*	48	3,300	12.8	28.2	2.20
Case 4							
Counterpoise	27-2	250	13	7,100	94.1	70.0	0.74
Tower leg below ground		10*	200	3,000	4.9	30.0	6.1
Case 5							
Ground rod	27-1	53	68	3,900	85.0	68.5	0.82
Tower leg below ground		10*	200	1,800	15.0	31.5	2.10
Case 6							
Ground rod	27-4	60	35	7,600	75.0	81.0	1.09
Tower leg below ground		10*	200	1,800	25.0	19.0	0.76
Average							
Counterpoise							0.74
Ground rod							1.00
Tower leg							3.00†

*Approximate. †2.25 is probably a better average on account of the high 6.1 value in case 4.

2. A total tower current of 150,000 amperes has been indicated as a maximum with 10 per cent above 80,000 amperes. This compares with previous data[9] of 100,000 amperes and 45,000 amperes respectively.

3. The maximum benefits of counterpoises are obtained with several short ones rather than by using the same total length in a single counterpoise.

4. To aid in the transfer of current from the counterpoise to tower legs, the bonding of tower legs together near the ground line (in addition to relying solely on the structural bracing of the tower) seems advisable (see Figure 11).

5. Currents circulating in the tower members such as crossarms, hanger bars, and so on, are comparatively small in each member and are the result of shunting current out of the main tower-leg path.

6. The current shunted from the tower legs by the bracing members in the conventional four-leg tower has been shown to average about 50 per cent of the measured tower-leg current. (Detailed data not presented here.)

7. Based on the measured d-c resistance, the counterpoise is less effective in carrying current than the normal tower leg in the ratio of approximately 1 to 3. The ground rod, on the average, carried more than its proportionate share of current, with the tendency for deep driven rods to be slightly more effective than shorter ones. The data, however, are too limited for one to draw this as a definite conclusion.

8. The correlation of measured conductor currents with probable line-surge impedance gives indicated conductor voltages which appear highly questionable. This situation appears not only on the line proper but also adjacent to stations.

9. Currents measured in line conductors at stations are of the same general order as currents which have previously been reported[12] as measured in lightning arresters, thus tending to indicate that protective equipment at the station is not subjected to high lightning currents unless by direct stroke.

10. In no case has there been an apparent direct stroke to any of the five stations involved in this investigation.

11. The maximum rate of measured voltage rise of lightning surges measured at the entrance to a station was 600 kv per microsecond with a median value of 200 kv per microsecond. Ten per cent of the records showed over 300 kv per microsecond. While it might seem that voltages of 1,000 kv per microsecond are not prevalent, it should be pointed out that the records obtained cover only three years of investigation work, were taken at only five stations, and were not measured at the equipment terminals within the station.

References

1. SURGE-VOLTAGE INVESTIGATION ON 132-KV TRANSMISSION LINES OF THE AMERICAN GAS AND

Progress in Development of Trolley-Coach Overhead Reflected in Higher Service Standards

L. W. BIRCH
MEMBER AIEE

Synopsis: The expansion of trolley coach operation to include 50 transit systems in the United States and Canada has been greatly helped by the progress made in the development of overhead distribution materials. Beginning with the installation of the first transit-type trolley coaches in Salt Lake City in 1928, the inadequacy and limitations of the earlier designs of equipment were recognized. Since 1928 it has been necessary to add many new devices and redesign many old devices to provide better reliability and permit of greater flexibility of operation. The development of trolley-coach overhead materials has been concentrated mainly on

1. Current collection
2. Hangers and insulation
3. Curve materials
4. Turnouts and crossovers

The improved current collection equipment incorporating the carbon-insert shoe has been responsible for the reduction of wear on fittings and trolley wire. The carbon-insert shoe has also been responsible for the complete elimination of trolley-wire lubrication formerly required for the all-metallic shoe collector.

Improvements in insulated hangers have resulted in a more dependable distribution system and have been responsible for the elimination of considerable secondary insulation formerly inserted in the supporting span wire between positive and negative trolley wires.

The curve segment, available in any degree, is replacing the conventional pull-over arrangement on all trolley-coach overhead systems. Today the conventional pull-over is only installed on curves used jointly by both street car and trolley coach.

Special assemblies for turnouts and crossovers are constructed with the same insulating units. These interchangeable units have been strengthened both mechanically and electrically to meet the severe service on heavy lines. Automatic electrical equipment has been developed for all classes of turnouts and has practically replaced the older types of manually operated devices.

Today's trolley-coach overhead is both mechanically and electrically stronger than the overhead available a few years ago. In combination with the carbon-insert shoe there is less fitting and trolley-wire wear, less dewirements, and less maintenance cost.

A VEHICLE propelled by electric energy collected from a trolley-wire system has this advantage—that power is always available. It is not necessary to refill a tank or tender. Trolley-coach operation is of this type and is successful because the overhead distribution system is capable of delivering continuous power.

As electrical engineers we are all interested in electric transportation. The trolley-coach overhead distribution system is part of a $70,000,000 trolley-coach investment which, in turn, is part of the mass transportation systems on 50 properties. These systems represent a total investment of $700,000,000.

Problems of Operation and Design

Primarily a trolley-coach overhead system differs from a street-car distribution system in that there is no rail return. An aerial negative contact wire replaces the rail. The support and insulation of two aerial contact wires for operation with mobile current collectors presented several interesting problems. These include:

1. *The current collector, itself.* Numerous single-pole and two-pole collection equipments have been built and tried on commercial trolley-coach routes.
2. *Building for operating speeds of 30 to 40 miles per hour.* At these speeds the trolley coach may be touring to one side of the trolley wires.
3. *Selecting a trolley wire and collector that would prevent excessive wear.* Experience with street-car current collection demanded that the trolley wire last as long for trolley-coach operation as for street-car operation.
4. *Insulating the positive and negative trolley wires.* At both crossovers and turnouts the positive trolley wire crosses the negative trolley wire.
5. *Providing a touring range to permit of curb loading, also the passing of other vehicles in traffic.* The trolley coach should tour over, at least, three traffic lanes.
6. *Automatically selecting the proper path at a turnout point.* This necessitates the selection of either turnout or main line at the will of the operator, and the selection must be made without the operator leaving the coach.

These problems were not solved on the first installation. Our present overhead system is the result of the development of 20 years of experience. The pioneer systems, such as those installed in Staten Island, Philadelphia, Baltimore, Toronto, Windsor, Rochester, and Petersburg, as well as in English cities, were very simple, but they were responsible for many important developments and standards in use today. For example, the advantages possible with four trolley wires providing two-way operation rather than two trolley wires providing "single-track" operation,

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L. W. Birch is engineer in the transportation department, Ohio Brass Company, Mansfield, Ohio.

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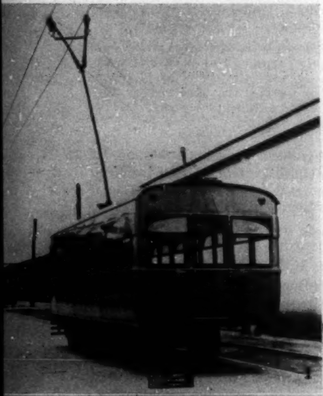
were recognized during the early '20's. Furthermore, considerable additional conductivity was made available for the carrying of electric energy.

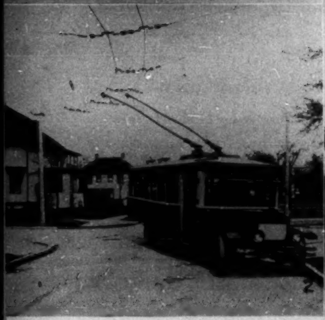
The selection of 2/0 and 3/0 trolley-wire sizes was made on the early installations, because the short life of 1/0 and the additional supporting structure necessary for 4/0 were recognized as economically unsound. As early as 1923 the American Transit Association selected a 24-inch spacing between positive and negative trolley wires and recommended a trolley-wire height of 18 feet above the street. These limitations are standard today. After considerable experimentation with a single-pole collector, two individual poles were finally accepted on the Staten Island property as the standard. The general method of insulating the positive from the negative contact wire, the length of insulation, and the location of the contact wires with respect to the curb line are other features developed 20 years ago which have helped to secure a well-standardized overhead design.

Since 1928, when Salt Lake City installed the first modern 40-passenger transit type of trolley coach, each new installation of trolley coaches has added some new features to the overhead system. These new features have dealt chiefly with the improvement and development of new *devices*, the *dimensional* standards and limitations having remained unchanged. It is an interesting thought that without some of the early standards selected for trolley-coach overhead distribution design, we might have had as many different gauges between positive and negative trolley wire as the railroads had track gauges 75 years ago.

Typical Distribution System

The standard tangent distribution system consists of a pole structure either wood, concrete, or steel, which supports four trolley wires with the poles spaced at approximately 100-foot intervals. The trolley wires are attached to insulated hangers and clamps, and, in addition to the insulation placed in the supporting hangers, a secondary insulation, either porcelain or wood, is inserted in the span wire near the pole. The center line of one pair of trolley wires is usually located 13 feet from the curb line thus permitting the trolley coach to load at the curb and to tour around double-parked vehicles.





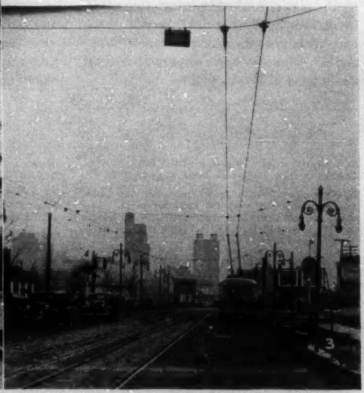


Figure 1. Early single-pole current collector equipped with trolley wheels, 1921

Figure 2. Early two-pole collection equipment, Windsor, Ont., 1922

Figure 3. Standard tangent construction consists of a pole structure, either wood, concrete, or steel, that supports four trolley wires with poles spaced at approximately 100-foot intervals

Development of Collection Equipment

Early collection equipment included a free-swiveling trolley base mounted on top of the coach, a long trolley pole, and a free-swiveling wheel harp for operation under the trolley wire. The trolley base was similar to that installed on street cars, the trolley pole was a lightweight alloy steel pole insulated from the trolley harp, and the harp itself was a wheel collector similar to an ordinary caster. With this combination the trolley coach could tour on either side of a pair of trolley wires, the limitation to the touring range being chiefly the length of the poles. This early assembly of equipment is similar to that employed today except that the wheel collector has been replaced with a shoe collector. Owing to the point bearing of the trolley wheel and to the swiveling action of the device, the wheel collector was subject to many dewirements when passing through frogs and crossovers, and milling action of the spinning wheel on the trolley wire, particularly with the

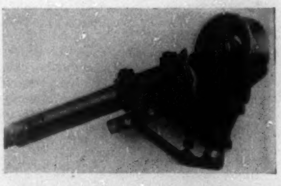


Figure 4. Early collection equipment included a free-swiveling harp equipped with a four-inch trolley wheel

The harp was insulated from the steel trolley pole

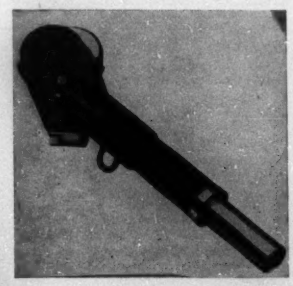


Figure 5. Higher trolley-coach speeds were responsible for the development of the collector shoe

This shoe is all-metallic. The harp is insulated from the trolley pole

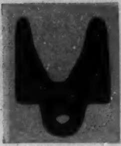


Figure 6. Grooves wore in the early forged-steel and malleable-iron collector shoes

These grooves were injurious to overhead equipment and trolley wire

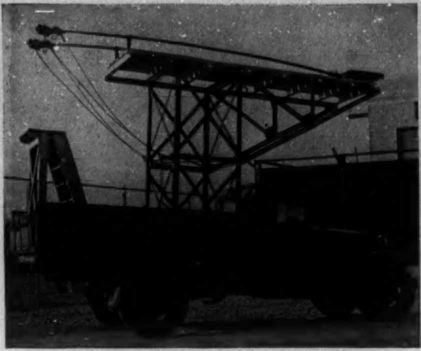


Figure 7. A lubrication truck equipped with two trolley poles and roller type of lubricator for spreading graphite lubricant on the trolley wires

This lubricant was necessary with an all-metallic shoe

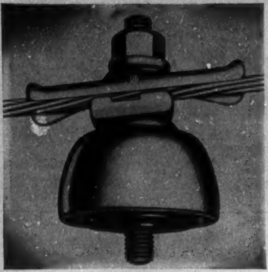


Figure 11. A molded-insulation hanger with adjustable feature for alignment on sloping span wires

trolley coach touring off-center, was quite injurious. These facts were responsible for the development of the shoe.

With the installation of the larger fleets of trolley coaches and the use of higher accelerations and higher speeds, the wheel collector was abandoned in favor of a shoe collector. The history of the development of the shoe collector is interesting. The first shoe collectors were all-metallic, either malleable iron or forged steel. The wearing of a groove in these collectors produced a cutting edge that was injurious to trolley wire, particularly round trolley wire that was supported with a trolley ear encircling the wire. Soft-nosed or bronze-tipped shoes diminished this cutting action but did not prove entirely satisfactory. All of these metallic shoes required lubrication of the contact wires for the prevention of excessive wear to wire and fittings. Usually a graphite lubricant was applied with special roller-type lubricator attached to trolley poles mounted on a line truck or a service truck.

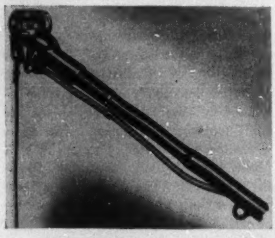


Figure 8. This shoe collector widely used in the United States and England is equipped with a bronze holder in which is clamped a carbon insert

Only the carbon insert contacts the trolley wire

Five years ago a collection shoe consisting of a solid piece of carbon permanently embedded in a bronze shoe was installed on a complete system. This was a real step in the right direction, but trolley-wire lubrication was still necessary since the bronze of the shoe continued to rub the wire. The final step was the development of a collecting shoe consisting of a bronze holder in which is clamped a carbon insert that collects electric energy from the trolley wires and operates in a manner similar to a carbon brush on a commutator. This small carbon insert is replaced when it is worn to the allowable limit. Lubrication of trolley wire has been completely eliminated since the carbon insert lubricates and burnishes the trolley wire.

Effect of Shoes on Trolley-Wire Life

When the metallic shoe was first introduced for collection purposes, dewirements diminished, there was less arcing, and collector noise disappeared, but, on the other hand, the cutting effect of the groove worn in the metallic shoe, and the wear on trolley wire and fittings was greatly increased.

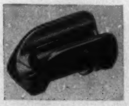


Figure 9 (left). Carbon insert installed in bronze holder

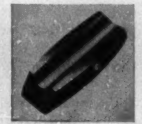


Figure 10 (right). Pregrooved carbon insert

This wear was controlled by the substitution of grooved trolley wire for round trolley wire. All fittings were attached to the upper lobe of the grooved trolley wire, the lower lobe being free of attachments, thus presenting a smooth underrun to the collector. At this time it was estimated that the life of a 2/0 grooved trolley wire would be 500,000 bus passes. Actual experience showed figures as low as 300,000 bus passes. At the present time the combination of a *carbon*-insert shoe operating on grooved trolley wire has increased 2/0 grooved-trolley-wire life to figures varying from two million to seven million bus passes. This is equivalent to saying that 2/0 grooved trolley wire might last on the average line from 20 to 70 years. Most installations today are being equipped with 2/0 wire. The 3/0 wire is selected only where conductivity

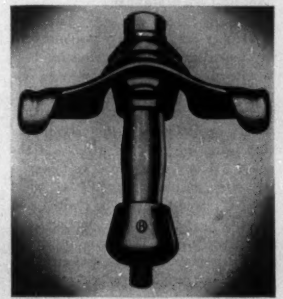
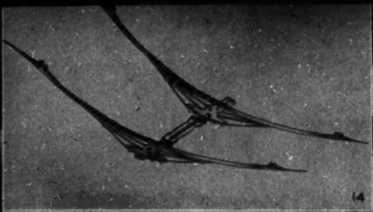
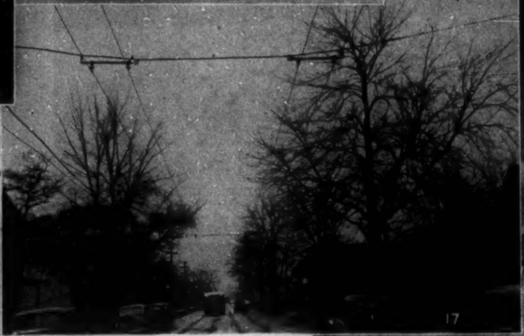
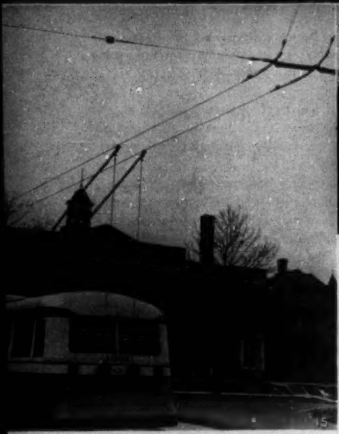


Figure 12. The wood-stick hanger provides a longer leakage path than is possible with most of the hangers equipped with molded insulation







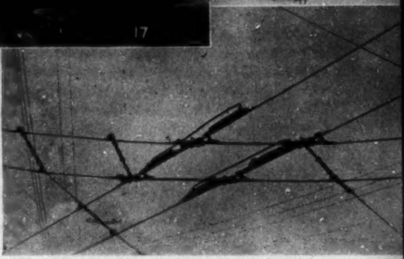


Figure 13. The usual conventional curve designed for street-railway work requires the frequent installation of pull-overs in order to align the trolley wire with the curvature of the track

Figure 14. The segment is the equivalent of a large pull-over; however many 90-degree curves are built with either two 45-degree segments or three 30-degree segments

Figure 15. Segments can be used for trolley-coach overhead since the collection equipment permits of touring either side of the trolley wires

Figure 16. Long radius segments do not restrict speeds

Figure 17. Recent feeder span installations make use of the copper feed span for supporting the trolley wires

Figure 18. A typical crossover showing a location of insulating units in one line; the other line is not insulated

demands. Bronze trolley wire, because of its greater life and ability to hold sufficient tension, so necessary for good operation, is almost universally used.

Trolley-Coach Hanger

A trolley-coach hanger or insulator used for supporting the trolley wires has been developed for this class of two-wire suspension. The hanger carries sufficient insulation in itself to eliminate further secondary insulation, with the exception of that placed in the span wires near the poles. However, in some instances a secondary insulator, usually a wood stick, is placed between the positive and negative hangers in the supporting span. Insulation in the hanger is usually a molded insulation of high dielectric and has sufficient resistivity to heat, moisture, and mechanical injury to permit of long life. Recently several installations have been made with a wood-stick hanger. This type of hanger provides a longer leakage path than is possible with the molded insulation hanger. Hangers are built with adjustability in order to properly align the trolley clamps to prevent interference with the collecting shoes.

Curve Segment

The usual conventional curve designed for street-railway work requires the frequent installation of pull-overs in order to align the trolley wire of the street car with the curvature of the track. This is necessary because of the type of rigid collection equipment normally used on street cars. In the case of the trolley coach, it is not necessary that the vehicle follow beneath the line of trolley wires since the swiveling action of both the trolley base and the harp permit off-center touring. With this equipment it is not imperative that a trolley wire follow any particular curve except that it must be within reach of the collection equipment.

The conventional curve employing numerous pull-overs has been almost entirely replaced by segment construction where the trolley coach does not operate jointly beneath the same trolley wire as the street car. The segment is a large pull-over built to provide an easy means of aligning trolley wires over the path of the coach and, at the same time, to eliminate many fittings and cables formerly used on the conventional street-railway curve. The segment is built with a comparatively large radius and, of course, connects the several chords on the curve. An adjustable feature reduces the total number of segments for a given job to a few. This device does not restrict speeds on curves, inasmuch as it has been designed to handle the maximum speeds possible for the various curves. It has been responsible for the elimination of some unsightly pull-over wires and has contributed to considerably less labor cost during installation.

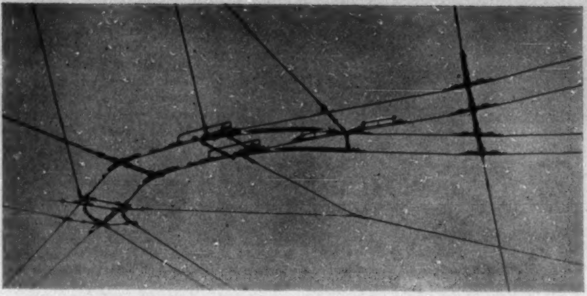


Figure 20. An electrically operated frog installed immediately ahead of a segment

Feeder Span

The frequency of feeder spans for tapping either positive or negative contact wires to aerial feeders is dependent upon electrical loads, cost of annealed trolley wire occasioned by a trolley wire break and the total cost of feed span installation. As a general rule feed spans for the same polarity are located at 800-foot intervals. This means one negative feed span and one positive feed span in an 800-foot section. Since tapping of the positive trolley wire requires good insulation in the feed span because of the closeness of the negative contact wires, special equipment has been designed for making these taps. The most recent arrangement uses the copper feed span as a supporting span, direct taps being taken from the span to the trolley ear as illustrated in Figure 17.

Special Work for Intersections

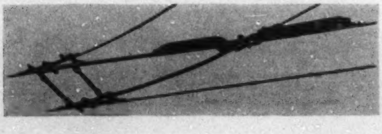
At locations where one trolley-coach system crosses another trolley-coach system, or where a trolley-coach system crosses a street-car line, insulated crossovers must be installed. At locations where one trolley-coach line leaves another trolley-coach line, or where a street-car line leaves a trolley-coach line, special turnout equipment is required. In all cases the special equipment includes devices to which a trolley wire is attached. These devices may be for the purpose of crossing two lines or for the purpose of taking one line off another line. In any event, the design of these devices has been made so that the cross section of the runner pieces, metallic or insulated, is approximately equivalent to that of the trolley wire. This uniform cross section of trolley wire and fittings reduces bumping and scrubbing and consequent arcing by the collector and also prevents considerable damage and wear. To the trolley-coach rider, it is important, because it is responsible for very quiet collection operation. Even the trolley-wire splicer is attached to the upper lobe of the trolley wire itself, in such a manner that there is no obstruction to the passage of the collector.

There are many types of crossovers and turnouts, most of which are constructed with standard parts. For instance, in Figures 18 and 20, similar crossover pans have been used, and the same insulation unit occurs repeatedly. The tips for connecting the trolley wire to the devices are the same for the same size of wire. In the case of the turnout, the standard arrangement shown in Figure 19 is equivalent to that used on all turnouts, the frog pan itself being the only piece that is selected for some particular type of operation. In these illustrations the standard insulating unit, standard tips, and crossover pan may be seen. Of course it is understood that the degree of turnout may vary the degree of the crossover pan in either a crossover assembly or turnout assembly.

Insulating Unit for Turnouts and Crossovers

The insulating unit used in all crossover and turnout assemblies must withstand the ultimate mechanical loads of the largest sizes of bronze trolley wire. It must provide adequate insulation during the passage of a collection shoe from positive to negative wires and must withstand the repeated arcing caused by the breaking of electrical loads. Structurally, this unit consists of two phenolic tubes, a tension member, and a compression member. The compression member is constructed of insulation only, while the tension member is reinforced with a steel core, properly insulated at the ends, that enables the assembled unit to withstand all mechanical loads transmitted by overhead trolley wires. The unit provides 12 inches of clear insulation between metallic sections, this distance being adequate to prevent the "carry-over" of the 600-volt arcs.

Figure 19. Typical turnout arrangement where one trolley-coach line joins another trolley-coach line



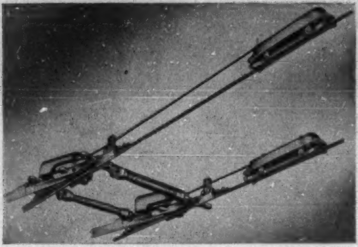
Fiber insulation formerly used between positive and negative wires is shown in this view. Fiber has now been replaced with a phenolic insulation

Types of Turnouts

Again looking at the standard turnout, a set of ordinary cast trailing frog pans will permit a trolley coach to "trail through" this arrangement from either the main line or the turnout, no additional guiding of the swiveling shoe being necessary. If the direction of operation is reversed, it can be readily seen that further guidance of the shoe is imperative, otherwise the shoe may either enter the straight line or the turnout path. Where the trolley coach enters a frog assembly of this type, the shoe must be guided.

One type of turnout where the shoe is guided to one path only, is equipped with a spring frog similar to a spring track switch. If the frog is set for the turnout at all times, the shoe will enter and take the turnout at all times. With the spring arrangement it is possible to trail from either the main line or the turnout, the same as a rail car trails through the spring track switch.

The movable runner of this frog, as well as all electrically operated frogs, is of the "double-tongue" type. A shoe collector



This frog is operated by the two collectors touching the contactors at the same time. In this view one collector is located ahead of the other; therefore, the two contactors could only be simultaneously touched when a trolley coach is turning into a side street. When the trolley coach is taking a turnout, one collector will lead the other collector

Figure 21. Location of contactors on trolley wires in front of Selectric frog

passing through the frog, either over the turnout or over the main line, will travel over one of the runners. Smooth operation of the shoe is accomplished in this way.

There are numerous locations where one trolley-coach line branches away from another line. At these locations it is necessary for the operator to select either the main line route or the turnout route. At these locations electrically operated frogs are usually installed, although in some minor cases, frogs operated with pull ropes have been used.

The electrically operated frog is of two types, the "power-on—power-off" and the Selectric type. In the former type the path at the point of turnout is selected by the operator of the coach by either coasting through the device (power-off) or by taking power (power-on) to actuate the solenoid-operated frog. In this assembly the frog pan is insulated from the trolley wire. It is, however, electrically connected to the trolley wire through a solenoid which operates the frog runners. If a coach passes through this section with the "power off," the runner remains in the main-line position. If, however, the coach passes through this section with the "power on," current passing through the solenoid actuates the frog runner and sets it to the turnout position. Many modifications of this type of frog are in use; however in principle they are similar to this one.

The other type of electric frog depends upon the angularity of the trolley-coach body with the trolley-coach wires. The electrically operated frog used with this combination is also equipped with only one solenoid; however, it is also equipped with a mechanical reset. The solenoid actuates the frog runner, and after a collector passes through the frog, it strikes a trigger and resets the frog to its original position.

Two contactors, one on each trolley wire, are located ahead of the frog. If the two contactors are placed abreast, and a trolley coach is continuing over the main line, the collector shoes will be abreast and will strike the two contactors simultaneously. Since these contactors are a part of a circuit to the coil, the frog runner is actuated as the shoes pass beneath them. After the collection shoe has passed through the frog, it strikes a mechanical resetting device and throws the frog runner back to its initial position, which in this case would be for the turnout. If the coach is to take the turnout, one trolley shoe will lead the other; therefore the shoes cannot strike the two contactors simultaneously and complete a circuit to the solenoid. If the trolley coach is taking a turnout there will be no movement of the frog runner, and since the reset position of the runner is for the turnout, the action is positive.

Other variations of this type of electric frog are in use, the variations being due chiefly to the locations of contactors. In addition to these variations, frequent installations of one half of an electric frog are used where a street-car line either enters or leaves the trolley-coach overhead system.

At the present time, an intersection assembly, turnout or crossover, is ordered and shipped as a unit. Previously, the transportation company selected and erected a multitude of small devices, but the confusion caused by this procedure resulted in an effort by all manufacturers to simplify the selection of the proper assemblies for an installation. Unit assemblies are easily identified.

Research an Important Factor

The design and development of trolley-coach overhead required many laboratory and field tests of materials and assemblies. Impregnated wood beams were first used in crossover assemblies to furnish both mechanical strength and insulation between positive and negative wires. Partially because of bulkiness and weight, but chiefly because of occasional mechanical failures, the wood beam was replaced with a fiber beam. Fiber furnished sufficient insulation for the 600-volt system and provided ample strength, but fiber has one inherent feature that was responsible for the discontinuance of its use. Fiber beams warp. As a consequence, a straight, smooth underrun was difficult to secure. Variations in moisture even produced warping in stock bins before the devices were shipped. The present tubular, phenolic members of an insulating unit do not warp, have good mechanical strength, and furnish excellent insulating qualities over a period of years. Many service tests were necessary during the development of this insulation.

Life tests on the insulation included repeated collector passages over the runners to determine the deteriorating effect of the arc. An insulating unit is tested with a shoe passing under it at speeds corresponding to the speeds of trolley-coach operation in the street. The life of the unit is determined for various values of current at 600 volts. These tests are responsible for the selection of the general design as well as the selection of materials that will withstand constant arcing. It is to be remembered that the auxiliary load of a trolley coach for lights, heaters, and compressor will reach 70 amperes, and this load must be broken at an insulator even when the traction load is cut off.

The development of the current collection equipment has continued for many years. The first carbon inserts used in

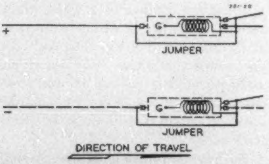


Figure 22. Wiring diagram of a "power-on—power-off" electric frog

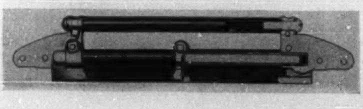


Figure 23. Cut section of the phenolic insulating unit used in crossover and frog assemblies

the present shoe wore out in less than 10 miles, the next carbon inserts in less than 30 miles, and the inserts finally accepted and offered for general use would not last more than 500 miles. The average mileage today for summer and winter, dry weather and wet weather, is considerably over 1,000 miles, and it required four years to secure this mileage. Because of the research conducted by the London Passenger Transport, the present development of the carbon-insert collector was no doubt hastened. Similar experiments were carried out in London and in this country. The exchange of data was very helpful in expediting the production of a satisfactory collector.

The usual problems with lightning and radio interference were encountered, and satisfactory modifications and improvements were developed to provide the degree of reliability demanded for city transportation service. Today, radio complaints are few and there is no record of damage to the traction motors due to lightning.

Reduction in Weight

The weight of the overhead system has always been a problem. A one-pound weight in the overhead system produces from five to eight pounds side load on the supporting structure, either pole or building. The reduction of weight in the overhead system reduces the side loads on the structures and, as a consequence, the cost of the system. Substituting 2/0 grooved trolley wire for 3/0 grooved trolley wire has reduced the weight 20 per cent. The weight of overhead fittings has been reduced 35 per cent since 1934. This weight reduction has permitted the use of many street-car poles that formerly supported two trolley wires. Now they support four trolley wires.

The Picture of Trolley-Coach Operation Today

In addition to the study and development of fittings for overhead design, each new trolley-coach installation has required a field study, a study of the application of the materials. Today, trolley coaches are operating in every climate, almost in every country. They operate at speeds up to 45 miles per hour, accelerate at four miles per hour per second, collect currents as high as 400 amperes, climb 13 per cent grades, run through subways, through congested streets, and over country highways. They do this quietly, swiftly, efficiently, under the overhead distribution system just described.

There are 3,300 trolley coaches now operating in the United States and Canada, 95 per cent of which have been installed within the last 10 years. These coaches, operating over 1,100 miles of route, totaled 100 million coach miles during 1940, and each coach averaged over 1,000 miles for each trolley-pole dewirement.

This is equivalent to saying the trolley coach can run at least five days without a dewirement, a performance that is a measure of the reliability of modern trolley-coach overhead.

The Electrical Strength of Nitrogen and Freon Under Pressure

H. H. SKILLING
MEMBER AIEE

W. C. BRENNER
ASSOCIATE AIEE

Synopsis: Results are given of an investigation of the electric strength of nitrogen, of dichlorodifluoromethane (Freon F-12), and of mixtures of these gases. Sparking voltages are presented as measured between spherical electrodes of brass and aluminum and between pointed electrodes of brass, at various spacings, and in gas at pressures ranging from one to several atmospheres. All measurements are for 60-cycle applied voltage. Dichlorodifluoromethane is found to withstand much higher voltages than either air or nitrogen; this advantage is more marked between points than between spheres, which suggests its use in certain types of insulation applications. A small percentage of dichlorodifluoromethane gas in nitrogen produces an anomalously large rise in the electric strength of the gas, indicating practical advantages of such mixtures.

PREVIOUS studies of the electric strength of air under pressure have been reported by the authors.[1,2] The present paper presents data from a continuation of this work for the primary purpose of studying the electric strength of the gas dichlorodifluoromethane, CCl_2F_2, commonly known as Freon F-12. This gas is readily available as a refrigerant and is known to have unusually high electric strength. It was studied both alone and mixed with nitrogen, and to complete the series of tests the electric strength of nitrogen was also determined.

Freon was obtained in liquid form, in a pressure cylinder, and gas from the upper part of the cylinder was released into the test chamber as desired. Nitrogen gas of commercial purity was used; it is guaranteed 95 per cent pure but is believed to be purer than the guaranteed value. It is supplied under pressure, and was released into the test chamber when needed. No purification of either gas was attempted, for it was believed that results obtained with gas of commercial purity would be more significant from an engineering point of view than results obtained with chemically pure gases. In the present investigation no irregularities or inconsistencies of data could be traced to the presence of impurities in the gases supplied.

The apparatus used is described and pictured in a previous publication.[2] The methods of measurement and technique employed in the study of Freon were the same as in the previous work with air, except as the use of mixed gases required special methods.

In order to obtain mixtures of gases of known composition for test purposes, it was necessary to evacuate the test chamber before admitting the gas or gases in which the test was to be performed. Pressure within the chamber was reduced by means of a vacuum pump to a value estimated at one millimeter of mercury before admitting nitrogen or Freon. By this means practically all of the residual gases were removed. This was particularly necessary for the purpose of removing all traces of Freon gas before testing in an atmosphere supposed to contain no Freon. The presence of an extremely small amount of Freon in nitrogen or air was found to be important in determining dielectric strength, sometimes increasing the sparking voltage by fifty per cent or more,

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H. H. SKILLING is associate professor of electrical engineering at Stanford University, Stanford University, Calif. W. C. BRENNER, formerly a graduate student in electrical engineering at Stanford University, is with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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although the amount of Freon could hardly have been more than a fraction of a per cent. The sparking voltage in such cases was highly erratic and undependable. Similar results were obtained when a trace of carbon tetrachloride was present in the test chamber.

After a series of tests with Freon, it was found best for the test chamber to be evacuated and left for several hours or days. Nitrogen was then admitted to the chamber, and it was again evacuated. This process removed all indication of the presence of Freon in subsequent tests.

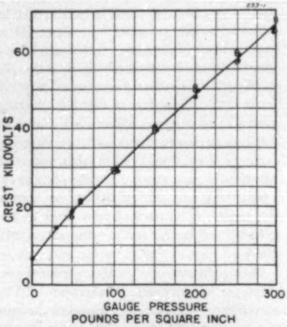

Figure 1. Comparison of sparking voltages in air and nitrogen

Spherical electrodes
Length of spark gap: 0.050 inch
○ Experimental points in air
● Experimental points in nitrogen

In order to obtain satisfactory mixing of gases when Freon and nitrogen were used together, it was found necessary to stir the gases with an electric fan placed within the test chamber. Natural mixing by diffusion did not take place in the course of several hours. When the fan was not used, the heavier Freon went to the bottom of the test chamber and remained partially unmixed with the nitrogen. Results without use of the fan were inconsistent and meaningless. Less than five minutes of operation of the fan was adequate to give thorough mixture of the gases, and results obtained with its use were consistent and could be repeated from day to day.

For test of sparking between spherical electrodes in gas mixtures containing Freon, aluminum electrodes were used in most cases. Aluminum was used because of its greater resistance to corrosive action under such circumstances. Brass was less resistant than aluminum, and steel was less resistant than brass. It is presumably not Freon itself that is harmful to the metal electrodes, for it is reported—in publications relating to its chemical properties—to be noncorrosive, and it is used satisfactorily for refrigeration. Electric discharge in Freon, however, is reported to produce chlorine and other corrosive products of decomposition. It is apparent from the present investigation that the products of electrical discharge in Freon are distinctly corrosive. Moreover, products of electric discharge in a mixture of Freon and air are very considerably more corrosive than those that result from Freon alone or in mixtures of Freon and nitrogen. Corrosion of electrodes in mixtures of Freon and nitrogen was about as severe as in pure Freon, and very much less than in mixtures of Freon and air.

Sparking in nitrogen was remarkably free from corrosion of any kind, and hundreds of sparks would barely tarnish the polished surface of either brass or aluminum spheres. Pointed brass electrodes lost none of their original sharpness after forty or fifty sparks in nitrogen, whereas they were noticeably rounded and measurably shortened in length by similar sparking in either air or Freon.

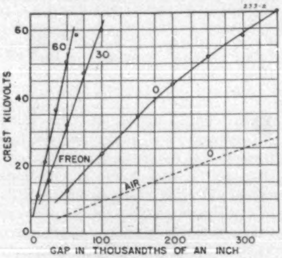

Figure 2. Sparking voltages in Freon

Spherical electrodes
Gauge pressure in pounds per square inch as noted on curves

Results in Nitrogen

In general, sparking voltages between spheres in nitrogen are the same as between spheres in air. This conclusion was checked carefully with an 0.050-inch gap at pressures from 1 to 21 atmospheres; sparking voltages in nitrogen could not be distinguished from those in air (see Figure 1).

A careful study of sparking between spheres at various spacings and pressures had been made in air,[2] and because of the similarity of results this was not repeated in nitrogen.

Despite the general agreement observed between sparking between spheres in air and in nitrogen, there were certain differences in detail. It has been mentioned[1,2] that in air there was a tendency for the sparking voltage to rise slightly as a number of measurements were made in rapid succession. No such result was noticed in nitrogen. It has also been mentioned that a spark would usually result in air if voltage was held for a period of several minutes at a value as much as five to seven per cent below the usual sparking value. This effect did not appear in nitrogen. The sparking values given in this report for nitrogen are average values. Most of the individual sparking values for pure nitrogen lie within one per cent of the average value, the most extreme variations being about five per cent.

It should be emphasized that a slight trace of Freon in the nitrogen would cause very erratic results, but that clean nitrogen was quite consistent.

Results in Freon

Sparking in Freon was investigated at pressures of from one atmosphere to about six atmospheres, with gaps varying from 10 mils to 250 mils (0.010 to 0.250 inch) between both brass and aluminum spheres. Sparking between points was also studied (see following). The highest pressure possible at room temperature was about six atmospheres, at which pressure the gas is in equilibrium with liquid Freon. (The vapor pressure of Freon at 70 degrees Fahrenheit is 84.82 pounds per square inch absolute, 70.12 pounds per square inch gauge[5]).

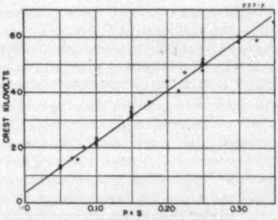

Figure 3. Sparking voltages in Freon

Spherical electrodes
Points are experimental; line is computed from $V = 183PS + 4.0$ kilovolts
P, absolute pressure in atmospheres
S, spark-gap length in inches

Freon was found to have a sparking voltage between spheres that was, for all pressures investigated, between 2 and $2^1/_2$ times as great as the sparking voltage in nitrogen or air at the same spacing and pressure (see Figure 2).

The nature of the spark in Freon was not noticeably different from the sparks observed in air and nitrogen, except in color. Sparks in Freon are intensely blue, in nitrogen they are purple, and in air white or slightly yellow. The intensity of sparks in the different gases, however, appears to be much the same when due allowance is made for the amount of current flowing.

Values of sparking voltage obtained in Freon were fairly consistent, although a variation of two or three per cent from the average value was not uncommon. Variations of as much as ten per cent were occasionally encountered, usually below the average sparking voltage rather than above. No "trends" of voltage values, as found in air,[2] were discovered in Freon, and there were no sparks at voltages radically different from the average value.

In general, sparking voltage between spheres in Freon was found to be proportional to the length of gap, and proportional to the pressure of the Freon gas. To be more precise, the voltage-spacing and voltage-pressure relations were found to be linear, but not exactly proportional, taking the same form as Paschen's law for sparking in air.

The breakdown voltage of Freon as determined in the present investigation can be expressed by the formula

$$V = 183PS + 4.0$$

where V is sparking voltage in kilovolts, P is pressure in atmospheres, and S is spacing in inches. It will be seen that this equation gives voltage values for sparking in Freon between 2.4 and 2.5 times the values given by Paschen's law for air.[2]

The preceding formula appears to be reasonably accurate over the range of conditions of test. The values of sparking voltage obtained by experiment, each an average of five to ten individual voltage readings, are all within ten per cent of the value given by the formula. The experimental range of pressure was from one to five atmospheres, and the range of spark length was from 10 to 350 mils, giving a range of the product PS from 0.05 to 0.35 (atmosphere-inch). This is shown in Figure 3. This formula was derived from the authors' results alone. However, it was found to give good agreement with the sparking voltages measured between flat electrodes in Freon by Trump, Safford, and Cloud (see Figures 3 and 4 of reference 9), for pressures up to about four atmospheres (60 pounds per square inch absolute). As the vapor-pressure of Freon is approached (six atmospheres at room temperature) the experimental sparking voltages are lower than would be expected from the formula. Agreement with the work of Trump, Safford, and Cloud appears to extend the range of applicability of the formula to direct voltage as well as alternating, to gap lengths as great as 0.7-inch, and to values of the product PS as great as 2.0 (atmosphere-inches).

Results in Freon-Nitrogen Mixtures

The electric strength of mixtures of Freon and nitrogen gas was measured under a variety of conditions to determine the effect of

(*a*) Dilution of Freon by nitrogen.

(*b*) Change of pressure.

(*c*) Change of spark-gap length.

Results, which will be discussed below, were intermediate between those obtained in pure Freon and those obtained in pure nitrogen. This was the outcome that was to be anticipated. One very interesting relation, however, was discovered. It was found that a very small amount of Freon in nitrogen has a disproportionately large effect in raising the electric strength; this is discussed below.

Considerable difficulty was at first experienced in work with mixed gases, and results could not be repeated from time to time until an electric fan was used to insure thorough mixing of the gases within the test chamber. When the fan was used, however, the difficulty was entirely overcome, and sparking voltages were thereafter obtained with about the same degree of consistency as in pure Freon: that is, individual readings of sparking voltage commonly varied two or three per cent above and below the average for a given condition, while a variation of as much as ten per cent was very unusual.

Mixtures of Freon and nitrogen that were given most attention contained 67 per cent Freon, 50 per cent Freon, 33 per cent Freon, 25 per cent Freon, and 5 per cent Freon by volume. Most of the work was done with a spark-gap length of 50 mils (see Figure 4). To give assurance that the normal relation between the sparking voltage and gap length exists in

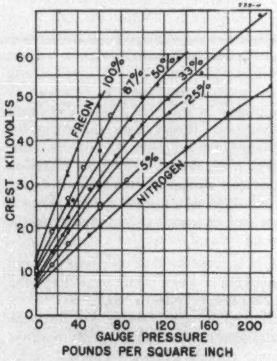

Figure 4. Sparking voltages in mixtures of Freon and nitrogen

Spherical electrodes
Length of spark gap: 0.050 inch
Per cent of Freon by volume as noted on curves

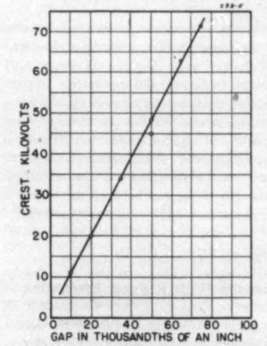

Figure 5. Sparking voltages in a mixture of 50 per cent Freon and 50 per cent nitrogen

Spherical electrodes
Pressure seven atmospheres (90 pounds per square inch gauge)

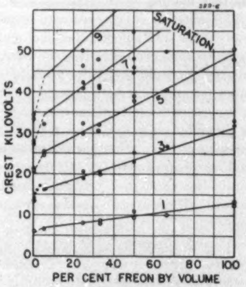

Figure 6. Sparking voltages in mixtures of Freon and nitrogen

Spherical electrodes
Length of spark gap: 0.050 inch
Pressure in atmospheres as noted on curves
Points are experimental; lines are computed from $V = (88PS + 1.9)(1 + 1.08F)$ kv

mixed gases, the gap length was varied from 10 to 75 mils in a mixture of 50 per cent Freon and 50 per cent nitrogen with the results shown in Figure 5. As with pure gases, the voltage-distance relation is linear, and experimental data for this mixture of 50 per cent Freon and 50 per cent nitrogen can be represented by the formula

$$V = 132PS + 2.5 \text{ kv}$$

(All experimental points lie within 10 per cent of this expression.)

This suggests the possibility of finding a simple equation to represent data for all Freon-nitrogen mixtures at all pressures and spacings. For this purpose the data of Figures 4 and 5 were replotted as Figure 6. In this form it is evident that the relation between the amount of Freon present and the sparking voltage is linear when the concentration of Freon is greater than five per cent. When the concentration of Freon is less than five per cent a small amount of Freon produces a disproportionately large increase in the sparking voltage, as shown in detail in Figure 6 at a pressure of three atmospheres. The linear relation shown in Figure 6 leads to the following equation for sparking voltage in Freon-nitrogen mixtures in which the amount of Freon is greater than five per cent:

$$V = (88PS + 1.9)(1 + 1.08F)$$

where V is kilovolts, P is pressure in atmospheres, S is spacing in inches, and F is the fraction of Freon by volume, in the gas. (For pure Freon $F = 1.0$; for half Freon, half nitrogen $F = 0.5$; note that for pure nitrogen it is *not* correct to let $F = 0$ as the formula does not apply for less than five per cent Freon.)

This equation is proposed with several reservations. As noted, it does not apply for very low concentrations of Freon. Another limitation is that the formula becomes inaccurate at high pressure. The agreement with experimental data is good for pressures up to five atmospheres. At pressures of seven atmospheres and more, the experimental sparking voltage is uniformly less than that predicted by the formula. There are two obvious explanations:

1. It is at about this same pressure that variation from a straight-line relationship becomes evident in pure air or nitrogen.
2. The discrepancies become more marked as the partial pressure of Freon in the gas under test approaches the vapor pressure of Freon.

A condition of saturation is approached when both pressure and concentration of Freon are high, corresponding to the upper right-hand corner of the chart. As the vapor pressure is approached the intermolecular forces become great and the Freon no longer approximates a perfect gas.

Finally, the formula given for mixtures of Freon and nitrogen is based on a limited amount of data. Since there are three independent variables in the equation, it would be an extremely lengthy procedure to determine the voltage for all possible combinations. The data at hand were obtained by recording slightly over a thousand individual sparking voltages, however, and appear adequate to give strong support to the above formula for pressures greater than one atmosphere, for spacings from a few mils to a few hundred mils, and for all concentrations of Freon greater than five per cent.

Some work was done with mixtures of air and Freon, but was discontinued when it was found that the products of electrical discharge were highly corrosive and tended to damage the apparatus. In particular, the spheres used as electrodes were tarnished after as few as three or four sparks and rapidly became coated with a grayish deposit. This corrosion, however, had very little effect on the electrical strength of the gap between the spheres. Indications are that the electric strength of air-Freon mixtures is similar to that of nitrogen-Freon mixtures.

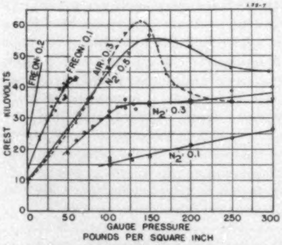

Figure 7. Sparking voltages in nitrogen, Freon, and air

Pointed electrodes
Gas tested, and spark-gap length in inches, as noted on curves

Results With Pointed Electrodes

A short investigation was carried out to determine some of the salient characteristics of sparking in air, nitrogen, and Freon, between electrodes with sharp points.

Sparking in air between points was discussed at some length in a previous paper,[1] and present measurements agree, where comparable, with the results which were then obtained. Experimental procedure is difficult because the nature of the results depends to a large extent on the degree of sharpness of the points, particularly for short gaps. In general, however, as pressure is raised, the sparking voltage reaches a maximum; this maximum occurs at 8 to 12 atmospheres pressure. When pressure is further increased, the sparking voltage becomes less—sometimes very slightly less, sometimes by as much as 50 per cent. The large decline of voltage with increasing pressure is obtained with very sharp points, while with blunt points the dip becomes less marked, and there may be no dip whatever if the points are either quite blunt or quite close together. The action is apparently dependent on whether or not corona precedes sparking; observation of corona has been made possible by the windows installed in the present test chamber.

Sparking in nitrogen between points was investigated for three lengths of gap. It was found that the sparking voltage in nitrogen is considerably lower than that in air for the lengths of gap studied. Curves of sparking voltage as a function of pressure are shown in Figure 7. No direct comparison with the strength of air is possible because of the different shape of the curves, but it is seen from the figure that a 0.3-inch gap in air will withstand about as much voltage as a 0.5-inch gap in nitrogen. There is a decided difference in the shape of the curves; the curve for the 0.3-inch gap in nitrogen is practically flat for pressures above ten atmospheres, while the corresponding curve for air has a hump in the neighborhood of ten atmospheres pressure. When the gap is increased to 0.5 inches the curve for nitrogen is also humped, although to a lesser extent. Very short gaps (0.1-inch or less) fail to show any hump in either air or nitrogen, at least for points of ordinary sharpness. It may be mentioned that the sharpness of the points used was such that they would readily scratch the fingernail, and this sharpness was retained during sparking in nitrogen but sparking in compressed air tended to burn and blunt the points.

It is probable that difference between sparking between points in nitrogen and in air results from the high electron affinity of oxygen. Oxygen will allow the attachment of electrons to form negative ions; nitrogen will not.[8] This will greatly affect the mobility of space charge in the gas, and since sparking voltage between points is largely influenced by space charge, it is natural that nitrogen and air behave differently between points although they behave alike between spheres.

The voltage required to spark between points in Freon is considerably higher than for equal distances in air. Curves for sparking in Freon are shown in Figure 7. Again a general comparison is practically impossible, but certain particular values may be compared in air and Freon.

The voltage required to spark 0.1 inch between points in Freon is for all pressures (from normal atmospheric pressure to the vapor pressure of Freon) greater than the voltage required to spark 0.3 inch in air or 0.5 inch in nitrogen. Over most of the pressure range the voltage required for the shorter gap in Freon is very considerably greater than for the longer gaps in air and nitrogen. It was found that a gap of 0.2 inch in Freon will support voltage comparable to published values for a gap of 30 to 50 millimeters (1.2 to 2.0 inches) in nitrogen.[7]

Since this paper was submitted, G. C. Nonken has published[10] sparking voltages for longer gaps in Freon. His curves for sparking between points (square rods) show the typical hump. A hump is just beginning to appear in the curve of Figure 7 of this paper at the highest pressure obtainable in pure Freon, and with longer gaps it appears at lower pressure. At gap lengths of six and eight centimeters this maximum sparking voltage is at almost atmospheric pressure, and voltage declines as pressure is raised.

At atmospheric pressure comparison may be made between results obtained in Freon and the AIEE standard needle-gap sparking voltages for air, as shown in Table I.

Table I

	Length of Spark (Inches)	
Voltage Kv Crest	In Air (AIEE Standard)	In Freon (Experimental)
14.3	0.47	0.10
24.0	0.82	0.20

It will be seen that in general the increase of electric strength gained by the use of Freon is greater with pointed electrodes than with flat or spherical electrodes. The apparent reason is that with pointed electrodes, sparking is preceded by corona discharge, while there is no corona between spheres. The conclusion is that formation of corona is greatly impeded by Freon gas.

One of the more important and useful aspects of the behavior of Freon is the following. The maximum sparking voltage between points in Freon is greater than the sparking voltage of the same point-gap in air or nitrogen at any practical pressure of air or nitrogen. With a gap of 0.1 inch, for example, between points, Freon will withstand about 40 kilovolts at 50 to 60 pounds gauge pressure. According to data given by H. J. Ryan[8] a gap of the same length in air will not support more than 35 kilovolts even though the pressure is raised to 1,500 pounds. With longer gaps the advantage of Freon appears to be even greater.

This advantage is peculiar to pointed electrodes. In nitrogen or air the sparking voltage between points reaches a maximum at a pressure of a few atmospheres and thereafter decreases with increasing pressure, or at the most, rises very gradually. When the gap in which sparking occurs is between smooth surfaces—between spherical electrodes, for example—the maximum strength of Freon may be equaled and exceeded in either air or nitrogen by sufficiently increasing the pressure but this is not possible if the spark occurs between points.

These facts appear to make Freon potentially more useful to prevent electric discharge from points or projections than to prevent sparking or breakdown in a substantially uniform electric field. On the other hand, it must be remembered that corona or other electric discharge must not ordinarily be allowed to take place in Freon, because the decomposition products are corrosive and somewhat poisonous.

Conclusions

1. The electric strength of nitrogen between smooth electrodes is almost exactly equal to that of air.

2. Sparking voltage between sharp points is higher in air than in nitrogen. See Figure 7.

3. Freon gas (dichlorodifluoromethane) between smooth electrodes will withstand about two and a half times as much voltage as air or nitrogen at the same pressure, but it cannot be used at pressures above about 70 pounds per square inch, gauge, as that is its approximate vapor pressure at ordinary temperatures. See Figures 2 and 3.

4. Mixtures of Freon and nitrogen are intermediate in characteristics between the two gases used alone. See Figures 4 to 6. The most interesting characteristic of mixtures is the large increase in electric strength produced by a very small amount of Freon in nitrogen. See Figure 6.

5. Freon gas between pointed electrodes increases the strength of the gap very greatly, the sparking voltage being found in some cases to be of the order of magnitude of four times that in nitrogen. The maximum sparking voltage in Freon is greater than the greatest sparking voltage that can be attained in air or nitrogen at any practicable pressure.

6. The greatest advantage in the utilization of Freon between smooth surfaces is where the pressure of gas that may be used is limited by mechanical considerations. For most purposes, nitrogen at 300 pounds per square inch pressure is superior to Freon, for it has as great an electric strength, is more consistent in behavior, and does not become corrosive in the presence of electric discharge. But if the gas pressure is limited by mechanical design to 150 pounds per square inch, or less, the possible use of Freon should be carefully considered.

7. The possibility of adding a small percentage of Freon to nitrogen, and thereby increasing its electric strength by 15 to 25 per cent appears to be a practical consideration.

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Lightning Investigation on Wallenpaupack–Siegfried 220-Kv Line of Pennsylvania Power and Light Company

EDGAR BELL
NONMEMBER AIEE

F. W. PACKER
MEMBER AIEE

THE Wallenpaupack-Siegfried line, the pioneer 220-kv circuit in the eastern United States, was completed in 1926.[1] Little information on lightning, and most of it incorrect, existed at the time, and the design chosen proved inadequate to cope with the severe lightning conditions encountered. Partly because of this fact an investigation was started that year by the General Electric Company, the Electric Bond and Share Company, and the Pennsylvania Power and Light Company, to learn the facts about natural lightning and means for protection against its disastrous effects. This investigation has been continued for 15 consecutive years.[2–8]

During 1926 the prevailing theory of lightning involved a bound charge on the earth due to cloud field, the sudden release of this charge, and generation of steep wave-front and high-voltage surges.[8] Because of this general conception overhead ground wires were not considered worth their cost, and tower-footing grounding was seldom talked of. The only investigational work up to this time had been carried out in laboratories, and was based on conjectured lightning strokes. Knowledge of the characteristics of natural lightning was very meager. By 1930, the direct stroke theory[10,11] was pretty well proved, and data on the remarkable effectiveness of overhead ground wires and buried tower footing grounding cables were being accumulated.

The lightning investigation which was in very large part conducted on the Wallenpaupack-Siegfried line had several closely interrelated aspects, namely:

1. Increasing insulation strength of line insulators and terminal equipment against flashover.

2. Spillway gap performance at terminals of line.

3. Dynamic current measurements at times of faults and estimation of fault locations.

4. Addition of means for shielding against lightning.

5. Lightning-arrester operation and performance.

6. Lowering structure-footing resistance by adding buried metallic cable.

7. Collection of detailed operating records relating to time of occurrence of flashovers and tripouts and to line construction in use.

8. (*a*) Field measurements at many locations along the line of magnitude, polarity, and wave front of surge voltages appearing on line conductors.

(*b*) Locations of structures hit by lightning and polarity of stroke current.

(*c*) Magnitudes and polarity of lightning currents in overhead ground wires, lightning rods, tower-structure members, and buried grounding cables.

(*d*) Instantaneous and integrated measurements of atmospheric voltage gradients and field strength, and induced voltages on aerial conductors.

9. Field measurements of lightning voltage and current wave shapes at attended laboratories located along the line, and which were equipped with cathode-ray oscillographs and surge-voltage recorders or rotating prism cameras.

Table I lists the principal measuring devices used. Since data obtained from these various sources usually have been from 5 to 15 years duration, and since additions and improvements to the line have been made in steps, and since careful analysis and detailed reporting of data have been made, a wealth of unique information exists, reliable in general to within 15 per cent.

The principal measuring devices used in large quantities in the field included:

1. The surge voltage recorder,[12,13] a clock-driven Lichtenberg figure camera used in conjunction with insulator string voltage dividers to indicate polarity, magnitude, and wave characteristics of lightning voltages on line conductors, and voltage-measuring antennas.

2. A simpler instrument without clock called a lightning-stroke recorder[14] was later used in large numbers to indicate surge polarity and location of strokes to structures.

3. A superior device using small magnetizable links called a surge crest ammeter[15,16] has given a wealth of information on lightning current magnitudes and polarity. Thousands of magnetic links have been employed on overhead ground wires, lightning rods, structure members, buried grounding cables, and lightning-arrester leads.

4. Other devices, such as the lightning severity meter and field intensity recorder. In addition, field laboratories[4,17,18] have been operated adjacent to the line during 1928 and 1929 near Wallenpaupack, during 1930 42 miles south of Wallenpaupack, and during 1939, 1940, and 1941, 12 miles south of Wallenpaupack on High Knob Mountain.

Table II lists magnitudes or range in value of various quantities measured on this line.

Table I. Principal Measuring or Counting Devices Used on or Adjacent to Wallenpaupack–Siegfried Line

Name	Purpose and Brief Description	Max. No. Used in Any One Year
Surge-voltage recorder	Lichtenberg figure camera with clock-driven film. For measuring polarity and magnitude of surge voltages on line conductors and elevated antennas	42
Cathode-ray oscillograph	Electron beam in vacuum chamber recording time-voltage graph on photographic film	2
Lightning-stroke recorder	Small and inexpensive surge-voltage recorder using stationary film	378
Field-intensity meter and rate of change of field meter	Light-beam recording on moving film, for recording atmospheric electric-field intensity, or rate of change of field	3 1
Lightning-severity meter	Modified Kodak recording on photographic film an integrated measure of light from neon lamp energized from antenna and recording atmospheric voltage. Not all used adjacent to Wallenpaupack-Siegfried Line	9
Surge indicator (flashover indicator)	Semaphore device with frangible link exploded by voltage drop across tower bridge, such as occasioned by insulator flashover. Exposed target easily visible to patrolman on ground	1,062
Surge-crest ammeter	Small laminated alloy steel cartridge held by wooden bracket in magnetic field surrounding conductor carrying lightning-surge current	1,000 Approx.
Graphic ammeter high-speed (Hall) recorder, or magnetic - oscillograph element	For recording dynamic current or voltage at line terminals at times of line faults	29

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EDGAR BELL is engineer, system planning division, and F. W. PACKER is transmission engineer, both of the engineering department of Pennsylvania Power and Light Company, Allentown, Pa.

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The success of this investigation has been due to the careful correlation between records of operating experience and the research measurements. One essential part of this investigation has been detailed information on insulator flashovers. During the fall of 1927 it became apparent that flashed insulators were not being properly recognized and reported, and so during 1928, 1929, and part of 1930 tower climbing patrols were made after every storm at every tower. In 1930 the surge indicator[4,14] had been invented. This was a device showing a large target to indicate insulator assemblies which had been subjected to high voltages. The surge indicator permitted overhead patrolling to be reduced to reasonable amounts and yet enabled practically all of the flashed insulator assemblies to be promptly found.

Table II. Maximum Magnitude or Range in Value of Lightning-Research Measurements Obtained on or Adjacent to Wallenpaupack–Siegfried Line

Potentials due to lightning	
Steel tower lines, conductors to ground	3,000 kv
Across 40 to 50 feet of vertical length of tower	50 kv approx.
Across 5 feet of earth near tower footing	Greater than 50 kv
Potentials due to switching	4.9 × normal crest line to neutral voltage
Attenuation of voltage surges	Proportional to e^2
2,000-kv surge	to $^1/_2$ value in 3.1 miles
1,000-kv surge	to $^1/_2$ value in 6.2 miles
500-kv surge	to $^1/_2$ value in 12.5 miles
Traveling waves due to lightning	
At end of line	
Time of crest	1 to 80 microseconds
Time to $^1/_2$ value on tail	4 to 160 microseconds
At middle of line	
Time to crest	0.1 to 15 microseconds
Time to $^1/_2$ value on tail	0.3 to 50 microseconds
Direct lightning stroke to line	$i = 300\epsilon^{0.8t}$ i = amperes t = microseconds
(Calculated from voltage oscillogram)	
1 microsecond	Current, 700 amperes Rate of rise, 600 amperes per microsecond
5 microseconds	Current 16,000 amperes Rate of rise, 13,000 amperes per microsecond
8 microseconds	Current 200,000 amperes
Atmospheric potential gradient	
Near earth at time of lightning strokes	Positive 180 kv per meter Negative 270 kv per meter
Current due to lightning stroke	
(By surge-crest ammeter measurements)	
In tower structure*	Up to 140,000 amperes (approx.)
Probable stroke current**	Up to 218,000 amperes (approx.)
Polarity negative (neglecting trace records and very small strokes)	99 per cent

*Two other next highest records 102,000 and 108,800 amperes.

**Summation of 5 adjacent and associated records of 20,500, 57,700, 108,800, 14,800, and 16,500 amperes.

Another equally essential result of the investigation has been the obtaining of complete information on individual line tripouts, including time, relay operation, fault location, estimates from magnetic or cathode-ray oscillographs, actual fault locations, insulators flashed, surge voltages measured, lightning-stroke-recorder records, surge-crest-ammeter measurements, structure-footing resistances, and so forth.

The original 64.7-mile line without overhead ground wires operated very poorly for $1^1/_2$ seasons with 27 lightning tripouts.[18] In addition lightning caused a Wallenpaupack transformer to fail within $3^1/_2$ months, and a second transformer failed three weeks later. The line was then operated at 66 kv and all transformers at Wallenpaupack and Siegfried were rebuilt at the factory. All were found to be well along the road to failure, but since rebuilding and the installation of protective spillway gaps at their terminals, no more failures have occurred. During 1927, 24 line miles were equipped with conventional 184,000-circular-mil ACSR overhead ground wires spaced ten feet six inches above conductors at towers. Figures 1 and 2 show the general construction design. Lightning flashovers still occurred on the "protected" section, and the 1928 record for the entire line was 15 lightning tripouts, at least one of which was on the overhead ground-wire section. By this time other interconnected 220-kv lines, completely equipped with similar overhead ground wires were used, and during that year these circuits experienced 5 lightning tripouts.

It was evident and has since been thoroughly proved that overhead ground wires are very desirable but are not necessarily fully effective. The next move was to lower the resistance of tower-footing grounding. As an experiment a 2.6-mile long continuous 2/0 copper cable, buried where possible, and connecting 14 towers over High Knob Mountain,[5,18] was installed early during 1929. This region had still experienced flashovers, and during subsequent years lightning strokes of large magnitude have been measured to steel structures and overhead ground wires, and the effects of lightning strokes have been observed to nearby poles and trees. No insulator flashovers have occurred since this pioneer installation of continuous counterpoise was made, and it has proved to be an excellent investment, as well as serving as a model for other lines and other utilities.

By 1930 the single season's record was not accepted as proof that continuous counterpoise, averaging in length 1,040 feet per tower, was necessary, and so each structure in 16.8 miles of line having overhead ground wires was equipped with four 50-foot-long cables (crowfeet). During 1931 the remaining 4.6 miles of line having overhead ground wires were similarly equipped. Results to date have been excellent but not perfect. During 1936, 24 towers having the higher resistances had one cable per structure increased in length by 200 feet or to 400 feet of cable per tower, but these installations likewise have not had a perfect record of complete lightning protection. Figure 3 shows schematic plan views of tower-footing-grounding cable design.

During 1930 again as an experiment a 3.8-mile section of line was equipped with nonconventional elevated shielding wires,[18,19] insulated from the tower top and supported by 40-foot-long wood poles and four to six guy cables per structure, which were bonded to the tower base by long crowfoot-type cables. These "diverting cables" have had a perfect record for lightning protection, but experienced one mechanical failure, due to imperfection in a cable socket. Figure 4 shows the general design employed.

During 1938 and 1939 the 36.9 miles of line still unequipped with overhead ground wires were provided with buried galvanized three-eighths-inch steel continuous counterpoise cable, and the resulting low tower-footing resistances apparently avoided a few line flashovers. During 1940 and 1941 this section of line was also provided with two overhead ground wires of conventional configuration and elevation above line conductors.

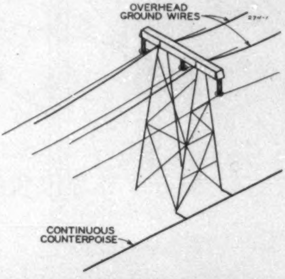

Figure 1. Configuration of conventional overhead ground wires and continuous counterpoise at standard suspension structures of Wallenpaupack–Siegfried line

Figures 2 and 5 show the design employed to support the overhead ground wires at standard suspension structures and at mast structures, respectively. Since the completion of the overhead ground wire no tripouts or flashovers have been experienced on the entire Wallenpaupack-Siegfried line. Inasmuch as this line is now completely equipped with overhead ground wires, with 39.5 miles of continuous counterpoise, and all structures in the remaining 25.2 miles having noncontinuous grounding cables which have a record of avoiding all but three or possibly five flashovers during 11 to 12 years, it is anticipated that the future lightning record of this line will be nearly ideal.

Of the references cited number 18 gives an excellent summary of the investigation up to 1938, including history, construction, photographs, measurements, results, and conclusions, and the reader is urged to refer to it.

Overhead Ground Wire and Buried Tower-Footing-Grounding Cable Construction

In Table III are listed data on lengths of line equipped and construction details. Of particular interest is the fact that the 1940–41 overhead ground wires were installed at 40 per cent of the estimated cost of duplicating 1927 construction. This saving was the result of a combination of circumstances. The selection of smaller and lighter conductor resulted in not only a saving in the cost of conductor material, but sufficient reduction in transverse loading on the suspension towers so as not to require any strengthening in order to maintain the desired factor of safety. The conductor selected was also of sufficient mechanical strength to maintain proper sag and clearance characteristics with a normal tension of 16 per cent of the ultimate which was felt to be well within the limits necessary to prevent any damage from high-frequency vibration.

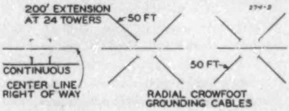

Figure 3. Plan of various tower-footing-grounding cable designs used on Wallenpaupack–Siegfried line. For diverting cables see Figure 4

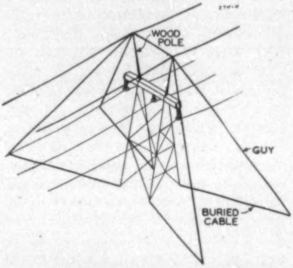

Figure 4. Configuration of 1930 lightning-stroke diverting cables, employing two wood poles to support and insulate the overhead ground wires from the structure

The mast type of structure used at angle locations as well as on very long spans necessitated a special design of additional structure due to the fact that operating conditions made it imperative that the line be restored to service every evening. The existing mast structures supporting the conductors could not be dismantled, lengthened, and rebuilt as was done during 1927. The design finally selected called for a pair of guyed steel masts, installed entirely independent of the existing triple mast structure, and supporting the overhead ground wires in their proper positions by means of a bridle arrangement. These 11 pairs of new steel masts were installed at approximately 40 per cent of the 1927 cost of dismantling and reconstructing existing mast structures. The bridle arrangement also provided excellent shielding over all the dead-end insulator assemblies at these points.

For counterpoise the use of galvanized steel has the advantage of reducing material costs over copper or other acceptable material, minimizing danger of theft (which generally exists where copper or copper-coated steel conductor is used), and providing grounding facilities at least very nearly as good as and possibly better than with smaller-size conductor ma-

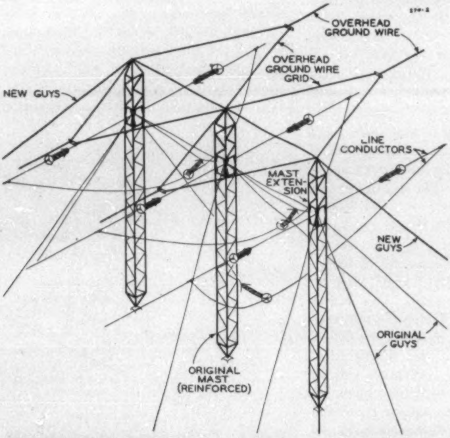

Figure 2. Configuration of overhead ground wires installed during 1927 at mast structures. These structures were rebuilt, strengthened, and lengthened

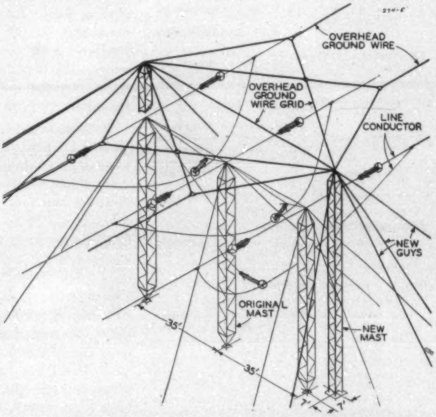

Figure 5. Configuration of overhead ground wires installed during 1940 and 1941 at mast structures. The original structures and guys were not altered. The additional masts and guys are entirely independent and self-supporting structures

terial. It is believed that this steel cable will function adequately for many years.

Tripouts of Wallenpaupack–Siegfried Line

During 1932, 47.1 additional circuit miles were added to the original 64.7 miles. The new circuit extended from Bushkill (roughly midway between Wallenpaupack and Siegfried) to Roseland, N. J. During 10 years, 9 tripouts occurred on the new circuit, and during 16 years, 295 tripouts occurred on the original circuit. Of these latter, 8 occurred on overhead ground-wire sections as follows:

1—Caused by sleet on line conductors (1933).

1—Caused by diverting guy-cable failure (1938).

3—Caused by direct lightning strokes before tower-footing grounding had been improved (1928, 1929, 1930).

2—Caused by direct lightning strokes after tower-footing grounding had been improved (1930, 1932).

1—Same as above, except that it also involved mechanical failure of an insulator assembly (1939).

Of the remaining 287 faults, 20 were caused by incorrect relay operation, operating mistakes, foreign objects against line conductors, system instability, or other causes, and one involved mechanical failure of an insulator assembly caused by a direct lightning stroke. Most, if not all of the rest, totaling 256 tripouts were caused by direct lightning strokes to unprotected line conductors. No permanent damage ever resulted except burns on insulators and occasionally shattered porcelain which was replaced later on a maintenance schedule.

In 1928, 1930, and 1932, other 220-kv circuits, totalling 283.7 miles equipped with twin conventional ACSR overhead ground wires, were placed in 220-kv interconnection service. On these, 98 tripouts have occurred at a rate of 2.7 tripouts per 100 circuit miles per year. The majority of these faults was caused by lightning. For the Wallenpaupack-Siegfried line where overhead ground wires were in use, 3 faults in 3 years were occasioned by lightning prior to installation of improved grounding and 3 faults in 11 years were caused by lightning subsequent to installation of crowfoot grounds. These figures reduce to 4.6 and 1.3 tripouts per 100 circuit miles per year and substantiate the conclusion that the Wallenpaupack-Siegfried line is subject to lightning and that, although once very vulnerable, is now well shielded.

Performance of Buried Tower-Footing-Grounding Cable

In reference 7, Tables II through V, and Figures 1 and 2, data are presented on lightning-current measurements in lightning rods, overhead ground wires, tower-structure members, and buried cables. Measurements have not been made directly of current flowing through tower footings, but measurements of currents in structure members and buried grounding cables show:

(*a*). Continuous counterpoise conducts up to 75 per cent and on the average 58 per cent of the total structure current; noncontinuous crowfeet conduct 11 to 48 per cent of the total structure current depending upon their lengths, and combinations of four crowfeet per structure conduct 42 to 70 per cent of the total structure current.

(*b*). The presence of continuous counterpoise causes 77 to 83 per cent of the total structure current to crowd to that side of the tower. Since the counterpoise conducts 58 per cent of this current, the footings must conduct the remainder or 19 to 25 per cent as compared with 17 to 23 per cent for the other two footings. The presence of one 250-foot crowfoot and three 50-foot crowfeet causes 52 to 58 per cent of the total structure current to crowd to the tower corner having the long crowfoot and leaves 14 to 16 per cent of the tower current in each of the other corners. Since the crowfeet conduct 70 per cent of total structure current, the remaining 30 per cent is distributed among the four footings, and there seems to be no tendency for any one footing to conduct more current than any others.

From these measurements it might be concluded that noncontinuous crowfoot cables are all that are required to produce ideal grounding, and that they are superior to continuous counterpoise. A number of other factors should be mentioned and may help explain why continuous counterpoise so far has a perfect performance record, whereas noncontinuous crowfeet cables have not; why we feel that economically continuous counterpoise is best suited to our conditions; and why we prefer this method of grounding on our 220-kv and 66-kv lines.

1. The 13-year record from High Knob is for continuous cable installed over a stony mountain where lightning has long been known to be prevalent. The cable is not buried at all locations. The inherent tower-footing resistances range from 50 to 148 ohms. The three largest lightning-rod current measurements were 57,000, 62,000, and 65,000 amperes. For this 14-tower section during 1926 (with no overhead ground wire or counterpoise in use) at least 6 insulator assemblies were flashed. During 1927 and 1928, 23 more flashed insulator assemblies were found. Overhead ground wires were in use for about three-fourths of this time and at least two of the flashovers occurred during 1928, while overhead ground wires were in use, but prior to installation of counterpoise. For the seven-year period, 1935 through 1941, at least 25 large lightning strokes have been measured; 21 ex-

Table III. Data on Overhead Ground Wires and Buried Tower-Footing-Grounding Cable Installations

Overhead Ground Wires			
Year installed	1927	1930	1940–41
Length in miles	24.0	3.8	36.9
Conductor material	ACSR	ACSR	Copperweld
Conductor size	184,000 cir mil	184,000 cir mil	$^7/_{16}$ inch special E.H.S. 7 strand
Spacing between cables	22 feet-7 inches	38 feet	22 feet–7 inches
Elevation above conductors at suspension towers	10 feet-6 inches	50 feet	10 feet-6 inches
Percentage suspension towers requiring strengthening	100	100	0
Percentage mast towers requiring rebuilding	100		0
No. suspension towers equipped	115	18	160
No. mast towers equipped	4	0	11
Percentage of towers where Stockbridge dampers were applied to line conductors and overhead ground wires	100	0	0
Tower-Footing-Grounding Cable			
Year installed	1929	1930–31	1938–39
Length in miles of line equipped	2.6	25.2	36.9
Number of structures equipped	14	123	171
Conductor material	Copper	Copper	Galv. steel
Conductor size	2/0 Stranded	2/0 Stranded	$^3/_8$" Stranded
Nomenclature	Counterpoise	Crowfeet	Counterpoise
Configuration	Continuous along one side of line and bonded to 2 tower footings	4 50*-foot-cables perpendicular to each other	Continuous along one side of line and bonded to 2 tower footings
Depth buried	About 1 foot	About 1 foot	About 1 foot in wooded areas, and $1^1/_2$ to 2 feet elsewhere
Extensions added**	None	To 24 towers	To 5 towers

*At 18 structures equipped with 1930 diverting cables, crowfoot cables were much longer in order to connect with the guy-cable anchors. Two structures had 6 guys and 6 cables.

**Extensions added to crowfeet consisted of 200 feet of cable added to one of the 4 existing cables. Extensions added to counterpoise consisted of two 250-foot-long crowfeet per structure.

ceeded 20,000 amperes, 8 exceeded 40,000 amperes, 2 exceeded 60,000 amperes, and the highest was 70,000 amperes.

2. The record of noncontinuous crowfoot-type cable shows three flashovers during two years before cables were installed and five flashovers during the 10- to 11-year period since cables were installed. Three of these latter flashovers caused line tripouts, but two apparently did not. In no case are lightning-current measurements available. In the last case (1939), an insulator-assembly failure also occurred at a tower having originally 114 ohms resistance. The failure was due to the spreading of a cracked insulator cap at the time of an unusually severe lightning stroke, but it is not certain that this insulator failure and line tripout would have occurred, had the insulator cap been in perfect condition, and it may be that the long crowfoot cables would, otherwise, still have a perfect performance record.

3. At the time the High Knob counterpoise and crowfoot cables were installed, 2/0 copper conductor was used, and it was all installed by hand labor (pick and shovel). In recent years it has been felt that galvanized steel counterpoise is perfectly satisfactory, and the 1938–39 installation consists of 36.9 miles of three-eighths-inch Siemens Martin stranded guy wire. It was, in general, installed by means of a tractor and special "counterpoise plow," which digs a trench 18 to 24 inches deep, lays the cable, and partly backfills the trench. The labor and material cost of installing galvanized steel radial crowfeet exceeds the labor and material cost of installing such a counterpoise.

4. Since the Pennsylvania Power and Light Company is not confronted with exceptionally dry and sandy soil conditions where deeply driven rods may be superior to buried cable, and since, except in very rare cases, no additional grounding is required beyond that provided by a single continuous counterpoise, we feel that, for our conditions, such installations are both electrically and economically ideal. For towers having exceptionally high footing resistance, supplementary grounding can be added. This was done for five towers over a mountain where the underlying strata are of slate formation and where resistances were 200, 237, 319, 165, and 329 ohms.

Another factor which may help explain the performance of continuous counterpoise is that current measurements are obtained at many towers, even seven or eight towers distant from the stroke, or $1\frac{1}{2}$ miles away in both directions. These measurements show a definite division of current at each structure, with part of the incoming current diverted up the structure and the remainder flowing in the counterpoise toward the stroke. At the end of the span, the counterpoise current is increased by current picked up from the earth. This current increase is of the order of 10,000 amperes in the span nearest the stricken tower, and drops to 1,000 amperes three or four spans away. In a similar manner, the overhead ground wire currents increase at each tower by the amount going up the structure, and thus serve to conduct currents as high as 30,000 or more amperes in stricken overhead ground-wire spans, and currents on the order of 10,000 amperes for strokes to structures.

On the other hand, where crowfoot cables are used, measurable currents in crowfeet are usually limited to 3 or 4 structures instead of 12 to 14 structures. Continuous counterpoise thus enables the lightning stroke to be fed by many multiple grounds (for example 10,000 to 14,000 feet of buried cable and 50 or more tower footings) rather than by 1,000 to 2,000 feet of cable and 12 to 16 tower footings. Current measurements obtained since 1933 when plotted on charts similar to Figures 1 and 2 of reference 7 are very striking. They confirm our belief that continuous counterpoise is a very effective device for conducting lightning currents and keeping tower-footing potentials low, and that continuous counterpoise is adequate for our conditions.

Summarized Results

1. Lightning-surge voltages were early found to be caused by negative strokes in almost every case. Practically every positive-voltage record was found to have occurred at time of insulator flashover at the tower and line conductor to which the surge-voltage recorder was connected. Such positive voltage records were caused by the recorder and earth being temporarily positive with respect to the stricken line conductor. Many thousands of surge-crest-ammeter records have likewise rarely indicated positive lightning strokes and never any of large magnitude.

2. Measurements of induced voltages, or usually failure to obtain measurements of induced voltages and absence of flashovers which were not clearly associated with direct lightning strokes, showed 12 to 15 years ago that induced lightning voltages are insufficient to cause trouble on lines insulated for 220-kv operation.

3. Hundreds of measurements of voltage across 40 to 50 feet of structure steel seldom indicated voltages exceeding 40 kv, and practically never indicated voltages exceeding 50 kv, and showed that most of the rise in tower potential at times of lightning strokes occurred in the earth surrounding the footings.

4. Twenty-eight measurements of voltage in the earth around tower footings at times of lightning-current discharge indicated that 20 per cent of the potential drop from tower to true earth, exists within five feet of the footings. The voltage across five feet of earth exceeded 50 kv and 40 kv for seven and five cases, respectively. In eight other cases the discharge seemed to be of long continued duration rather than a rapid and violent impulse.

5. Well-defined lightning-current measurements show that strokes either contact structures directly or else one (but not both) of the overhead ground wires between structures. For unprotected sections of line, it was early found that the middle conductor rarely flashed over, but that the outer conductors (and particularly the conductor nearest to approaching storms) experienced frequent flashovers. Furthermore, nearly all flashovers were confined to one conductor with two flashed assemblies (one at each of two adjacent structures) being the usual condition.

6. No flashovers are known to have occurred to overhead ground-wire structures of the Wallenpaupack-Siegfried line having meggered tower-footing resistances of less than 13 ohms. For towers of about 20 ohms resistance, strokes up to 50,000 amperes usually if not always are safely conducted, and strokes up to 100,000 amperes have been safely conducted occasionally.

7. Increasing the number of insulator units on outside conductors from 14 to 16 during 1929 and 1930 resulted in no discernible improvement. After the two transformer failures during 1926 and examination of the remaining transformers, all eight single-phase units were rebuilt, and spillway protective gaps set at $42\frac{1}{2}$ inches were installed on each phase conductor near their terminals. No transformer failures have since occurred, and periodic examination has disclosed no more lightning damage, but spillway gap flashovers were frequent at Wallenpaupack until 1934 when 220-kv lightning arresters were in successful operation. Since then three spillway gap flashovers have occurred, but none were on the two protected phases. Of 15 arrester current discharges measured, 6 exceeded 1,500 amperes.

8. During 1930 fault location was facilitated by cathode-ray oscillograph measurements of the time between surge reflections from the faults and from line terminals. Since that time fault-location estimates have been made by relating dynamic-current measurements obtained at Wallenpaupack, Siegfried and Roseland substations.

9. Many cathode-ray oscillograph time-voltage records were obtained during 1928, 1929, and 1930 of lightning voltages on line conductors. The first oscillogram of this nature was obtained during 1928 at the Wallenpaupack laboratory. Another was occasioned during 1930 by a typical negative stroke close to the oscillograph, and, contacting the conductor to which it was coupled, and, causing line insulation flashover less than 100 feet away showed a negative rise in voltage exceeding 2,760 kv and with a rate of rise of 1,540 kv per microsecond between the ranges of 750 and 2,760 kv. This record is believed to be the only one of its kind ever obtained.

10. Unfortunately, the High Knob lightning laboratory equipped with a cathode-ray oscillograph and special cameras has failed to obtain any important data. This has been due to failure of strokes to contact the lightning rod on the structure over the laboratory building, or the overhead ground wires within one span, during periods of laboratory operation. The lightning rod

on the laboratory tower was struck during 1936, 1937, 1938, and 1939 by negative strokes measuring 33,000, 62,000, 38,000, and 17,400 amperes, and strokes measuring 15,000 and 47,000 amperes occurred to the overhead ground wires in the immediately adjacent spans during 1936 and 1938. Of the 25 large strokes measured to the counterpoise section of line between 1935 and 1941 13 were to lightning rods on the five towers atop High Knob summit.

11. None of the measuring devices or auxiliary equipment used has ever caused a service interruption, and none has ever failed with the exception of a few lightning-stroke recorders connected to measure voltage near tower footings.

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Lightning Investigation at High Altitudes in Colorado

L. M. ROBERTSON
MEMBER AIEE

W. W. LEWIS
FELLOW AIEE

C. M. FOUST
MEMBER AIEE

I. Introduction

THE operation of transmission and distribution lines in the high altitudes of the Rocky Mountain region had indicated that lightning strokes were not so severe as at lower altitudes. Also glow discharges or corona currents from the earth have been observed at high altitudes from pointed objects and rocks. This investigation was made to determine the probable lightning current at altitudes from 6,000 feet to 13,500 feet and to measure corona current. It has been found that the probable stroke current decreases with increase of altitude from sea level to approximately 18,000 feet altitude at which point it appears that no current would be present. The highest mean temperatures at 18,000 feet do not exceed 32 degrees Fahrenheit, and comparison of temperatures in free air at altitudes up to 13,500 feet check temperatures on the earth's surface and indicate that freezing temperatures may limit the formation of lightning.

Measurements of corona current from the earth during lightning storms indicated as much as 480 microamperes with a potential gradient of 94 kv per foot.

II. Description of Line and Territory

The Shoshone–Denver 100-kv transmission line of the Public Service Company of Colorado was constructed in 1908 and 1909 on steel towers and is not equipped with overhead ground wires. However, a single continuous counterpoise wire has been installed from Leadville to Denver and a double counterpoise over the high passes at Argentine and Hagerman. Figure 1 shows a profile of the line and the location of the counterpoise wire and magnetic links. These counterpoise wires are buried in the ground to a depth of approximately one foot wherever possible. The line passes through the heart of the Rocky Mountains starting at Shoshone Plant at an altitude of 6,000 feet and terminating at Denver at an altitude of 5,280 feet, passing over the Continental Divide three times: Hagerman Pass, altitude of 12,000 feet; Fremont Pass, altitude of 11,300 feet; and Argentine Pass, at an altitude of 13,500 feet. The average altitude of the line is about 10,000 feet.

The type of territory varies widely from plains and hills to rugged mountains and includes cultivated lands, sand and gravel, and various rock formations, some of which are mineralized.

III. Measurements and Measuring Equipment

It had long been noted that lightning strokes at high altitudes did not appear to be so destructive as at lower altitudes, as indicated by the operation of transmission and distribution lines and it was assumed that the currents in lightning strokes might be smaller. Measurements were started in 1937 by installing surge-crest ammeter links on some of the towers. The complete investigation was underway in 1938 with brackets on certain towers where past experience had indicated the most lightning activity, and brackets were placed on the counterpoise on most of its length. The surge-crest ammeter links have been described elsewhere.[1] In some cases a complete installation of brackets was made on a tower which included four legs and eight braces. Figure 2 shows one of the typical towers with brackets on one of the legs and two braces.

In 1940 a corona and surge-voltage-recording installation was made on top of Argentine Pass at 13,500 feet elevation, and a similar installation a few miles west of Denver at about one mile elevation.

The installation at Argentine Pass is shown in Figure 3, and the connection arrangement in Figure 4. To measure corona currents at the tip of the lightning rod, a recording microammeter was connected between the lightning rod and the ground. For protection of the microammeter should a direct stroke hit the

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L. M. ROBERTSON is transmission engineer in charge of transmission and station engineering, Public Service Company of Colorado, Denver, Colo. W. W. LEWIS is transmission line engineer, central station engineering department, and C. M. FOUST is of the general engineering laboratory, both with General Electric Company, Schenectady, N. Y.

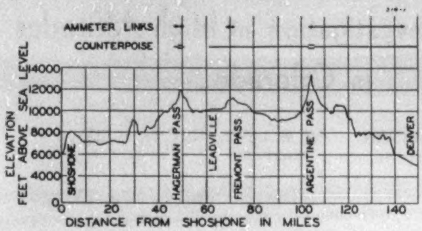


Figure 1. Profile of Shoshone–Denver 100-kv transmission line, showing ground elevation above sea level, and location of counterpoise and surge-crest ammeter links

lightning rod, an inductance was connected in series and a protective gap in parallel. Lightning-rod discharge currents are a measure of cloud field gradient at the earth's surface. Accordingly, current measurements were especially desired immediately before and after direct lightning strokes. To record the time of such strokes and to measure crest-current values a series resistance was inserted in the lightning-rod circuit and a surge-voltage recorder connected across this resistance. Magnetic links were mounted on the lightning-rod down lead to obtain a check reading of crest-current values. The recording microammeter was adjusted to indicate both positive and negative currents. The surge-voltage recorder, because of its double polarity registration characteristics, was capable of measuring crest-current values of either positive or negative lightning strokes.

With one such installation at the high elevation of Argentine Pass, one at the medium elevation of Denver, and a third at a much lower elevation on a line in Eastern Pennsylvania, it was planned to obtain data on discharge levels and polarities over a wide range of elevations. Accompanying this program of field-data taking is a laboratory investigation in which the discharge characteristics of points under different conditions of electric field configuration are being studied.

IV. Direct-Stroke Record

After each lightning storm the transmission-line patrolmen checked the magnetic links, and those which were magnetized were sent into the central office where they were calibrated and each case studied to determine the tower and stroke current. This information was co-ordinated with the patrolman's observations of the time of day of the lightning storms and location along the line. This was checked with the records and oscillograms of Petersen coil operations and line tripouts. In this way it was possible to locate where one-phase-to-ground, two-phase-to-ground, and three-phase faults occurred and to determine tower and stroke current for each case. After the stroke current was calculated the altitude of the tower was determined, and a point was plotted on Figure 5 which shows stroke-current values against altitude. Table I shows a summary of the stroke currents for various altitude groups, namely, 6,000 to 8,000 feet, 8,000 to 10,000 feet, 10,000 to 12,000 feet, 12,000 to 14,000 feet, for the four years 1938 to 1941 inclusive. Each one of these groups was analyzed individually to determine the probable stroke current in the group.

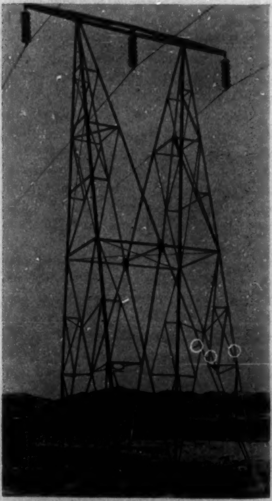


Figure 2. Typical tower showing location of surge-crest ammeter brackets (circled) on tower leg and braces

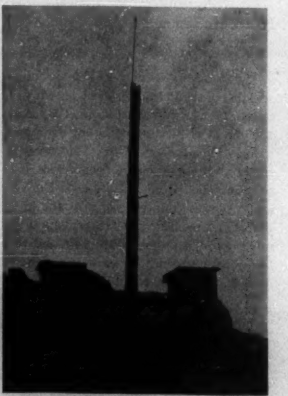


Figure 3. Installation on Argentine Pass at altitude of 13,500 feet for measuring earth corona current and surge voltages

The probable stroke current curves are shown in Figure 6. The upper curve has been presented by Lewis and Foust[3] and was derived from investigations made at altitudes below 2,100 feet. It has been used to represent conditions at low altitudes. The other curves indicate the probable lightning current as derived from the Colorado investigation for three altitude groups as well as a summarizing curve for this investigation, extending from 6,000 to 14,000 feet. The curves indicate a decrease in probable stroke current with increase in altitude. This conforms to observations over 30 years of operation in this district.

The curves in Figure 6 were studied to determine the relation of this decrease of current with the increase of altitude. This was done by selecting the values of current on the five per cent abscissa line for the various altitude group curves of Figure 6. Thus were obtained the four points in Figure 7 marked with circles, including one point at sea level, one at 7,000 feet, one at 11,000 feet, and one at 13,000 feet, and curve *A* was drawn through these points. The average altitude for each group was used as the ordi-

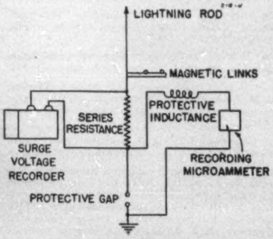


Figure 4. Schematic diagram of earth-corona-current and surge-voltage-recorder equipment

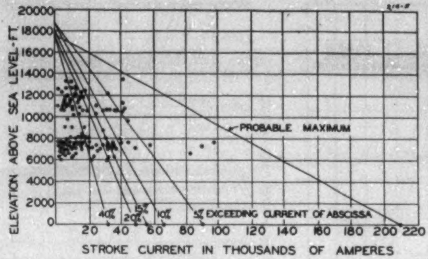


Figure 5. Elevation above sea level at which the strokes occurred on the Shoshone–Denver 100-kv line and the probable maximum current at various altitudes from sea level to 20,000 feet

This also shows the current for various percentages of maximum current at the same altitudes

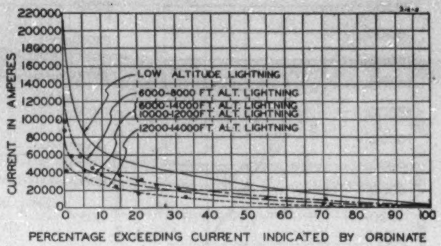


Figure 6. Cumulative curves of probable stroke currents from 734 strokes on transmission lines at low altitudes,[3] and 145 strokes to the Shoshone–Denver 100-kv transmission line at altitudes from 6,000 to 13,500 feet

nate. In obtaining the point in Figure 7 marked with a square, the value of current which the highest five per cent of all observed strokes exceeded was plotted against the average of the altitudes at which occurred the strokes in this highest five per cent. Line *B* was then drawn from the five per cent value for sea level, obtained from the upper curve of Figure 6, through this last point marked with a square. This line intersects the zero current at about 18,000 feet.

It will be noted in Table I that the greatest number of strokes observed was in the group from 6,000 to 8,000 feet altitude. Also, it can be noted in Figure 7 that the five per cent point for this altitude group is the closest to line *B*. This shift of the points in the various altitude groups has been noted from year to year and it is assumed that with a larger number of observations the two lines would approach an intermediate line indicated as line *C* of Figure 7. The same procedure was used for the highest 10 per cent, 15 per cent, 20 per cent, and 40 per cent of the stroke currents, and the resulting curves are plotted on Figure 5. All of these lines of Figures 5 and 7 appear to intersect the zero current at about 18,000 feet altitude. This had been noted in 1938 and has continued through each year of the investigation. It therefore appeared to be of interest to attempt to verify this indicated absence of lightning above 18,000 feet and to attempt to find an explanation.

V. Temperature Observations

Temperature is one of the factors which decreases with increase of altitude, and studies were made to determine the relationship which this might bear to the conclusions previously mentioned. A graphic thermometer was set on top of Argentine Pass during the summer of 1941 to obtain temperatures at 13,500 feet, since the highest altitude for which Weather Bureau information is available is at Leadville which is at an altitude of 10,200 feet. Data were also obtained from the Denver weather bureau of temperatures in the upper air as obtained by balloon sondes and described elsewhere.[3] Data for the year October 1940–September 1941 are presented in Table II. The straight lines in Figure 8 indicate, for example, the variation of temperature with altitude from the balloon readings for three days in July 1941. The corresponding mean temperatures for Argentine Pass, Leadville, and Denver are also indicated. It will be noted that the temperature in free air corresponds rather closely with temperatures at the three locations just mentioned. In Figure 9 are similar average curves for each month of 1941 and for the year, plotted from the data of Table II. This figure indicates that the mean temperatures on the earth at any particular altitude correspond very closely to those at the same altitude in free air, and it appears that the temperature which might exist at altitudes on the earth at 18,000 to 20,000 feet would be as indicated in Figure 9. This shows that at this altitude the temperature never exceeds 32 degrees Fahrenheit, which may be a significant fact.

Minser obtained observations from airplanes,[5] and Figure 10 is a generalized

Table I. Range of Probable Stroke Currents at Various Altitudes

1938, 1939, 1940, 1941

Altitude	6,000 to 8,000 Feet					8,000 to 10,000 Feet					10,000 to 12,000 Feet					12,000 to 14,000 Feet					
Amperes	1938	1939	1940	1941	Four Years	1938	1939	1940	1941	Four Years	1938	1939	1940	1941	Four Years	1938	1939	1940	1941	Four Years	All Years
1,000– 5,000	6	3	14	6	29	2	0	0	0	2	2	2	3	0	7	4	0	0	0	4	42
5,001– 10,000	1	0	6	2	9	2	0	1	0	3	0	1	3	2	6	4	0	1	0	5	23
10,001– 20,000	10	5	3	3	21	4	0	2	2	8	0	1	7	2	10	2	0	1	0	3	42
20,001– 30,000	0	0	5	1	6	1	0	1	0	2	0	1	3	0	4	0	1	0	0	1	13
30,001– 40,000	5	1	5	0	11	1	0	0	0	1	0	0	2	0	2	0	0	0	0	0	14
40,001– 50,000	0	1	1	0	2	1	0	0	0	1	0	0	2	1	3	0	0	1	0	1	7
50,001– 60,000	0	1	0	0	1																1
60,001– 70,000	0	0	0	0	0																0
70,001– 80,000	0	0	0	0	0																0
80,001– 90,000	1	1	0	0	2																2
90,001–100,000	0	0	0	1	1																
Total	23	12	34	13	82	11	0	4	2	17	2	5	20	5	32	10	1	3	0	14	145
Total strokes					82					17					32					14	145

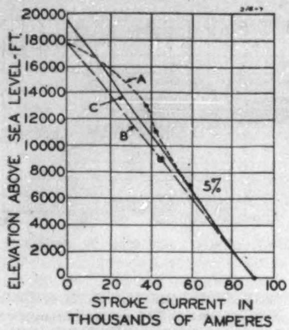


Figure 7. Illustrates method of obtaining percentage lines in Figure 5

Shows the value of the five per cent largest strokes occurring in altitude bands of 6,000 to 8,000 feet, 10,000 to 12,000 feet, and 12,000 to 14,000 feet, plotted against mean altitude for each band. Also shows the average altitude of the five per cent largest strokes

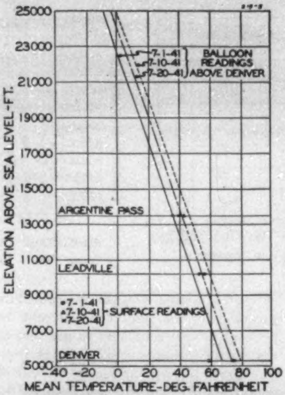


Figure 8. Temperature in free air above Denver from balloon sondes up to 25,000 feet altitude for July 1, 10, 20, 1941, and mean temperatures at Denver, Leadville, and Argentine Pass on corresponding days

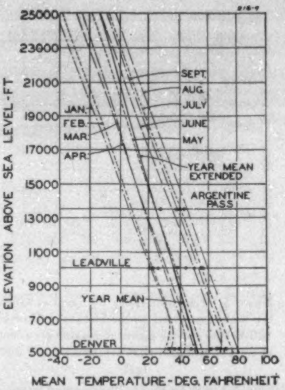


Figure 9. Balloon sonde mean temperatures above Denver for January through September, and for the year 1941, for altitudes up to 25,000 feet, and mean temperatures for same months at Denver, Leadville, and Argentine Pass

diagram showing that at altitudes between 12,000 and 20,000 feet there is a mixture of sleet and water with temperatures between 32 degrees Fahrenheit and −6 degrees Fahrenheit. This region of somewhat mixed charge lies between the upper region predominantly positive and the lower region predominantly negative. From an analysis of a large number of actual cases, he found that lightning discharges to aircraft in flight occur most frequently in the zone adjacent to the freezing isotherm, that is, from 26 degrees Fahrenheit to 34 degrees Fahrenheit. The mean altitude at which discharges to aircraft were encountered was 10,000 feet with a maximum of 18,000 feet and a minimum of 2,000 feet, with no one level predominant.

Simpson and Scrace[6] also found by sending up balloons that a separation of charges in the cloud takes place, the lower portion of the cloud being predominantly negative and the upper portion predominantly positive, and that the region of separation between the negative charge and the upper positive charge occurs at a level where the temperature is between −10 degrees centigrade and −20 degrees centigrade (14 degrees Fahrenheit and −4 degrees Fahrenheit), as shown in Figure 11. The recording thermometer on Argentine Pass near the microammeter showed temperatures ranging from 26 degrees Fahrenheit to 51 degrees Fahrenheit at times when records of corona discharge were obtained.

Transmission line patrolmen have observed for a long time that lightning might occur in a snow storm, but it had also been noted that under these conditions the temperature was near freezing and accompanied by wet heavy snow. This has taken place at all altitudes.

Table II. Balloon Sonde Temperatures in Air Above Denver and Mean Temperatures at Denver, Leadville, and Argentine

		Mean Temperatures			Balloon Sondes at Denver (Ft)			
		Denver	Leadville	Argentine	5,000	10,000	15,000	20,000
January	1941	34.3	21.0		33*	20*	0*	−21*
February	1941	37.2	25.6		31*	23*	2*	−18*
March	1941	37.2	20.4		41*	31*	12*	− 6*
April	1941	46.8	31.4		50**	32**	12**	− 8**
May	1941	61.0	44.8		55**	38**	19**	0**
June	1941	65.6	49.4	40.0	70**	50**	28**	7**
July	1941	72.8	55.6	39.0	75**	55**	34**	13**
August	1941	71.7	55.2	38.0	80**	60**	38**	17**
September	1941	61.7	46.0	26.0	68**	54**	33**	11**
October	1940	55.8	41.2		46*	42*	25*	7*
November	1940	37.8	25.4		36*	28*	14*	0*
December	1940	35.6	25.0		0*	0*	−15*	−36*
Year		51.2	36.7		49	36	16.8	− 2.8

*Observations at midnight only.

**Average of observations at noon and midnight.

All of these observations indicate some correlation between temperature and altitude and the presence or absence of lightning discharges, but the exact relation is still somewhat obscure. However, the observations do indicate that freezing temperatures may limit the formation of lightning.

Another set of contributing factors pertinent to the decrease of lightning current with altitude are the following: The breakdown strength of air decreases about three per cent per 1000 feet, so that at the altitudes of this investigation the breakdown strength of air varies between approximately 60 and 80 per cent of the sea level value. With less voltage available between cloud and earth, it would be expected that the lightning-current values would be lower. Furthermore, it is possible that in some cases the line itself is very close to the cloud, if not in the cloud. In such cases the difference of potential between cloud and line would be very small. In such high altitudes many discharges would be expected, each of small current magnitude, and further, some altitude would be reached at which disruptive discharges would be of insignificant proportions. This is for the reason that the projection of the mountains into the cloud regions limits the accumulation of charges by increasing cloud-to-ground leakage, at the same time precipitating discharges before cloud charge can build up to average low-altitude lightning proportions.

VI. Effective Counterpoise Resistance

Tower-footing resistance tests were made in 1934, using ground Megger, before counterpoise wires had been installed. This was a very dry year with only 8.93 inches precipitation which was 63.5 per cent of normal. Tests were made on various towers ranging in altitude from 6,000 to 13,500 feet and located in various types of earth including clay or loam, sand and gravel, and rock. The resistances are plotted in Figure 12 against altitude and found to vary from 40 ohms to 950 ohms. The same towers were tested again in 1935 when the precipitation was 17.2 inches or 21.8 per cent in excess of normal. The results are also plotted in Figure 12 and show that the resistance varies from 30 ohms to 360 ohms. These curves indicate that the moisture had a considerable influence on resistance values. The curve also shows that the resistance increases with increase in altitude. This may be accounted for by washing of salts from the ground by rains and snow and by the high-resistance type, of rocky territory encountered at high altitudes.

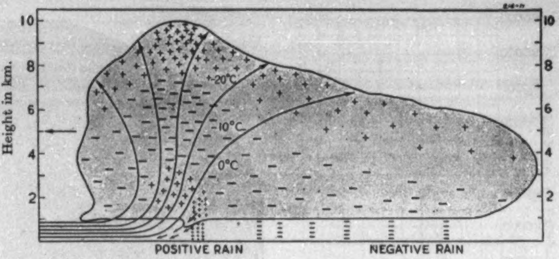
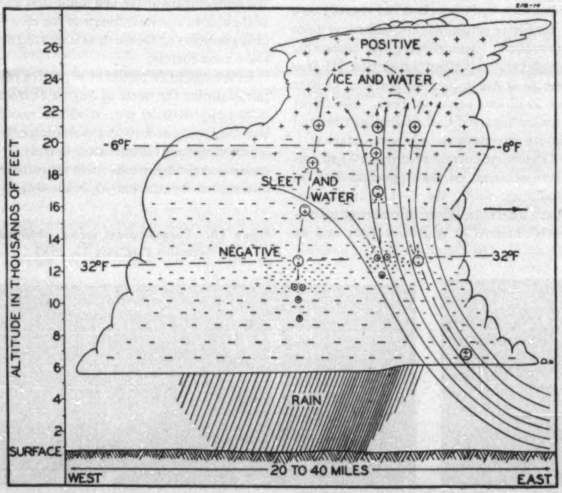


Figure 11. Generalized diagram by Simpson and Scrace,[8] showing air currents and distribution of electricity in a typical heat thunderstorm

During the course of observation of lightning strokes and their measurements, the record of the one-phase-to-ground, two-phase-to-ground, and three-phase faults was noted. This was obtained from automatic oscillograph, Petersen coil operation, and relay indications. The line operation was co-ordinated with the stroke measurements to determine what type of fault occurred. For the four years covered by the investigation, 145 strokes were recorded, of which 56 produced single-phase-to-ground faults, 20 two-phase-to-ground and 5 three-phase faults, 64 strokes were observed where no line faults were produced. It is probable that a majority of the strokes occurred to a conductor, thus causing flashover at an adjacent tower, sometimes with very little current in the stroke. If the stroke current were of sufficient magnitude so that the product of current times footing resistance exceeded the flashover of the insulator strings, then one or two additional phases might flashover, possibly influenced by the phase position of the generated voltage. Strokes to towers would flash over one, two, or three phases, if the tower were raised in potential above the insulator string flashover, the number of phases depending on the condition of the individual strings and the phase position of the generated voltage.

Table III shows the length of counterpoise at which readable values of current were read. These distances are plotted against current magnitude in Figure 13. This figure indicates that the greater the lightning current the greater the distance readable current was carried by the counterpoise. A possible explanation for this is that the larger current values are associated with more extensive clouds. The electrostatic field under these clouds covers a large area, and on the occurrence

Figure 10. Generalized diagram by Minser,[8] showing the distribution of meteorological elements and mechanism of the separation of electrical charges in a typical thunderstorm



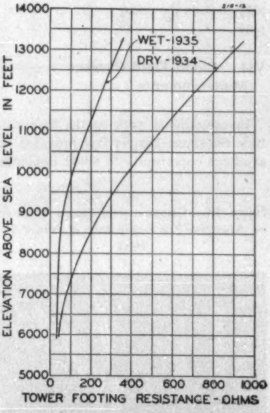


Figure 12. Tower-footing resistance for elevations from 6,000 to 13,500 feet above sea level for the dry year of 1934, and the wet year of 1935, as measured by Megger

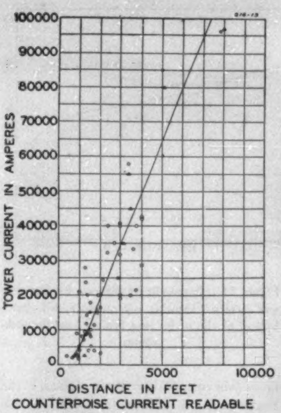


Figure 13. Length of counterpoise at which readable values of current were read for various tower currents

Triangle points are from Table III

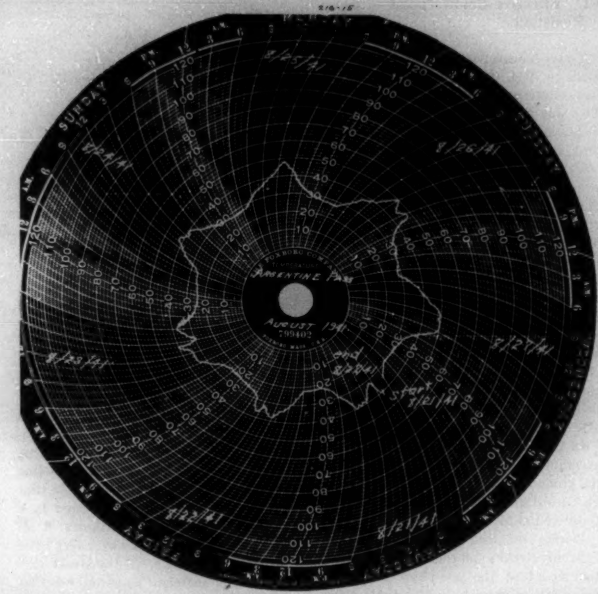


Figure 15. Thermometer record at Argentine Pass during the period corresponding to the corona record of Figure 14

of a stroke, thereby collapsing the field, current flows from the entire area into the stroke. Much of this current makes its way to the extensive counterpoise system, which offers a low resistance path to the tower involved in the stroke. However, since only a portion of the entire counterpoise is involved with any one cloud, the effective resistance of the counterpoise is always somewhat greater than indicated by Megger measurements, which presumably include the entire length of counterpoise.

VII. Cloud-Field Current Measurements

Investigational work on the high altitude Shoshone–Denver line provided an excellent opportunity to study discharge currents from the ground surface during lightning storm periods. Such corona streamers had been observed visually on a number of occasions, but had never been measured. Accordingly, the measuring equipment described in section III was set up at Argentine Pass and near Denver in 1940 and again in 1941. No direct strokes occurred to the lightning rods and no surge-voltage records were obtained.

Discharge-current records for 51 storms were obtained on the Argentine Pass installation during the 1941 lightning season. In Table IV are summarized all records obtained at Argentine Pass and at Denver. The latter are few in number, particularly because the recorder was not in operation during a portion of the period of data taking. This table shows time of occurrence of a storm, the maximum amplitudes of both positive and negative polarity currents occurring in each storm, the temperature at the beginning and end of the storm, and an estimate of the cloud-field gradients at the earth as indicated by the corona current.

In the specimen record of Figure 14 obtained during the storm of August 21 from 9:00 p.m. to 10:30 p.m. it will be noted that beginning at 9:10 and extending over a period of some 20 minutes, sharply increasing corona currents were repeatedly interrupted by discharges, undoubtedly

Figure 14. Corona-current record obtained on Argentine Pass during storm of August 21, 1941

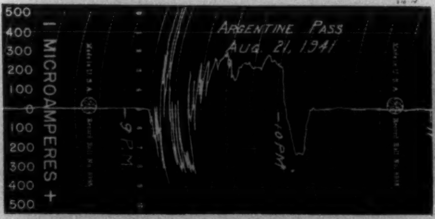
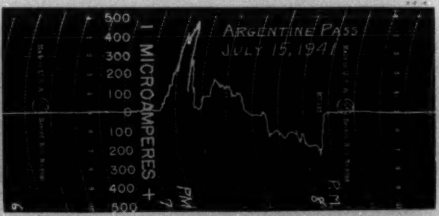


Figure 16. Corona-current record obtained on Argentine Pass, July 15, 1941



due to nearby lightning strokes. The extreme deflections of the microammeter pointer which leave only single line traces on the record show that a lightning stroke occurred with resulting abrupt change in field gradient. The amplitudes of these wide swings are due partly to sudden release of induced charge in the lightning rod, that is, induced surge voltage and overswing of the sensitive instrument movement. Following this early period of the storm came a steady flow of rod discharge current extending over a period of about 40 minutes and of negative polarity. During the next five or six minutes there occurred a gradual change of cloud field from negative to positive polarity without the sharp lightning stroke changes and apparently due to the movement of a positively charged portion of the cloud to the vicinity of the lightning rod. Several records of this general type were obtained during the 1941 lightning season. In Figure 15 is reproduced the thermometer record at Argentine Pass during the period corresponding to Figure 14.

Typical of a storm not accompanied by frequent nearby lightning discharges is the record shown in Figure 16 and obtained at Argentine Pass on July 15 from 6:50 p.m. to 8:20 p.m. This record shows a gradual increase in gradient beginning at 7:05 p.m. over a period of 10 minutes and reaching a high negative gradient of 94 kv per foot at 7:15 p.m. At this time, a single stroke discharged this high gradient down to about half of its previous maximum level. Continuing, the gradient changed gradually to positive in about 20 minutes, building up to a gradient of 75 kv per foot, which disappeared within three or four minutes time, presumably as this portion of the cloud moved away from the lightning rod.

In considering the records obtained at Argentine Pass, it is well to carry in mind that this elevation places the lightning rod well up into the cloud field on many occasions. Under these conditions the ideal case of a lightning rod projecting from the earth surface up into a uniform field is not obtained. Such an arrangement could, therefore, not be used in the laboratory to calibrate for gradients indicated by the Argentine Pass setup. The tip of the lightning rod at Argentine Pass extends 18 feet above the level of the earth surface and it is presumed that cloud charges may at times be within a very few feet of the rod. Accordingly, in the laboratory calibration, corona currents were measured for a long rod extending more than 10 feet above the grounded plane with the tip located only four feet away from a high potential plane which was some three feet in diameter. Voltage from a continuous voltage-rectifying-type generator providing up to 500 kv was applied to the high potential plane over a wide range of voltage levels of both positive and negative polarity. Corresponding rod currents were measured for each voltage level applied. The figure for voltage gradient was obtained by dividing applied voltage by the distance between rod tip and high potential plane. Correction was made for the relative air density of approximately

Table III. Length of Counterpoise at Which Readable Values of Current Were Read

Tower Current	1938 (Feet)	1939 (Feet)	1940 (Feet)	1941 (Feet)	Average (Feet)
0– 5,000	1,220		650	1,450	1,110
5,001– 10,000	1,300	1,300	900	1,500	1,250
10,001– 20,000	1,730	2,150		1,500	1,460
20,001– 30,000		3,850	2,000		2,920
30,001– 40,000	2,870		3,250		3,060
40,001– 50,000	3,000			4,000	3,500
50,001– 60,000		3,300			3,300
60,001– 70,000					
70,001– 80,000					
80,001– 90,000		5,000			5,000
90.001–100,000				8,000	8,000

Table IV. Cloud-Field Current Records

1	2	3	4	5	6	7	8	9	10	11
Location	Month and Day 1941	Time		Current in Microamperes		Cloud-Field Gradient Kv Per Foot		Temperature Degrees Fahrenheit		
		Start	End	Pos.	Neg.	Pos.	Neg.	Start	Min.	End
Argentine Pass	June 21	11:25A	1:10P	130	20	64	30	50	39	51
	22	1:00P	6:50P	20	30	30	36	52	38	39
	24	2:00P	6:00P	230	115	75	59	48	29	29
	July 5	2:20P	2:50P	260	63	78	47	56	51	51
	5	3:45P	4:50P	478	240	94	76	41	36	36
	5	6:40P	7:30P	68	0	50	0	32	31	31
	9	3:35P	4:10P	100	20	51	30	56	50	50
	10	4:30P	5:00P	40	0	40	0	52	47	47
	11	1:10P	2:20P	350	200	85	72	56	40	40
	11	4:00P	5:10P	340	230	84	75	40	34	34
	11	5:30P	7:00P	100	70	57	50	33	30	30
	12	5:10P	5:50P	110	210	59	72	44	44	45
	14	2:10P	2:40P	120	0	60	0	47	45	46
	14	7:20P	8:10P	20	20	30	30	35	33	33
	15	3:30P	4:10P	100	0	57	0	40	40	41
	15	6:50P	8:20P	240	480	75	94	36	29	29
	17	7:20P	7:40P	170	63	68	49	42	39	39
	18	6:40P	7:20P	78	43	52	42	39	37	37
	18	7:40P	8:30P	20	103	30	58	36	33	33
	19	3:20P	5:00P	170	153	68	67	43	42	42
	19	5:10P	5:50P	0	172	0	68	39	35	35
	19	8:40P	9:10P	150	22	66	31	34	34	34
	21	8:30P	9:00P	48	0	44	0	33	33	34
	21	9:30P	11:10P	0	22	0	31	34	34	34
	Aug. 1	5:00P	6:10P	100	0	57	0	49	48	48
	3	8:20P	9:00P	42	38	41	40	34	34	34
	5	4:20P	5:30P	58	32	47	36	41	41	41
	5	7:20P	10:20P	120	190	60	70	38	36	36
	7	2:00P	3:00P	195	32	70	36	44	39	39
	10	1:20P	3:00P	240	200	76	71	44	34	34
	10	5:10P	6:40P	28	200	35	71	38	34	34
	12	2:40P	3:20P	58	125	47	62	46	41	41
	12	3:50P	4:50P	78	72	52	50	39	36	36
	14	1:20P	3:00P	68	103	50	58	44	32	32
	15	7:40P	8:50P	40	42	41	41	34	33	33
	18	2:30P	3:10P	240	202	76	71	31	31	33
	18	3:30P	4:30P	0	95	0	56	33	33	38
	18	9:20P	10:00P	210	45	72	43	30	30	30
	19	10:30P	11:00P	0	118	0	60	28	28	28
	20	6:50A	7:10A	0	50	0	44	26	26	26
	20	8:50A	9:20A	58	0	47	36	30	28	28
	20	10:20A	11:00A	50	72	44	50	30	30	32
	20	6:00P	7:00P	0	133	0	62	37	32	32
	21	11:40A	12:10P	20	160	30	66	38	34	34
	21	9:00P	10:30P	300	300	91	72	30	25	25
	22	1:20P	2:20P	140	20	64	30	36	33	33
	22	4:40P	5:20P	140	40	64	41	34	28	28
	23	2:10P	3:00P	290	100	80	57	34	30	30
	29	6:10P	6:50P	0	40	0	41	36	35	35
	Sept. 1	11:50A	12:40P	160	108	67	58	42	36	36
	3	8:40P	9:30P	230	62	74	48	29	26	26
Denver	Aug. 12	3:30P	6:00P	122	142	61	65			
	21	12:30P	2:00P	24	42	33	41			
	23	1:30P	4:00P	60	102	47	58			
	Sept. 1	11:30A	2:00P	30	142	36	65			

0.6 at Argentine Pass. The positive and negative current values were about equal for the same voltage levels. The use of such a laboratory setup, of course, involves speculation as to field conditions and means that the gradients indicated are at this time only approximate. However, additional field and laboratory data and its correlation will provide more positive results as the investigation proceeds.

In columns 5 and 6 of Table IV, maximum current values for each storm are tabulated. Both positive and negative values are recorded for about 80 per cent of all storms, the remaining 20 per cent giving either positive or negative current exclusively. Discharge currents ranged up to 480 microamperes both positive and negative. A positive instrument current means that the rod tip is positive relative to the ground and indicates a positive-inducing cloud field. A negative current means that the rod tip is negative relative to the ground and indicates a negative-inducing cloud field. Planimeter surveys of all records show that integrated time and amplitude for positive polarity clouds was 55 per cent and negative polarity clouds 45 per cent.

Columns 7 and 8 of Table IV give equivalent voltage gradients as indicated by the laboratory calibrations previously described. These gradients range up to 94 kv per foot. In 1930 two of the authors reported field-gradient measurements at the ground surface up to 87 kv per foot at about 1,200 feet above sea level.[7] These early results were obtained on an instrument of the ballistic galvanometer type which was connected to the cloud field through a capacitance arrangement and which recorded only on the occurrence of a lightning stroke. The average record was below 10 kv per foot. The new measurements range substantially above the earlier values as would be expected due to the close proximity of the cloud to the lightning rod.

VIII. Conclusions

1. One hundred and forty-five stroke currents were measured at high altitudes, ranging in value from 2,000 to 96,800 amperes, with 64 per cent negative and 36 per cent positive. These figures should be compared with figures for low-altitude transmission lines, in which approximately 95 per cent of the strokes were of negative polarity and 5 per cent positive. It is possible that on the high altitude line, where the line may at times be within the cloud, the positive portion of the cloud may be discharged to the line more frequently than in the normal case.

2. Strokes caused 56 one-phase-to-ground faults, 20 two-phase-to-ground faults, and 5 three-phase faults. The remaining 64 strokes caused no line disturbances.

3. The observed stroke current decreases with increase of altitude from sea level to 13,500 feet.

4. Results indicate that there may be no lightning strokes if the ground level is above an altitude of 18,000 feet.

5. The mean temperature at an altitude of 18,000 feet in free air above Denver never exceeds 32 degrees Fahrenheit.

6. Mean temperatures which would prevail *if* the earth's surface were at 18,000 feet elevation may be the same as that obtained from the Balloon Sondes and may never exceed 32 degrees Fahrenheit.

7. The mean temperature for Leadville (altitude 10,200 feet) and Argentine Pass (13,500 feet) conforms very closely to temperatures at corresponding altitudes in free air as obtained by Balloon Sondes at Denver.

8. This investigation and other observations indicate that freezing temperatures may limit the formation of lightning.

9. The length of counterpoise at which readable values of current were read increases directly with increase of stroke current.

10. Current in tower legs and braces divides approximately as the cross-section area of the steel, although erratic readings were obtained in many cases.

11. Counterpoise wires carried practically all the current with little in tower footing.

12. The microammeter measuring earth discharge at Argentine Pass showed indications for 51 different storms up to 480 microamperes. Potential gradient was calculated from these records up to 94 kv per foot.

13. Polarity characteristics of field gradients at 13,500 feet as determined by the integrated area under the amplitude–time curves, were 45 per cent negative and 55 per cent positive. Comparison of maximum discharge currents per storm also showed positive and negative polarities to be about equal. Particular storms, however, are occasionally predominantly positive or negative.

14. The Shoshone–Denver line passes over an area of differing geological formations but, except for resistivity, these formations do not appear to influence the locations where lightning strokes occur nor the value of current in a stroke. In fact, contour of the ground and its relation to the direction of travel of the storm appears to have more bearing on the location of the strokes than geological formations.

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Supplementary Control of Prime-Mover Speed Governors

S. B. CRARY
MEMBER AIEE

J. B. McCLURE
ASSOCIATE AIEE

THE development of interconnected systems has required improvements in the manual and automatic control of load, frequency, and time. The speed governors of the prime movers have always provided the medium for supplementary control of frequency, load, and time. Experience with various types of control devices and the factors influencing their functioning have been discussed frequently in the technical literature. Recently the problem has been given renewed attention[1–5] with an effort to correlate experience with theory and to provide a basis for determining the proper characteristics of governors[2] and their supplementary control.

In attacking this problem, the authors and their associates first made an analysis of the prime-mover governors when performing their essential function of speed control.[1,2] Such an analysis was necessary to provide the required background for the problem of system control of frequency and load by means of supplementary devices. The work on prime-mover governors resulted in conclusions as to their desirable characteristics. This paper presents what appear, from analysis and experience, to be logical conclusions as to the proper characteristics and functions of the supplementary control devices, and, accordingly, is a sequel to the previous paper dealing with prime-mover governors.[2] At the same time this is a companion paper to the one by Concordia, Weygandt, and Shott,[6] presenting the results of an analysis of tie line control. The problems of load, frequency, and time control have usually been treated as related to the operating problems of particular systems. This paper has as its purpose a discussion of the various factors which apply more generally for all types of systems.

System Operation

Economic generation of power, to always meet the demand, to satisfy transmission facilities, and, at the same time, keep frequency and time within certain limits, gives rise to a variety of problems. Every power system or section of a power system is subjected under normal conditions to load changes of rather large magnitude during the course of its daily operation. Most systems have appreciable load "drop offs" and "pickups" during the morning, noon, and evening hours. These load changes may be of the order of one to two per cent of system capacity per minute. Around the average load may be a fringe due to loads which are more rapidly applied and removed. Also, occasionally emergencies arise which will cause sudden changes in loading and frequency.

The characteristics of all the control devices of a system, whether automatic or manual, determine its performance during these changes and readjustments. The co-ordination of the functioning of these devices becomes increasingly important with the number of interconnections and the size of the individual loads which may be applied, and the intelligent solution will necessarily be a compromise among all the requirements involved. After reviewing the factors which appear to be essential for the proper control of generator output, tie-line load, frequency, and time, we have reached the following conclusions:

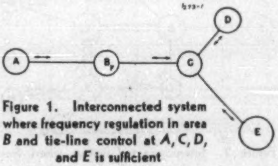

Figure 1. Interconnected system where frequency regulation in area *B* and tie-line control at *A*, *C*, *D*, and *E* is sufficient

1. The duty on the regulating stations can be reduced, for the same allowable frequency deviation, by using more governors which have the proper characteristics.
2. Automatic frequency control may be obtained with simultaneous operation at several stations provided with time-error droop correction.
3. Maintaining absolutely flat frequency, time and tie-line loads is an unnecessarily severe requirement.
4. The required rates of response on the larger systems, with a reasonable amount of regulating capacity, are well within the ability of the prime-mover equipments and controllers.
5. Large rapid load changes are difficult to confine to an area because of limitations in energy supply and of inherent time lags.

The following discussions of the essential factors support the above conclusions.

SYSTEM SPEED GOVERNORS

All of the active speed governors of a system tend to support the frequency following system changes in load. Their contribution is made at a rate comparable with the rate at which the loads are usually applied. Parallel-operated machines require some regulation in order to operate properly in parallel. A previous investigation[1] has indicated that the optimum regulation in order to keep the frequency deviation to a minimum, without resulting in undue or unnecessarily long periods for the readjustment to take place, was of the order of about six per cent incremental regulation for steam units. Also, in practice hydroelectric units with droop-correction mechanisms usually end up with an adjustment of around five per cent on the larger systems.

Tests on systems indicate that the composite or effective system regulation is considerably greater than the optimum and may be more of the order of 10 to 20 per cent because of dead band, operation of some machines with blocked governors, and operation at wide open valve points. An approximate characteristic for some systems has been found to be a one per cent load change for a 0.1 of a cycle frequency deviation. This corresponds to an over-all system regulation of 17 per cent. This difference between over-all system regulation and the regulation of an individual unit tends to encourage blocking the governors of the new and efficient units with modern governors so that they will not be able to take more than their share of load changes. This aggravates the situation by resulting in a still larger composite regulation. Experience has indicated that the lower the composite regulation of a system (not so low as to result in too small damping) the smaller will be the frequency change resulting from a suddenly applied or rejected load of a given magnitude. Thus, the more correct solution[8,11] would be to increase the number of active governors and decrease the dead band of all these governors to as small values as is practical.

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S. B. Crary and J. B. McClure are with the central station engineering department, General Electric Company, Schenectady, N. Y.

It should be recognized that, until a reasonable percentage of capacity is equipped with governors having small dead band and more uniform incremental regulation, units with improved governors will be doing more than their proportionate share of governing. If a steam unit has a six per cent incremental regulation and no dead band, the load change on that unit for a ±0.1 of a cycle fringe in system frequency will be ± 3 per cent of rating. If the instantaneous frequency deviation is reduced to ±0.05 of a cycle by an over-all improvement in governing alone, the change in load on such a unit will be reduced to ±1.5 per cent of rating, without any increase in regulating station rates of response. Thus, the support of system frequency would be distributed among units having more nearly the optimum regulation, and comprising a greater percentage of the system capacity. This would result in more rapid damping of load oscillations and in more stable operation, with less frequency deviation. Furthermore, the load changes on individual plants which were not assigned the duty of regulating would be so small that they could be considered essentially base-load stations. This improvement in system performance is the reward for reduction of dead band and for the employment of governors having more uniform incremental regulation. It is important, on the other hand, to realize that the regulation of individual units should not be made less than the optimum, for then the frequency fringe may actually be increased rather than decreased. Since there exists an *optimum regulation* it does mean that (even though it is not particularly critical) if the regulation of system is too small, an unnecessarily long time for the damping out of frequency oscillations may be required, while if it is too great, the frequency deviation may not be as small as could be realized otherwise.

Exceptions arise in system operation when it may not be desirable to have all units operating with active governors. It may be desirable to block the governor for small frequency changes (for example, a magnetic pull-out device[9]) of a steam unit at light load in order to prevent variations in output which may be large in comparison with the load the unit is carrying, although small in comparison with its rating. Also, for a base-load unit with high efficiency or with a boiler and boiler control unable to cope with the normal load changes, it may be considered necessary or advisable to operate the unit blocked. These exceptions, however, are less likely to exist when a larger number of units are operating with active governors and the frequency deviations are carefully kept within close limits by supplementary control.

Frequency Control

Allocating the system generation has ordinarily been accomplished among generating stations by communicated dispatches. The control of frequency and the responsibility for holding frequency can, of course, be distributed in this same manner. However, to hold frequency within close limits requires a constant readjustment at the regulating stations. It was a very natural step that this duty should be largely taken over by automatic control and should be assigned to particular stations whose incremental costs, depending upon the system loading, were such as to make them the most economical units for this control. Many systems are operated under a semiautomatic and manual control of frequency. The frequency regulating station is changed during the day or the loading of other units of the system is changed in order to allow the regulating units to operate within their capacity to hold frequency. The use of automatic control at the station or stations which are assigned to the duty of holding frequency at a particular time relieves the operating personnel of a very onerous task. In addition, since automatic control is constantly at work restoring frequency to normal, closer frequency regulation is obtained with the result that there is less tendency for manual stations to contribute by their own initiative. This usually results in better system operation and less unnecessary transfer of load between stations. In general, the loads which are most critical to frequency deviation on a system do not require as close a frequency regulation as is desired, by the system operators, for the best operation of the system.

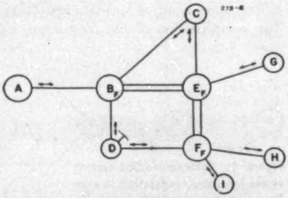

Figure 2. Interconnected system where frequency regulation may be used advantageously in areas *B, E,* and *F* with tie-line control at *A, C, D, G, H,* and *I*

It has become general practice to use one station for holding frequency and time. The other regulated units control tie-line loads. In systems of this type, one station of the whole interconnected group, no matter how large, or a group of closely tied stations, controls frequency. This has come about naturally, it being a fairly easy way to distribute the responsibility among regulated stations and still use the simplest type of control. This type of control is particularly well-adapted to a system interconnected[3,7] as shown in Figure 1, where the other sections of the system are separated from the section holding frequency by only a few tie lines. These other sections can be assigned the duty of holding tie-line loads.

However, for the type of system shown in Figure 2, it becomes more difficult to assign the individual stations or areas to regulate tie-line loads and it may become simpler for two or more stations to be assigned the responsibility of regulating frequency. This is not readily accomplished by the ordinary method used in frequency regulated stations of having a controller which holds essentially flat frequency without any direct control of time error. Parallel operated frequency controllers of this type have been operated when it was possible to have slow rates of response and to keep the two stations in range together by frequent communicated dispatches and manual readjustments. Completely automatic parallel operation can be accomplished, however, by providing the frequency regulating stations with mechanisms which measure accurately the time error and proportion their contribution to the frequency regulation of the system in proportion to their assigned regulating capacity and the time-error deviation. This control principle has been successfully used for many years and provides means for automatically dividing responsibility by what has been termed time-error droop correction,[10] employing a time-error range which is well within practical requirements for electrical time on the systems, that is, one second for complete regulating range.

Accuracy of Standards, Time, and Frequency Limits

Reasonable electrical time accuracy is one of the by-products of automatic frequency control. In fact, it is not difficult to provide acceptable electrical time on manual control with a master clock and Arlington time as reference.

In their enthusiasm, some operating groups, when operating as independent systems, endeavored to maintain very close to flat time. In principle, this means that following each interval of low frequency a forced high-frequency interval is required and vice versa, resulting in much unnecessary correction at the regulating station. This principle of operation is particularly troublesome in an interconnected area and imposes an unnecessary transfer across tie lines and an unnecessary burden on the regulating stations. Most systems have abandoned this ideal objective and are content with keeping time within ±1 to 2 seconds which is relatively easy to obtain, providing the regulating stations are always kept within range.

Flat frequency-control devices employing resonant circuits are not inherently good time standards. A continuous error of 0.01 of a cycle will integrate 14 seconds error in a 24-hour period. Thus, it is customary to supplement this type of control with some form of time standard, such as master clock, tuning fork, or crystal oscillator. The ultimate in time standard, of course, is a common continuous master frequency or time.[4] At present, the cost of the necessary equipments is the only obstacle to a widespread adoption of a common master standard.

Any of the available time standards is sufficiently accurate for backup purposes, as a supplement to straight frequency control so far as system time is concerned. Ordinary master clocks have an accuracy of one second per day; tuning forks one-half to one second per day; crystal oscillators about 0.1 second per day, and even Arlington time sends out its correction log sheets at the end of each month—revealing that its daily time signals may be off as much as 0.1 second. With any of these time standards, it will still be necessary for a system to have a correcting period, probably during early morning hours, to correct for drift in time standard or for system errors which may have accumulated when the controlling stations were not kept within range.

In appraising the value of a continuous master frequency, it should be kept in mind that it does not improve the parallel operation of flat frequency regulators. It merely applies equal amounts of calibration to all controllers in terms of the differential between system time and master time with the result that system time is maintained within prescribed limits and load equalization must still be accomplished by communicated dispatches and manual readjustments.

In contrast to this, the application of a master time standard to frequency controllers using the time-error droop principle results in perfect parallel control. Thus, the several frequency-control areas shown in Figure 2 may be used in any combination without frequent communication and without readjustment, except to satisfy changing system conditions.

The advantages of time-error droop control will be recognized as interconnections become more widespread and the number of frequency-regulating stations increase.

During the steady load periods the larger interconnected areas are able to maintain frequency within ±0.05 of a cycle and these limits may increase to approximately ±0.15 of a cycle during the rapid load changes. On smaller systems the steady fringe is more nearly ±0.1 of a cycle and the swings may reach ±0.25 of a cycle during pickup and drop-off intervals. The sudden load changes that are applied to the larger systems are necessarily smaller in proportion to the total capacity than may occur on the smaller systems—thus causing smaller frequency deviations for similar governor performance.

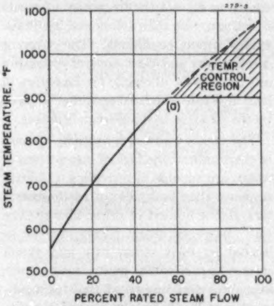

Figure 3. Typical temperature-flow characteristic of modern boiler with convection-type superheater

Tie-Line Controllers

The control of frequency on an interconnected system is ordinarily a comparatively easy assignment and does not require a particularly fast rate of response. The tie-line controllers, on the other hand, are required to hold the tie lines within certain limits in order to prevent pulling out of step, overloading, or too great or long a load deviation from the assigned tie-line loadings, and it is logical that the tie-line controllers should have a proportionately faster correction rate than that of frequency-regulated stations. This also results in a more quickly balanced system after a load change occurs in that the generating capacity within the area in which the load change has taken place quickly picks up the load without requiring an unnecessary change in the frequency regulating station. As the regulated capacity responding to tie-line control increases, it is important to reduce the amount of unnecessary correction. This may be accomplished by employing the well-known principle of frequency bias.[3] Briefly, by combining frequency deviation and tie-line watt deviation, it is possible to determine the location of the net load change associated with these deviations, that is, whether it is internal or external to the area, and the tie-line controller thus responds for the portion of the load change within that area, and when properly adjusted allows the tie line to contribute to external load changes, by an amount equal to the composite governor regulation of that area.

In a closely knit system operating with a frequency control alone, the control may be as rapid in response as is permissible for frequency stability provided there is sufficient energy supply. In the limit this type of system could approach a single unit with a governor having zero speed droop and having as small a droop-correction time lag as may be obtained with stability.

The kilowatt transfer between two systems is not a steady quantity. Load changes in the several areas are continually being applied, and system constants, together with existing governor dead bands, do not produce critical damping to the original oscillation. Before the tie line has steadied down following a load change, another load change has appeared with the result that almost a continuous oscillation exists. This oscillation in watts may be as much as ten per cent of the tie-line capacity and occurs at a period varying between one and five seconds, depending upon the system inertias on either end and the synchronizing coefficient of the tie line. Thus, the watt sensitive element in a tie-line controller need not attempt to follow the instantaneous watts but rather the average of the fringe watts associated with this relatively fast oscillation.

Types of Tie-Line Controllers

A controller which is in equilibrium only when the controlled quantity has been returned to its desired value is called "the floating type." A controller which is in equilibrium when the controlled quantity has been returned to a value which deviates from the desired value by an amount proportional to the controlled quantity is called "the proportional type." In terms of regulation "the floating type" is a flat controller having zero regulation, while "the proportional controller" has a definite positive regulation.

Practically all supplementary controllers of turbine governors are of the floating type. The synchronizing motor receives impulses, either continuous or intermittent, until the controlled quantity has returned to the desired value. The proportional controller may be a device which exerts a pull on the governor arm in proportion to the deviation of the controlled quantity.

The analysis presented in the companion paper[6] shows that equally good performance may be obtained from the two types of controllers if they are properly designed. The proportional controller requires a sufficiently long time lag between the deviation sensitive element and the resulting proportional force that it exerts on the governor. The floating type requires a sufficiently slow rate of correction to allow for stable operation. Since the latter type is the most easily obtained, it is most generally used.

For quick response a continuous controller, either floating or proportional, is better than an intermittent type. A controller whose rate of correction or correcting force is proportional to the deviation is capable of better performance than one which does not have this proportional feature. In all cases, stability may be obtained by providing the controller with a sufficiently slow rate of correction or long time lag. Rates of response considerably greater than those generally used now may be obtained by proper design and do not necessarily require the use of a stabilizer. For best performance of a steam unit, with optimum controller design, the governor incremental regulation should be fairly uniform at a value of about six per cent, with small dead band.

TELEMETERING

In many cases the tie line to be controlled is not adjacent to the regulating station, and the watt indication is then telemetered. Except in a few special cases, relatively slow types of telemetering now are being used successfully for indication and control purposes. For these special cases continuous and instantaneous types of telemetering have been successfully employed.[12,13]

RATE OF RESPONSE

Systems operating with a large number of active governors in good condition and with supplementary control will have but small frequency deviations for the usual load changes. The loads are absorbed by the active governors, and then the supplementary control may be used to return the average frequency and tie-line loads to normal. Under such conditions the supplementary control need not be particularly fast in response, corresponding only to the rate at which the average system load changes. This then allows for a relatively slow change in the output of the frequency-regulating station. It has been found that within limits the greater the allowable frequency departure, the less is the required speed of response of the frequency-regulating stations. Also, the greater the number of active governors, the less is the required speed of the response of the frequency-regulating station in order to prevent the frequency departure exceeding that which is tolerable. Operation in this manner relieves the frequency-regulating units by taking advantage of the diversity of load changes and allows them more opportunity to correct the frequency at a rate consistent with their steam and hydraulic energy supply conditions. The governor mechanisms and their control ordinarily are capable of allowing for increases in load at a rate which exceeds that allowed by the steam or hydraulic conditions.

The steam units of a station, because of the smaller time lags of steam than of water, are capable of providing a quicker response than hydraulic or hydrogenerators, if not limited in prime energy supply. However, steam plants may be so limited in their steam rate and steam storage capacity that many steam-generating units are limited at the steam-generation end. Hydraulic units may, however, have sufficient storage capacity but may require an appreciable time lag depending upon ability to change the hydraulic power to the turbine rapidly. When all these circumstances are considered it may well be that a low head hydraulic plant provides a more rapid means for regulating than a steam unit.

STEAM AND HYDRAULIC LIMITATIONS

The prime-mover energy storage and rate at which it may be changed are very often the most important limitations of the rate of response of a regulating station. In some instances slow boiler-control response may be an unnecessary restriction on the amount of load which can be quickly picked up. The control of the boiler should be such as to allow for as rapid change in steam flow as is possible without resulting in undue stressing of boiler or turbine. Most of the experience to date with regulating stations has been obtained using lower temperature boilers and turbines; some having chain grate stokers and others slightly faster underfeed stokers. Limitations in regulating rates are established under these conditions largely by fuel bed condition.

Very little experience is available on the newer high-pressure high-temperature boilers and turbines, using pulverized fuel (with mills having some storage capacity) or the somewhat faster response gas and oil-fired high-temperature boilers as regulating stations. The temperature-flow characteristics of a modern boiler using convection-type superheaters are such as to result in a decrease in temperature with decrease in steam flow; particularly is this true for flows below point *a* of Figure 3. Also, in a steam turbine the temperature distribution changes with flow as shown in Figure 4 for constant initial temperature. This is further aggravated by reduction in initial temperature at reduced flows. Accordingly, a reduction in flow may result in temperature difference of an appreciably greater magnitude in the turbine than in the boiler. The stresses in the turbine and also in the boiler depend upon the rate at which this change in flow and of temperature is made. A given amount of load change may be made instantly, while larger load changes should be made more slowly. This reasoning applies primarily to periodic changes, as the stresses due to varying temperature changes are more likely to result in fatigue stress. Even high temperature boilers and turbines usually can be expected to withstand without fatigue stress the changes in temperature resulting from instantaneous changes in flow of at least 15 per cent of rated flow, in the region in which boiler temperature is under reasonable control, and 100 per cent flow in about 30 to 60 minutes, depending upon the case under consideration.

The demands of speed governors and their prime-mover energy supplies due to normal frequency changes do not impose a severe burden on the supply. However, in units used for regulating purposes with supplementary control, it may be necessary to provide means such that any supply limitations may be avoided. It is evident from Figures 3 and 4 that conservative practice on a modern high-temperature installation of this type would be to confine the regulating range to flows in excess of 50 per cent and to a rate which does not overstress the critical locations. In this range the temperature changes in the turbine are appreciably reduced for variations in flow and the regulating range and rate may be correspondingly increased. In areas responsive to tie-line regulators, the burden may be distributed over a number of stations[8] and, likewise, frequency regulators operating in parallel can be used to advantage. Thus, there is every justification for believing that the number of regulating units will increase, thereby reducing the burden on any one unit, just as the active participation by all units on governors reduces the burden.

On some present-day systems the regulating capacity is so distributed as to limit the required maximum rate to 10 per cent of regulating-unit capacity per minute over a limited range. This may be considered a moderate regulating rate in comparison with load changes encountered on some smaller systems with rough loads. For a system which has a rate of load change of one per cent per minute this would require a regulating capacity of at least 10 per cent of system capacity.

It will be recognized that in case of emergency, a boiler and turbine should be capable of dropping or picking up full load almost instantly if operating temperatures had been previously established following the starting-up cycle. Such operation, although tolerable in an emergency, cannot be considered good practice if done periodically, unless special provision for such operation has been made in the design.

Large Rapid Load Changes

For those systems in which the suddenly applied load changes are an appreciable part of the total connected capacity, it is found that the frequency deviation will be correspondingly greater. If this exceeds what is considered a tolerable limit, it becomes necessary to use supplementary control having a greater rate of response. This then puts a greater burden on the regulating station, and, in order to avoid too great a frequency deviation, it may mean a sacrifice in economy, or a provision for larger energy-storage capacity, or an increase in the rate at which the prime energy is generated. It is evident that, except for the change in system loading with change in frequency, the net load change must be absorbed by generating capacity, at the corresponding rate of load change. For systems where the load change is a large part of the total load this may become a serious problem.

A large strip mill load may have a load increase of 30,000 kw in 5 to 20 seconds. If it were necessary to follow this load change with regulating capacity comparable with present-day practice on the larger systems, over a million kilovolt-amperes would be required. Fortunately, this type of system is the exception rather than the rule. In these systems, experience has indicated that the limitation is primarily in the available steam supply, and when operated as isolated systems, speed governors alone are relied on for speed control resulting in corresponding large frequency changes. Unless special provision is made for steam supply capacity of the prime movers, large fluctuations in steam pressure will result.

Such a system which finds itself subjected to large suddenly applied loads; for example, a strip mill load which is large compared with its generating capacity, may interconnect with another system in order to help absorb the load changes. If this is done, the frequency deviation will be reduced, and a portion of the load change will be transmitted across the interconnecting tie. In general, the reduction in frequency deviation and the load change transmitted across the tie will be in proportion to the capacity of the external system to the total capacity. By interconnecting with another system in this manner, the local system makes use of the additional prime-mover energy capacity of the other connected system, the load changes being distributed among an increased number of actively governed generators. If the frequency deviation produced by the rapidly varying load is within tolerable limits without the use of supplementary control, little regulating capacity is needed to handle such a load because it will be removed as quickly as it is applied. Accordingly, the supplementary control need correct only the average frequency deviation. This provides a very practical and reasonable method for handling such rapidly varying loads.

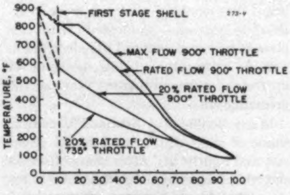

Figure 4. Temperature distribution in a modern high-temperature steam turbine

Occasionally there has been a demand for adequate control of rapid tie-line swings. In this case it becomes necessary to provide special control. The studies which have been made indicate that the limitation may not be in the control equipment so much as it is in the energy supply of the prime movers. From both analysis and experience, hunting or instability between the regulating units is more likely to occur when the rate of response of the regulating units is increased. For units which require a higher rate of response of their supplementary control, it is necessary for their governors to be in good mechanical condition with small dead band and to be stable mechanisms when operating alone. Under these conditions the rate of response with properly designed supplementary control can be further increased before the instability region is reached (see Figure 21—reference 6). The results of the analysis presented in the companion paper[6] indicate that with no limitation in energy supply the tie-line deviation may be held to 15 per cent for a continuous controller and 25 per cent for any intermittent controller for a load applied at a rate of 150 per cent of regulating capacity per minute, the regulated capacity being the total capacity of the local area and tied to the major system by a comparatively weak tie. Both this analysis and experience indicate that the control mechanism is not the limiting factor, since the energy supply limitation makes itself felt earlier.

Summary

It appears that the greatest improvement in over-all system frequency and tie-line regulation will be brought about by further improvement in the composite regulation of the system governing and by more widespread and co-operative application of supplementary control equipments which are already developed.

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PCC Car Operating Results in Pittsburgh

W. J. CLARDY
MEMBER AIEE

Synopsis: The problem created by the decline in transit revenues during the depression led the Pittsburgh Railways Company to initiate a long-range service-betterment program. The most important phase of the plan was the use of modern street cars, as developed by the Presidents' Conference Committee, to replace the earlier types of rolling stock on the property. With 301 of these PCC cars in service, and 100 additional units scheduled for early delivery, sufficient data are available to show exceptionally favorable operating results. Actual experience in service has demonstrated that car maintenance comes down 28 per cent, track and roadway maintenance drops 21 per cent, accidents decrease 25 per cent, the number of cars required for specific schedules is reduced 10 per cent, and revenues increase 8 per cent. This favorable outcome has caused continued expansion in the use of such equipment and established a superior urban-transit service

PEOPLE now have the utility in city transit which is so vital to the conduct of daily affairs. The streamlined PCC car, which gets its alphabetical name from the Presidents' Conference Committee sponsoring its development, provides a degree of excellence in public travel that has established a superior performance standard. Pittsburgh was one of the first cities to adopt this modern transit unit. A fleet of 301 cars is in operation and 100 additional vehicles are under construction. This is the largest installation of PCC cars, and, with a total of 401, there will be sufficient to operate base schedules on all street-car lines. Initial service dates back to August 1936, and the results are enlightening. Car maintenance has decreased 28 per cent, track and roadway maintenance has dropped 21 per cent, accidents per 10,000 car miles have come down 25 per cent, schedule car requirements have been lowered 10 per cent, and gross revenue has increased 8 per cent.

Operating Improvements Needed

During the lean years of the early '30's, the Pittsburgh Railways Company recognized the need for restorative measures to regain lost gross revenue and to lower operating costs. When the depression began to slow things down, one logical recourse was finding means of speeding things up. This involved re-examination of existing schedules to establish a program that would give service more beneficial to patrons. Coincident with this plan, it appeared essential to reduce maintenance expenditures below the level set up for the more prosperous years of the preceding era.

Many people had attained a critical frame of mind with respect to the street car, and rightly so. After years of seeing and riding in essentially the same noisy uncomfortable vehicle, such an opinion was understandable. Urban passengers responded to this neglect of their needs by transferring their patronage to the public carrier which provided adequate service. Modern street cars were needed to meet the demand for greater usefulness in city travel by providing service superior to any other vehicle for surface mass transportation. Action was required to give fast, convenient schedules, lower the cost of the car ride, relieve street congestion, and handle rush-hour traffic effectively.

Plans for Better Transit Service

Careful analysis of the transit problem led to the conclusion that immediate action was required for speeding up and reconditioning the outmoded rolling stock. Such a plan was put into effect to lower maintenance costs and arrest declining revenues. Motors were rewound for higher speeds, brakes were modified to obtain more effective stopping characteristics, and car interiors were improved.

It was recognized that the most satisfactory operation required modern street cars. However, the immediate benefits of existing rolling-stock improvements could be garnered pending the fulfillment of a long-range program for the procurement of new vehicles. Sufficient cars were reconditioned to handle more than 50 per cent of the mileage operated.

The plan for the purchase of new rolling stock was initiated with the installation of a single trial PCC car in August 1936. The first 100 cars ordered were placed in service from March through May 1937, the second 100 during November 1937, and the third 100 in April 1940. The fourth order for 100 cars will soon be ready for delivery. The type of car procured is shown in Figure 1, and the extent of the installation is illustrated by Figure 2.

Figure 1. Modern PCC car in operation in Pittsburgh

These modern cars have many outstanding advantages which are assisting in meeting transit problems in Pittsburgh. Some of the more important are as follows:

1. Performance is far in advance of any vehicle now using city streets. The speedy quiet-operating trolley car can save from 6 to 15 minutes of every hour's travel time in comparison with the older types.
2. A high degree of agility is attained with comfort to riders. Deep-cushioned forward-facing seats, wide clear-view windows, ample stanchioning, adequate ventilation, uniform distribution of heat, and plentiful indirect illumination conform to the demands of passengers.
3. The control and braking equipment permits attainment of a degree of smoothness of performance that is unexcelled in surface travel.
4. Schedule speeds are higher than any other form of public transportation on congested city streets. It is possible to use

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W. J. Clardy is transportation application engineer, transportation and generator engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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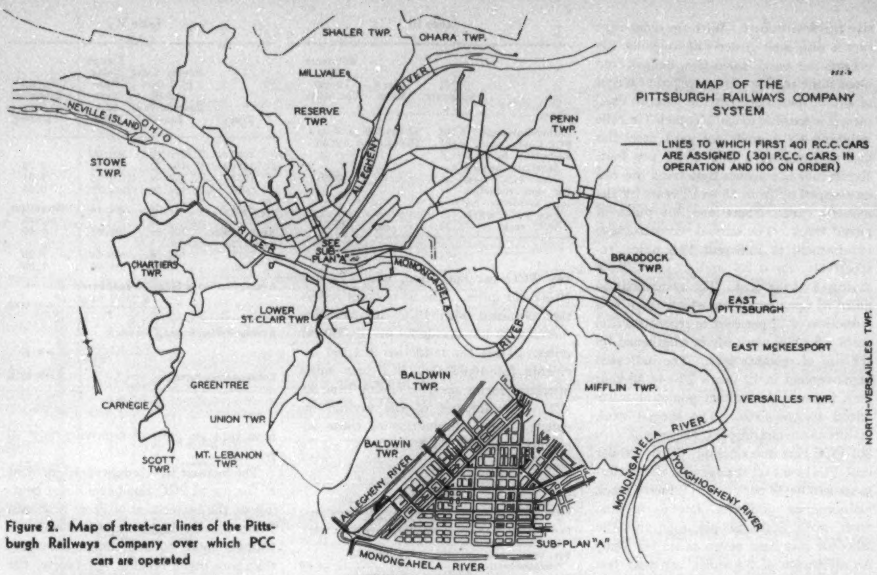
MAP OF THE
PITTSBURGH RAILWAYS COMPANY
SYSTEM
LINES TO WHICH FIRST 401 P.C.C. CARS ARE ASSIGNED (301 P.C.C. CARS IN OPERATION AND 100 ON ORDER)
SHALER TWP.
OHARA TWP.
MILLVALE
RESERVE TWP.
PENN TWP.
NEVILLE ISLAND
OHIO
STOWE TWP.
ALLEGHENY RIVER
RIVER
SEE SUB-PLAN "A"
MONONGAHELA
RIVER
CHARTIERS TWP.
BRADDOCK TWP.
EAST PITTSBURGH
LOWER ST. CLAIR TWP.
EAST MCKEESPORT
NORTH-VERSAILLES TWP.
BALDWIN TWP.
MIFFLIN TWP.
VERSAILLES TWP.
GREENTREE
CARNEGIE
UNION TWP.
MONONGAHELA RIVER
YOUGHIOGHENY RIVER
BALDWIN TWP.
SCOTT TWP.
MT. LEBANON TWP.
ALLEGHENY RIVER
SUB-PLAN "A"
MONONGAHELA RIVER

Figure 2. Map of street-car lines of the Pittsburgh Railways Company over which PCC cars are operated

fewer units—10 to 20 per cent—a factor that is reflected directly in the fare cost.

5. Fast schedules and the corresponding reduction in the number of cars minimize traffic congestion and allows the most efficient use of city streets by the greatest number of people.

6. Starting is fully automatic with an unlimited selection of rates—speed of starting—to conform to the particular conditions of street traffic encountered.

7. Smooth electric braking is provided under automatic control. It functions at all times—even if the trolley loses its contact with the overhead wire. In addition magnetic track and air brakes are available.

PCC Car Operating Results

The rehabilitation of old cars to secure higher schedule speeds and reduce maintenance proved quite successful. Results were particularly beneficial in providing better service with lower operating costs. The investment required was more than justified and served to establish a transition to the important part of the program—replacements with modern cars.

PCC car operation has produced some attractive savings in vehicle inspection and maintenance. In comparison with the earlier-type cars, the wheel-grinding period has been doubled (20,000 to 40,000 miles), wheel life is increased from 140,000 to 200,000 miles, brake shoes last 15 times as long, and inspections have gone up from 250 to 5,000 or 10,000 miles, the higher figure being applicable to cars without hand brakes. Parts for PCC cars cost more, but fewer are subject to rapid wear, and the over-all result indicates less expense. Actual labor costs for specific jobs are about the same, so the decrease in replacements means less hours worked.

An analysis has been made of the comparative expenditures for the three-year period from June 1937 to June 1940. Materials are taken from storeroom requisitions and segregated between old and PCC cars. Labor is distributed in accordance with the number of cars passing through the shop—67 per cent earlier-type and 33 per cent PCC. Fixed expenses, such as superintendence, shop equipment, and shop expenses are allocated on a car-mile basis. The total costs for the years studied are shown in Table I.

Table I

Cars	Car Miles	Cents per Car Mile
PCC	32,500,000	1.70
Old-type cars	60,912,000	2.49

These data show a net saving of 0.79 cents per car mile for the PCC cars, which excludes painting, since none had been through the paint shop. A liberal allowance for this item reduces the net saving to 0.7 cents per car mile or a gain of 28 per cent.

Track- and roadway-maintenance costs have been substantially reduced by PCC car operations. The extensive use of rubber and the light weight of the trucks of the new cars are two pertinent factors affecting the action upon track and roadway in comparison with older-type equipment. The principal use of rubber is in the wheels and journal springs. The resilient construction permits normal deflections of one-fourth inch for wheels and two inches for journal springs. The Presidents' Conference Committee directed a program of exhaustive impact tests to determine comparable vertical accelerations of resilient and conventional steel wheels. Based on these data, the earlier-type cars in Pittsburgh impart impact forces to the track structure six times as great as PCC cars incident to track irregularities and vehicle movement. Track damage because of this action loosens the entire structure including the paving bond. Surface water enters, resulting in rapid deterioration and expensive maintenance. Special track work is particularly vulnerable because of the frogs and switches.

The lower impact forces and lighter weight of PCC cars permit a less expensive track structure. With the older-type cars a nine-inch girder rail weighing 134 pounds per yard, wood ties, ballast, and blockstone paving were required at a cost of $12.50 per foot. In the case of PCC cars, it is feasible to use $5^3/_4$-inch Tee rails weighing 100 pounds per yard, steel ties and concrete at a cost of $9 per foot. Experience has shown that track life can be stepped up from 25 to 32 years by the modern cars. There are 394 miles of paved track. The annual reconstruction requirement is 15.8 and 12.3 miles, respectively, for a 25- and 32-year life, a saving of 44 per cent. The system has 54 miles of open track on which there is a reduction of 13 per cent in reconstruction costs. Special work life is lengthened by the use of modern cars. The indicated improvement is 10 years (20- to 30-year life), because the impact pounding is reduced to one-sixth. The special work maintenance has dropped 14 per cent with 301 PCC cars in operation. With 100 per cent PCC cars on the system, the betterment will be 33 per cent. Ordinary track maintenance includes heavy repairs. Such work costs $1 per foot and the schedule has been seven miles per year. An allowance of 2.5 miles per year has proved ample which saves 12 per cent.

During the year ending June 30, 1941, 29,481,000 car miles were operated, with 16,923,000 or 57.3 per cent PCC. A summary of the percentages saved in track and roadway expense is shown in Table II.

Table II

PCC Cars in Use	Per Cent 100	80	57
Savings			
Reconstruction—paved track	44.0	33.2	25.1
Reconstruction—open track	13.4	10.7	7.6
Special work	33.3	26.6	19.0
Ordinary maintenance	12.0	9.6	6.9
Average	37.1	29.6	21.1

PCC cars have reduced accidents because of their many safety features and quick stopping ability. Analyses of four comparative 12-month periods show decreases of 24, 25, 25, and 26 per cent of accidents in comparison with earlier-type cars. A typical illustration is the accompanying data for the year ending September 30, 1940 shown in Table III.

Table III

	Accidents	Car Miles	Accidents Per 10,000 Car Miles
Earlier-type cars	5,166	14,729,129	3.51
PCC Cars	3,531	13,346,580	2.65
Reduction in accidents per car mile with PCC cars			0.86
Per cent reduction in accidents per car mile with PCC cars			25

The average accident rate for the four years from 1932 through 1936, prior to any PCC car operation, was 2.74 per 10,000 car miles. The 25 per cent reduction obtained with PCC car operation means 0.69 less accidents per 10,000 car miles. Thus, the reduction is 1,167 accidents for 16,923,000 PCC car miles operated during the year ending June 30, 1941. An analysis of the savings incident to the elimination of these accidents is shown in Table IV.

Table IV

Per cent total accidents resulting in nonlitigated settlement	15.97
Per cent total accidents resulting in litigated settlement	4.66
Saving in nonlitigated settlements (1,167 × 0.1597)	186
Saving in litigated settlements (1,167 × 0.0466)	54

PCC cars provide faster schedule speeds than the earlier types because of their more rapid starting and stopping characteristics, higher free-running speeds, and quicker interchange of passengers. This makes it practicable to give the same frequency of service with fewer units in the case of the modern vehicles. A tabulation is included in Table VI which shows improvements made in schedules with high-speed cars and the first 201 PCC cars. In comparison with the earlier low-speed types the average betterment is 16 and 12 per cent, respectively, for base and peak service.

Considerable improvement was realized when the earlier-type cars were speeded up which meant less schedule speed increase for the system with the introduction of PCC cars. Also, the speeded-up cars are used with PCC cars in peak service, which limits the schedule speeds obtainable with the latter vehicles. A study of the routes on which the 301 PCC cars are now operating shows that improved performance permits replacing 10 of the earlier-type cars with 9 of the new type—a gain of approximately 10 per cent.

Table V

Year	Routes Using PCC Cars (18-Hr Base Service) Index	Routes Using Old-Type Cars Index	Spread Between Two Classes of Routes
1933	100.00	100.00	
1934	104.37	107.09	−2.72
1935	104.94	106.95	−2.01
1936	114.56	114.27	0.29
1937	117.65	112.14	Transition period
Year ending 3/31/39	109.66	102.97	6.69
1939	108.58	103.63	4.95
1940	112.78	104.95	7.83

Average spread in 1934, '35, and '36 =

$$\frac{-2.72-2.01+0.29}{3}=-1.48$$

Average spread in last 3 periods =

$$\frac{6.69+4.95+7.83}{3}=6.49$$

Total average spread =7.97%

The increase in schedule speed incident to the use of PCC cars has a direct bearing on the betterment of gross revenues. Performance is highly important and contributes more to attracting passengers than any other feature. Of course, the comfort of the vehicle is of consequence from the "satisfied-customer" viewpoint. The first 201 PCC cars placed in service have been operating a sufficient length of time to show a definite "revenue trend" for the new equipment.

An analysis has been made which indicates the results shown in Table V.

The year ending March 31, 1939, was the first complete 12 months after all of the routes using the 201 new vehicles had been equipped for operation of the 18-hour base service. The gain in revenue shows a spread of practically eight per cent in favor of PCC cars.

PCC Cars Extensively Used

The PCC car is an outstanding city-transit vehicle with exceptional utility. Its success for improving urban travel is demonstrated by the favorable results obtained in Pittsburgh and the 15 other cities which use the vehicle. It serves to reduce car maintenance, lower the upkeep of track and roadway, decrease accidents, raise schedule speeds, and increase revenue. A degree of excellence is established in public travel that is a creditable contribution to the advancement of city transit.

Table VI. Pittsburgh Railways Company Data on Schedule-Speed Improvements With High-Speed and PCC Cars

Route	Miles Round Trip	Low-Speed Cars				High-Speed Cars						PCC Cars					
		Min. Round Trip		Schedule Speed—MPH		Min. Round Trip		Schedule Speed—MPH		% Schedule Speed Increase		Min. Round Trip		Schedule Speed—MPH		% Schedule Speed Increase	
		Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak	Base	Peak
2—Etna	12.48	57	60	13.10	12.50	50	53	15.00	14.10	14.50	12.80	46	54	16.30	13.90	24.40	11.20
3—Millvale	10.03	57	60	10.60	10.00	45	49	13.40	12.30	26.40	23.00	44	52	13.70	11.60	29.20	16.00
4—Troy Hill	5.47	38	41	8.60	8.00	33	35	9.95	9.40	15.70	17.50						
6—Brighton Road	9.49	50	56	11.40	10.20							45	51	12.70	11.20	11.40	9.80
7—Charles Street	6.34	47	50	8.10	7.60	45	48	8.45	7.90	4.30	3.95						
8—Perrysville	10.41	49	55	12.70	11.40	50	53	12.50	11.80	−1.58	3.50						
10—West View	14.36	69	72	12.50	12.00							58	68	14.85	12.70	18.80	5.80
13 & 14—Emsworth & Avalon	17.85	82	86	12.90	12.30							72	80	14.70	13.20	14.00	7.30
15—Bellevue	16.25	79	82	12.30	11.90							69	76	14.10	12.90	14.60	8.40
18—Woods Run	8.81	50	54	10.60	9.80	49	54	10.80	9.80	1.90	0						
19—Western	5.96	35	37	10.20	9.70	34	39	10.50	9.20	2.90	−5.20						
27—Carnegie	14.57	71	74	12.30	11.80	62	67	14.10	13.00	14.60	10.20	62	68	14.10	12.80	14.60	8.50
30 & 31—Crafton	8.86		41		13.00		39		13.60		4.60						
30 & 31—Sheridan	9.60	48		12.00		42		13.40		11.70							
30 & 31—Ingram	11.69	56	59	12.50	11.90	52	56	13.50	12.50	8.00	5.00						
38—Mt. Lebanon	11.21	61	65	11.00	10.30	51	57	13.20	11.80	20.00	14.60	52	53	12.90	12.70	17.30	23.30
39—Brookline	10.06	56	59	10.80	10.20	48	49	12.60	12.30	16.70	20.60						
40—Mt. Washington	8.84	57	70	9.30	7.60	50	55	10.60	9.70	14.00	27.60						
42—Dormont	10.06	56	59	10.80	10.20	44	48	13.70	12.60	26.90	23.60						
47—Carrick T.	12.52		71		10.60		66		11.40		7.50						
50—Carson St.	7.05	49	52	8.60	8.10	44		9.60		11.60		44	48	9.60	8.80	11.60	8.60
53—Carrick	12.12	69	74	10.50	9.80	55		13.20		25.70		59	69	12.30	10.50	17.20	7.20
55—Homestead, B. & E.P.	25.28	128	138	11.80	11.90	112	116	13.50	13.70	14.40	19.10	112	124	13.50	12.20	14.40	10.90
56—McKeesport (McK.)	26.40	124	129	12.80	12.30												
56—McKeesport (P)	25.72					114	119	13.50	13.00								
57—Glenwood	9.70	44	48	13.20	12.10	41	46	14.20	12.70	7.60	5.00						
58—Greenfield	9.76	52	55	11.30	10.60	43	49	13.60	12.00	20.40	13.20						
60—E. Liberty & H.M.	11.35	67	69	10.20	9.90	61	63	11.20	10.80	9.80	9.10						
60—E. Liberty & H.R.	13.69					69	73	11.90	11.20								
64—Wilkinsburg & E. Pgh	25.40	138	141	11.00	10.80	113	123	13.50	12.40	22.70	14.80	120	130	12.70	11.70	15.50	8.30
66—Wilkinsburg	15.20	81	84	11.30	10.80	74	79	12.30	11.60	8.90	7.40						
67—Swissvale R.	18.27	94	98	11.70	11.20	78	83	14.10	13.20	20.50	17.80						
67—Swissvale 13th	20.41											98	102	12.50	12.00		
68—McKeesport 6th	30.15	165	165	11.00	11.00	144	154	12.60	11.70	14.50	6.40	135	138	13.40	13.10	21.80	19.10
68—McKeesport P.	32.43											149	152	13.10	12.80		
69—Squirrel Hill	12.36	66	69	11.20	10.70	62	65	12.00	11.40	7.10	6.50	64	68	11.60	10.90	3.60	1.90
71—Negley	13.26	78	81	10.20	9.80	60	66	13.30	12.00	30.40	22.40	62	73	12.80	10.90	25.50	11.20
73—Highland	13.52	76	80	10.70	10.10	62	65	13.10	12.50	22.40	23.80	62	72	13.10	11.30	22.40	11.90
75—Oak. & E.L. & Wilkinsburg	14.81	86	91	10.30	9.80	70	75	12.70	11.90	23.30	21.40	71	83	12.50	10.70	21.40	9.20
76—Hamilton	15.74	85	91	11.10	10.40	71	75	13.30	12.60	19.80	21.20	70	79	13.50	12.00	21.60	15.40
77 & 54—N.S. & Carrick	24.43	136		10.80		122	135	12.00	10.90	11.10							
82—Lincoln	14.44	89	96	9.70	9.00							81	84	10.70	10.30	10.30	14.50
87—Ardmore	28.80	150		11.50		137	146	12.60	11.80	9.60							
87—Ardmore S.	2.20	12	13	11.00	10.20	14	17	12.50	10.30								
88—Frankstown	14.76	83		10.70		75	80	11.80	11.10	10.30		74	78	12.00	11.40	12.20	
88—Frankstown S.	1.84	10	11	11.00	10.00	8	9	13.80	12.30			14	15	11.70	11.00		
94—Aspinwall	14.82	82	87	10.80	10.20							72	77	12.40	11.50	14.80	12.70
96—E.L. & 62nd	9.31	54	54	10.40	10.40	50	50	11.20	11.20	7.70	7.70						

Improvement in Modern Meter-Testing Technique

EDWARD E. LYNCH
MEMBER AIEE

MARK A. PRINCI
ASSOCIATE AIEE

THROUGH more than a score of years a variety of devices has been developed to simplify the procedure and to reduce the hazard of personal errors in the technique of watt-hour meter testing. Of these devices the stroboscope and the photoelectric timer have already become generally accepted tools of the meter-testing art. Indeed, meter-testing problems differ somewhat in the field, in the laboratory, and in the factory, and consequently, no single device nor combination of devices can be considered ideally applicable to all three forms of testing. However, in each case much is to be gained through the use in some form of the many schemes[1,2] which have recently been developed. Such schemes apply equally to single-phase and polyphase meter testing, though in this discussion we shall be concerned only with the single-phase watt-hour meter and the testing problems peculiar to the factory. Briefly these problems are:

1. Removal of the hazard of personal error in testing.
2. Maintenance of closer control of quality.
3. Elimination of errors in portable standards through handling.
4. Reduction of skill required in testing.
5. Shortening of testing time.
6. Reduction of handling of meters.
7. Elimination of storage space for meters awaiting test.
8. Maintenance of a uniform flow of the product from assembly through to packing.

All of these factors contribute to the achievement of greater uniformity and calibration accuracy as well as to a generally improved quality of the product.

The system which was evolved and which is described in this paper is the result of an investigation conducted under actual manufacturing conditions. A small production line was set up which afforded ready means for close study of the factors outlined above and for trial of the several schemes investigated. It became immediately apparent that testing should become as nearly as possible an integral part of the assembly procedure, if the necessity for storing meters for test were to be avoided. That is, the testing procedure must be broken down into operations that might readily be accomplished during or immediately after the assembly of the meter and in synchronism with the cycle of assembly operation. Logically then, the testing procedure must be broken down into these two major steps:

1. Adjustment of calibration at each of the three conventional load points (100 per cent load, unity power factor; 10 per cent load, unity power factor; 100 per cent load, lagging power factor 50 per cent).
2. Verification of calibration accuracy at all three load points.

Conventionally, calibration adjustments at these three load points are made in successive cut and try steps. Usually a rough adjustment of light load is made by applying potential only to the meter being tested and adjusting until the meter disk does not creep. This is the familiar "creep method." Lag-load adjustment, because of the uniformity of controlled manufacturing processes, is set within close limits in assembly. The setting of full load or the location of the damping magnet with respect to the center of the meter disk is done by successive test runs and adjustments until the desired accuracy is achieved. When the accuracy at all three load points called "as found" readings are obtained, a simple calculation enables the tester to make those minor adjustments which will bring the meter accuracy at all points within the desired limits. Obviously, the smaller the spread in the "as found" readings the simpler the calculation and adjustment required on a particular meter. However, many cut and try steps are required before the "as found" readings are within sufficiently small limits to permit a single set of adjustments which will bring the entire calibration within the accuracy required.

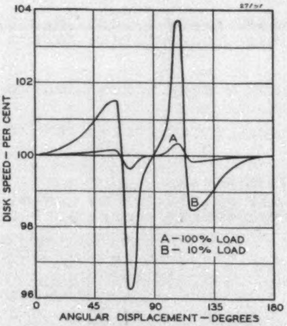

Figure 1. Curve showing angular velocity of watt-hour meter disk as a function of displacement

Speed is given in per cent of nominal-load value for a General Electric type I-30 watt-hour meter

In addition to the above limitations of the conventional procedure, long-time test intervals must be taken for each test run if a high degree of accuracy is to be achieved. This is due to the fact that meters are usually tested in gangs of as many as 15, connected to a common load with a rotating standard, for an interval corresponding to a given whole number of revolutions of the rotating standard. The displacement from the starting position of the disk of each meter is a measure of the error at the particular load point being tested. The precision within which these readings of accuracy can be made depends on the length of the test interval. Further, there is the problem of correcting for the error in the rotating standard.

Thus it is apparent, that much manipulation is required, before a satisfactory set of "as found" readings is obtained. It is also evident that, if the preliminary steps could be accomplished less laboriously, and if the entire calibration accuracy could be had at once, the procedure would be tremendously simplified. The discussion which follows, therefore, will describe the usefulness of the devices developed in this investigation to accomplish the desired simplification in procedure.

Calibration Adjustment

Full-Load Adjustment

Consistent with the scope of the investigation, the means provided for making calibration adjustment must be simple and, if possible, render instantaneous indication of changes in calibration. The stroboscope[3,4] is immediately suggested. We must, however, consider its limitations, for its practical value is dependent upon the angular velocity of the object being viewed, a distinct image, and a fre-

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Edward E. Lynch is meter engineer and Mark A. Princi is development engineer in the works laboratory, both with General Electric Company, West Lynn, Mass.

quency of light impulses which is compatible with good vision. The last two of these requirements, over the common ranges of frequency required, are readily met by mechanical or electronic means, or by a combination of both.

The third requirement deals with the angular velocity of the moving element, and an analysis of the conditions required for a stationary image or for a "hunting" image is necessary to understand its influence. Assuming a constant rate of light flashes upon a moving element, any change in angular velocity of this element will produce a change in the rate of displacement of the stroboscopic image with respect to a fixed reference point. When synchronism is achieved a stationary image results.

At synchronism $f = \frac{Nw}{2\pi}$

where

f = rate of light flashes in cycles per second
w = angular velocity of disk in radians per second
N = number of equally spaced elements

If the disk velocity is alternately greater and less than the synchronous speed, the image will be seen to advance and recede according to fluctuations in velocity. This characteristic is called "hunting." It is largely this condition which limits the universal application of the stroboscope in watt-hour meter adjustment, since the operation becomes tedious and time consuming and, in cases of excessive hunting, requires a high degree of skill to achieve accuracy. If observation of image displacement is hindered by hunting, the fluctuating image must be followed through its entire excursion (advance and recession) and the resultant advance and recession noted.

Figure 1 shows the speed–displacement characteristic of a watt-hour meter at both full load and ten per cent load. The magnitude of the variations in this curve is constant at all values of load, so that in percentage this magnitude decreases with increased load. The speed characteristic of modern meters is, therefore, quite uniform at the high values of load, but at light loads a large variation in angular velocity exists.

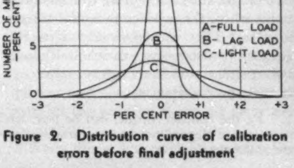

Figure 2. Distribution curves of calibration errors before final adjustment

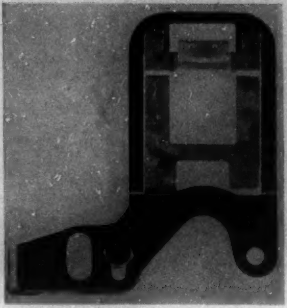

Figure 3. Adjustable-type compensated lag plate

The hunting of the image at these light loads could be eliminated if the flashing rate were made to fluctuate at a frequency corresponding to the irregular angular velocity of the disk and kept in time phase. This condition can be actually achieved only with difficulty and it is more practical to utilize a uniform flashing rate.

With the above facts in mind, a quantitative consideration of the angular velocities of the disk at various values of load on the meter, together with the desired limits of accuracy at each value of load, will define conclusively the limits of stroboscopic application to meter testing. A modern watt-hour meter such as that from which the characteristics were determined for Figure 1 has 720 equally spaced elements on the edge of the disk. At nominal load its speed is 1.745 radians per second, and it would, therefore, require a flashing rate of 200 per second to synchronize it at this speed or 20 per second at one-tenth this speed.

The image advance of this meter will be 7.2 elements per revolution of the disk when 99 per cent accuracy is obtained, or 0.72 element per revolution when 99.9 per cent accuracy exists. These displacements can be instantaneously observed if there is no hunting and may not be imperceptible with a small amount of hunting. If, however, hunting produces an image fluctuation greater than about five times the image displacement caused by the inaccuracy in meter calibration, then the personal error becomes large, and the stroboscope cannot be used except by specially skilled workers. This value is arbitrary and is the opinion of the authors obtained through experience.

The hunting magnitude is influenced by various factors which can be conveniently classified as follows:

I. Those that produce an essentially constant hunting magnitude at all loads:

(*a*). Eccentricity of the disk.

(*b*). Nonuniformity of flashing rate.

(*c*). Nonuniformity of disk marking.

(*d*). Nonhomogeneity of the disk metal.

(*e*). Bent disks.

II. Those that produce their maximum effect at low loads:

(*a*). Variation in potential torque with disk position.

(*b*). Variation in friction with disk position.

Since the maximum total hunting will be produced at lower loads, we can see not only the difficulty of adjusting with small torques such as at light loads, but also the advantage of adjusting for nominal-load calibration with 200 per cent load current. An analysis of tests has shown that nominal-load adjustment can be made at 200 per cent load without affecting the load-speed characteristics of the meter appreciably, and for this reason, the fixture to be described later is designed to operate at 200 per cent load.

Figure 4. Front view of section of conveyor system and calibration-adjusting stations

LIGHT-LOAD ADJUSTMENT

It can be concluded from the foregoing analysis that the stroboscope does not lend itself satisfactorily to calibration adjustment at light loads. Its convenience is also lessened, due to the necessity of alternately and repeatedly adjusting each load until all errors are sufficiently small. This is due to the effect of the light-load adjustment on other loads.

To be most convenient, the light-load adjustment must be accomplished as nearly as possible without influencing the other load adjustments, and the well known "creep" method which adjusts the torque due to potential alone so that the disk will not creep makes this possible.

The method consists of applying voltage to the potential coil and, without any load current, moving the light-load adjustment until the meter disk will not creep in either direction. In principle, it is one used for many years as a rough method of adjusting meters at light load.

Let us analyze just what we accomplish by so doing, for an analysis will show that although we are not fundamentally getting the adjustment we would like, we nevertheless accomplish a very good approximation of light-load adjustment.

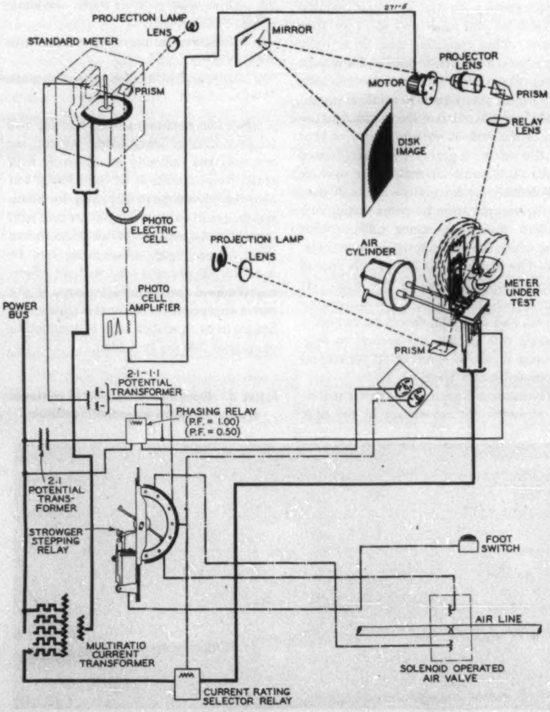
27/-6
PROJECTION LAMP
LENS
STANDARD METER
PRISM
MIRROR
PROJECTOR LENS
MOTOR
PRISM
RELAY LENS
DISK IMAGE
PHOTO ELECTRIC CELL
PROJECTION LAMP
LENS
AIR CYLINDER
METER UNDER TEST
PHOTO CELL AMPLIFIER
POWER BUS
PRISM
2-1-1-1 POTENTIAL TRANSFORMER
PHASING RELAY (P.F. = 1.00) (P.F. = 0.50)
2-1 POTENTIAL TRANS-FORMER
STROWGER STEPPING RELAY
FOOT SWITCH
AIR LINE
MULTIRATIO CURRENT TRANSFORMER
SOLENOID OPERATED AIR VALVE
CURRENT RATING SELECTOR RELAY

Figure 5. Line schematic diagram of internal connections in adjusting station

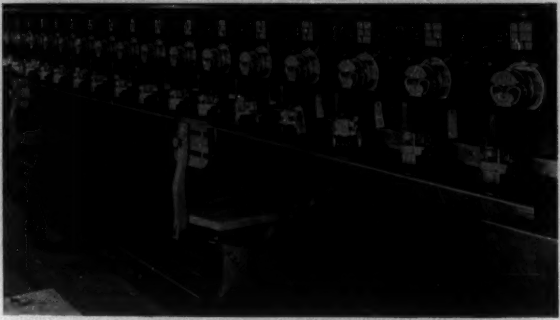

Figure 6. Portion of conveyor system showing bank of verification stations

A definite but small error is introduced when we attempt to make the potential torque, T_e, equal to zero for, in a meter correctly calibrated at full, light, and lag loads, we find that T_e is rarely zero.[5] This small error will be a variable influenced by two factors shown below.

If we consider a general equation, such as the following, and realize that at the moment of "no creep" with no motion of the disk, the inertia and damping terms (both dependent on disk motion) are not present, and since the balance is performed with no load current, the equation[6] reduces from

$$J\frac{d^2\theta}{dt^2}+(D_m+D_i+D_e)\frac{d\theta}{dt}+(T_{ei}+T_i+T_e-T_f)=0$$

to

$$T_f=T_e$$

which means that the driving torque due to potential flux alone is made equal to the friction torque (see appendix).

This simple equation is complicated by the fact that T_e (similar to Figure 1) is not a simple function but depends on the disk displacement. Therefore, our calibration will depend on what part of the curve T_e we make equal to the friction torque.

By using care to see that the anticreep holes are kept 90 degrees from the element air gap, we can adjust the meter at a disk displacement shown in Figure 1, as 0 or 180, thus attempting to adjust at a point where the average potential torque is zero. This will obviously give the greatest consistency possible, although two factors will tend away from extremely precise results:

1. T_f, the friction torque, can be positive or negative depending on the direction of the tendency to creep.

2. The point of adjustment may differ from that at which the shunt torque has its average values.

Although the above discussion might indicate that this method will not inherently give absolutely correct adjustment, a practical application of it has indicated that a very high percentage of meters will be obtained which are within normal allowable tolerance. A curve showing the distribution of accuracies over a large number of meters tested is shown in Figure 2 and is not inconsistent with the above analysis. For a preliminary adjustment there seems to be no better method of setting the light-load adjustment.

Any effort to try to adjust to a slight forward creep to compensate for the average negative error is apt to result in "personal" errors based on "judgment" which will be greater than the above inherent error, and, therefore, it is to be discouraged.

LAG-LOAD ADJUSTMENT

The third load point at which calibration is made consists of setting the lag plate which compensates for the phase angle of the potential coil. Adjustment of this compensator is made at 50 per cent power factor and 100 per cent load current. From the preceding discussion of the stroboscope, it is apparent that reasonable accuracy may be had by using that device for making adjustment. Other methods, such as creep at zero power factor, torque method, or rotating-standard method, are also suitable, and at this juncture, the question naturally arises, why not use one of these methods—for instance, the stroboscope? This might well be accomplished, and many modifications have been developed which make the stroboscopic method convenient. However, any such scheme would require a succession of tests at each of the three loads and a return, until the error at each point has become sufficiently small to to give satisfactory calibration. In our application a "straight-line" procedure was sought, and since such a method involves definite advantages, any one which was not directly applicable to such a straight-line procedure was considered undesirable.

Adjusting the meter not to creep with zero power factor might well be used to accomplish this purpose, but it was found that, with meters equipped with an adjustable lag plate as shown in Figure 3, a quicker and equally satisfactory method is to set the position of the lag plate mechanically.

When this is done with an accurate fixture, a large percentage of meters will be correctly calibrated for lagging loads. A distribution curve of accuracies to be expected are shown in Figure 2b, which summarizes the experimental results on hundreds of meters over a period of several months. Rigid manufacturing control is necessary of the factors entering into the amount of lagging required and that obtained, such as the resistance of the potential coil and the conductivity of the lag plate, and so forth.

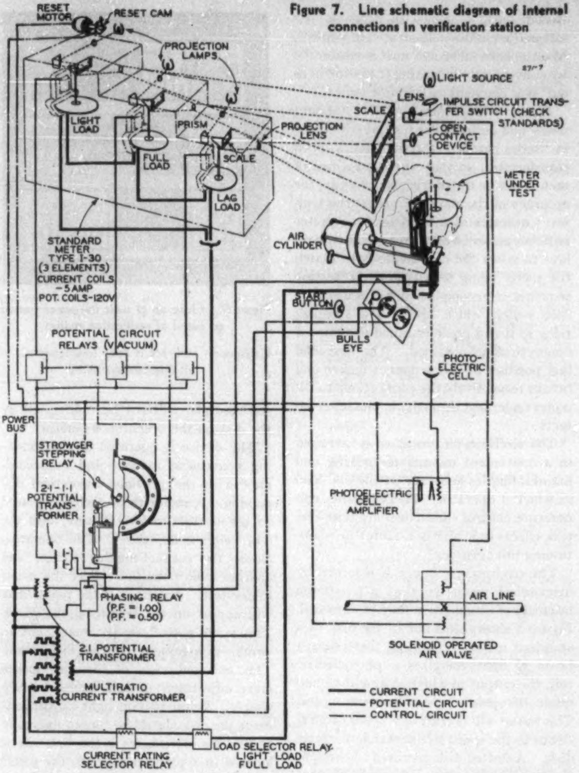
RESET MOTOR
RESET CAM
PROJECTION LAMPS
LIGHT SOURCE
LENS
IMPULSE CIRCUIT TRANSFER SWITCH (CHECK STANDARDS)
SCALE
OPEN CONTACT DEVICE
PRISM
PROJECTION LENS
LIGHT LOAD
FULL LOAD
SCALE
LAG LOAD
METER UNDER TEST
AIR CYLINDER
STANDARD METER TYPE I-30 (3 ELEMENTS) CURRENT COILS -5 AMP POT. COILS-120V
START BUTTON
BULLS EYE
POTENTIAL CIRCUIT RELAYS (VACUUM)
PHOTO-ELECTRIC CELL
POWER BUS
STROWGER STEPPING RELAY
2-1-1-1 POTENTIAL TRANSFORMER
PHOTOELECTRIC CELL AMPLIFIER
PHASING RELAY (P.F. = 1.00) (P.F. = 0.50)
AIR LINE
SOLENOID OPERATED AIR VALVE
2:1 POTENTIAL TRANSFORMER
MULTIRATIO CURRENT TRANSFORMER
CURRENT CIRCUIT
POTENTIAL CIRCUIT
CONTROL CIRCUIT
CURRENT RATING SELECTOR RELAY
LOAD SELECTOR RELAY LIGHT LOAD FULL LOAD

Figure 7. Line schematic diagram of internal connections in verification station

A micrometer fixture which can be adjusted from time to time to compensate for the drift due to manufacturing tolerance is an advantage, but for the highest degrees of accuracy, a slight further adjustment will be necessary on a small percentage of the meters.

A device in which this final adjustment can be conveniently made will be described later, but at this point it may be well to indicate the steps necessary to obtain this high degree of accuracy and to show how they fit into the manufacturing procedure.

I. While subassemblies are being made:

(*a*). Set lag plate mechanically.

II. After assembly:

(*b*). Set light-load adjustment by creep method.

(*c*). Set full-load adjustment stroboscopically.

III. After preliminary adjustment:

(*d*). Check calibration at all three loads.

(*e*). If necessary, inprove light- and lag-load accuracies.

(*f*). Recheck calibration at all three loads.

Adjusting Device

For best results, adequate equipment must be used for the various operations. A very satisfactory fixture for accomplishing the preliminary adjustments mentioned above is shown in figure 4. Its schematic circuit is illustrated in figure 5. Maintenance on such a unit is minimized by conveniently mounting it to slide in or out of a conventional filing cabinet.

Figure 5 illustrates how the standard meter and meter under test are connected in "series-parallel" through multitapped transformers, so that various ratings of meters can be tested without affecting the accuracy of the standard meter which always operates at a fixed load. Selector switches mounted on the panel operate relays to select the desired loads to match the meter being tested, and the testing sequence is accomplished by means of a foot switch which operates a stepping relay so that a predetermined sequence of connections is obtained. The first and last position in this sequence makes and breaks respectively the connections to the meter under test through air-pressure contacts.

The stroboscopic standard is arranged in a convenient manner for setting and maintaining its accuracy at the one load at which it operates. Thermostatic temperature control eliminates any temperature effects in it and is a factor in maintaining this accuracy.

The stroboscopic image is obtained by mechanical means so that a maximum intensity of illumination may be obtained. Figure 5 shows slots cut in the disk of a standard meter. Through these slots a beam of light energizes a photoelectric cell, the output of which is amplified and made to operate a synchronous motor. This motor will operate at a speed proportional to the speed of the standard meter disk. A slotted disk mounted on the synchronous motor shaft interrupts another beam of light which through a suitable optical system projects an image of the teeth on the edge of the disk onto a screen. The standard meter is connected to operate continuously so that when creep adjustment is made, the image, though not stroboscopic, will not be obliterated by the disk on the synchronous motor.

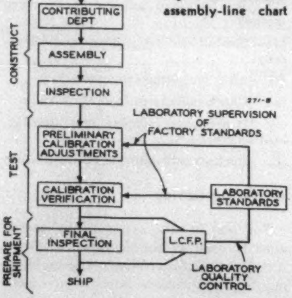

Figure 8. Product assembly-line chart

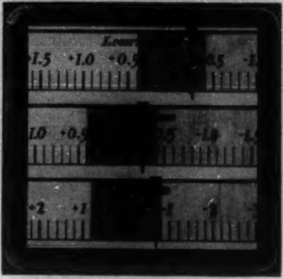

Figure 9. Close-up of scale images as viewed on panel of verification station

Calibration accuracy is read simultaneously at all three load points

The device is operated in the following convenient manner and in accordance with the previous theoretical discussion. A meter to be tested is placed in the contact-making fixture, and the foot switch is depressed. This operation closes the contact-making fixture and energizes the potential coil of the meter under test. An image of the meter disk will appear on the screen on the panel. This is not a stroboscopic image, but an actual enlargement of the disk teeth. This enlarged view of the disk makes creep adjustment of light load extremely simple. Before making light-load adjustment the disk should be turned manually so that the creep holes in the disk will be located in a plane parallel to the meter element. Then the light-load adjustment may be set so that no motion of the disk is observed on the screen.

The next depression of the foot switch energizes the current circuit of the meter under test, and there will appear a stroboscopic image on the screen. The position of the damping magnet is now set so that the image displacement is equal in both directions, if a slight amount of hunting is present. Having made this full load adjustment, another pressure on the foot switch will de-energize the meter under test and release the pressure on the contact block. The meter is then removed to the verification device where a picture of the calibration accuracy is obtained.

Verification of Calibration

Though for many years precise methods of watt-hour meter calibration have made use of precision instruments and constant load, it has been shown[7] that greater reliability is achieved by means of standard watt-hour meters giving for the first time the standard of watt-hour units. This standard, though maintained in the laboratory, can be readily transferred to the factory, and control maintained. Thus, it was decided to adopt the rotating-standard comparison method for the verification device. Essentially, the device consists of suitable switching and revolution-counting mechanism, together with the familiar portable standard watt-hour meter. The design and arrangement of the apparatus, however, are such as to achieve the results expressed in our introduction, namely, to obtain a direct indication of the complete calibration accuracy of the meter, and, in this respect, we believe that it is unique. The description which follows will deal chiefly with the novel design features and flexibility of the device.

Verification Device

The unit shown in Figure 6 in a multiple group was designed to give simultaneous indication of the complete meter-calibration accuracy. It employs a photoelectric timer which makes its operation automatic. The measurement is made by comparing the speed of the meter under test at each load with the speed of a separate standard for each value of load. This comparison follows automatically in sequence so that, at the end of a given time, the entire calibration appears upon the screen. The line diagram, Figure 7, shows schematically the arrangement of the apparatus by means of which this verification is made. In this figure, a light beam is directed from above the meter under test, through the path of the anticreep holes and onto a photoelectric cell. The amplified output of the photoelectric cell energizes a sensitive relay. This arrangement is such that for each half revolution of the meter disk having two anticreep holes, the above relay will operate once. The operation of this relay actuates a stronger stepping relay which in turn energizes relays to perform the following operations in the sequence indicated.

As the starting button is pressed with a meter in position, the standard meters are reset to zero through motor-driven cams, and the meter under test is energized. With the first light impulse after reset, light load is thrown on the meter under test, and the next impulse energizes the light-load standard. After a predetermined number of revolutions of the meter under test, the stepping relay has advanced to a contact that stops the light-load standard and throws full load on the meter being tested. The next impulse starts the full-load standard which runs until stopped by the stepping relay after a fixed number of revolutions of the meter undergoing test. As the full-load standard is stopped, lag load is thrown on the meter and the standard runs until stopped by the stepping relay. This again occurs after a given number of revolutions of the meter which energizes the photoelectric cell.

After these operations a picture of the meter accuracy at full load, light load, and lag load appears magnified in full view on a screen. This picture is obtained by projecting and focusing the images of transparent indicators which rotate with the standards and which replace the pointers for indicating the displacement of the pointer from zero. The transparent indicators are marked in divisions to represent error in tenths of a per cent and a manually operated index can be set to the point at which the zero position of the projected indicator image should stop if there is no error in the meter under test.

Once the accuracy of the meter has been visualized, the apparatus is ready to function again, or a release button can be pressed to disconnect the meter making it ready to remove the checked meter and replace it with one to be verified.

Conclusions

It will be readily observed that a suitable arrangement of the devices described when operated in the sequence discussed will result in an operating system which permits a uniform and progressive testing routine, readily coupled to conveyorized assembly manufacture. Figure 8 shows how the parts coming from contributing departments can be assembled and adjusted in a simple, uniform routine which moves along in progressive steps until the meter is ready for shipment, and which allows a laboratory check of the product to be made on sufficient quantities and at definite intervals without inconveniencing or upsetting the normal advance of these conveyorized operations. Although less skill is required by the operators, due to less tedious routine and more mechanized operations, more consistent accuracy can be expected, as errors are easily visible on an enlarged scale, and personal errors in judgment are eliminated.

The operation of this straight-line setup will, of necessity, reduce inventories of meters awaiting test and insure better service, since when a meter starts through there is little chance of sidetracking it, unless defects are found during inspection. This improved service means more meters delivered in quicker time and with full assurance of the high quality which any meter organization would be proud to associate with their own work.

Appendix

$$J\frac{d^2\theta}{dt^2}+(D_m+D_i+D_e)\frac{d\theta}{dt}-(T_{ei}+T_i+T_e-T_f)=0$$

where

J = Inertia of moving element (gram millimeters per second per second)
θ = Angular displacement of disk (radians)
t = Time (seconds)
D_m = Damping factor relating to permanent magnet (gram millimeters per second)

$$D_m=\frac{st}{g}G_m(\phi_m)^2 10^{-8}$$

D_i = Damping factor relating to current flux (gram millimeters per second)

$$D_i=\frac{st}{g}G_i(\phi_i)^2 10^{-8}$$

D_e = Damping factor relating to potential flux (gram millimeters per second)

$$D_e=\frac{st}{g}G_e(\phi_e)^2 10^{-8}$$

T_{ei} = Driving torque due to interaction of current and potential flux (gram millimeters)

$$T_{ei}=4\pi\frac{stf}{g}G_d\phi_e\phi_i\cos\mu\,10^{-8}$$

T_i = Driving torque due to load current flux (gram millimeters)

$$T_i=4\pi\frac{stf}{g}G_{di}\phi_i^2\cos\mu_i\,10^{-8}$$

T_e = Driving torque due to potential flux (gram millimeters)

$$T_e=4\frac{stf}{g}G_{de}\phi_e^2\cos\mu_e\,10^{-8}=f(\theta)+K$$

T_f = Friction torque (gram millimeters)
$f(\theta)$ = Anti-creep torque (a function of disk rotation in gram millimeters with an average value of zero)
K = The constant component of T_e which when added to $f(\theta)$ gives T_e
s = Conductivity of disk (mhos per centimeter cube)
t = Thickness of disk (centimeters)
g = Gravitational constant (centimeters per second per second)
G_m = Damping constant relating to permanent magnet (dimensionless)
G_i = Damping constant of load current flux (dimensionless)
G_e = Damping constant of potential flux (dimensionless)
G_d = Driving constant relating to current and potential flux (dimensionless)
G_{di} = Driving constant relating to current flux (dimensionless)
G_{de} = Driving constant relating to potential flux (dimensionless)
ϕ_m = Useful permanent magnet flux (Maxwells)
ϕ_i = Useful load current flux (Maxwells, effective value)
ϕ_e = Useful potential flux (Maxwells, effective value)
f = Frequency (cycles per second)
μ = Angle between ϕ_i and ϕ_e (radians)
μ_i = Equivalent angle of components of ϕ_i which produce torque T_i
μ_e = Equivalent angle of components of ϕ_e which produce torque T_e

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Single-Phase A-C Electric Locomotives on the Pennsylvania Railroad—Protection and Tonnage Rating

H. C. GRIFFITH
MEMBER AIEE

THE single-phase electric locomotive is thought of quite generally as a piece of motive power for heavy traction service. To an electrical engineer, however, it consists fundamentally of electric motors geared to wheels with means for controlling the applied voltage to handle a variable load at varying speeds.

From this point of view it is interesting to consider the protective features of the motors and their associated electrical apparatus to develop devices and methods which are necessary to guard against the hazards common to the operation of all electrical devices and those special to an electric locomotive. This paper will consider these features as they apply to the electric locomotives operating on the Pennsylvania Railroad.

The Electrical Apparatus

The single-phase a-c locomotive with series motors receives its power from a trolley at a relatively high voltage in order to provide economical distribution and good voltage regulation. The commutator-type motors must operate at a relatively low voltage in order to keep the commutators within a reasonable size because of the limitation of volts between bars.[1] A transformer is, therefore, necessary. Having the transformer to provide the lower voltage, a method for tap changing under load can then be introduced to control the voltage applied to the motors. The motors are permanently connected in parallel groups with the motors in each group in series. This permits a motor group to be cut out of service with no effect on the remaining groups, and the locomotives are occasionally so operated when trouble develops in a motor circuit. The method, Figure 1, of tap changing involves the use of preventive coils and heavy-duty contactors, and, in order to subdivide the voltage steps, a buck-boost transformer is introduced to triple the number of steps.[4] In addition to this, a-c series motors are operated at a low magnetic flux to provide favorable commutation conditions, and in operating at start and low speed when the armature current must be high, the field is shunted, Figure 2, to keep the flux in the same range as at continuous rating. It is also necessary to provide a commutating resistor (or interpole shunt) to produce the quadrature component of the interpole flux, and this shunting must be varied in one or more steps from high power factor at low speed to a lower power factor at high speed to provide adequate neutralization of transformed voltage in the armature coils.[1] The operation of the main-field shunt and the commutating-resistor contactors is controlled by relays, Figures 3 and 4, operating from the voltage across the main field and across the armatures, respectively, Figure 5. Finally contactors must be provided to reverse the main-field connections for operation in the reverse direction.

This equipment comprises the fundamental electrical necessities of the locomotive. In addition, there are numerous electrical auxiliaries, such as forced-ventilation blowers, air compressors, heating boiler, control batteries and charging equipment, lighting and cab signal equipment. The protection of this auxiliary equipment, while necessary, will not be covered specifically in this discussion.

Protection Required

Having the picture of the main features of the locomotive, the next step is to consider against what hazards the electrical equipment must be protected. These may be stated as follows:

1. Overload
2. Overvoltage surge
3. Failure of insulation
4. Incorrect operation of tap-changing switches
5. Excessive field at start
6. Unequal motor speeds—slipping
7. Failure of cooling air
8. Transformer overheating
9. Overspeed
10. Excessive current
11. Overheating of motors

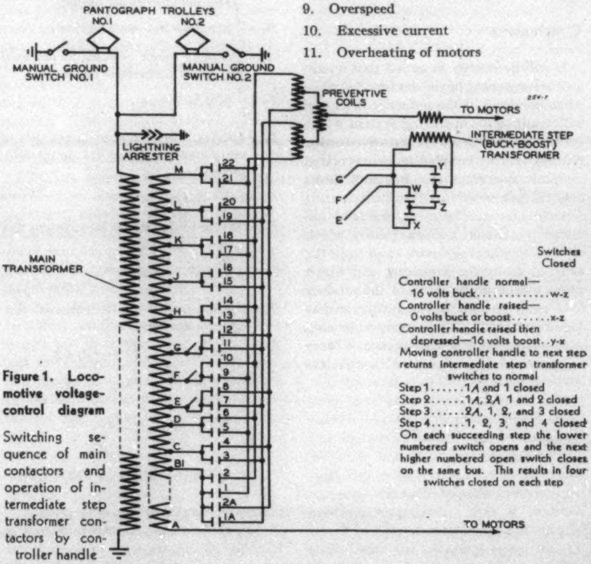
PANTOGRAPH TROLLEYS
NO.1
NO.2
MANUAL GROUND SWITCH NO.1
MANUAL GROUND SWITCH NO.2
PREVENTIVE COILS
TO MOTORS
INTERMEDIATE STEP (BUCK-BOOST) TRANSFORMER
LIGHTNING ARRESTER
MAIN TRANSFORMER
M
L
K
J
H
G
F
E
D
C
B1
A
22
21
20
19
18
17
16
15
14
13
12
11
10
9
8
7
6
5
4
3
2
1
2A
1A
G
F
E
Y
W
Z
X
TO MOTORS
Switches Closed
Controller handle normal—16 volts buck..............w-z
Controller handle raised—0 volts buck or boost........x-z
Controller handle raised then depressed—16 volts boost..y-x
Moving controller handle to next step returns intermediate step transformer switches to normal
Step 1.......1A and 1 closed
Step 2.......1A, 2A 1 and 2 closed
Step 3.......2A, 1, 2, and 3 closed
Step 4.......1, 2, 3, and 4 closed
On each succeeding step the lower numbered switch opens and the next higher numbered open switch closes on the same bus. This results in four switches closed on each step

Figure 1. Locomotive voltage-control diagram

Switching sequence of main contactors and operation of intermediate step transformer contactors by controller handle

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H. C. GRIFFITH is electrical engineer with the Pennsylvania Railroad Company, Philadelphia, Pa.

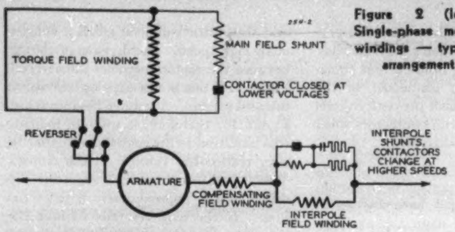

TORQUE FIELD WINDING
MAIN FIELD SHUNT
CONTACTOR CLOSED AT LOWER VOLTAGES
REVERSER
ARMATURE
COMPENSATING FIELD WINDING
INTERPOLE SHUNTS, CONTACTORS CHANGE AT HIGHER SPEEDS
INTERPOLE FIELD WINDING

Figure 2 (left). Single-phase motor windings — typical arrangement

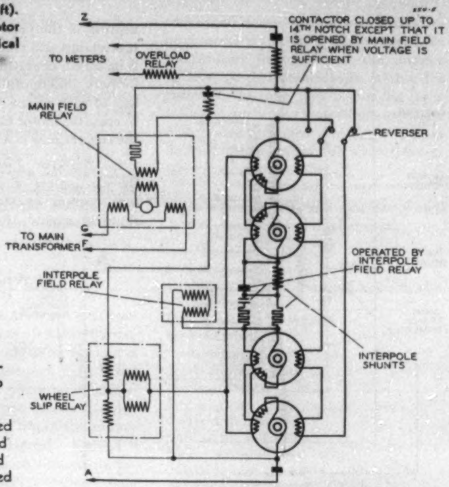

Z
TO METERS
OVERLOAD RELAY
CONTACTOR CLOSED UP TO 14TH NOTCH EXCEPT THAT IT IS OPENED BY MAIN FIELD RELAY WHEN VOLTAGE IS SUFFICIENT
MAIN FIELD RELAY
REVERSER
TO MAIN TRANSFORMER
G
F
OPERATED BY INTERPOLE FIELD RELAY
INTERPOLE FIELD RELAY
INTERPOLE SHUNTS
WHEEL SLIP RELAY
A

Figure 5 (right). Motor-field control and slip relays

■ {Closed at low speed
Open at high speed

= {Open at low speed
Closed at high speed

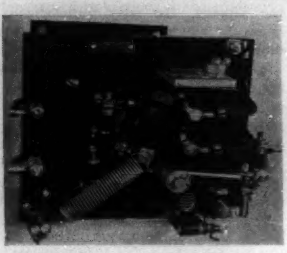


Figure 3. Interpole-field relay

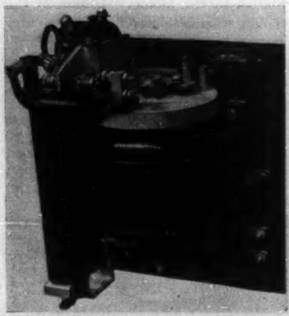


Figure 4. Main-field relay

This is a formidable list, but may be considered by classification of the manner in which protection can be provided.

Fully Automatic Protection

This method is provided for the first five conditions:

1. *Overload.* When the motor current exceeds a value beyond which it is unsafe to operate the motors for even a short period, the motor contactors are opened by the usual overcurrent relay.

2. *Overvoltage Surges.* These surges appear on the high-voltage side of the transformer and require the use of a lightning arrester.

3. *Failure of Insulation.* Faults of this nature are detected by a relay, Figure 6, known as the pantograph relay. In the secondary, or motor, circuits such detection is simplified by the fact that the circuits, Figure 7, are normally free from ground except through a current transformer, so that any secondary ground will cause the relay to operate. The primary winding of the transformer is protected by current differential between the ends of the winding. The operation of the relay first opens the motor and auxiliary switches, and, if this fails to clear the fault, an automatic switch grounds the pantograph collector, throwing the interrupting duty on to the substation circuit breaker. This relay is described in detail in the Luther paper.[2]

4. *Incorrect Operation of Tap-Changing Switches.* Tap changing on main transformer is performed by the introduction of three impedance (preventive) coils, the two smaller of which have their ends connected to four busses to which the taps from the main transformer are connected by the tap-changing contactors. The mid-points of these two preventive coils are connected to the ends of another larger preventive coil, to the mid-point of which is connected the outgoing lead to the motors. In this way four taps on the transformer are used on each of the running voltage steps, Figure 1, and a change to the next step requires the moving of only one of the four tap connections, permitting smooth and even transition in the voltage change. The impedance of the coils is sufficient to reduce the circulating current between the taps to a low value, while the reactance to the load is cancelled out by the two directional flow to the mid-point leads. In order to prevent two tap-changing contactors from closing on the same bus, thereby short-circuiting a portion of the main transformer, interlocking is used between contactors. This has been extended in recent designs to include a group of interlocking relays, Figure 8, so connected that when a contactor fails to open properly, in addition to the fact that no other

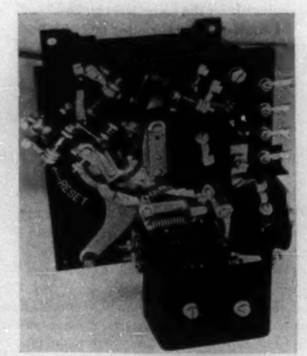


Figure 6. Pantograph relay

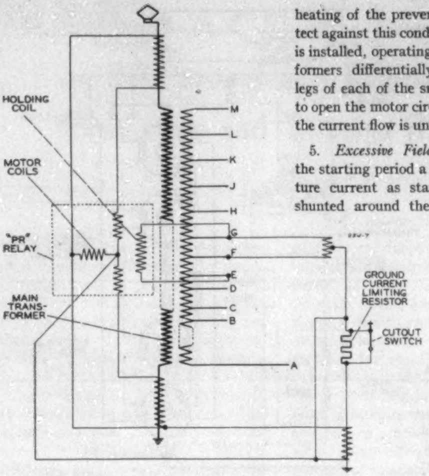
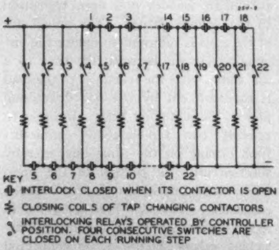

HOLDING COIL
MOTOR COILS
"PR" RELAY
MAIN TRANS-FORMER
M
L
K
J
H
G
F
E
D
C
B
A
GROUND CURRENT LIMITING RESISTOR
CUTOUT SWITCH

Figure 7. Pantograph ("PR") relay connections

contactor on the same bus can be closed, the contactors on the other busses cannot proceed to the other voltage taps. This prevents a large voltage difference being established across the preventive coils if a contactor on one of the lower steps remains closed while the contactors on the other busses have closed on the higher voltage steps.

This new arrangement of interlocking requires the use of fewer interlocking fingers on the tap-changing contactors and uses instead separate interlocking relays in an enclosed cabinet, Figure 9, providing a cleaner, more accessible and reliable arrangement.

This interlocking is complete excepting under one condition, that is, when a contactor fails to close in proper sequence. Under this condition one end of a preventive coil will be disconnected from the main transformer causing an unbalance of currents which will cause excessive heating of the preventive coils. To protect against this condition a thermal relay is installed, operating from current transformers differentially connected in the legs of each of the small preventive coils to open the motor circuit contactors when the current flow is unbalanced.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KEY
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22
INTERLOCK CLOSED WHEN ITS CONTACTOR IS OPEN
CLOSING COILS OF TAP CHANGING CONTACTORS
INTERLOCKING RELAYS OPERATED BY CONTROLLER POSITION. FOUR CONSECUTIVE SWITCHES ARE CLOSED ON EACH RUNNING STEP

Figure 8. Simplified interlocking scheme for tap-changing contactors

5. *Excessive Field at Start.* During the starting period a portion of the armature current as stated hereinbefore, is shunted around the main, or exciting, field to minimize commutation troubles. This function is performed by a relay, Figure 4, operating from the voltage across the main field.

Semiautomatic Protection

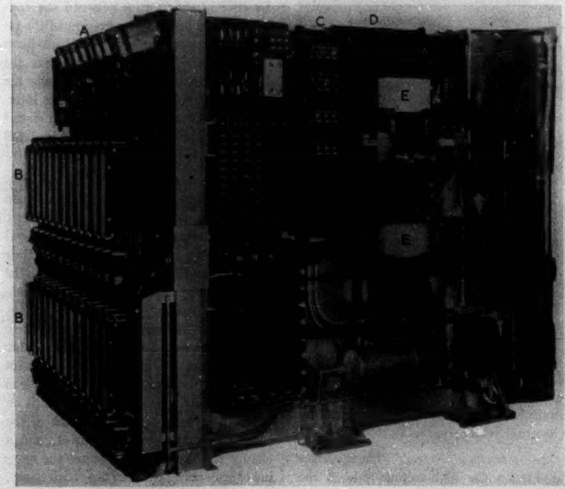
6. *Unequal Motor Speeds.* High peripheral velocity is normally designed into the traction motors, and if a pair of driving wheels slips, tractive power is lost, and the motor will soon reach a dangerously high speed. The division of voltage between the motors becomes unbalanced, Figure 4, if one motor slips or two slip at unequal speeds. A voltage balance relay, Figure 10, therefore, is used to indicate this condition to the engineman so that he may reduce the voltage on the motors. This indication is received when the slip differential is approximately 5 miles per hour. If the engineer fails to take the necessary action or is unable to control the slipping, the relay automatically opens the motor switches when the slip differential reaches 20 miles per hour.

7. *Failure of Cooling Air.* The motors and transformers are designed, because of space and economic limitations, to require forced ventilation. Failure of the air supply would permit rapid overheating. A relay in the air stream or a centrifugal relay on the blower shaft indicates such a failure to the engine crew.

8. *Transformer Overheating.* The main transformer is cooled by oil circulated by a pump. This oil passes through a radiator and is in turn cooled by air from the same blowers which provide forced ventilation to the motors. Failure of this cooling scheme to keep down the oil tem-

Figure 9. Main transformer showing voltage-control equipment

A—Auxiliary-equipment contactors
B—Tap-changing contactors
C—Interlocking relays
D—Intermediate step (buck-boost) transformer
E—Intermediate step (buck-boost) contactors



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perature will operate a thermostatic alarm.

Indication

9. *Overspeed.* Speed is indicated to the engine crew by means of a speedometer. Excessive speed is dangerous to the armature windings and commutators. Dependence must be placed on the engine crews to keep the speed within limits safe to the equipment and also within the local speed restrictions of the roadway.

10. *Excessive Current.* Current is indicated by a separate ammeter for each of the parallel motor groups. The rate of acceleration is determined by the current value permitted, limited of course by the weight on driving wheels. A limit is set on the maximum motor current, which limit is appreciably below the setting of the overload relays, and proper attention to the ammeter readings during acceleration is required to prevent slipping and to stay within the range of good commutation.

Indicating Devices

Indicating lights are provided in the locomotive crew compartment to indicate the following:

(*a*). Low water in oil-fired train heating boiler

(*b*). Driving wheels slipping

(*c*). Forced-ventilation blowers stopped, or high transformer oil temperature

(*d*). Motor-overload relay operated

(*e*). Pantograph relay operated

(*f*). Main-motor-field shunting switches closed

(*g*). Fuel pump on heating boiler stopped

In addition to this, a buzzer operates to warn the crew for the first two conditions, that is, low water in heating boiler, or driving wheels slipping.

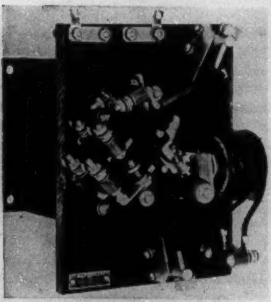


Figure 10. Wheel-slip relay

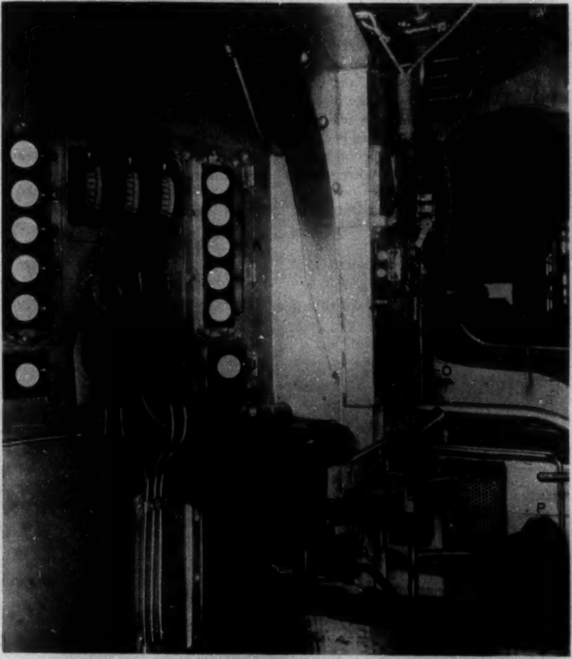
The indicating light panel, Figure 11, contains a row of amber lenses, on each of which is engraved the designation which it indicates. It is located beside and to the left of the engineman. On this same panel is the series of cab signal indication lights duplicating the aspects of the wayside signals. Here also are located the speedometer, the ammeters for each of the motor circuits, and the usual air gauges for the brake system.

Tonnage-Rating Limitation

11. *Overheating of Motors.* The temperature rise of the motor fields and armatures is the practical limit to the amount of work which the motors can do. These limitations are set by the kind of insulation used and must not exceed the permissible values, or the life of the motors will be materially shortened. Even if it were practical to have continuous indication of the controlling "hot-spot" temperatures, it would still not be practical to send out on the railroad an electric locomotive with a train which, operated under the normal speed and grade requirements of the route, might exceed the temperature limitations of the motors. In fact, no protective device, within the ordinarily accepted meaning of the term, is as yet available for avoiding excessive motor temperatures, without unduly limiting the use of the full short-time capacity of the locomotive.

Figure 11. Indicating and operating devices in motorman's compartment

A—Ammeters
B—Low water in boiler
C—Blower-stopped and transformer-temperature indication
D—Overload relay tripped
E—Drivers slipping
F—Pantograph relay tripped
G—Fuel pump tripped
H—Speedometer
I—Air gauges
J—Field change-over
K—Cab signal indicator
L—Buzzer
M—Emergency grounding switch
N—Pantograph switch
O—Headlight switch
P—Brake valve
Q—Master controller



It is, therefore, necessary to resort to other than electrical or mechanical means of providing this very necessary protection. The obvious method is, of course, so to safeguard the operation of the locomotive as to avoid overheating of the equipment. At the same time, however, the necessity for economical operation dictates that the maximum possible work be performed by the locomotives. To meet these two conditions a tonnage rating, that is, the maximum train which can satisfactorily be handled, is determined for each class of locomotive and each route.

The working out of these ratings requires that complete motor heating and cooling characteristics be available from shop tests of the equipment. The method for arriving at these characteristics has been covered in detail in the paper by Fremont Felix and H. G. Jungk presented at last winter's convention in Philadelphia.[3]

In addition to this, the usual motor-output characteristics for different speeds and voltages are required. Then, with the profiles, alignments, and speed restrictions of the route to be used, it is possible, assuming a weight of train, to determine the motor-output requirements at all points on the runs.[5] The time, distance, and output results are tabulated, and, by using the heating and cooling characteristics of the motors, the temperature rise at each point on the route is determined. If the permissible temperature has been exceeded, the run is recalculated with a lesser weight of train. Similarly, if the maximum permissible temperature has not been reached, a new calculation is made using a heavier train. In this manner, the maximum tonnage which the locomotive can handle over each route, without overheating the motors, is determined.

In addition to these ratings, this protective method requires a thorough education of the personnel to make it effective. Yard masters must be informed of the importance of keeping the weights of trains within the specified limits. Having a proper tonnage, the engineman must learn how to operate the locomotive in a proper manner. This proper operation involves avoiding excessively difficult starts in yards and on grades, avoiding prolonged operation in weak field, taking full advantage of the momentum of the train in negotiating grades, and a general knowledge of the operating characteristics and capabilities of the locomotive.

The results of such predeterminations of tonnage ratings on the Pennsylvania Railroad have proven most satisfactory. After the ratings had been determined by calculation, they were checked by making test runs. In order to make these road tests, thermocouples were built into the windings of several of the motors. A locomotive so equipped was assigned to a regular revenue train which was built up to the predetermined tonnage for the route to be followed, and the train was operated over the road. By means of recording meters a continuous record was made of motor current and temperature. Tests of this nature have been made over all the principal routes, and in all cases the measurements have closely checked the calculations. It has not been necessary to reduce any of the calculated ratings in actual operation.

Conclusions

The electric locomotive is composed of electrical apparatus which is subject to the usual hazards of similar equipment. The methods of protection require some of the usual types of device. The specialized application, however, introduces the factors of loading and operation as protective methods to a greater extent than is usually the case with generally similar equipment in other services. It has been the purpose of this paper to summarize and describe the electrical protection of the equipment, including not only the protective devices but those features of the utilization of the locomotive which become in reality protective methods.

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Modern Electrical Equipment for Industrial Diesel-Electric Locomotives

LANIER GREER
MEMBER AIEE

Synopsis: Modern electrical equipment for industrial Diesel-electric locomotives is widely different in many respects from that used five or six years ago. New and improved high-temperature insulating materials and modern synthetic varnishes have been developed and are used extensively today. Also new and improved methods of using these and the older materials have been developed. These, together with new design constants, have made possible a reduction in weight per horsepower transmitted with an improvement in the quality of the product.

The Diesel engine and generator are close-coupled and mounted as a unit, thus assuring proper alignment and, at the same time, reducing the space required for mounting on the locomotive. A new and improved method of generator excitation has been developed which assures positive control of the engine speed and power. This provides a very flexible transmission of power from the prime mover to the rail.

The modern traction motor has a cylindrical instead of a rectangular frame. It is a multipole instead of the conventional four-pole design. It is mounted integral with a double-reduction gear unit which is completely enclosed in an oiltight gear case. All bearings on the motor armature shaft and in the gear case are antifriction except those on the axle, which are sleeve bearings. All bearings in the gear unit are oil-lubricated with the same oil that lubricates the gearing. Both the high and low speed pinions are straddle-mounted to insure proper alignment of the gearing at all times.

Industrial Diesel-electric locomotives are used in slow-speed switching service where high tractive effort is most important. For this type of work, double-reduction gearing with maximum reduction shows much better transmission efficiency than the conventional single-reduction gearing.

FOR many years most of the machinery in American industrial plants has been driven electrically. During the past five or six years Diesel-electric drive has taken a definite place in moving raw materials to and finished products away from the manufacturing plants. This widespread use of electric power for locomotives has been brought about largely by two simultaneous developments.

1. *Modern High-Speed Diesel Engine.* Through standardization and the use of essentially the same Diesel engine for locomotives, trucks, tractors, and standby power plants, the cost per installed prime mover horsepower has been lowered greatly. Also, at the same time, the necessity for light weight has been realized and this, together with higher speeds, has resulted in lighter weight engines that still retain their ruggedness and ability to produce power.

Usually these engines are equipped with two-speed governors set for idling speed and full speed. Between these speeds the fuel supplied to the engine is controlled manually by the position of the locomotive operating handle. This is known as an automotive-type governor and is the type used with the equipment described in this paper.

There is another type of governor that is used sometimes in industrial Diesel-electric locomotives and is used on practically all of the larger Diesel engines. With this type engine speed is set by the governor for each position of the locomotive operating handle. In each of these positions the governor supplies the engine with the amount of fuel necessary, up to full fuel, to maintain a given speed. This is known as a variable-speed governor.

2. *Modern Electrical Propulsion Equipment.* During this same period of time, new materials and methods have been developed and adopted for use in this electrical equipment. Through design and research, improvements have been made in, and ways have been found to use, standard materials to better advantage. These have made it possible to reduce weight and cost per horsepower and at the same time improve the quality of the product.

These two developments, together with improved locomotive design, have made a very economical Diesel-electric locomotive. This industrial locomotive has low initial cost, low maintenance cost, low operating cost, and high availability. It has many of the desirable features of a straight electric locomotive but does not require a large investment for plant and equipment. Locomotives of this type weighing from 20 tons to 65 tons inclusive are being manufactured by several locomotive builders.

A unit of electrical propulsion equipment for these Diesel-electric locomotives consists of a d-c commutating pole generator connected directly to a Diesel engine, one or two d-c commutating pole motors geared to the axles, and control to assist in maintaining a smooth even flow of power from the prime mover to the wheels. There are one or more of these units per locomotive, depending upon the weight and performance required.

Traction Generators

Mechanical Features

The generators have only one bearing, either ball or spherical roller, which is located at the commutator end of the machine and carries approximately one half of the weight of the armature. This bearing is mounted in a cartridge type of housing which is so arranged that the armature can be removed without exposing the bearing to dirt and other foreign substances. The use of all-metal labyrinth seals insures that the grease will be kept in and dirt will be kept out of the bearings. The other end of the armature is bolted directly to the engine crankshaft through a flexible steel disk coupling which is mounted integral with the generator fan. This coupling is designed so that it is rigid torsionally and radially. The torsional rigidity makes it possible to use the generator armature and fan for the engine flywheel, which eliminates the conventional flywheel. The radial rigidity is necessary to maintain the electrical air gap on the generator. At the same time the coupling has flexibility in angular and axial directions to eliminate the necessity for exceptionally close machining tolerances.

Figure 1 shows a modern Diesel-electric generator.

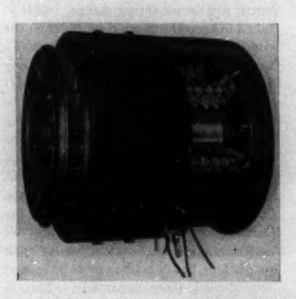

Figure 1. Modern d-c traction generator

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LANIER GREER is a designing engineer of the transportation motor engineering department, General Electric Company, Erie, Pa.

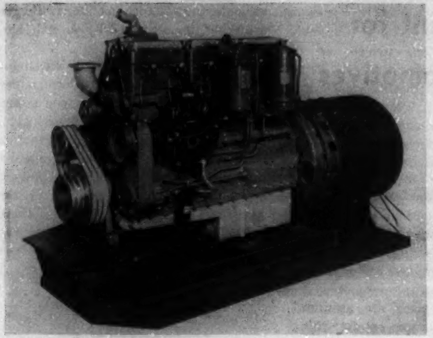

Figure 2. Modern high-speed Diesel engine with directly connected traction generator

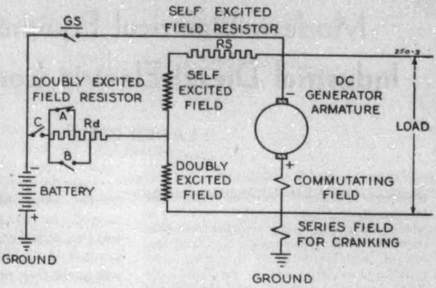

Figure 3. Diagram of connections for traction-generator excitation

The bell housing on the engine has been made special to serve the dual purpose of supporting the generator frame and providing a space in the formed openings for generator-fan discharge. When the generator frame is bolted to the bell housing and the armature to the engine crankshaft through the flexible coupling, the engine and generator become an integral unit and are mounted as such. Figure 2 shows a typical engine-generator unit. The generator fan located in the bell housing provides for multiple ventilation of the machine. One stream of air is drawn over the armature, through the air gap and between the field coils, and the other under the commutator and through the core.

Another important part of this modern generator is the improved brush holder. To collect current properly from the commutator it is necessary to have brush holders that will keep the brushes on the commutator even though it may be slightly irregular or eccentric. This is accomplished by special brush-holder, brush and brush-spring design. Care is taken to keep the brush riding against the trailing edge of the brush holder at all times. This is done by using a trailing brush with a beveled and clip top arranged so that the brush-spring pressure provides a component of force, holding the top of the brush in place, and at the same time provides the necessary radial force to keep the brush in contact with the commutator. The bottom of the brush is held in place by friction on the commutator. Due to the fact that all materials are being worked harder than ever before, brush holders must be spaced and aligned more accurately. This is accomplished by a brush holder designed so that it can be located easily and accurately, one which remains in a fixed position after it is located. These design features improve commutation, give long brush life, and insure generator characteristics that do not change from day to day due to change in brush fit or location.

The commutator must be kept tight and true. This is accomplished by using commutator-design constants which have been developed over a period of years of experience with high-speed equipments. Even with proper commutator design, difficulties occur in manufacture. These are corrected and a good commutator produced by seasoning it after the armature is completely assembled. This process consists of subjecting the commutator to centrifugal forces and thermal stresses in excess of those which are encountered in service, and tightening and grinding the commutator until it operates smoothly, both hot and cold.

In order to provide smoothness of operation, and to reduce noise and vibration to a minimum, it has been found necessary to dynamically balance the complete armature of all modern high-speed Diesel-electric locomotive generators.

ELECTRICAL FEATURES

In the past few years insulating materials and methods have been improved greatly. Some of these high-temperature materials are asbestos cloth, asbestos paper, fiber glass, and mica. With the aid of modern synthetic high-temperature varnishes, asbestos and mica, as well as fiber glass and mica, have been combined to form tapes and wrappers. These materials have made it possible to use thinner insulation of a better quality and get a better-insulated machine. This leaves more space for copper and iron and at the same time improves the heat-transfer coefficient through the insulation. Another advantage of these materials is their ability to stand high temperatures and still have long life. By using high temperatures the weight of material per horsepower transmitted is reduced. By the inherent long life of the materials, maintenance costs are kept low.

Careful study of the magnetic circuit has made it possible to so proportion the various parts of the generator that all materials are utilized to better advantage. This also has resulted in reduced weight for a given amount of power transmitted.

The modern locomotive generator is equipped with a series field winding and operates as a motor for cranking the engine from a 32-volt or 64-volt storage battery. This eliminates the starting motor drive and ring gear, and at the same time reduces the amount of apparatus to be maintained. This type of engine cranking has proved to be very successful and reliable.

The locomotive operating character-

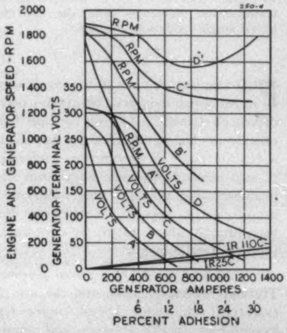

Figure 4. Characteristics of Diesel-engine traction-generator unit

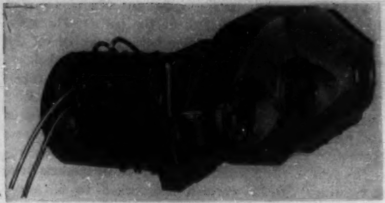

Figure 5. Modern d-c traction motor and double-reduction gear unit

istics are largely built into the generator. For a number of years Diesel-electric industrial locomotives did not have the most desirable operating characteristics. Full advantage was not taken of the fact that electric drive is the most controllable and most flexible of all drives. Recently, detailed studies have been made of various generator-excitation schemes to obtain the very best possible operating characteristics. Some of the more desirable of these are:

1. As the locomotive operating handle is moved from the idling position toward the full throttle position, the locomotive speed increases with the engine speed.
2. As the engine speeds up, the generator voltage increases to provide increased locomotive speed either with or without additional fuel.
3. Smooth acceleration is obtained without jerks or hesitation at all values of throttle opening.
4. The engine must not be loaded so heavily that it does not accelerate rapidly and evenly, nor loaded so lightly that it accelerates too fast and gives the impression of a "slipping clutch."

All of these features have been built into the newest industrial Diesel-electric locomotives by use of a generator which is essentially self-excited. This generator is provided with a doubly excited split field which receives most of its excitation from its own armature and the balance of its excitation from the storage battery on the locomotive. Each section of the field is designed so that approximately one half of the resistance of the circuit is external and does not change with temperature. This minimizes the variation of generator characteristics with temperature change in the windings.

A simple schematic diagram of the connections which are used is shown in Figure 3. The resistor R_s in the self-excited circuit is set to give the proper value of excitation for full power and full speed, Figure 4, curves D and D', and remains set for all other values of speed and power. The resistor R_d in the doubly excited field circuit is varied in three steps by the two switches marked A and B in Figure 3. As the locomotive operating handle is moved from the idling position towards the full throttle position, R_d is set at the three different values which are necessary to insure a full even flow of power at all positions of the operating handle. These changes are made by a cam-operated switch, which is connected to the locomotive operating handle. A double cam switch, which is in the throttle-linkage mechanism between the operating handle of the locomotive and the engine fuel pump, changes the fuel pump setting to provide the proper amount of fuel.

From Figure 4 it can be seen that the speed-ampere and volt-ampere characteristics are very steep. This provides voltage and current which are supplied to the traction motors on the locomotive, causing its speed to follow that of the engine.

Referring to Figure 4, curve A, the first power position on the motor-current resistance (IR) line is at approximately 12 per cent adhesion on the locomotive. This value of adhesion has been proved by experience to be one which gives no jerks but provides a smooth, rapid start. This point will be practically the same at every start, because the volt-ampere curve crosses the motor-current resistance (IR) line at a steep angle, which will change very little as the temperature of the motor windings changes. This is shown by reference to the two motor-current resistance (IR) lines shown on Figure 4, one at 110 degrees centigrade and the other at 25 degrees centigrade. Since there are no notches on the locomotive operating handle quadrant, there are infinite steps of power and speed between the first power position, Figure 4, curves A and A', and the full power position, Figure 4, curves D and D'. Curves B and B', C and C' are shown to illustrate the shape of intermediate power and speed positions. Additional study of the engine-generator characteristics shows that the generator with its controlled excitation is acting to electrically govern the engine at all speeds below that at which the mechanical governor begins to operate.

Further reference to Figure 4, curves D and D', shows that there is a wide range of amperes over which the generator volt-ampere curve is equal to the engine output, less the generator losses and a small loss of power due to the speed of the engine not being held constant. This makes it easily possible and very desirable to use only one motor combination if more than one motor per power plant is required. With a single motor, or with motors in parallel, if two motors are used per power plant, practically full power utilization can be obtained from 30 per cent adhesion on the locomotive to 60 per cent of maximum permissible operating speed. This is very desirable since it simplifies the locomotive control.

Battery-Charging Equipment

Battery charging on industrial Diesel-electric locomotives is done by one of two methods, depending on whether a 32-volt or 64-volt battery is used.

The 32-volt battery is charged with automotive-type equipment. This consists of a 750-watt shunt-wound generator which is mounted on the engine in such a way that it can be driven by a belt or from an auxiliary shaft. A vibrating contact regulator is used in the field of the generator to hold approximately constant voltage over a range of charging current, which is limited by a series coil in the regulator. The generator is self-ventilated by a small fan.

When a 64-volt battery is used, a 1.5-kilowatt shunt-wound charging generator is used. It is mounted on the floor of the locomotive and belt-driven from a pulley on the end of the traction generator shaft. The voltage on the charging generator is held constant by a vibrating contact

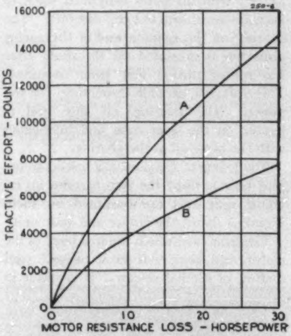

Figure 6. Comparative motor-resistance-loss curves for traction motors with single-reduction and double-reduction gearing

regulator operating in the shunt field. The current is limited by a ballast resistor in the battery-charging circuit. This generator is self-ventilated by a fan mounted directly on the armature shaft.

Traction Motor and Gearing

MECHANICAL FEATURES

The modern Diesel-electric industrial locomotive traction motor is very different from the conventional railway motor. The new motor has a rolled-steel cylindrical fabricated frame instead of a cast-steel rectangular frame. This new construction has a number of manufacturing advantages and also is more desirable magnetically since casting blowholes are eliminated. The motor is very accessible for original assembly and for repairs. It is equipped with double-reduction gearing instead of the conventional single-reduction gearing.

The commutator is of special design to meet service requirements. In addition to the use of design constants that give a sturdy strong commutator, it is given a thorough seasoning after the armature has been completely assembled.

The armature is designed to withstand the stresses of high-speed operation that go with double-reduction gearing. The coils are held in the core slots by wedges and the end windings are held with binding wire. The completed armature is dynamically balanced to very close limits so that it operates smoothly and with very little vibration at all speeds. It is also equipped with antifriction bearings on both ends. The commutator end bearing housing is designed so that the armature can be removed without opening the bearing housing and exposing the bearing to dirt. This bearing is also arranged with all-metal labyrinth seals to keep grease in and to keep dirt out. The bearing on the opinion end of the motor armature is mounted on the shaft after the motor pinion has been mounted. This forms a straddle-mounting for the pinion. The bearing on this end is housed in the gear case and lubricated with the same oil as the gearing.

The frame of the motor is bolted to the gear case so that the two become an integral unit and are mounted as such. Figure 5 shows the motor and gear unit.

The gear case which forms a part of the motor and gear unit is a rugged steel casting of special design. It is made in one piece except the gear cover which is a malleable iron casting and is bolted to the gear case along a 45-degree line. All bearings and pinions are assembled so that they can be replaced without removing the motor and gear unit from the locomotive. The motor and gear-unit support is so located that, for either direction of motion of the locomotive, there will be no tilting to throw the gear mesh out of alignment or place a twisting load on the axle bearings under the heavy tractive efforts encountered in service. This type of hardened gearing operating in an oiltight gear case has a long life, which should be equal to that of the locomotive. The gear unit is equipped with flood-lubricated sleeve axle bearings and thrust surfaces, and with antifriction intermediate and high-speed bearings operating in an oil bath. Both the high-speed and low-speed pinions are straddle-mounted to maintain alignment. The gearing is straight spur on both reductions.

The motor is self-ventilated. This is found to be practical in a motor which is used with double-reduction gearing, since even in slow-speed switching service, motor speeds are high enough to make the motor fan effective. The fan which is mounted on the armature shaft at the end opposite the commutator provides for multiple ventilation of the machine.

ELECTRICAL FEATURES

In the motor use has been made of the various insulating materials, varnishes, and methods used in the traction generator. By this means the best possible quality has been obtained with the smallest amount of insulation. This has made it possible to get more tractive effort rating per pound of material used.

In order to further conserve material, motors have been designed with more than the conventional four poles. The latest industrial Diesel-electric locomotive motor is a six-pole machine. The multipole design reduces length of end windings and commutator. Also the use of multipoles provides for better distribution of field windings. This, in connection with detailed studies of the magnetic circuit, has enabled the designer of modern motors to materially reduce the size and weight of the field structure.

The field coils have also been reduced in size by the use of new insulating materials and methods. Both exciting and commutating coils are edgewise-wound and formed to fit the poles. The completed coils after being mounted on the poles receive a number of dips and bakes in modern synthetic insulating varnish. With this type of coil the heat-transfer coefficients are much higher than for a conventional coil. After the poles and coils are assembled in the frame, the complete assembly is dipped in modern synthetic varnish and baked to fill completely and insulate all connections and joints so that they are sealed and protected from water, oil, and other foreign materials that may enter the motor.

The design constants of the motor have been made such that the motor has a very steep speed curve, and a wide range of field control can be used. These, together with the engine-generator characteristics, Figure 4, make a locomotive with a wide range of full utilization of available engine horsepower.

Since industrial Diesel-electric locomotives are essentially slow-speed machines, and transmission efficiency is an important factor, double-reduction gearing is superior to single-reduction gearing for this service. A much greater reduction can be obtained with double-reduction gearing *than with single-reduction gearing*, and, *at the same time*, clearance under the gear case *can be* maintained, as specified by the Interstate Commerce Commission. This can be done while still having ample top speed for industrial service. Figure 6 shows motor losses at 110 degrees centigrade copper temperature, with an assumed two volts for brush drop, under starting conditions where good efficiency is most difficult to obtain, and where the difference between transmissions is most noticeable. Figure 6, curve *A*, is for the double-reduction motor with maximum reduction.

Figure 6, curve *B*, is for a comparable single-reduction conventional motor with maximum reduction. A study of this curve clearly shows the advantage of double reduction for locomotives that are not required to operate at high speed but are required to provide high tractive effort. Since at the starting point of the locomotive all the losses are in resistance, the difference between curve *A* and curve *B* in Figure 6 shows the additional tractive effort that can be produced with the double-reduction gearing over the single-reduction gearing for a given horsepower loss.

Correction for Saturation

THOMAS C. McFARLAND
MEMBER AIEE

IT is generally agreed that the virtual or air-gap voltage of a salient-pole synchronous machine determines the saturation in the magnetic circuit. Studies have indicated that the major portion of the saturation occurs in the poles of such machines. Consequently, it is logical to apply a correction as a linear addition to the nominal or excitation voltage vector of the two-reaction diagram drawn with unsaturated constants, in order to ascertain the actual per-unit excitation necessary under load conditions which produce a certain air-gap voltage. It is the purpose of this paper to present an analytical method for determining the necessary correction.

Careful tests have shown that the correction for saturation which is to be added to the excitation voltage obtained without saturation can be quite accurately determined for a given condition of loading by moving the zero-power-factor saturation curve for that load so that it coincides with the no-load saturation curve below saturation.[1] In Figure 1 the dashed curve is determined by moving the zero-power-factor load-saturation curve to the left a distance $Od=bc$. The horizontal intercept ab between the air-gap line and the dashed curve is the total per-unit correction corresponding to the condition of loading which produces a per-unit value of i_d equal to the per-unit armature current of the load-saturation curve, and a component of air-gap voltage in phase with the excitation voltage equal to Oe.[1,2]

If a family of zero-power-factor load-saturation curves is available, by moving each curve to coincidence with the no-load saturation curve as explained above, a family of dashed correction curves is obtained. Since at zero power factor $i_a = i_d$, it follows that *the parameter for this family of correction curves is* i_d. Having such a family of curves the procedure would be to determine the excitation voltage by drawing an unsaturated two-reaction diagram in accordance with the equivalent air-gap line drawn through a point on the no-load saturation curve equal to the air-gap voltage for the desired condition of loading. From this diagram i_d could be determined, and from the family of correction curves the intercept between the air-gap line and the curve of parameter i_d could be read corresponding to the direct-axis component of the air-gap voltage.

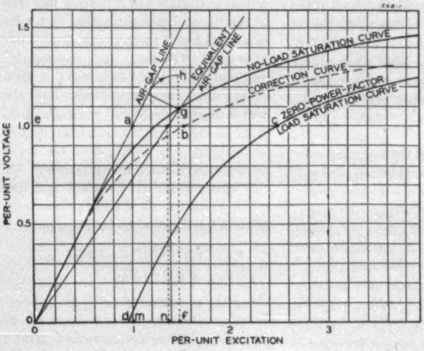

Figure 1. Saturation curves

The foregoing method of correcting for saturation is predicated upon the possibility of obtaining a family of correction curves, and this, in turn, depends on the ease and accuracy of the method used to secure the load-saturation curves. If the machine is already built, it may be possible to get these curves by test methods, but unless these methods involve but a few simple no-load tests the practical importance of the curves is nil. The greatest value of the correction curves would come in designing synchronous machines and in the predetermination of their performance. An empirical procedure which facilitates the application of the above principles is to express the saturation curves by a general mathematical equation and to employ saturation factors derived from the no-load saturation curve.

A modification of Froelich's equation for expressing a family of saturation curves is as follows:

$$V = \frac{A(i - Ci_d)}{B + i - Di_d} \tag{1}$$

wherein

A, B, C, and D are constants to be evaluated
i = per-unit field amperes or ampere turns
i_d = per-unit direct-axis component of armature current
V = per-unit volts

Solving the equation for field excitation,

$$i = \frac{BV + (AC - DV)i_d}{A - V} \tag{2}$$

From equation 1 it is seen that when $i = Ci_d$, $V = 0$. Hence, by subtracting Ci_d

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THOMAS C. McFARLAND is associate professor of electrical engineering, University of California, Berkeley, Calif.

from the right side of equation 2 the expression for the correction curve is obtained. As a result, for the correction curve

$$i=\frac{BV+(C-D)Vi_d}{A-V} \tag{3}$$

The equation for the air-gap line, in per-unit values, is

$$i=V \tag{4}$$

The *total correction for saturation* is the difference between the air-gap line and the correction curve. Whence, subtracting equation 4 from equation 3

$$\Delta=\frac{V[V+(B-A)+(C-D)i_d]}{A-V} \tag{5}$$

To evaluate the constants of the preceding equations it is necessary to have the no-load saturation curve, the unsaturated per-unit direct-axis synchronous reactance, and one point on the full-load zero-power-factor saturation curve at about rated voltage. Constants A and B are found by solving two simultaneous equations which are derived by choosing two corresponding sets of values of V and i from the no-load saturation curve and substituting them into the relation

$$V=\frac{Ai}{B+i} \tag{6}$$

which is the expression for the no-load saturation curve, obtained from equation 1 by making i_d equal zero. The values of V and i should be chosen with one set at rated voltage and the other set with considerable saturation.

The constant C is numerically equal to the unsaturated per-unit value of direct-axis synchronous reactance. From equation 1 it is seen that when $V=0$, $C=i/i_d=i$ when $i_d=1$ per-unit. Excitation i is necessary to circulate rated current when the machine is short-circuited. If i is held constant and the short circuit is removed, the voltage of an unsaturated machine will rise to the air-gap line. This voltage is the unsaturated synchronous-impedance drop. By virtue of the definition of unit field excitation as that value which produces unit volts at the air-gap line, the per-unit value of i must equal the per-unit value of unsaturated direct-axis impedance. Hence, neglecting armature resistance, C is numerically equal to the unsaturated per-unit x_d.

If a test point on the full-load zero-power-factor saturation curve near rated voltage is available, the constant D may be found by substituting the corresponding values of V and i for this point into equation 1, along with the values of A, B, and C, letting i_d equal unity.

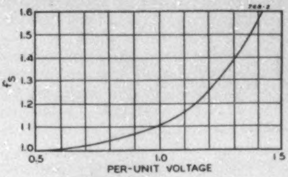

Figure 2. Saturation factor

When no test point on the full-load zero-power-factor saturation curve is available, the following empirical procedure may be used. At zero power factor the per-unit air-gap voltage may be taken equal to the algebraic sum of the per-unit terminal voltage and the per-unit armature-leakage reactance. Through a value of voltage on the no-load saturation curve equal to the per-unit air-gap volts, draw an equivalent air-gap line. In Figure 1 point g represents a per-unit value of air-gap volts. At rated voltage unit field excitation for the equivalent air-gap line is greater than unit excitation for the actual air-gap line by the ratio *On/Om*. Therefore, the excitation-voltage vector from an unsaturated two-reaction diagram referred to the actual air-gap line must be multiplied by this ratio to refer it to the equivalent air-gap line.

To account for increased saturation due to the presence of armature current a saturation factor[3] may be used. In Figure 1 the saturation factor corresponding to g is the ratio *fh/fg*; *gh* is the perpendicular distance of point g from the air-gap line. A corresponding factor for other voltages on the no-load saturation curve may be found in a similar manner. A plot of saturation factors as a function of per-unit volts is shown in Figure 2.

A point on the correction curve corresponding to rated voltage is found as the product of the saturation factor at rated volts and the ratio *On/Om*. Addition of the unsaturated per-unit value of direct-axis synchronous reactance drop caused by load current i_d gives the desired point on the zero-power-factor load-saturation curve for substitution in equation 1 to determine constant D.

As an illustration of the foregoing principles consider the computation of excitation characteristics for the 40-horsepower, 440-volt, 6-pole, 1,200-rpm, 0.8-power-factor synchronous machine for which test data are given by Robertson, Rogers, and Dalziel.[1] The unsaturated per-unit values of the machine constants are: $r_a=0.04$, $x_s=0.086$, $x_d=1.045$, $x_q=0.55$. The no-load saturation curve is shown in Figure 1.

To determine constants A and B values of $V=1.0$ and $V=1.4$ are chosen. Respective values of i are 1.25 and 3.04. Substitution of these values in equation 6 gives two equations from which it may be determined that $A=1.906$ and $B=1.133$.

C is found from the test data as being equal numerically to $x_d=1.045$.

From Figure 1 the ratio $On/Om=1.38$.

From Figure 2 the saturation factor corresponding to rated voltage is seen to be 1.1.

Applying the procedure outlined above the per-unit excitation for rated voltage at the full-load zero-power-factor saturation curve is $(1.1\times1.38)+1.045=2.563$. Substitution of these values in equation 1 gives

$$1.0=\frac{1.906(2.563-1.045)}{1.133+2.563-D},$$

whence $D=0.803$

By equation 5 the correction for saturation is

$$\Delta=\frac{V(V-0.773+0.242i_d)}{1.906-V}$$

In applying this relation i_d is scaled from the two-reaction diagram and V is interpreted as the projection of the air-gap voltage on the excitation voltage. For leading power factors with generators and for lagging power factors with motors the effect of armature reaction is magnetizing. In these cases the sign of i_d must be taken negative.

When drawing the two-reaction diagram both the per-unit excitation and the power angle are usually desired. It has been shown that it makes little difference in the excitation whether the saturated or unsaturated constant x_q be used, but the power angle is definitely influenced.[1] The saturated value of x_q should be used for accuracy. Since the rate of saturation of the quadrature axis is roughly half that of the direct axis a saturation factor for x_q can be found from Figure 2 as $1.0+(f_s-1)/2$. Dividing the unsaturated value of x_q by this factor gives a saturated value to be used in constructing the vector diagram.

As a check on the accuracy of this method, consider Figure 5 of the paper by Robertson, Rogers, and Dalziel.[1] On this vector diagram $V=1.217$ and $i_d=0.647$. Substitution of these values in the relation for the total correction gives

$$\Delta=\frac{1.217(1.217-0.773+0.157)}{1.906-1.217}=1.06$$

Using the unsaturated value of x_d the excitation voltage for the case shown is 1.85 per unit. Adding the correction gives a per-unit excitation of 2.91 which, by Figure 4 of the paper is equivalent to 2.91×3.6=10.5 amperes. Table I of the paper shows a test value of 10.7 amperes.

Consideration of Figure 6 in the paper by Robertson, Rogers, and Dalziel[1] shows for an underexcited motor a condition of loading where $V=1.095$ and $i_d=0.25$. Here

$$\Delta=\frac{1.095(1.095-0.773-0.06)}{1.906-1.095}=0.352$$

The excitation voltage from this diagram is found to be 0.835. Whence the total excitation is 0.835+0.352=1.187. This is equivalent to 1.187×3.6=4.27 amperes. Table I shows a test value of 4.36 amperes.

Application of the above procedure to other machines has shown that this empirical method will result in values of per-unit excitation under saturated conditions which check fairly closely with actual test results. Besides giving reasonable accuracy it has the further advantages of requiring a minimum of test (or predetermined) data and of being easy to apply.

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A 2,500,000-Kva Compressed-Air Powerhouse Breaker

L. R. LUDWIG
MEMBER AIEE

H. M. WILCOX
MEMBER AIEE

B. P. BAKER
MEMBER AIEE

I. Introduction

THE use of compressed-air breakers for indoor service has become established by several field installations since the presentation of papers[1,2] describing these devices two years ago. At that time complete tests had been made justifying ratings to 1,500,000 kva. Since then, powerhouse requirements have demanded the development of similar breakers for 2,500,000 kva. During the same interval new laboratory facilities have been provided[3] which are capable of completely testing these large breakers. This paper describes the theory and construction of this new breaker and for the first time presents test results of full 2,500,000 kva under three-phase fault conditions, together with a study of associated voltage recovery rates.

With the completion of the 2,500,000-kva rating, a complete series of indoor air breakers from 500,000 kva up is available which are common in fundamental design and operating pressure. In fact, compressed air was chosen as the interrupting medium because it was found after study of many types of interrupter that this means alone was adequate throughout the range for an oilless breaker.

II. Transverse-Blast Compressed-Air Interrupters

The transverse-air-flow type of interrupter has been shown to be capable of interrupting currents in excess of 60,000 amperes and fundamentally does not appear to have the current limitation inherent in a nozzle type of interrupting element. Consequently, for the 2,500,000-kva breaker this general type of interrupter was chosen. Figure 1A shows a diagrammatic sketch of the form of the interrupting device, and Figure 1B shows a partial assembly of the actual apparatus.

The interrupter is built up of alternate splitter plates and cooler units, disposed substantially parallel to the air blast and perpendicular to the arc. The arc is drawn from back to front across the discharge end of the blast tube and blown into the ends of the slots of the splitter plates. The moving contacts are of the blade type, hinged at the lower end and rotated back and forth by an insulated pull rod. The outer contacts are for carrying current only, and the middle one has the additional function of drawing the arc in the interrupter. The current-carrying contacts are completely isolated from the arcing contact, and, upon opening, they part approximately one inch ahead of arcing contacts. In the closed position, deep engagement into the stationary finger contacts is obtained. In the full open position the moving contacts are all completely withdrawn from the interrupter, so that an isolating air gap is placed in series with the interrupter, and all contacts are accessible for inspection. The assembly as shown is easily removed from the breaker; likewise, the interrupter can be removed as a unit from the contact assembly and supporting structure.

The actual interruption in an arc chamber of this type is accomplished by the air stream driving the arc against and between the lower ends of the splitter plates. When a normal current zero is reached the arc core is in a comparatively highly ionized condition, as indicated by the considerable leakage current and damping effect of the breaker on the recovery voltage, after the current zero.

The rate of deionization in a conducting gas column depends largely on conditions at the boundary where diffusion and recombination are most effective; consequently, it is very important to approach the current zero with a highly turbulent atmosphere surrounding the arc core. In compressed-air circuit breakers these boundary conditions are largely determined by the way in which the air flows with respect to the arc. If an arc is blown into a splitter of refractory material, it will be forced against the edge of the splitter, where it will remain more or less stationary with respect to the splitter. The flow of high-velocity air parallel to the splitter will cause one side of the arc to be highly turbulent, a condition very favorable to deionization. The other side of the arc, however, will be closely pressed to the splitter where the air velocity is substantially zero, and deionization will proceed very slowly. If, however, the splitter is made of some gas-evolving material such as fibre, an entirely different set of conditions will exist adjacent to the splitter.[4]

As the air flow, parallel to the splitter, forces the arc against the splitter edge, the heat from the arc liberates gas from the fibre which projects itself away from the splitter and into the arc stream at high

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L. R. Ludwig is division manager, H. M. Wilcox is in the switchgear engineering department, and B. P. Baker is research engineer, research laboratories, all with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

The authors are indebted to a number of Westinghouse engineers and laboratory operators for assistance in this development, and particularly to R. C. Cunningham for his contributions.

Table I. Three-Phase Opening and Closing–Opening Tests on a Westinghouse Compressed-Air Circuit Breaker

4,000 Amperes, 15 Kv; 2,500,000 Kva Tested 13.2 Kv

Test No.	Current Interrupted (RMS Amperes)			Inches Contact Separation at Arc Extinction			Arcing Time (Cycles)			Tank Pressure (Lb Per Sq In.)	Operating Duty	Circuit Transient Recovery-Voltage Rate (Volts Per Microsecond)
	Phase 1	Phase 2	Phase 3	Phase 1	Phase 2	Phase 3	Phase 1	Phase 2	Phase 3			
1	7,200	6,200	7,400	1.3	0.8	1.3	0.6	0.5	0.6	150	O	700
2	6,800	7,200	6,200	1.2	0.8	1.2	0.6	0.5	0.6	150	O	700
3	6,800	7,100	6,200	1.3	0.9	1.3	0.7	0.6	0.7	150	CO	700
4	6,200	7,900	7,700	1.2	1.4	1.4	0.6	0.7	0.7	150	O	700
5	24,700	20,000	27,000	2.0	1.4	2.0	0.7	0.6	0.7	150	O	1,170
6	31,000	22,000	20,000	1.4	1.1	1.4	0.7	0.6	0.7	150	CO	1,170
7	*48,000	*49,000	*49,000	1.4	1.4	0.8	0.7	0.7	0.5	150	CO	1,750
8	66,000	53,000	62,000	2.0	2.0	1.2	0.6	0.6	0.3	150	O	1,750
9	84,000	70,000	82,000	3.8	3.8	2.2	0.7	0.7	0.5	150	O	2 080
10	91,000	73,000	90,000	1.4	2.7	2.7	0.4	0.5	0.5	150	O	2,080

* Currents closed (amperes)

	Phase 1	Phase 2	Phase 3
Crest values	150,000	95,000	148,000
Rms values	92,000	59,000	86,000

velocity. This gas bombardment causes the arc stream to be highly turbulent on the side toward the splitter, and the air flow causes the arc to be highly turbulent on the side away from the splitter. Thus, the entire body of the arc lends itself very readily to deionization by diffusion, by reason of its high turbulence.[4] As a result, the space adjacent to a single splitter can be made to recover dielectric strength at a rate equal to the rate of rise of recovery voltage of the fastest powerhouse circuits, providing the ionized gas preceding the current zero is removed as fast as the current decreases toward its zero value. This condition is easily obtained if the rms value of the current being interrupted is small. As the current increases, the amount of gas liberated from the splitters becomes greater, and the air blast may become incapable of removing it fast enough. A 60,000-ampere arc in an arc-chute throat two inches wide will liberate sufficient gas from the splitter to cause the flow of gas to reverse a distance of six inches against a driving pressure of 150 pounds per square inch. If the rate of rise of recovery voltage of the circuit is slow, the stalled flow will have time to recover velocity, and dielectric will be restored. However, if the recovery rate is fast, the space below the splitters will remain clogged too long and reignition will result. Enlarging the throat of the arc chute or increasing the air pressure will obviously improve conditions; however, both result in an increased air consumption for a nominal gain in interrupting ability. Decreasing the volts per splitter by increasing the number of splitters results in some gain, but again as the number of splitters is increased, the gas-evolving surface is also increased, and the gas removal becomes more of a problem.

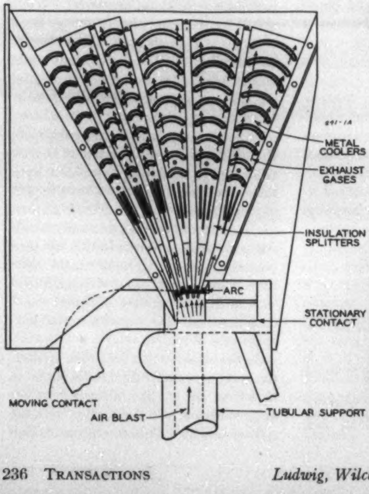

Figure 1A (left). Schematic diagram of a transverse-flow compressed-air circuit breaker

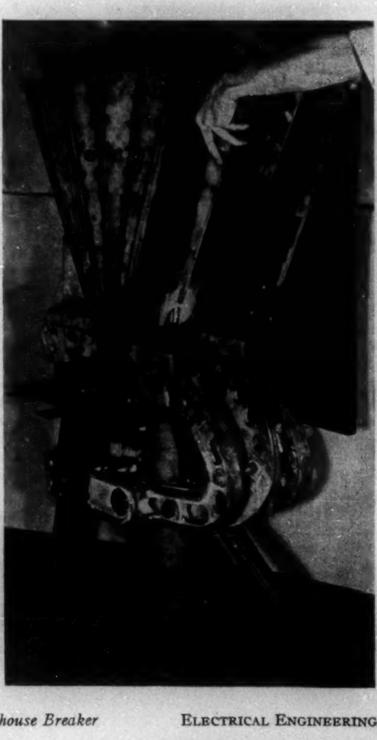

Figure 1B (right). Pole-unit assembly of a 2,500,000-kva 15-kv compressed-air circuit breaker showing the contacts and partially dismantled interrupter, after having interrupted 11 short circuits in excess of 1,500,000 kva

Likewise, spacing the splitters further apart allows more gas to escape, but this allows the arc to loop between the splitters a greater distance which in turn causes more gas to be liberated.

Figure 2 shows the fundamentals of a simple solution to this problem. The arc chute consists of a narrow throat portion and a wide portion. The splitters are full width and terminate at the narrow throat, having a notch cut into them just above the throat. The arc is blown into this notch which is well up into the wide portion of the chute. Since the throat is narrow and the approach to it is streamlined, the air approaches the arc with a high velocity. When the instantaneous value of the current is large, the arc does not block the flow of air, but exerts only enough pressure on it to deflect it to the sides, thus allowing the flow to continue. This diverted flow washes away the extraneous ionized flame as the instantaneous value of current decreases. By providing an escape for a continuous flow of air, the pressure on the arc never exceeds that which is necessary to deflect the air around it, and consequently, it is not pressed too tightly against the splitters which always results in too great evolution of gas and greater clogging. By using the means shown in Figure 2, it has been possible to build the interrupter for 2,500,000 kva in reasonable dimensions and obtain entirely satisfactory operation with a tank pressure of 150 pounds per square inch, even with very high-voltage recovery rates.

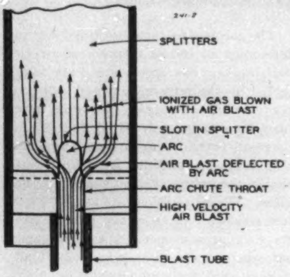
SPLITTERS
IONIZED GAS BLOWN WITH AIR BLAST
SLOT IN SPLITTER
ARC
AIR BLAST DEFLECTED BY ARC
ARC CHUTE THROAT
HIGH VELOCITY AIR BLAST
BLAST TUBE

Figure 2. Cross-sectional diagram of arc chutes showing air flow under arcing conditions

III. Breaker Construction and Operation

Figure 3A shows a 4,000-ampere breaker mounted in a steel cell and set up for test at the East Pittsburgh high-power laboratory, for the Consolidated Edison Company. Figure 3B shows a schematic side elevation of the same breaker. It consists essentially of an air-storage tank at the bottom, an air-operating mechanism at one side, suitable connecting levers for operating the contacts, an air-duct between the tank and the vertically situated arc chamber, and a separate mechanically operated blast valve for each pole.

This type of construction naturally places all parts of the breaker which operate at ground potential including the pneumatic and electric control, in the lower compartment of the cubicle. The tank and mechanism which constitute the heaviest parts of the breaker may, therefore, be supported directly on the floor where it is most accessible and completely separated by a horizontal barrier from the high potential compartment just above. This compartment, shown with the doors standing open, houses the contacts, interrupting elements, and all other live parts. The phases are separated from each other by removable vertical barriers extending inwardly from the hinge points of the doors. The contacts are very accessible for inspection; likewise, the barriers and interrupters may be easily withdrawn. Above the interrupter are diffusion chambers and mufflers, one for each phase. Upon interruption, the three mufflers discharge their gas into one common gas receiving chamber, which communicates with the outside through ventilating grilles as shown.

The flow of air for arc interruption is, therefore, as short and direct as possible, with no turns as it passes from the tank, through the blast valves and blast tubes across the arc and through the interrupter into the muffler and gas-receiving chamber at the top.

This is a very important factor in reducing pressure requirements and the quantity of air consumed. Tests have shown that with earlier experimental forms of breakers built with a single blast valve and numerous turns in the blast air-supply piping that the major portion of the energy stored in the air during compression is dissipated before the air stream

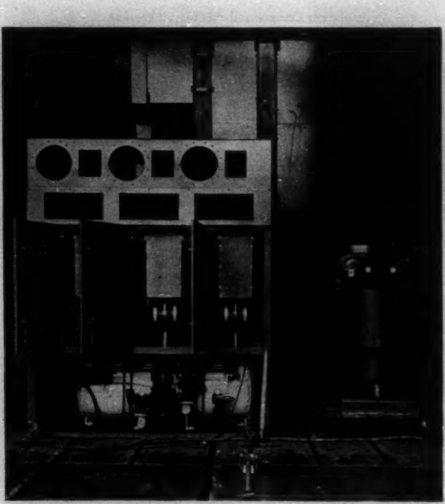

Figure 3A (left). Three-pole 2,500,000-kva 15-kv 4,000-ampere compressed-air circuit breaker in steel cell, set up for test at East Pittsburgh high-power laboratory

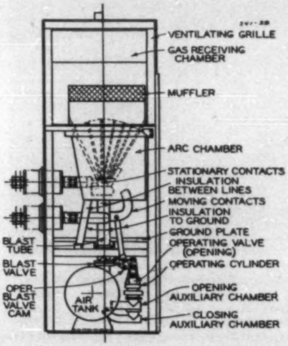
VENTILATING GRILLE
GAS RECEIVING CHAMBER
MUFFLER
ARC CHAMBER
STATIONARY CONTACTS
INSULATION BETWEEN LINES
MOVING CONTACTS
INSULATION TO GROUND
GROUND PLATE
OPERATING VALVE (OPENING)
OPERATING CYLINDER
OPENING AUXILIARY CHAMBER
CLOSING AUXILIARY CHAMBER
BLAST TUBE
BLAST VALVE
OPER. BLAST VALVE CAM
AIR TANK

Figure 3B (right). Sectional elevation showing side elevation of the breaker in Figure 3A

reaches the arc chamber. With straight-line air flow these losses are reduced several fold. The three mechanically operated blast valves can be closely timed with respect to contact separation so that an extremely high rate of flow of air through the interrupter occurs during actual interruption, but the over-all air consumption is quite nominal.

The operating mechanism consists of a single air cylinder, in which a piston moves vertically. The piston is connected to a torsion shaft through a lever, piston rod, and a single set of links. The torsion shaft operates three insulating pull rods which in turn are attached to the moving contacts of the three pole units. The blast valves (one for each pole unit) are located at the top of the tank and are operated by rocker arms from cam surfaces which are a part of the torsion shaft. It is, therefore, possible to accurately synchronize the opening of the blast valve with the drawing of the arc in the interrupter. The cams and their co-operating parts are arranged to open the blast valve during the opening motion of the contacts only.

To open the breaker, air is admitted to the upper side of the piston from the opening auxiliary chamber, through a pneumatic relay, in response to a tripping impulse in an electropneumatic pilot valve. To close the breaker, air is admitted below the piston, from the closing auxiliary chamber, through a pneumatic relay in response to a closing impulse in another electropneumatic pilot valve.

The volume of each of the auxiliary chambers is adjusted to some suitable percentage of the volume of the operating cylinder and communicates with the main-pressure storage tank through a small hole of such size that it can charge an auxiliary chamber to full pressure in approximately one second. However, only a small amount of air can flow through this hole during the fraction of a second that it takes the piston to move from one end of the cylinder to the other. Thus, for instance, if the volume of the opening auxiliary chamber is one half of the volume of the operating cylinder, the pressure on the piston at the end of its travel will be approximately one third of the pressure at the beginning of its motion. By this means, it is possible to obtain an accelerating force of several thousand pounds at the beginning of motion and reduce this force to any predetermined value toward the end. This simple force system together with the toggle makes it possible to close the breaker against the heaviest short-circuit current; lock it in the closed position by moving the toggle over center, and upon opening, obtain high-speed contact separation.

If, however, it is necessary to open the breaker immediately after closing, the air below the piston which was used to close the breaker must be exhausted. To take care of this, an exhaust valve is built into the side of the cylinder and actuated by the motion of the piston. This insures that an opening operation is independent of the time the breaker has remained in the closed position; likewise, it can be used to insure that the closing is independent of the time the breaker has remained in the open position. The equivalent of trip-free opening and high-speed reclosing is thus accomplished without latches, springs, or trip-free levers.

IV. Air-Supply System

The mechanical operation and arc interruption of indoor compressed-air circuit breakers has been found to be unaffected by the temperature and moisture content of the air supply. However, moisture removal from the air is desirable to decrease corrosion, protect insulation, and eliminate any tendency from ice formation in the valves or other pneumatic devices. These provisions are met by a system consisting of an intake filter, a two-stage compressor operating [illegible]50 pounds per square inch; a cooling coil, small-storage, and moisture-elimination reservoir; a second cooling coil and large main-storage reservoir. This system is connected to the line through an automatic reducing valve which drops the pressure to 150 pounds per square inch.

Elimination of oil vapors in the air system, essential to prevent explosion, is provided by the use of large slow-running compressors to prevent undue temperature rise and adequate cooling by suitable coils to condense oil vapors. The compressor is also of such a design that it is

Table II. Three-Phase Opening and Closing–Opening Tests on a Westinghouse Compressed-Air Circuit Breaker

4,000 Amperes, 15 Kv; 2,500,000 Kva Tested 13.2 Kv Line to Line, 60 Cycles

Test No.	Current Interrupted (RMS Amperes) Phase 1	Phase 2	Phase 3	Inches Contact Separation at Arc Extinction Phase 1	Phase 2	Phase 3	Arcing Time (Cycles) Phase 1	Phase 2	Phase 3	Tank Pressure (Lb Per Sq In.)	Operating Duty	Circuit Transient Recovery-Voltage Rate (Volts Per Microsecond)
11	7,300	6,200	8,300	0.9	1.1	1.1	0.3	0.4	0.4	150	O	700
12	6,100	7,750	7,300	1.3	1.1	1.3	0.5	0.4	0.5	150	O	700
13	7,100	6,000	7,900	2.1	1.2	2.1	0.7	0.4	0.7	150	O	700
14	7,400	6,000	6,900	1.5	1.5	1.0	0.6	0.6	0.4	150	O	700
15	8,200	6,800	6,400	1.0	1.5	1.5	0.4	0.6	0.6	150	O	700
16	70,000	84,000	84,000	3.0	3.0	1.0	0.5	0.5	0.2	150	O	2,080
17	8,300	7,700	6,100	1.1	2.1	2.1	0.4	0.6	0.6	150	O	700
18	7,700	6,900	6,400	1.1	2.0	2.0	0.4	0.5	0.5	150	O	700
19	98,000	80,000	82,000	2.0	3.5	3.5	0.5	0.6	0.6	150	O	2,080
20	7,800	8,300	6,300	1.5	1.5	1.0	0.5	0.5	0.4	150	O	700
21	102,000	98,000	80,000	3.0	3.8	3.8	0.4	0.6	0.6	150	O	2,080
22	105,000	93,000	82,000	3.8	2.5	3.8	0.8	0.6	0.8	125	O	2,080
23	48,000	51,000	49,000	2.7	2.7	1.7	0.7	0.7	0.5	125	CO	1,750
24	6,700	6,100	6,900	0.6	1.1	1.1	0.2	0.3	0.3	150	CO	700
25	Timing test at 10,000 amperes 3,300 volts									150	CO	
26*	Laboratory circuit on phase 2 failed four cycles after breaker closed the circuit; consequently the breaker was not called upon to interrupt appreciable current									150	CO	2,080
27†	8,300	7,700	5,900	1.1	1.8	1.8	0.4	0.7	0.7	150	O	700
28†	6,500	8,400	6,900	1.5	2.5	2.5	0.5	0.8	0.8	150	O	700
29†	25,000	28,000	20,000	2.1	1.1	2.1	0.8	0.4	0.8	150	O	1,170
30†	60,000	71,000	56,000	2.0	2.0	1.8	0.5	0.5	0.4	150	O	1,750
31†	74,000	87,000	77,000	3.0	4.1	4.1	0.7	0.8	0.8	150	O	2,080

* Currents closed (amperes)

	Phase 1	Phase 2	Phase 3
Crest values	160,000	247,000	197,000
Rms values	95,000	150,000	113,000

† In tests 27–31 the line leads were connected to the lower terminals of the breaker, and the upper terminals short-circuited.

difficult for crank-case oil to be carried into the air line.

Auxiliaries in the air-supply system consist of safety valves and two automatic checks to prevent air from either storage reservoir flowing backward to the compressor, necessary gauges, automatic switch to start and stop the compressor, and an automatic alarm valve which provides an indication if the pressure drops due to lack of compressor operation or serious leakage in the system.

Air auxiliaries incorporated as a part of the circuit breakers themselves consist of an additional filter to prevent scale, and so on, from the line entering the breaker tank, an automatic two-way check valve which prevents flow of air from the breaker back into the line and also automatically closes in the event of too large an air flow to the breaker, which could be occasioned by exceptional breaker leakage. An alarm valve and automatic lock-out valve are incorporated as part of the breaker to prevent its operation in case the breaker pressure becomes too low. A safety valve and gauge are also included on each breaker tank.

Table III. Single-Phase Tests to Determine Effect of Recovery Voltage

Voltage (Kv)	Current Interrupted (Amperes RMS)	Natural Frequency (Cycles Per Second)	Circuit Voltage-Recovery Rate (Volts Per Micro-second—at Test Voltage)	Arcing Time (Cycles)	Inches Contact Separation at Interruption
13.2	8,000	200,000	13,600	0.8	2.5
13.2	8,000	200,000	13,600	0.7	1.5
13.2	8,000	200,000	13,600	0.8	2.3
13.2	8,000	200,000	13,600	0.5	1.4
13.2	8,000	60,000	3,900	0.6	1.4
13.2	8,000	60,000	3,900	0.8	2.3
13.2	8,000	60,000	3,900	0.5	1.2
13.2	8,000	60,000	3,900	0.9	2.6
13.2	8,000	13,000	1,000	0.2	0.4
13.2	8,000	13,000	1,000	0.6	1.4
13.2	8,000	13,000	1,000	0.6	1.3
13.2	8,000	13,000	1,000	0.3	0.6
13.2	8,000	10,250	800	0.3	0.6
13.2	8,000	10,250	800	0.6	1.3
13.2	8,000	10,250	800	0.2	0.3
13.2	8,000	10,250	800	0.4	0.9
13.2	8,000	10,250	800	0.7	1.8
13.2	8,000	3,900	300	0.6	1.3
13.2	8,000	3,900	300	0.4	0.8
13.2	8,000	3,900	300	0.4	0.8
13.2	33,000	100,000	4,130	1.5	5.3
13.2	35,000	100,000	4,130	1.4	6.3
11.0	30,000	24,000	1,370	0.5	1.7
13.2	37,000	24,000	1,640	1.1	4.2
11.0	53,000	240,000	7,100	0.4	1.3
11.0	52,000	32,000	2,390	0.9	4.2
12.0	76,000	32,000	2,390	1.2	8.0

Tables I, II, and III are the results of a series of 58 consecutive tests made without any maintenance on the breaker. Toward the end of the series the first two splitters in the arc chute were eroded sufficiently to lose some of their effectiveness, as evidenced by an increased variation in arcing time. The tests included in Table III, although valuable in indicating the life of the chute, are not as typical of the single-phase performance of the breaker in service as an earlier series of nine consecutive tests which was started with a fresh set of splitters and which gave the data presented in Table IV.

The breaker tank contains sufficient air for two-breaker operations without obtaining additional supply from the main system.

V. Test Results

The breaker was tested with the high-power laboratory facilities described in the MacNeill–Batten paper,[3] using two parallel generators at 13.2 kv, 60 cycles. Complete tests have been made, both single-phase and three-phase.

Tables I and II show a series of 31 consecutive tests made on an interrupter of this type. The tests were all three-phase, 13.2-kv, both opening and closing-opening, and with current values varying from 6,000 amperes to 105,000 amperes. The test with the highest current of this series, 105,000 amperes, was made with a tank pressure of 125 pounds per square inch and on a circuit with a rate of rise of recovery voltage of 2,080 volts per microsecond.

The maximum current closed was 247,000 amperes crest which had an rms value of 150,000 amperes. In nine of the 31 tests, the power interrupted exceeded 1,500,000 kva. For the 31 tests, the maximum arcing time for any phase did not exceed 0.8 cycle, which due to asymmetry of the current wave means that the breaker never failed to clear the circuit when the contacts were more than a small fraction of an inch apart.

Table IV. Single-Phase Interrupting Tests on a Westinghouse Compressed-Air Circuit Breaker

4,000 Amperes, 15 Kv, 2,500,000 Kva Tested at 150 Pounds Per Square Inch Tank Pressure

Voltage (Kv)	Current Interrupted (Amperes RMS)	Natural Frequency—(Cycles Per Second)	Circuit Voltage-Recovery Rate (Volts Per Micro-second—at Test Voltage)	Arcing Time (Cycles)	Inches Contact Separation at Interruption
10,000	5,000	9,800	570	0.2	0.7
10,000	22,400	22,800	1,320	0.4	1.6
10,000	39,000	25,300	1,470	0.3	1.2
10,000	52,000	27,400	1,590	0.4	1.5
10,000	60,000	27,400	1,590	0.25	1.0
10,000	71,000	27,400	1,590	0.5	2.0
12,000	94,000	27,400	1,910	0.5	2.2
12,000	82,000	27,400	1,910	0.7	3.0
12,000	84,000	27,400	1,910	0.6	2.8

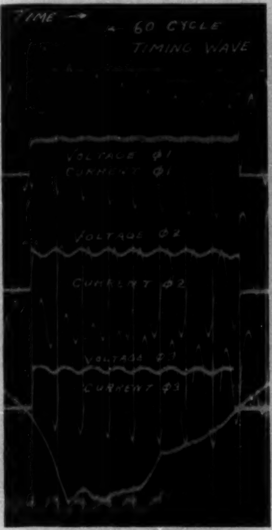

Figure 4. Magnetic oscillogram of typical closing–opening operation test

Table I

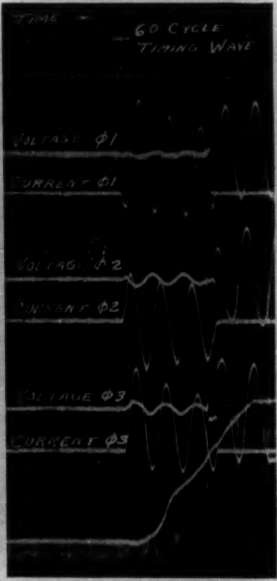

Figure 5. Magnetic oscillogram of an opening operation

Test 10, Table I. 13.2 kv. Current interrupted 91,000, 73,000, and 90,000 amperes

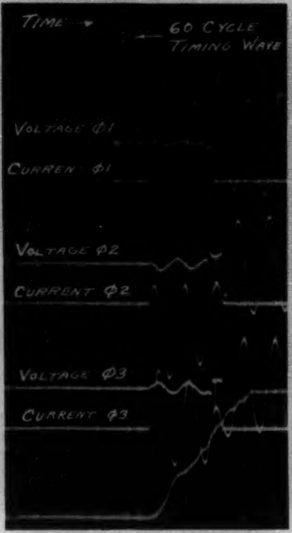

Figure 6. Magnetic oscillogram of an opening operation

Test 21, Table II. 13.2 kv. Current interrupted 102,000, 98,000, and 80,000 amperes

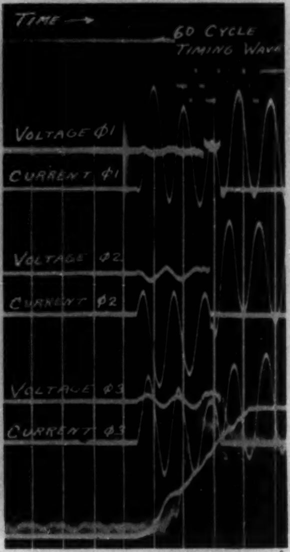

Figure 7. Magnetic oscillogram of an opening operation

Test 22, Table II. 13.2 kv. Current interrupted 105,000, 93,000, and 82,000 amperes

Figure 4 shows the oscillogram for test 7, Table I, which was a closing–opening operation interrupting 48,000, 49,000, and 49,000 amperes for the three phases, and closing in on 150,000, 95,000, and 148,000 amperes crest or 92,000, 59,000, and 86,000 amperes rms respectively.

Figure 5 shows the oscillograms for test 10, Table I, which was an opening operation, interrupting 91,000, 73,000, and 90,000 amperes respectively for the three phases.

Figure 6 shows the oscillogram for test 21, Table II, which was an opening operation, interrupting 102,000, 98,000, and 80,000 amperes for the three phases.

Figure 7 shows the oscillogram for test 22, Table II, also an opening test, with 105,000, 93,000, and 82,000 amperes interrupted for the three phases respectively. For this test the initial pressure in the tank was lowered to 125 pounds per square inch.

The ten tests recorded in Table I were made to demonstrate the breaker to the engineers representing the Consolidated Edison Company. This was the first witness test of an interruption of a three-phase fault approximating 2,500,000 kva.

Figure 1B shows the condition of the splitters, coolers, and contacts following the series of 40 tests as listed in Tables I, II, and III.

The first two splitters showed erosion. The coolers were blackened slightly, and the arcing contacts were pitted; however, the breaker was still capable of carrying current and performing further interrupting duty.

The external demonstration on all of these tests was negligible except for some smoke from the grille in the gas-receiving chamber. The noise was held to acceptable limits by the muffler system previously described.

During the development of the 2,500,000-kva breaker, a total of 140 successful interrupting tests was made which were in excess of 1,500,000 kva. The function of such a large number of tests was to study design variations, various operating pressures, and different types of circuit-recovery transients. It was found that the problem of obtaining satisfactory interruption of 2,500,000 kva was one of considerable magnitude, even when a proven breaker of 1,500,000 kva was already available.

VI. Effect of Circuit-Recovery Rate

In powerhouse service generator short-circuit currents would often exceed the capacity of circuit breakers if reactors were not used to limit these currents. These reactors are often placed near the circuit breaker, and this practice generally leads to calculated circuit voltage-recovery rates which are very high.

In case no reactor is used, the highest circuit voltage-recovery rate will be established by the natural frequency of the generators. Generally a single-frequency transient only is involved and the circuit recovery rate may reach approximately 2,500 volts per microsecond.

When the reactor is introduced, its natural period as well as the natural period of the generator is involved, and double-frequency recovery transients result. A natural frequency of the generator may be approximately 30,000 cycles, but the natural frequency introduced by the reactor

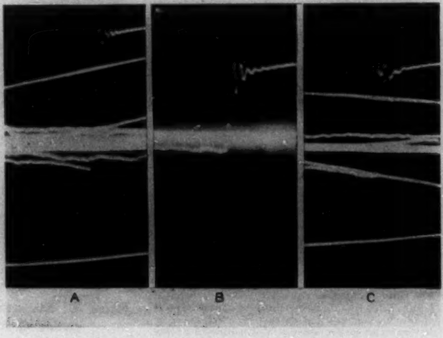

Table III. From left to right:

(A) 13.2 kv, 35,000 amperes, 100,000 cycles per second. 4,130 volts per microsecond

(B) 11.0 kv, 53,000 amperes, 240,000 cycles per second. 7,100 volts per microsecond

(C) 12.0 kv, 76,000 amperes, 32,000 cycles per second. 2,390 volts per microsecond

Figure 8. Cathode-ray oscillograms corresponding to three single-phase tests

Linear Couplers for Bus Protection

E. L. HARDER
MEMBER AIEE

E. H. KLEMMER
ASSOCIATE AIEE

W. K. SONNEMANN
ASSOCIATE AIEE

E. C. WENTZ
ASSOCIATE AIEE

Synopsis: A scheme of bus protection offering advantages in simplicity, speed, and size uses linear couplers (air-core mutual reactances) in place of current transformers. This solves the troublesome problem of saturation and provides a linear relationship between secondary voltage and primary current. The coupler secondaries for a given bus are connected in a series loop with the relay. When the currents entering and leaving the bus are equal, the net induced voltage in the relay loop is zero. For a fault on the bus, however, the net induced voltage, proportional to the fault current, operates the relay. The problems are:

1. To utilize effectively the smaller available energy.
2. To build couplers of sufficiently equal mutual reactance and unaffected by stray fields.

A toroidal coil solved the latter problem. Thorough tests have shown that the performance is strictly linear with respect to primary current, practically unaffected by the primary d-c transient, and thus can be calculated accurately and simply.

THE linear coupler transformer is a constant mutual reactance connecting the primary circuit to the relay. It introduces a new principle into the protective relaying art, a principle* that is fundamentally sound and that eliminates completely, at its source, the most troublesome problem that has been standing in the way of simple high-speed bus protection. That problem is saturation of the current transformers by the d-c transient current that flows for a number of cycles after the occurrence of a fault. Its solution consists of dispensing with the iron, a solution that appears so obvious on the face of it that it is fair to inquire why it was not adopted years ago. This paper might stop right here were it not for the answer to the last question.

As most frequently occurs, there were a number of obstacles to be hurdled between the quite obvious idea of leaving out the iron, and the completion of a successful bus-protective system using linear coupler transformers. Efficient coupler designs had to be developed, capable of deriving the maximum amount of energy obtainable from the available space without the use of iron. New methods had to be devised to use effectively the lower energy level inherent in the elimination of the iron. This energy is adequate for the operation of an efficient a-c plunger-type element in most cases. Also, the development of the copper oxide rectifier to a highly reliable state during the last several years has made available in a-c circuits the sensitive and reliable action of the d-c polar-type of relay. This has greatly extended the range of sensitivities possible with ironless transformers.

New circuits were necessary, better suited than the conventional bus-differential circuits, for use with the accurately linear mutual reactance. Here a new principle* was applied—a radical departure from the principle of the conventional current transformer.

The influence of external fields and positional effects of the primary conductor on

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E. L. HARDER is central station engineer, E. H. KLEMMER switchgear engineer, both in East Pittsburgh, Pa., W. K. SONNEMANN is relay engineer in Newark, N. J., and E. C. WENTZ transformer engineer in Sharon, Pa., all with Westinghouse Electric and Manufacturing Company.

The authors acknowledge the assistance of C. A. Woods in connection with the bushing-type couplers, and of other associates who have contributed to the success of this project.

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* The principle of perfect linearity between primary current and secondary induced voltage in couplers as described.

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may reach 200,000 cycles. Circuit recovery rates based on the first crest of the recovery transients may be as high as approximately 12,000 volts per microsecond.

The performance of the 2,500,000 compressed-air breaker has been studied with a wide variety of recovery-voltage conditions. A number of the important tests are summarized in Table III. The first part of the table lists a series of interruptions at 13,200 volts single phase and 8,000 amperes. The circuit-recovery rate was varied from 300 volts per microsecond to 13,600 volts per microsecond. To obtain the high recovery transients, a reactor was placed directly in the test cell with only a few feet of cable between it and the breaker. Interruption was satisfactory in all cases.

The second portion of the table lists tests made at higher currents. To obtain these the laboratory circuit capacitance was reduced, as much as practical, and during several of these tests double-frequency recovery transients were obtained. Three cathode-ray oscillograms of these interruptions are shown in Figure 9. One at 53,000 amperes was obtained with a circuit adjusted for 7,100 volts per microsecond. The second was obtained at 35,000 amperes with the circuit adjusted for 4,130 volts per microsecond, and the third was obtained at 76,000 amperes with the circuit also adjusted for 2,390 volts per microsecond. All three of these circuit conditions represent double-frequency transients, but in the second and third oscillograms the higher frequency is so greatly damped by the conduction current of the breaker following current zero that little trace of the oscillation appears in the recovery-voltage transient recorded by the oscillograph.

These are the highest published voltage-recovery rates measured on circuits on which heavy short-circuit tests have been made.

VII. Conclusions

The complete test results show that the 2,500,000-kva compressed-air breaker, which is the largest air powerhouse breaker ever built, is entirely adequate in interrupting capacity even in the case of extremely high-circuit voltage-recovery rates. The small external demonstration, ease of maintenance, complete freedom from fire hazard, and general mechanical simplicity indicate that there will be a growing tendency to utilize this type of breaker for indoor powerhouse installation.

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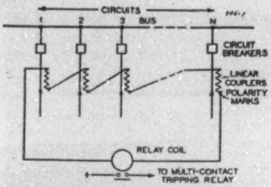


Figure 1. Schematic one-line diagram of connections showing application of linear couplers and relay to bus protection

the response had to be dealt with. This was done not by brute force, but by bringing into play a useful theorem relating to coils wound on nonmagnetic cores of toroidal or ring shape.

Methods had to be devised for winding toroidal coils conforming to the requirements for efficient energy utilization and high accuracy without undue economic limitations.

Thus, it is not simply the elimination of iron in a transformer but rather the technique of using linear couplers in relaying circuits and presentation of the verifying tests which have necessitated this paper.

To more fully understand the place filled by the linear-coupler scheme of bus protection, mention should be made of certain other schemes in current use. Bus-protection schemes that have received the most attention in recent years have revolved about some form of current-differential protection. The relays considered have varied from simple overcurrent relays[1] to various forms of restrained relays. Restrained relays have utilized either the fault current itself,[2] or harmonics in the differential current.[3] Another method of attacking the problem from the standpoint of directional comparison[4] was presented at the 1941 summer convention.

Impedance and reactance schemes,[6] measuring the impedance from the main incoming sources into the feeder reactors have also found considerable application where reactors are used and ratios of fault impedance to reactor impedance are such as to provide discrimination. However, they are necessarily restricted to certain busses which happen to fill these requirements. The fault-bus scheme[1] is also ideal for certain new installations that can be arranged to accommodate it.

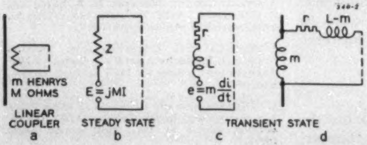


Figure 2. Basic equivalent circuits of linear coupler

Table I. Greatest Allowable Ratio of Maximum Through Fault* to Relay Setting for 2-to-1 Safety Factor**

Coil Mutual Reactance Tolerance (Per Cent)	Ratio of Maximum Through Fault to Relay Setting
±5.0	5:1
±2.5	10:1
±1.5	17:1
±1.0	25:1
±0.25	100:1

*Rms symmetrical.

**Larger ratios can be covered with proportionately reduced safety factors.

In all of the current-differential schemes, the over-all performance necessarily depends upon the performance of the current transformer. For busses near large generating stations, the presence of the d-c component in asymmetrical fault currents has greatly magnified the problem through its saturating effect[7] on the current transformers.

Another related problem is the large ratio of maximum external fault current when the relay should not trip to minimum internal fault current when the relay should trip. A large ratio between these values is encountered in those stations where the neutral is grounded through an impedance, thus materially limiting the minimum phase-to-ground fault current for internal faults without limiting the maximum phase-to-phase and three-phase fault current for external faults.

The very sensitive relay setting required under this condition greatly magnifies the problem created by d-c saturation at high overcurrents. The problem has been solved in some instances by the use of extremely large current transformers designed not to saturate on the d-c component. What is felt to be the most practicable scheme to date utilizing current transformers of standard proportions is the multirestraint variable percentage scheme.[2] In this scheme particular attention is given to providing a sufficient number of restraining elements, actuated by the secondary fault current. Also, the principle of variable percentage characteristics is utilized by which means the relay sensitivity is reduced at the higher currents where current transformer performance is poorest. By this means a range of 100/1 between maximum external fault current and relay setting may be covered without fear of false operation. An operating time of three to six cycles is secured through the use of the high-speed induction-type relay.

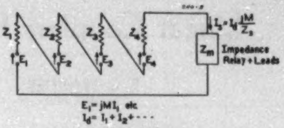


Figure 3. Series-connection equivalent circuit of linear couplers

Heretofore, the problem has been attacked from the standpoint of either making the current transformers more nearly perfect, which is a costly method, or of designing the relay to meet the limitations of the current transformers. It is immediately obvious that if the current transformers were perfect in their response a simple overcurrent relay differentially connected would be all that would be necessary. One line of attack which has been recently introduced involves the use of a special current transformer with air gaps in the core.[5] This arrangement can be made high speed but cannot cover the 100-to-1 range of the multirestraint variable-percentage scheme. The authors state its range as approximately 10 to 1.

The linear-coupler scheme described herein is essentially a high-speed scheme covering a range at the present state of development of 17 to 1 with a 2-to-1 factor of safety. Tests have shown this rating to be quite conservative.

Summary

Linear couplers are distinguished from current transformers in having a constant mutual impedance even when the entire primary current acts as exciting current, with no current in the secondary. It is the function of a current transformer to produce in its secondary a miniature replica of the current that flows in its primary. It is the function of a linear coupler to produce in its secondary an internal voltage proportional to the current in its primary. To form a differential circuit, these voltages are added in series (Figure

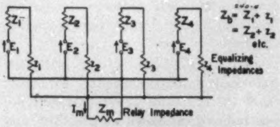


Figure 4. Parallel-connection equivalent circuit of linear couplers

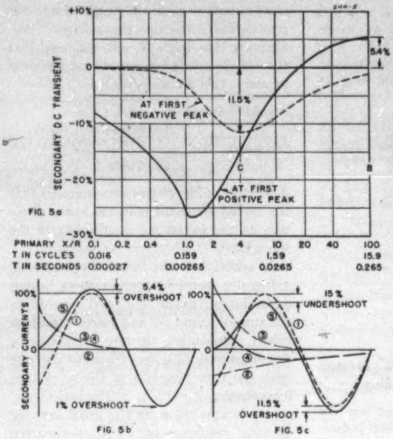


FIG. 5a

FIG. 5b

FIG. 5c

Figure 5. D-c transient current in linear-coupler secondary circuit when primary current is initially fully offset

Drawn for $X_s = r_s$. First positive peak taken at 0.375 cycle and first negative peak at 0.875 cycle from instant of fault since secondary current leads primary current by 0.125 cycle

5a. Secondary total d-c transient component, in per cent of a-c component positive crest, with respect to primary d-c time-constant, T, or 60-cycle X/R ratio

5b. Condition B of part (a), $X/R = 100$, $T = 15.9$ cycles

5c. Condition C of part (a), $X/R = 4$, $T = 0.64$ cycle

(1) Secondary a-c component current
(2) Forced d-c transient component
(3) Free d-c transient component
(4) Total d-c transient component
(5) Resultant secondary current

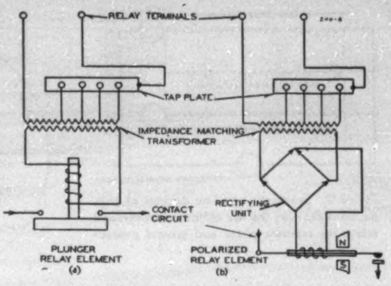


Figure 6. Schematic internal diagram of relays

(a) Simple plunger-type element
(b) Sensitive polar-type element

1), as distinguished from current transformers in which the currents are added in parallel. For an external fault the vector sum of all the primary currents is zero. Thus, the vector sum of all induced voltages is zero, and also the relay current is zero. For an internal fault the sum of the primary currents through legitimate circuits is equal to the fault current. Consequently, the vector sum of all induced voltages is proportional to the fault current. The current in the relay is equal to the vector sum of the voltages acting around the relay loop divided by the loop impedance. Thus, this current is also proportional to the fault current.

A one-cycle relaying scheme including couplers and relays has been developed, operating on the principle just outlined, and has been subjected to an exhaustive series of tests. Through type couplers of 0.005 ohm mutual reactance were built in several sizes and shapes to fit in the usual bushing current-transformer compartments or for separate mounting.

Sufficient energy is obtainable from couplers of this proportion to operate an a-c plunger-type relay for fault currents down to 1,500 amperes with a six-circuit bus and 2,000 amperes with a ten-circuit bus. Couplers of reasonable cost can be built to the desired mutual reactance within a tolerance of ±1.5 per cent, thus permitting a maximum false differential of three per cent. Taking a 2-to-1 factor of safety, this results in a minimum allowable setting of six per cent or $^1/_{17}$ of the maximum through fault current. The sensitivities obtained are ample for busses where the setting does not need to be below $^1/_{17}$ of the maximum bus fault.

The tests have demonstrated this simple and straightforward scheme to be thoroughly reliable and practically free from transient effects, so that the performance can be readily calculated. Operation under one cycle was obtained for practically all internal faults and in no cases were operations obtained on external faults within the limits prescribed for proper application.

Tests have verified the feasibility of testing an actual bus installation with low steady-state currents to determine the performance to be expected under fault conditions.

The following paragraphs include an outline of the theory of the linear-coupler scheme, a description of the apparatus employed, and a résumé of the combination tests which have been conducted to verify the theory and prove the equipment designs.

Theory of Linear-Coupler Circuits

NOMENCLATURE

M, m—Mutual reactance and inductance between the primary conductor and secondary winding of a linear coupler

$Z = r + j\omega L$—Secondary self-impedance of a coupler

$Z_s = r_s + j\omega L_s = r_s + jX_s$—Total impedance of coupler-relay loop circuit

E—Induced voltage in coupler

E_s—Vector sum of induced voltages in coupler-relay loop

I, i—Primary current, vector, and instantaneous values. Subscripts 1, 2, 3, and so on designate the various bus circuits

I_s—Secondary current in the relay, vector value

I_f—Fault current, into the bus for an internal fault; into and out of the bus for a through fault

I_d—Vector sum of primary currents; equal to I_f for internal faults, and zero for external faults

EQUIVALENT CIRCUIT

As illustrated in Figure 2b, the equivalent circuit of a linear coupler is simply an internal voltage of value jMI, in series with an impedance of value Z. The equivalent circuit for transient conditions, Figure 2c, is of the same form. However, the internal voltage is then $m\,di/dt$. An alternate form convenient for determining the transient response is also shown in Figure 2d.

As will be shown later, the transient response of the couplers considered is negligible so that the steady-state response can be used for analyzing fault conditions. Thus, for calculations, the series connection shown in Figure 1 may be treated through its equivalent circuit, Figure 3.

SERIES CONNECTION — STEADY - STATE CONDITIONS

The total voltage acting around the series circuit, Figure 3, is the vector sum of the individual voltages shown. If the mutual reactance, M, is the same for each coupler, the total voltage is equal to this mutual reactance, times I_d, the vector sum of the currents flowing into the bus.

For *internal fault* conditions, the I_d cur-

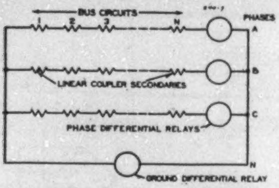

BUS CIRCUITS
1
2
3
N
244-7
PHASES
A
B
C
N
LINEAR COUPLER SECONDARIES
PHASE DIFFERENTIAL RELAYS
GROUND DIFFERENTIAL RELAY

Figure 7. Schematic one-line diagram of connections showing the use of linear couplers and relays for separate phase and ground protection

rent is equal to the fault current I_f, and the voltage induced in the loop is jMI_f. The secondary current flowing in the relay is obtained by dividing this induced voltage by the relay loop impedance. That is, for internal faults,

$$I_s = I_f(jM/Z_s) \qquad (1)$$

For *external fault* conditions since the vector sum of the currents flowing into the bus is zero, the net voltage induced in the loop circuit is zero, and consequently the relay current is zero. If the incoming and outgoing mutual reactances differ by three per cent, a relay current is obtained having a value three per cent of that which flows for an internal fault of the same magnitude. Thus, with couplers built to a standard mutual reactance within ±1.5 per cent, the mutual reactances of the incoming and outgoing couplers might differ by three per cent under the worst conditions. In order to maintain a 2:1 factor of safety, the relay should not be set below six per cent of the maximum through fault, since current values of three per cent are possible during external fault conditions when the relay should not operate. Table I shows the mutual reactance tolerances required to permit different ratios of maximum through fault to relay setting with a 2:1 safety factor.

SERIES CONNECTION—TRANSIENT CONDITIONS

Internal Faults. An asymmetrical primary current wave contains an a-c component and a d-c transient component, each of which produces a counterpart in the secondary loop circuit, having impedance $Z_s = r_s + j\omega L_s = r_s + jX_s$.

The secondary a-c component amplitude is M/Z_s times that in the primary, as shown for the steady-state analysis.

A primary d-c component of time constant, T, produces a secondary forced d-c component of the same time constant. Referring to Figure 2d, a current of time constant, T, encounters mutual impedance $-m/T$, secondary branch impedance $r_s-(L_s-m)/T$, and sum of branches or secondary loop impedance r_s-L_s/T. Hence, the initial forced d-c component in the secondary relay branch is $-(m/T) \div (r_s - L_s/T)$ times that in the primary.

For a primary current wave initially fully displaced, the initial primary d-c component is equal to the negative amplitude of the primary a-c component, and therefore, the ratio of their secondary components is obtained by dividing their transformation factors, given above. Recognizing that $\omega m = M$, $\omega L_s = X_s$ and that the components have opposite signs in the primary, this division gives:

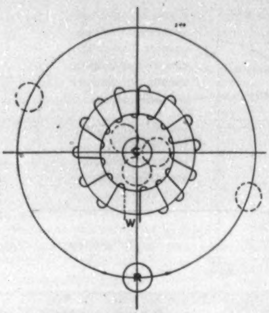
$$\frac{\text{Forced d-c transient initial magnitude}}{\text{Secondary a-c component amplitude}} = \frac{Z_s/r_s}{\omega T - (X_s/r_s)}$$

A positive value for this ratio signifies that the forced transient is in the same direction as the initial a-c component in the secondary.

In addition to the forced d-c transient, a free d-c transient current flows having an initial amplitude equal and opposite to the sum of initial a-c and forced transient initial amplitudes, thereby preventing a discontinuity. The free transient dies out with the time constant of the secondary loop circuit, $T_s = L_s/r_s$. The ratio X_s/r_s approximates unity for the linear-coupler circuits, resulting in T_s approximately 0.00265 second or one-sixth of a cycle.

The forced and free d-c transients decay and the a-c component reverses, as shown in Figures 5b and 5c until somewhat less than one-half cycle after the fault a positive peak is reached, which may be more or less than the steady-state value, depending on the magnitude and direction of the total d-c transient. Progressing further, the first negative peak is reached, and it can be greater than the steady a-c value if the total transient is negative at this point.

The values of the total d-c transient at the first positive and first negative peak are shown in Figure 5a for the X_s/r_s ratio involved in linear-coupler circuits. The maximum overshoot of the first positive peak occurs for a long primary time constant. It is 5.4 per cent for a time constant of 15.9 cycles as illustrated in Figure 5b. The maximum overshoot of a negative peak occurs for a short time constant of about 0.64 cycle (condition C, Figure 5a) and is 11.5 per cent. It is illustrated



C-CENTER CONDUCTOR
R-RETURN CONDUCTOR
W-TOROIDAL WINDING

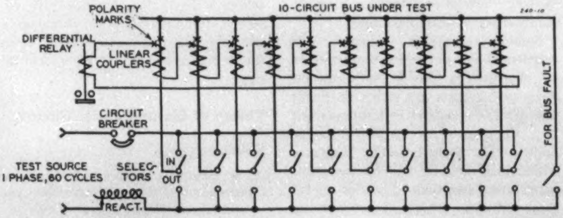
Figure 9. Positional effects of inner and return conductors on coupler winding

As the center conductor is moved into the various possible positions, the mutual inductance will vary slightly. This variation is eliminated in a design with fixed primary bar

As the return conductor is moved along the arc of a circle, the mutual inductance of it with respect to the torodial winding will vary, and will be substantially zero at two points

Figure 8. Type *LC* linear-coupler transformer

Figure 10. Arrangement of test bus for ten primary circuits and test diagram of primary and secondary circuits



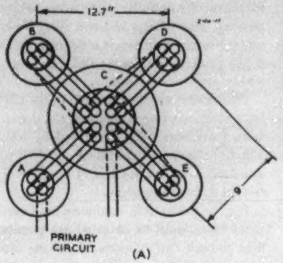
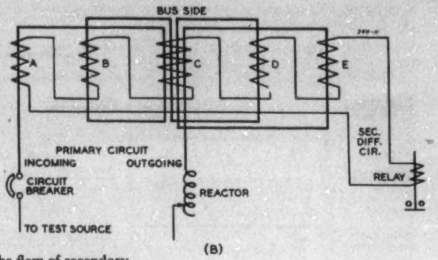


Figure 11. Five-circuit bus test diagram and arrangement to obtain long d-c time constants in fault current transient

A. Physical arrangement of couplers and primary winding

B. Test diagram of primary and secondary circuits



in Figure 5c. Succeeding cycles involve much less overshoot. In the range of time constants usually involved, neither overshoot is much over 5 per cent, as was verified by the tests. Practically this transient is negligible in making relay settings.

For larger ratios of X_s/r_s much larger percentage overshoots occur on the first cycle. Kennedy and Sinks[8] show 60 to 80 per cent overshoot at the first positive peak, with air-gap-type current transformers.

External Faults. The amount of overshoot is of particular importance in connection with high-speed relaying since the false differential through the relay during through faults is increased in direct proportion to it.

PARALLEL CONNECTION

The current in the relay for the parallel connection has been given.[6] With the nomenclature of Figure 4 it is

$$I_m = I_d \frac{jM}{Z_b + nZ_m}$$

where n is the number of circuits, that is, $n=4$ for the four-circuit bus, Figure 4. This current is zero for external faults and is a definite proportion of the fault current for internal faults. For this connection, it is necessary to have all secondary branch impedances equalized to a common value Z_b. Resistance differentials due to temperature differences must, therefore, be considered. Assuming a relay matched to the parallel impedance of the couplers, a 30-degree centigrade temperature differential between incoming and outgoing circuits would cause approximately eight per cent false differential on through faults.

The parallel connection has the advantage that it would trip correctly for an internal fault even with an inactive coupler open circuited. However, it is subject to the temperature errors mentioned above, and is not as readily supervised as the series loop arrangement. Also, even with equal mutual reactances the parallel connection involves the flow of secondary a-c and transient currents during through faults, whereas the series connection involves none. The simpler series loop arrangement is, therefore, preferred.

Relaying Schemes Using Linear-Coupler Transformers

A schematic one-line diagram of connections utilizing linear couplers and a sensitive relay is shown in Figure 1. The requirements of a suitable relay to be utilized in this scheme are as follows:

1. Since the amount of energy available is small, the relay must be quite sensitive. That is, the volt-amperes consumed by the relay at minimum pickup must be as low as possible.

2. Because of the small amount of energy available, the impedance of the relay should be matched to that of the linear couplers in order that maximum efficiency may be obtained. This involves making the relay impedance equal to the series impedance of all of the couplers with which it is used. There are variations in the self-impedances of the various coupler designs depending principally upon the space available. Also, there are variations in the number of circuits to a bus depending upon the application. For this reason the relay should have taps so that its impedance may be approximately matched to that of the linear couplers to suit the application.

3. Since the relay normally operates with no restraint at all, it should be shockproof.

A schematic internal diagram of the relay wiring is shown in Figure 6. Taps are shown on the primary of a transformer to provide the necessary impedance-matching characteristic. With a given setting on the relay element, the use of a tapped primary winding on the transformer supplying this element will obviously change the minimum tripping current of the relay as expressed in terms of current in the tapped winding. However, this does not change the amount of energy required to operate the relay as expressed in volt-amperes.

The minimum primary current upon which the scheme shown in Figure 1 will operate is determined by the mutual impedance of the linear couplers together with the total impedance of the secondary circuit, and the minimum pickup energy required by the relay. In any given application the minimum value may be determined from the known constants of the circuit by the simple calculations indicated by equation 1.

In determining upon a suitable relay design for this application, requirement 1 as listed above requires the most attention. Providing impedance taps as listed under requirement 2 is a simple matter. In making tests of the over-all scheme, two relay designs were provided which are not necessarily the last word. The first of these involves a simple plunger-type of overcurrent element of medium sensitivity which is inherently shockproof. A second relay was built around a polar-type relay element. This element required the use of a rectifying unit for its operation and operates reliably at very low energy. The sensitivity of the two elements is dealt with more fully under test results, but it is of interest to note here that for each relay the minimum pickup current as expressed in primary amperes was calculated with gratifying accuracy.

Separate Ground Relay. At the present time linear couplers have not been manufactured which would be sufficiently accurate to permit their use in an application where the range of maximum external fault current to minimum internal fault current is as great as 100/1, as sometimes occurs on high-impedance grounded systems. A possible means of expanding the range involves the use of a separate ground relay. The schematic diagram of connections for this is shown in Figure 7. This scheme anticipates the use of a ground relay set sufficiently low to detect the minimum internal ground fault. However, means must be taken to prevent the relay from operating for heavy external interphase faults. In other words, at the current magnitude experienced for heavy interphase faults, the variation in response of linear couplers may be suffi-

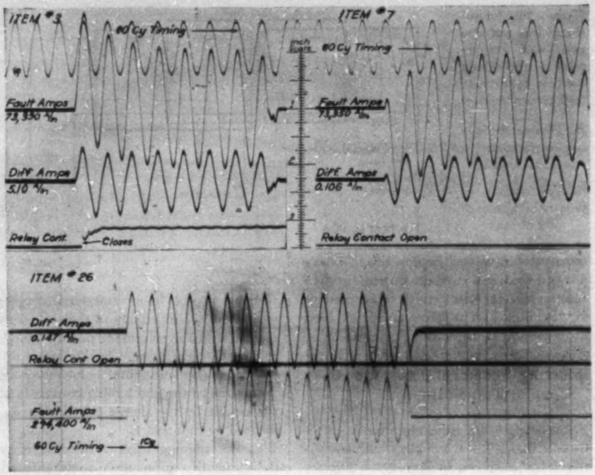


Figure 12. Oscillograms of pertinent fault tests

(*a*) For item 3 test, a 55,200-ampere bus fault showing high-speed one-quarter-cycle operation of plunger-type relay

(*b*) For item 7 test, a 50,800-ampere a-c component through fault with the couplers deliberately adjusted to give maximum error differential. It shows 1.61 per cent differential and no operation of the plunger-type relay (set to respond for a 2,450 ampere bus fault, 4.83 per cent of the 50,800-ampere through fault). While the fault current was initially 95 per cent offset with a short 1.2-cycle d-c time constant, the differential current shows negligible transient effect

(c) For item 26 test, a 125,000-ampere a-c component through fault initially 95 per cent offset with a long 8.2 cycle d-c time constant. It shows negligible transient effect in the differential current. The 0.89 per cent false differential did not operate the relay (set to respond for a 1,820-ampere bus fault, 1.45 per cent of the 125,000-ampere through fault)

cient to cause operation of the ground relay because of its sensitive setting.

The separate ground relay will be used only in those stations where there is an impedance in the station neutral connection limiting the phase-to-ground fault current on the bus to a value too low for the phase relays to detect.

The Linear Coupler Design and Construction

The linear coupler is simply an air-core mutual inductance, and as such may be designed and built in any of a large number of ways. The choice of method was selected after study of the required properties of the coupler:

(*a*) Accurate constant mutual inductance with respect to the primary circuit.

(*b*) Negligible mutual inductance to any external or neighboring circuit.

(*c*) A sufficiently high ratio of mutual inductance to internal impedance to obtain the power output required for the relay.

Requirements (*a*) and (*b*) practically demand that the secondary winding be a toroidal or ring winding, as this type of winding is practically without mutual inductance to external circuits and has constant mutual inductance to the primary circuit. Current in a secondary winding, uniformly wound on a nonmagnetic toroidal core approaches a uniform current sheet of finite thickness which produces only circular flux lines confined entirely within the winding and core. All this flux links a conductor passing anywhere through the opening of the core, and none of it links a conductor not passing through no matter how close it may be. The presence of neighboring iron outside of the winding has no influence since there is no magnetomotive force outside of the current sheet. Thus, the secondary winding has a definite mutual reactance with respect to a linking conductor and zero mutual reactance with respect to a conductor that does not pass through the opening.

While the mutual reactances have been visualized in terms of the flux linking the primary for current in the secondary, the relationship is reciprocal. Thus, for the ideal case of a perfectly uniform secondary winding, the voltage induced therein per ampere in the primary is a definite value if the primary conductor links the core, and is zero if it does not.

Practically, a close approach to the uniform sheet winding is realized, the departures therefrom being evaluated by test. The secondary is wound back on itself to avoid any single-turn effect of progressive spiraling around the core.

For bus protection, minimum fault currents which must be detected are usually high enough that economical designs can be made to deliver sufficient energy to the relay when only one primary turn is used. The practical design for bus protection, therefore, usually takes the general form of a through-type current transformer, with an air-core ring winding. Figure 8 shows a type for separate mounting.

The calculation of the mutual inductance and output capacity of the device is very easily worked out according to well-known fundamental formulas. The principal manufacturing problem is to make ring windings which are sufficiently perfect

1. To be free from induction from neighboring conductors.
2. To have a mutual inductance which departs from the exact required values by only a sufficiently small per cent for all possible positions of primary conductor.

These two requirements may each be expressed in terms of the variation from the desired value. The actual coupler will have a definite, though very small mutual inductance with the return conductor, which may be expressed in per cent of the mutual inductance to the primary conductor. This inductance will vary with the spacing and angular position of the return conductor, according to Figure 9, and will usually be zero for two positions and maximum for two other positions.

A final source of variation is error in calibration of the mutual inductance. In order to obtain mutual inductances within the necessary tolerances, adjustments are required after winding the coils. The linear couplers are tested by balancing them against a standard mutual inductance.

Considering all of the sources of variation as mentioned above, it appears quite practical at the present time to control the mutual reactances of couplers within ±1.5 per cent in commercial production.

Combination Tests

Combination tests were made to prove that it was practical and safe to use a simple overcurrent-type high-speed relay with linear couplers for differential protection, regardless of the number of circuits in the protected zone. This meant demonstrating that the couplers were linear in fact and practically unaffected by any stray fields produced by other circuits.

A bus setup accommodating 10 circuits, was made for test purposes as shown in Figure 10, in which the primary circuits were spaced at 12-inch centers. Four different types of 0.005 ohm, ±1 per cent linear couplers, having different dimensions, were available. One or more of each type were used in various combinations with their secondary windings in series with the relay to form bus differentials of 10, 6, and 2 circuits. Also, the parallel secondary connection of couplers was tested for a six-circuit bus to prove that the parallel connection was feasible, even if not as desirable as the series connection.

An exhaustive series of tests was made to cover the variations in relay type and sensitivity, in coupler size and shape, in fault-current magnitudes and transients, in fault-current distribution, and in the astatic factors. The astatic effects were checked:

1. By interchanging couplers on the primary circuits.
2. By rotating the couplers on their axis.
3. By placing the couplers off center with respect to their primary conductor.
4. By varying the distance from the coupler to the bus.
5. By turning the coupler upside down.
6. By placing magnetic materials between the couplers and in close proximity.

All these astatic effects were found small for spacings involved in practice.

A special setup per Figure 11, using multiple primary turns, was made to get the effect of large current with a smaller source current in order to obtain increased d-c time constant. It should be noted that each primary turn feeds in through one of the smaller outer couplers and returns through the larger center coupler. This arrangement simulates a five-circuit bus with four equal sources feeding to an external fault through the center coupler.

Test Results

Table II gives pertinent information and results of representative tests for 10-, 6-, and 2-circuit busses per Figure 10 and for the special five-circuit bus per Figure 11.

Items 1 to 7 apply for the 10-circuit bus per Figure 10 using the plunger-relay set to pick up at 0.085 ampere in the coupler secondaries. Calculations based on equation 1 give pickup for internal faults at

Table II. Summary—Linear-Coupler Bus Differential Test Results

Item No.	Bus				Fault		Induces I_fM Volts	Differential			Relay			Remarks
	No. of Circuits Total	In	Out	Idle	Type of	I_f Amperes		I_s Amperes	I_sZ_s Volts	% Diff.	Type and Pickup Amperes	Times Pickup	Contact in Cycles	
1	10 Per Figure 10	10	0	0	Internal Pickup	2,500	12.5	0.087		100	Plunger 0.085	1.02	5.05	Pickup I_f=2,450 test, 2,430 calculation
2		1	0	9		2,560	12.8	0.089		100		1.05	4.30	=2,450 test, 2,430 calculation
* 3		4	0	6	Internal High current	55,200	276	2.08		100		24.4	0.23	
4		5	0	5		103,600	518	4.20		100		49.4	0.16	
5		5	5	0	External	60,200	301	0.0082	1.08	0.36		0.096	None	Represents average % differential
6		4	1	5	External	57,000	285	0.0056	0.72	0.25		0.066	None	Normal low % differential
* 7		4	1	5	External	50,800	254	0.0310	4.09	1.61		0.365	None	Adjusted for maximum % differential
8	10 Per Figure 10	5	0	5	Internal Pickup	532	2.66	0.0180		100	Polar 0.0173	1.04	4.11	Pickup I_f=511 test, 509 calculation
9		1	0	9		532	2.66	0.0180		100		1.04	4.97	=511 test, 509 calculation
10		4	0	6	Internal High current	50,400	252	2.165		100		125	0.64	
11		5	0	5		100,000	500	4.620		100		267	0.64	
12		5	5	0	External	60,200	301	0.0068	1.12	0.37		0.393	None	Represents average % differential
13		4	1	5	External	56,400	282	0.0028	0.66	0.23		0.162	None	Normal low % differential
14		4	1	5	External	48,600	243	0.0240	3.46	1.42		1.39	1.72	Adjusted for maximum % differential
15	6 Modified Figure 10	3	0	3	Internal sensitivity	1,540	7.70	0.1360		100	Plunger 0.132	1.03	4.37	Pickup I_f=1,500 test, 1,530 calculation
16		3	1	2	External	51,200	256	0.0672	3.70	1.44		0.51	None	Adjusted for maximum % differential
17		3	0	3	Internal sensitivity	300	1.50	0.286		100	Polar 0.0279	1.02	5.45	Pickup I_f=294 test, 320 calculation
18		3	1	2	External	50,600	253	0.0688	3.44	1.36		2.47	1.18	Adjusted for maximum % differential
19	2 Modified Figure 10	1	0	1	Internal sensitivity	1,208	6.04	0.1360		100	Plunger 0.132	1.03	5.18	Pickup I_f=1,170 test, 1,140 calculation
20		1	1	0	External	50,600	253	0.1003	4.10	1.62		0.76	None	Adjusted for maximum % differential
21		1	0	1	Internal sensitivity	252	1.26	0.0280		100	Polar 0.0279	1.00	5.75	Pickup I_f=252 test, 246 calculation
22		1	1	0	External	49,200	246	0.1015	4.00	1.63		3.64	0.86	Adjusted for maximum % differential
23	5 Per Figure 11	5	0	0	Internal sensitivity	1,820	9.10	0.1140		100	Plunger 0.114	1.00	Yes	Ammeter test, I_f raised to pickup
24		4	1	0	Induced volts	20,000	100	0	0.89	0.89		0	None	Voltmeter test, secondary differential circuit open
25		4	1	0	External	77,800	389	0.0446	3.57	0.92		0.39	None	97% offset, T_{dc}=8.7 cycles
* 26		4	1	0	External	125,000	625	0.0693	5.54	0.89		0.61	None	95% offset, T_{dc}=8.2 cycles
27		4	1	0	External	216,000	1,080	0.1210	9.68	0.90		1.06	1.65	

*Denotes oscillogram shown in Figure 12.

2,430 amperes. Test item 1 with 10 circuits energized, and item 2 with only one circuit energized, both give pickup at 2,450 amperes when the fault current is proportioned for unity pickup. Based on ±1 per cent tolerance in the couplers this sensitivity should be safe for through-fault currents of 25 times 2,450 or 61,000 amperes. Items 5 to 7 for external faults show that it was safe as the relay current remained below half pickup value. In items 6 and 7 the fault current into the bus was supplied by four circuits and out from the bus through one circuit with the five remaining circuits "idle" representing feeders with no feed back. Item 6 shows normal low (0.25 per cent) differential and item 7 the maximum (1.61 per cent) differential obtainable after deliberately adjusting the coupler positions for maximum astatic effects. The oscillogram, Figure 12b, for item 7, shows a fault current of 50,800 amperes rms a-c component, I_f, plus 68,300 amperes initial d-c component decaying with a short-time constant of 1.2 cycles; and a secondary differential current, practically free of d-c transient, having an a-c component, I_s, of 0.031 ampere which gave no relay operation. These measured data and additional derived data are given in the tabulation. Based on a 0.005-ohm mutual (five volts induced per 1,000 amperes) the 50,800-ampere fault should induce +254 volts total in the incoming-circuit couplers and −254 volts in the outgoing-circuit coupler, which would leave no differential voltage to circulate secondary current. Actually, 0.0310 ampere flowed which multiplied by the loop impedance, Z_s, indicated a differential of 4.09 volts or 1.61 per cent. The relay current, I_s, equals 0.0310 ampere and represents 0.365 times its pickup value of 0.085 ampere. Oscillogram, Figure 12a, for item 3, shows one-fourth cycle operation of the plunger-type relay for a 55,200-ampere high-current internal fault.

Items 8 to 14 also apply for the 10-circuit bus but show the performance of the more sensitive polar-type relay having a sensitivity of 511 amperes. Items 15 to 18 cover a six-circuit bus arrangement. Items 19 to 22 are for a two-circuit arrangement and were included to show the maximum obtainable differential.

The performance of the five-circuit bus arrangement, Figure 11, for long d-c time constant is shown in items 23 to 27. The fault current I_f tabulated is the equivalent value based on a single primary conductor. For item 23, the center-coupler secondary was deliberately reversed to represent an internal fault and the current increased gradually until the relays just operated at 1,820 amperes. The center-coupler secondary connection was then restored to normal, and the induced and differential voltages measured with the secondary circuit open. These induced voltage tests, item 24, showed 0.89 per cent differential over a wide range of steady-state currents, and this percentage differential was substantiated by subsequent transient-fault tests, items 25 to 27. This, therefore, illustrates the possibility of testing an actual bus installation with low steady-state currents to determine the performance to be expected under fault current. Figure 12c shows the oscillogram for item 26, a 125,000-ampere a-c component through fault (representing 69 times relay sensitivity) which was initially 95 per cent offset, involving a 170,000-ampere d-c component decaying slowly with a time constant of 8.2 cycles or 0.137 second. Again the differential current is not offset but quite symmetrical, and the free transient as shown at the end of the fault lasts only a small portion of a cycle. A comparison with Figure 12b for item 7 shows that the d-c component and its time constant have a quite negligible effect on the differential current. Based on the differential current of item 26, showing 0.61 of relay pickup for a 125,000-ampere through fault, relay operation is expected for through faults in excess of 125,000/0.61 = 205,000 amperes; and item 27 shows relay operation at 216,000 amperes with 1.06 times relay pickup, verifying the 205,000 ampere through-fault pickup.

Summarizing, the primary current required to pick up the relay for bus faults was found to be almost precisely as calculated and showed no perceptible transient effect on pickup or speed of operation as a result of d-c component current. The pickup depended only on fault-current magnitude and was independent of its distribution. For external faults the secondary differential was found to average about one fourth of the tolerance band. It did not exceed the tolerance band even when a deliberate attempt was made to get maximum astatic effect. The tests, therefore, verified the fact that the maximum differential for through faults can be calculated with assurance. Above all, the staged fault tests demonstrated that the d-c transients, which are so troublesome with current transformers because of the d-c saturation effects in the iron, have a very negligible effect on the coupler-differential performance, and therefore, it is quite feasible to test an actual bus installation by circulating low-value steady-state current.

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Facilities for the Supply of Kilowatts and Kilovars

HOLLIS K. SELS
MEMBER AIEE

THEODORE SEELY
ASSOCIATE AIEE

Synopsis: Increased system kilowatt capacity may be realized by the reduction of generator kilovar requirements and the provision of reactive capacity sources at other points in the system. The factors to be considered in the choice of various reactive sources are discussed. Benefits to be realized from the various reactive sources are described. The paper is based upon system plan studies of a large eastern utility.

IN determining a system's capacity requirements for supplying the electric load—kilowatts and kilovars under all conditions of operation throughout the year—a comprehensive capacity and load study is necessary. This was particularly emphasized by two operating experiences on the system of the Public Service Electric and Gas Company. The first episode was a system voltage disturbance which occurred on October 30, 1938, and the second a system shutdown which occurred as a result of a 132-kv bus fault at Roseland switching station on July 11, 1940. Investigations of these operations introduced the following subjects for consideration:

1. The proper kilowatt and kilovar loading of individual generators, considering their economy, thermal, and stability characteristics.
2. The amount and distribution of various forms of kilovar capacity throughout the system.
3. Certain improvements in system protection, particularly with respect to more rapid fault removal and the maintenance of adequate backup protection.
4. Installation of certain tap-changing-under-load equipment to maintain bus voltages within certain limits and to make all generator kilovar capacity available to the system at all times.
5. And finally a more thorough analysis of the kilowatt and kilovar loads on the system.

Studies have progressed to such a point that a fairly complete report can be made of the conclusions which have been reached. Continuing studies may lead to other equally important conclusions, but, in general, they will probably be only refinements of the conclusions now reached. The application of these conclusions is particularly important at this time with the present scarcity of materials and the necessity for added capacity to meet greatly increased industrial loads.

General Description of System

A description of the Public Service Electric and Gas Company system is necessary as some of the results of the studies presented may hold for this system alone; similar studies would have to be conducted on other systems to reach corresponding conclusions. The layout of this system is not unlike many others but the load for the 1941 hourly integrated peak, which was 852,100 kw, is probably more concentrated than most, being spread over an area of approximately 1,400 square miles. This area is roughly rectangular in shape, and about 100 miles long.

Five generating stations supply the territory, three of which, Essex, Kearny, and Marion, are located in the Newark meadows area. Four stations, Essex, Kearny, Marion, and Burlington, are connected together by a 132-kv bulk power system; Perth Amboy, the smallest and least important station, feeds directly into the subtransmission system supplying central New Jersey. Three stations, Essex, Marion, and Burlington, also supply local subtransmission systems.

Other subtransmission areas are supplied by Hudson, Athenia, West Orange, Metuchen, Trenton, and Camden switching stations, which are connected to the bulk power system. In addition, the Roseland switching station connects the Public Service system to the Pennsylvania-New Jersey 220-kv system and the New Jersey Power and Light Company 110-kv system. Several other small interconnections are tied to the various subtransmission networks.

Figure 1 shows diagrammatically the location of the generating stations and switching stations, and the layout of the bulk supply and subtransmission lines.

All generators are normally hand-regulated and controlled under orders of a central load dispatcher. Several large synchronous condensers and frequency changers are located at switching stations and several small synchronous condensers and numerous small motor-generator sets which can be overexcited are located in substations. The larger synchronous condensers provide automatic regulation to some degree. Practically every four-kilovolt distribution circuit is provided with ± ten per cent or ± five per cent induction or tap-changing regulators and many circuits with low power-factor load are equipped with one or more banks of static capacitors. There are 708 four-kilovolt distribution circuits; 143 of these are entirely underground, 28 are more than 50 per cent underground and 87 are less than 50 per cent underground and the remaining 450 circuits are practically all overhead. There are 506 radial distribution circuits, 109 pure multiple-network circuits and 93 combination multiple-network and radial circuits supplying 90 separate networks.

Kilowatt Supply

In purchasing new units, it has been the practice to purchase turbines which have sufficient capacity to deliver full generator kilovolt-amperes output at 100 per cent power factor. Therefore, it has been possible to rerate the kilowatt output of the turbine-generators originally purchased on an 80 per cent or 85 per cent power-factor basis by

1. Increasing the power-factor and kilowatt rating of the generator and in some cases the power output of the turbine.
2. Taking advantage of any increased rating developed on field tests.
3. Using autotransformers to step up the voltage rating of the generator thereby gaining increased kilowatt, kilovolt-ampere, and power-factor ratings.

Table I shows the present turbine-generator capacities as of December 8, 1941, totaling 919,550 kw which is greater than the original kilowatt ratings by 20 per cent primarily due to increases in power-factor ratings. Work is now going forward on the installation of autotransformers on Kearny units 2 and 4 which will increase their kilowatt rating from 47,250 kw at 90 per cent power factor to 54,300 kw at 95 per cent power factor each.

Appreciating that these increased ratings approach the economy, thermal, and stability limitations of the machines, extensive studies and tests have been carried out to determine the values of these limitations. After an analysis of the various

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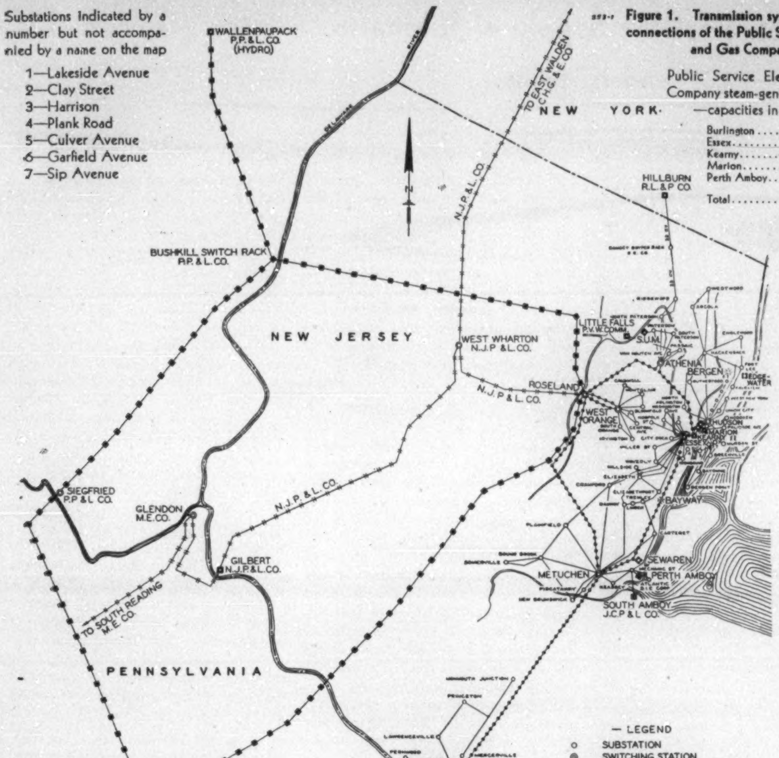
HOLLIS K. SELS is transmission and substation engineer, and THEODORE SEELY is assistant distribution engineer, both with the electric department of the Public Service Electric and Gas Company, Newark, N. J.

Figure 1. Transmission system and interconnections of the Public Service Electric and Gas Company

Public Service Electric and Gas Company steam-generating stations—capacities in kilowatts

Station	Capacity
Burlington	180,000
Essex	243,000
Kearny	325,500
Marion	150,800
Perth Amboy	20,250
Total	919,550


Substations indicated by a number but not accompanied by a name on the map
1—Lakeside Avenue
2—Clay Street
3—Harrison
4—Plank Road
5—Culver Avenue
6—Garfield Avenue
7—Sip Avenue
WALLENPAUPACK P.P.&L.CO. (HYDRO)
NEW YORK
TO EAST WALDEN C.H.G.&E.CO
N
N.J.P.&L.CO.
HILLBURN R.L.&P.CO.
BUSHKILL SWITCH RACK P.P.&L.CO.
NEW JERSEY
WEST WHARTON N.J.P.&L.CO.
LITTLE FALLS P.V.W.COMM.
SUM.
ATHENIA
BERGEN
EDGEWATER
ROSELAND
N.J.P.&L.CO.
WEST ORANGE
HUDSON
MARION
KEARNY
ESSEX
BAYWAY
SIEGFRIED P.P.&L.CO.
GLENDON M.E.CO.
N.J.P.&L.CO.
GILBERT N.J.P.&L.CO.
TO SOUTH READING M.E.CO.
SEWAREN
METUCHEN
PERTH AMBOY
SOUTH AMBOY J.C.P.&L.CO.
PENNSYLVANIA
TRENTON
EMILIE P.E.CO.
PLYMOUTH MEETING P.E.CO.
BURLINGTON
WESTMORELAND P.E.CO.
RICHMOND P.E.CO.
TO CONOWINGO P.E.CO. (HYDRO)
DELAWARE P.E.CO.
SCHUYLKILL P.E.CO.
CAMDEN
NEW JERSEY
PENNSYLVANIA
CHESTER P.E.CO.
DELAWARE
CHRISTIANA D.P.&L.CO.
DEEPWATER D.L.&P.CO. A.G.&E.CO.
LEGEND
SUBSTATION
SWITCHING STATION
FUTURE SWITCHING STATION
GENERATING STATION
FUTURE GENERATING STATION
13 KV AND 26 KV LINES
66 KV LINES
110 KV LINES
132 KV LINES
FUTURE 132 KV LINES
220 KV LINES

machine limits, Table II was prepared to give the kilowatt and kilovar capacity figures for 1941 under various conditions. The "summer normal" ratings are based on sufficient reduction in kilowatt output to obtain the maximum kilovar output with full kilovolt-ampere output. This is illustrated by the generator-output chart of Figure 2. These charts are not available for all machines so that some of these ratings are subject to some adjustment after further study and tests.

On the generator-output chart, the vertical line at the right represents the nominal output of the turbine in kilowatts. The quarter circle represents the kilovolt-ampere rating of the generator while the almost horizontal line represents the kilovar limit of the generator as determined by the maximum possible field current. The heavy line comprised of the vertical line, possibly a segment of the quarter circle, and the almost horizontal line represent the actual kilowatt- and kilovar-output limits of the generator. The important factor to note is that the full kilovolt-ampere output of the gener-

Table I. Turbine-Generator Capacities as of December 8, 1941

	Unit No.	Kva	Kw	Power Factor
Burlington	1	13,703	12,333	0.90
	2	13,703	12,333	0.90
	3	13,704	12,334	0.90
	4	22,500	18,000	0.80
	5	144,000	125,000	0.87
	5	207,610	180,000	
Essex	1	25,000	22,500	0.90
	2	25,000	22,500	0.90
	3	44,444	40,000	0.90
	4	40,000	36,000	0.90
	5	40,000	36,000	0.90
	6	40,000	36,000	0.90
	7	58,825	50,000	0.85
	7	273,269	243,000	
Kearny	1	44,444	40,000	0.90
	2	52,500	47,250	0.90
	3	44,444	40,000	0.90
	4	52,500	47,250	0.90
	5	44,444	40,000	0.90
	6	100,000	90,000	0.90
	1 mercury	25,000	21,000	0.84
	7	363,332	325,500	
Marion	1	12,632	12,000	0.95
	2	28,000	26,600	0.95
	4	10,000	9,000	0.90
	5–25 cycles	9,000	9,000	1.00
	6	9,000	8,100	0.90
	7–25 cycles	9,000	9,000	1.00
	8	9,000	8,100	0.90
	9	20,000	19,000	0.95
	10-high pressure	62,500	50,000	0.80
	9	169,132	150,800	
Perth Amboy	1	5,000	4,500	0.90
	2	5,000	4,500	0.90
	3	12,500	11,250	0.90
	3	22,500	20,250	
Total	31	1,035,843	919,550	
60 cycles	29	1,017,843	901,550	
25 cycles	2	18,000	18,000	
Steam	30	1,010,843	898,550	
Mercury	1	25,000	21,000	

Table II. Public Service System Kilowatt and Kilovar Capacities—1941

		Summer Normal		Winter 0.90 Power Factor Also, Summer Continuous Emergency		Winter 0.95 Power Factor Where Possible		Summer and Winter, Kilovars at 0 Kw
		Kw	Kilovars	Kw	Kilovars	Kw	Kilovars	Kilovars
Burlington	1					12,333	5,967	
	2					12,333	5,967	
	3					12,334	13,500	
	4					18,000	13,500	
	1-2-3-4	50,000	33,000	55,000	31,400	55,000	31,400	38,000
	5	125,000	70,000	125,000	70,000	125,000	70,000	94,000
	Station	175,000	103,000	180,000	101,400	180,000	101,400	132,000
Essex	1	20,000	15,000	22,500	10,900	23,750	7,800	16,000
	2	20,000	15,000	22,500	10,900	23,750	7,800	16,000
	3	38,000	23,000	40,000	19,400	40,000	19,400	26,000
	4	33,000	22,000	36,000	17,400	37,500 (*A*)	13,900	26,000
	5	33,000	22,000	36,000	17,400	37,500 (*A*)	13,900	26,000
	6	33,000	22,000	36,300	17,400	37,500 (*A*)	13,900	26,000
	7	50,000	31,000	50,000	31,000	55,000	19,600	37,000
	Station	227,000	150,000	243,000	124,400	255,000	96,300	173,000
Kearny	1	36,000	26,000	40,000	19,400	40,000	19,400	31,000
	2	45,000	27,000	47,250	23,000	49,900	16,400	34,000
	3	36,000	26,000	40,000	19,400	40,000	19,400	31,000
	4	45,000	27,000	47,250	23,000	49,900	16,400	34,000
	5	36,000	26,000	40,000	19,400	40,000	19,400	31,000
	6	90,000	44,000	90,000	44,000	90,000	44,000	52,000
	1 mercury	20,000	15,000	21,000	13,300	21,000	13,300	20,000
	Station	308,000	191,000	325,500	161,500	330,800	148,300	233,000
Marion	1	10,000	8,000	12,000	4,000	12,000	4,000	9,000
	2	22,500	12,000	26,600	9,000	26,600	9,000	15,000
	4	9,000	4,400	9,000	4,400	9,500	3,120	6,000
	5–25 cycles	9,000	0	9,000	0	9,000	0	0
	6	8,100	3,900	8,100	3,900	8,550	2,800	6,000
	7–25 cycles	9,000	0	9,000	0	9,000	0	0
	8	8,100	3,900	8,100	3,900	8,550	2,800	6,000
	9	17,000	10,000	19,000	6,000	19,000	6,240	12,000
	10-high pressure	45,000	43,000	50,000	37,500	59,300 (*B*)	19,500	47,000
	Station	137,700	85,200	150,800	68,700	161,500	47,460	101,000
Perth Amboy	1			4,500	2,180			
	2			4,500	2,180			
	3			11,250	5,450			
	Station	18,000	13,000	20,250	9,810	20,250	9,810	16,000
Total all stations		865,700	542,200	919,550	465,810	947,550	403,270	655,000
Firm reactive capacity			260,900		260,900		260,900	262,900
System total		865,700	803,100	919,550	726,710	947,550	664,170	917,900

A—Essex 4, 5, 6 can deliver 37,500 kw each only if Essex 7 is in operation. With the low-pressure boilers only, there is insufficient steam-pipe capacity to these units.

B—Depends on ability of low-pressure units to absorb steam from high-pressure unit 10.

ator is not available at all power factors; Public Service system machines have ratings as synchronous condensers of only about 65 per cent of their kilovolt-ampere rating.

Since stability ratings are relative values, the criterion assumed for the transient-stability studies of individual machines was that the phase-angle displacement for a selected rating should not exceed 50 per cent of the pull-out angle in the time required to clear a fault at the machine terminals. The studies show that the phase-angle displacement is much more dependent on the turbine power output than on the generator power factor. Therefore, generator power-factor limits have been raised to 95 per cent which are shown in Table II.

The kilowatt ratings given are within all limitations on all condensing turbine-generators. In the cases of the noncondensing units (that is, the superposed machines) the ratings given are within all limitations except the stability limit for faults on the 13,200-volt bus to which the particular machine is connected. Therefore, during lightning or sleet storms when system trouble is frequently experienced, it may become desirable to favor the loading of these units by shifting the load from them to less efficient units normally operating in reserve.

So far consideration has been given only to the limitations in the electrical plant, but the use of superposed units and large high-pressure boilers introduces the factor of boiler capacity into the kilowatt supply problem. Low-pressure boilers connected to a main header with one spare boiler installed for a group of ten or so were desirable and economical. However, with high-pressure high-capital-cost boilers which should require cleaning only once in six months, the economy of a spare boiler is questionable. Therefore, a large high-pressure boiler outage generally makes unavailable some high-pressure turbine capacity due to this lack of flexibility in the boiler plant. The tables shown in this paper do not make allowances for both boiler and generator outages in different stations. This problem requires further study and a definite conclusion regarding the relations of the boiler plant to system capacity.

Kilovar Supply

Table III shows the present-system reactive capacity other than in generators as of December 1, 1941. Some of this capacity is considered as either a part of the load or nonfirm; 260,900 kilovars is firm capacity, which is considered the

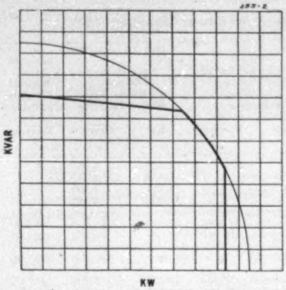


Figure 2. Generator output chart showing typical relative kilowatt and kilovar capacity

same as generator capacity. This has been installed largely to correct power factor and relieve overloaded or low-voltage conditions as they have developed individually. Recently, as the static-capacitor program developed, the broader picture of kilovar supply has been studied so as to co-ordinate the entire program. Prompted by the desire to obtain increased kilowatt generator ratings to meet the recent greatly increased industrial loads, the past conception of carrying the major part of the kilovar load on the generators has had to be abandoned in favor of kilovar capacity installed at the load. The problem has therefore developed into the manner in which this could be accomplished with the greatest speed and lowest cost.

Kilovar capacity can be provided by the use of

Extra copper in the generator stator and field windings

Synchronous condensers

Oversize synchronous motors on motor-generator sets

Static capacitors

Reactive capacity on customers' premises

The use of reactive capacity in one form or another and in appropriate locations, can accomplish the following results in addition to the obvious one of carrying reactive load:

Control voltage (raise only, with static capacitor)

Reduce kilvolt-ampere load between the capacity and the generator

Reduce system investment

Reduce system losses due to kilowatts and kilovars

An analysis has been made of the system costs to determine those elements which are affected by low power factor, starting with the generator as a base and extending to the first load point of the distribution circuit. In the generating station, only the cost of the generator and its switching equipment has been assumed to vary. In switching stations and substations, only transformers and switching equipment have been considered, omitting land, building, masonry compartments, and switchboard. In bulk supply lines, rights of way have been omitted, and in subtransmission and distribution circuits rights of way, poles, and conduits have been neglected. On this basis the incremental investment per kilovolt-ampere which varies with the load power factor has been determined.

The relative net cost of installing kilovar capacity in generators, large synchronous condensers in switching stations, small synchronous condensers in substations and static capacitors on distribution circuits has been determined, evaluating the cost of the equivalent kilovar capacity, the added system investment component effected by kilovar loading, and the incremental losses. These computations show that

1. The static-capacitor installations are the most economical because they are close to the load.

2. The small added cost of providing reactive capacity in generators is next least expensive even with the added system investment and no saving in losses.

3. Adding capacity in the form of synchronous condensers wherever located is the most expensive of all due to their higher initial cost.

Of course, these conclusions apply only to the Public Service system or to those cases where the breakdown of system costs is nearly the same. Other situations would have to be analyzed similarly to arrive at a proper answer.

The limit on the amount of static capacitors which can be installed without switching depends upon the kilovar load at light-load periods, the permissible generator power factor, and the limitations on the distribution system. To determine the maximum amount of static capacitors which can be installed, the annual kilovar load duration curve for the Public Service system was estimated for 1942 based on past records and is shown in Figure 3. This figure shows a minimum net 250,000-kilovar load including the effects of the inherent reactive capacity in the system; however, an investigation of the possible locations showed that the capacitor installations should be limited to 150,000 kilovars effective at their location. At peak loads this would have the effect of reducing the kilovar load on generators by nearly 200,000 kilovars. It is felt that further experience may show that the distribution plant can absorb more of this capacity without encountering the problem of picking up the system after a major shutdown, and also without experiencing a self-excitation problem on lightly-loaded generators. After the limit of static capacitors is reached, if additional reactive capacity cannot be provided economically in new generators or maintained in existing generators, because of the value of their kilowatt output, synchronous condensers offer a quick and economical way in which kilowatt generation can be obtained to meet unexpected demands. The higher over-all cost of large synchronous condensers may be partly offset by the convenience of their operation and their stabilizing effects.

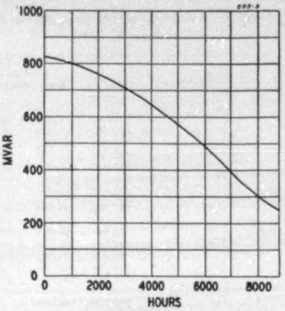


Figure 3. Annual megavar load-duration curve for 1942

Static Capacitors

Provision of reactive capacity for system purposes in the distribution plant, in the form of static (shunt) capacitors, in addition to improving system conditions with respect to increased kilowatt and kilovar capacities and reduced system losses, affords marked advantages in the distribution plant itself in the form of postponement of facilities, improvement of voltages, and decrease of losses. These features of the shunt capacitor have been so well-publicized that extended comment appears unnecessary. It should be pointed out, however, that when system requirements dictate such installation of shunt capacitors, the location within the associated part of the distribution system may be so selected that optimum concurrent distribution benefits are achieved. Since the major justification of such installation lies with the system requirements, some expansion of the natural field

Table III. System Reactive Capacity
(Other Than in Generators)

	Kw	Kvars	
Considered as Firm Capacity			
Synchronous condensers			
Athenia switching station		40,000	
Roseland switching station		30,000	
Trenton switching station		15,000	
Garfield substation		5,000	
Gloucester substation		4,000	
Hoboken substation		5,000	
Olden Avenue substation		5,000	
			104,000
Railway motor-generator sets with oversized motors			
Norfolk Street substation		5,000	
Plainfield substation		4,900	
			9,900
Frequency changers			
Marion generating station	0	32,000	
	16,000	30,000	
	18,000	28,000	30,000 average
Static capacitors on distribution lines		99,000	
			99,000
220-kv transmission system			
Public Service share of capacitance		18,000	
			18,000
Total firm reactive capacity			260,900*
Not Considered as Firm Capacity			
Frequency changer			
Metuchen switching station (available at times on application to Pennsylvania Railroad and Philadelphia Electric companies			20,000
Total nonfirm reactive capacity			20,000
Reactive Capacity Considered as Part of the Load			
25 railway motor-generator sets		29,000	
			29,000
Bulk transmission system			
132-kv cable 9 miles		35,000	
132-kv open wire miles		22,900	
			57,900
Subtransmission system			
13/26-kv cable 554 miles		27,500	
13/26-kv open wire 638 miles		1,500	
			29,000
Total Reactive Capacity in Load			115,900

*Plus or minus 2,000 depending on load on Marion frequency changer.

Table IV. Comparison of 1942 Summer Loads and 1941 Summer Normal Capacity

	All in		One out		Two out	
	Megawatts	Megavars	Megawatts	Megavars	Megawatts	Megavars
Load	818	825				
Less trolley bus	− 11	0				
Net load	807	825	807	825	807	825
Installed capacity	866	803	866	803	866	803
Loss of first unit			−125	− 70	−125	− 70
Loss of second unit					− 90	− 44
Interconnection	0	0	0	0	0	0
Unavailable reserves	0	− 37	0	− 37	0	− 37
Net Public Service capacity	866	766	741	696	651	652
Total Public Service reserve and excess Public Service capacity	59	− 59	− 66	−129	−156	−173
Authorized 1942 program		150		150		150
After completion of program	59	91	− 66	21	−156	− 23

of the shunt capacitor is warranted in the realization of minor distribution economies. Under present long-delivery schedules for equipment, the capacitor may well be employed as a temporary expedient for overload conditions without extreme penalty, because of its low installation cost and high mobility. Furthermore, long-range economy of the capacitor versus other forms of plant addition may now indicate the capacitor as the proper solution because of the temporary nature of certain loads now being taken on and the high salvage value which is realized in the capacitor.

The experience gained from some 125,000 kva-years of shunt capacitor operation may be of interest to those who are as yet undecided as to the desirable characteristics of capacitors. On the Public Service system bulk capacitors are installed in 180-kva, three-phase banks, using 12 individual 15-kva units for each installation. The banks are wye-connected, with mid-point solidly connected to circuit neutral. Indicating-type cutouts are used on phase connections, phase and neutral lightning arresters are usually employed, and capacitor cases are grounded.

No case of harmonic resonance has as yet been encountered with shunt capacitors. Some instances of telephone inductive interference on common-neutral circuits equipped with capacitors have required use of neutral reactor on the capacitor bank; but the cost of such corrective equipment is not regarded as sufficient to prove out the capacitor for its peculiar purposes.

The operating record of all capacitor units on the Public Service system is regarded as highly satisfactory. Of a total of some 5,600 15-kva units, 25 units have been removed from the lines for various reasons. Of this number, 17 units failed electrically after service periods of from one month to two years. The remaining eight units developed mechanical difficulties, such as bulged tank, leaking bushing, or open tank seam, requiring removal of the unit for repairs.

Improvements in System Protection

Recognizing that markedly increasing the kilowatt rating of generators has decreased their original margin of stability, it has become necessary to adopt several measures for improving the system protection. Many stability studies have shown that the best means of increasing the stability of a system, aside from eliminating faults or restricting them to a single phase, is rapid fault removal. Reference has already been made to the slight improvement in stability with higher generator field strengths or lower power-factor operation of high-speed low-inertia superposed generator units. A careful review of the system has been made which has produced the following recommendations:

1. Rebuilding of overstressed circuit breakers for higher speeds and greater capacities.
2. Reinsulating of certain station busses and lines with post-type insulators.
3. Reconstruction of a major part of the open-wire subtransmission system to a protected phase design.
4. Installation of automatic-generator field control to insure the proper maintenance of adequate field strengths at all times.
5. Installation of high-speed carrier and pilot-wire relays in the bulk supply lines and more important subtransmission lines.
6. Installation of voltage-controlled system-segregating relays to act as backup protection on prolonged system disturbances.
7. Splitting of 132-kv system into separate or higher impedance systems by operating with sectionalized busses at certain locations.
8. Continuance of bus differential protection on all principal busses under all conditions of operation.

Tap-Changing Equipment

Following the normal practice of hand field regulation on generators and carrying as much of the kilovar load on the generators as is possible or economical may lead to excessive voltage gradients in the transmission and distribution system. Also faced with the necessity of maintaining certain voltage limits on switching station, substation, and high-voltage customer busses and holding within a ±3 per cent voltage variation on regulated primary and secondary customers' services, it becomes necessary to install either synchronous condensers in substations or tap-changing-under-load equipment on certain step-up and step-down transformers even though the generator busses are regulated higher during heavy-load periods and lower at light loads. In some cases tap-changing equipment is cheaper, if sufficient kilovar capacity is available, and the kilovar load can be transmitted without incurring too much loss.

In fact, tap-changing equipment is necessary to some extent to avoid building up excess reserves of kilovar capacity. Network-analyzer load-flow studies have demonstrated that unavailable kilovar capacity may be as much as ten per cent of the installed capacity, and the installation of tap changers in transformers between generating stations can frequently make much of this capacity available and still meet given bus-voltage limitations. Therefore this type of equipment may avoid the installation of kilovar capacity by keeping the unavailable kilovar capacity to a minimum and maintaining customers' service voltages within given limits when sufficient kilovar capacity is available in generators or condensers at other locations. It is, therefore, possible to install kilovar capacity at the most advantageous location which will release other capacity for the benefit of other locations. The installation of a ±5 per cent tap-changing transformer at Essex generating station will release approximately 25,000 kilovars of excess station capacity to the system and at the same time will improve the 132-kv system voltage conditions.

Analysis of Loads and Comparison With Capacity

There must be sufficient power and reactive capacity (kilowatts and kilovars) in service to handle the system loads under all reasonable conditions. It is already obvious that the power capacity is provided by turbine generators. In the Public Service system there is a slight amount available in interconnection diversity entitlements and load curtailment obtainable by operating all-service busses on their gasoline engines. The maximum power capacity is a relatively definite figure, being fixed by the outputs of the various turbines as shown in Table II.

The reactive capacity consists of the excess capacity in generator stator and field windings and the capacities of synchronous condensers and motors and static capacitors, plus a proportionate share of the capacitance in the 220-kv system. The maximum reactive capacity is a relatively indefinite figure since the excess capacity in generators is highly variable depending on the kilowatt load carried as shown in Figure 2. According to the power output of the generators and their power factors, their reactive outputs may vary through an overall range of 0 per cent up to 65 per cent of their kilovolt-ampere ratings.

As with kilowatts the installed kilovar capacity on the system must be greater than the arithmetic sum of the kilovar loads and the assumed reserves except that

1. Generator kilowatt outputs and bus-voltage limitations make it impossible to distribute the kilovar load among the sources exactly.

2. When kilowatts are being taken from an interconnection during an emergency, it must be possible to supply kilovars to that interconnection in order to maintain a reasonable voltage.

Therefore, a greater percentage of kilovar reserves is required than for kilowatt reserves.

Kilowatt and kilovar loads are determined by the reading and summation of all sources of this capacity considering the losses in the transmission and distribution system as a part of the load. The kilovar losses in the system are partially offset by the inherent reactive capacity in the system considered as a part of the load. Kilowatt and kilovar quantities are metered directly because power-factor meters and kilovolt-ampere readings do not permit a sufficiently accurate determination of these quantities. System load data for Public Service Company are estimated for 1941 and 1942 as shown in Table A.

Table A

Condition	1941		1942	
	Megawatts	Megavars	Megawatts	Megavars
Annual peak, December (5–6 p.m. hourly integrated)	878	627	965	692
Winter day	742	709	817	777
Summer day	734	739	818	825

Every additional kilowatt of industrial load (day load) has associated with it approximately one kilovar of reactive load; this reactive load must be carried just as surely as the power load. Therefore, every kilowatt of turbine capacity added to the system must have added with it one kilovar of reactive capacity for industrial load and about one-half kilovar for residential load. Since reactive loads cannot be economically transmitted as far as power loads, this means that reactive capacity needs to be selected and located more carefully than power capacity, generally closer to the load.

It is evident from the system load data that the lower day load power factor requires a portion of the kilowatt capacity over the December peak to handle the succeeding heavy summer kilovar load. Also the December peak kilowatt load does not greatly exceed the following summer kilowatt load. Therefore in any given calendar year when the load is steadily growing the required installed capacity is determined when maintenance schedules are the heaviest and failures and system disturbances are most likely to occur, that is during the summer period.

Consideration has been given to kilowatt- and kilovar-load reduction by reducing system voltage, but since system- and customer-voltage regulation is obtained by feeder regulators installed in substations, the system voltage reduction would not take effect until the regulators had locked out. Since system stability is also rapidly decreased with system voltage, and tests have shown that a reduction in voltage reduces the load almost proportionately, this scheme of load reduction is not considered feasible or desirable. Hence the problem reduces to one of providing adequate facilities. If, for any reason these facilities are not sufficient, load must be dropped or customers must be called upon for load reduction.

A comparison of 1942 summer load and summer normal capacity is shown in Table IV for the Public Service system with one and two units out of service. Based on such a study, the 1942 program for 150,000 kilovars of additional reactive capacity was authorized. This is made up of 50,000 kva of static capacitors installed on distribution circuits which reaches the established limit of 150,000 kilovars of capacitor installations and 77,500 kva of synchronous condensers in 7,500-kva and 5,000-kva units installed on four-kilovolt busses in substations. This 127,500 kva of installed capacity together with the calculated reduction in system losses makes up the 150,000 kilovars shown in the tabulation. Some of this capacity may be used to permit higher power-factor operation of the generators during the summer. Maintenance schedules for 1942 have been arranged so that no more than one unit is expected to be unavailable for any cause during the summer, and the spinning reserves on the Pennsylvania-New Jersey interconnection are sufficient to meet all requirements.

A similar tabulation for 1943 shows that an additional 30,000 kilovars of capacity is required, and this is to be

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provided by the installation of a 30,000-kva outdoor hydrogen-cooled synchronous condenser at Roseland switching station. This will continue the maintenance of generator kilowatt ratings for high industrial loads.

Conclusions

It is not the intention of the authors to summarize or restate the many conclusions which have been tabulated throughout the article but to close their case by focusing attention on three things:

1. The data which form the basis of the studies apply only to the Public Service system and should not form the basis of a generalization but may be accepted as a method of analysis.
2. Reactive capacity in one form or another, particularly static capacitors, can quickly make available additional kilowatt capacity and, by proper location, postpone the installation of additional transmission and distribution facilities.
3. The conception of a separate kilowatt and kilovar load and supply should offer a practical means of presenting to administrative officers the highly technical problem of providing adequate electrical-supply facilities.

It is hoped that a method of analyzing facilities for the supply of kilowatt and kilovar loads has been presented so that utility engineers may at this time of shortages in materials approach the problem objectively with the intention of getting the utmost capacity out of existing equipment without decreasing the services which are so urgently needed by industry and the nation.

A Study of the Modified Kramer or Asynchronous–Synchronous Cascade Variable-Speed Drive

M. M. LIWSCHITZ
MEMBER AIEE

L. A. KILGORE
MEMBER AIEE

Synopsis: Several large wind-tunnel drives recently built have involved a system of speed control which has seldom been used commercially, and a number of new problems had to be solved. The system consists of a wound-rotor induction motor whose slip rings are connected to a synchronous motor driving a variable-speed d-c generator which feeds a constant-speed d-c–a-c set putting the major part of the secondary power back into the line.

This drive is found to be very efficient and particularly suited to very large fan or pump drives where a wide range of speed is required. The problems of steady-state and dynamic stability are discussed and some novel methods of analysis given.

LARGE wind tunnels require a wide range of speed and accurate speed control and if their use factor is high, a high efficiency over the working range is desirable. Also minimum disturbance to the power system is important in many cases. To meet these requirements a speed-control system has been adopted which, while not new in principle, involved the solution of a number of interesting problems. The system as shown in Figure 1 consists of a wound-rotor driving motor (*A*) whose secondary winding feeds a synchronous motor (S_1) driving a variable-speed d-c generator (DC_1) which in turn drives a d-c–a-c set (DC_2 and S_2) to return most of the secondary power back to the line. The name "modified Kramer set" is suggested by the authors since the scheme involves conversion of the secondary power to d-c and field control for the speed changes as in the well-known Kramer set. The term "modified" was used because the Kramer set used a rotary converter and the d-c power was usually fed into a d-c motor on the same shaft as the main motor. Another descriptive name would be asynchronous-synchronous cascade.

Speed Control and Design Features

Speed control is accomplished in this system by controlling the speed of the variable-speed set, and as long as the synchronous machine remains in synchronism with the induced secondary voltage, the motor must run at a speed corresponding to the difference in frequencies. The speed of the variable-speed set can be adjusted by changing field of either the d-c motor or the d-c generator, just as in a wide-range variable-voltage d-c system. The inherent speed regulation is determined by the regulation of the d-c machines. The application of an accurate speed regulator to such a system is a subject in itself, too lengthy for this paper. It is apparent that automatic control of the speed can be had by controlling the d-c fields.

The constant-speed set is started first since it is smaller and requires relatively little starting current. The variable-speed set may then be brought to speed and the synchronous motor field energized. In this manner it is possible to excite the main motor from the secondary while it is stationary and disconnected from the line. After careful adjustment of frequency and voltage, the primary winding may be synchronized just as an oncoming generator may be. This control also can be made fully automatic.

In order to understand the design problem involved, one must consider the power distribution as shown in Figure 2 for a typical fan curve. It will be seen that the maximum secondary power to be handled occurs at two thirds of synchronous speed, but the maximum torque on the variable-speed set will be at full speed. Hence it is desirable to work at full flux throughout the upper range of speed, obtaining speed control by changing the field of the constant-speed d-c motor. However, in order to limit the size of this d-c motor it is found desirable to obtain the lower part of the speed range by field control of the generator holding nearly constant d-c voltage. The exact point of changing from control of motor to generator field is determined by the economics of the design.

Steady-State Stability

Some interesting problems arise in connection with the stability of such a combination, and the methods developed for handling these may be of value in other similar problems.

Since the synchronous motor must stay in synchronism, the machines must be designed so that their combined characteris-

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M. M. Liwschitz is with The Polytechnic Institute of Brooklyn, Brooklyn, N. Y., and is also consulting engineer for Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. L. A. Kilgore is section engineer, large a-c motor section, transportation and generator department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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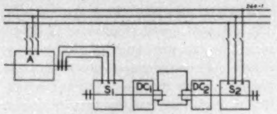

Figure 1. Schematic diagram of drive and auxiliary machines

Asynchronous–synchronous cascade

tics result in stability. For the steady-state synchronizing-torque calculation, the induction motor may be treated approximately as a transformer with high magnetizing current just as in the conventional induction-motor theory. However, the secondary circuit now includes the impedances and the internal voltage of the synchronous machine. Hence the stability calculation resolves itself into a simple two-machine problem.

Another exact method was given in a previous paper on the doubly fed machine.[1] This consisted in resolving the torque into components:

$$T=[e_1^2r_2s-e_2^2r_1+e_1e_2\sqrt{l_1^2+m_1^2}\times \sin(\delta+\alpha)]\frac{1}{l^2+m^2}$$

where l_1, m_1, l and m are functions of the slip s. The first component is the well-known induction-motor torque (but, of course, including the total secondary-circuit impedance). The second component is due to the primary resistance and secondary excitation and is negative. The third component is a function of the angle $(\delta+\alpha)$ and when smaller than 90 degrees results in positive torque increasing with the angle.

The components of the stator current of the induction motor are due to primary voltage and the synchronous-machine excitation.

$$I_{11}=E_{L1}\frac{f+jh}{l+jm}$$

$$I_{12}=-E_{L2}\frac{\cos\delta-j\sin\delta}{l+jm}$$

Thus the angle between I_{11} and $-I_{12}$ will be

$$\beta=\delta-\tan^{-1}\frac{h}{f}$$

The primary current I_1 is determined by the output and power factor desired at a given speed. For the same speed (slip) the current component I_{11} can be easily found by solving the equivalent circuit for $E_{L2}=0$ or by using adequate formulas or from the circle diagram of the induction machine for $E_{L2}=0$. Thus the current component I_{12} can be found as the geometrical difference between I_1 and I_{11}. In Figure 3, the primary current I_1 is fixed. The current component I_1 is found from the circle diagram A as the current corresponding to the slip on which I_1 is based. Assuming constant slip and variable angle δ between E_{L1} and E_{L2} the vector I_{11} will not change its position, the vector I_{12} will describe a circle (B in Figure 3) with the end point of I_{11} as center and I_{12} as radius. This circle is the circle diagram of the primary current I_1 for constant slip and variable angle δ. From this it is possible to judge the approximate overload capacity of the machine for the conditions given by the current I_1.

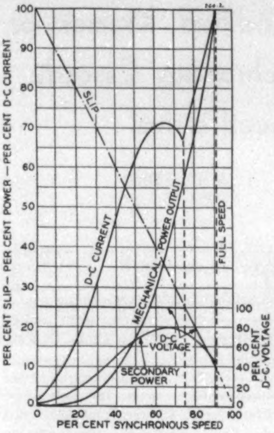

Figure 2. Characteristic curves

Dynamic Stability

The load, the driving motor, and the connected sets constitute a system of masses connected by springs or by electrical ties which act like spring connections. These masses can oscillate at a number of different natural frequencies corresponding to different modes of vibration. These modes of vibration could theoretically be excited by pulsating torques or could be set in sustained oscillation if there was sufficient energy fed into the oscillating system by the negative damping characteristics of some machine.

Before analyzing the rest of the system it is well to consider the nature of negative damping in such machines. A method of analysis for negative damping in both the doubly fed machine and the synchronous machines has been discussed in previous papers[1,2] and is being discussed in another current paper.[3]

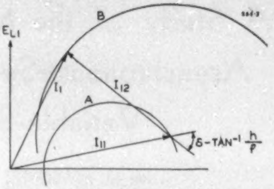

Figure 3. Circle diagram of the doubly fed machine

However, it is possible to get a very clear picture of the physical problems and fair quantitative results for large machines by making some simplifying assumptions. These are:

The per-unit primary resistance of the large induction motor is small.

The rotor impedances of the synchronous motor at the oscillation frequency are equal on the two axes.

This gives a revolving field of flux linking the stator winding of the induction motor which is determined only by the fixed primary voltage and frequency. Any small angular oscillations of the rotor induce additional voltages in the secondary at slip frequency plus the oscillation frequency and slip frequency minus oscillation frequency. These voltages are superimposed on the steady-state quantities and act through the reactance of the induction machine (as viewed from the secondary) and the equivalent impedances of the synchronous machine. Both of these extra components induce oscillation frequency currents in the rotor of the synchronous machine, and its impedance must be evaluated on this basis. This would form a basis for calculation as indicated in appendix A, in which the damping coefficient is determined from the speed-torque curve of the induction motor with its rotor short circuited through the impedance of the synchronous machine. The negative-damping coefficient (ratio of change in torque to change in velocity) in per unit is shown to be the slope of a line drawn between two points at $(s+\Delta)$ and $(s-\Delta)$ slip values. To further simplify the approximate solution, let us assume that the resistance of the rotor circuit of the synchronous machine is negligible while considering the effects of primary resistance, and that the primary resistance is negligible while considering the effects of secondary resistance. This is approximately true because the reactance predominates in determining the currents and amplitudes, so that the losses and in-phase components of currents may be approximated by calculating the effect of the resistances one at a time.

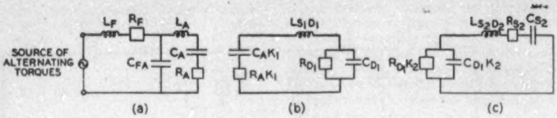

Figure 4. Electrical analogy for the parts of a mechanical system

This results in the approximate solution that the resistance of the secondary circuit acts to cause a negative damping whose value depends on the slope of the speed-torque curve of the induction motor short-circuited through the impedance of the synchronous machine, neglecting rotor resistances. Again since the actual rotor currents involved are at slip frequency plus and minus the oscillation frequency, this slope is to be determined between two points at slip values $(s+\Delta)$ and $(s-\Delta)$. This can readily be seen to be true for the case where the oscillation frequency becomes very low, and one may think of the oscillation as moving from point to point on the speed-torque curve.

The effect of the resistance of the synchronous-motor rotor circuits is to produce positive damping of any angular oscillations between the machines. Again the same concept of taking the slope of the speed-torque curve may be used, except that the slip is zero. One can make an approximate allowance for the difference on the two axes by taking the direct-axis equivalent circuit and a voltage proportional to the flux in the quadrature axis, and a voltage for the quadrature-axis flux proportional to the direct-axis flux. This relation is due to the fact that it is the flux on one axis that induces voltage in the rotor circuit of the other axis due to small angular oscillations.

Electrical Equivalent of Mechanical Systems

According to well-known methods one can analyze torsional vibration problems by representing the parts by their electrical equivalents, as shown in Table I. Thus the equivalent circuit for the fan load and the induction machines will be represented by Figure 4A. L_F and R_F correspond to the moment of inertia and the damping of the fan; C_{FA} represents the spring of the shaft between the fan and the rotor of the induction machine; L_A corresponds to the moment of inertia of the rotor of the induction machine. C_A and R_A correspond to the change of power of the induction machine due to the synchronizing torque, and the damping between the synchronous machine (S_1) and the induction machine. The resistance R_A can be negative for negative damping.

Table I

Mechanical Quantities	Electrical Equivalent
Angular velocity	Current (I)
Torque	Voltage (E)
Moment of inertia	Inductance (L)
Torsional flexibility (reciprocal of stiffness)	Capacity (C)
Damping (loss proportional to oscillation velocity squared)	Resistance (R)

The moments of inertia (L) will be expressed in pound-feet-seconds^2 per mechanical radian, the synchronizing torques and torsional stiffness $(1/C)$ in pound-feet per mechanical radian, and the damping torques (R) in pound-feet-seconds per mechanical radian.

Since the synchronous machine (S_1) has a different number of poles and a different angular velocity (speed) than the induction machine, the capacitance and the resistance that correspond to its synchronizing torque and its damping will have other values than those of the induction machine (C_A and R_A) namely

$$C_{S1}=C_A\left(\frac{P_{S1}}{P_A}\right)^2=C_AK_1$$

$$R_{S1}=R_A\left(\frac{P_{S1}}{P_A}\right)^2=R_AK_1$$

This is in accordance with the fact that the power which corresponds to the synchronizing torque and the damping is the same for both machines.

Any oscillations of the synchronous machine (S_1) will be transmitted to the DC_1 and an alternating electromotive force will be induced in the armature of this machine of the value

$$e=\frac{N}{2\pi}\,\frac{P}{a}\,\phi 10^{-8}\frac{dw}{dt}\text{ volts}$$

This electromotive force will produce a current that depends on the ohmic resistances and the inductances of the armature circuits of both d-c machines, and, as a consequence, power will be transmitted from the DC_1 to the DC_2. This power transmission will damp the oscillations, that is, it will act as a damping force. The inductance causes a lagging of the current and the resultant torque. This gives a component of torque 180 degrees out of phase with the displacement, hence it is analogous to a spring. Thus the d-c machines are connected by spring and damping torques. The corresponding capacitance and resistance can be assumed connected in parallel or in series. The paralleling has the advantage that the formulas obtained are more general, independent of the frequency of the oscillation.

For this case the damping torque of the DC_1 will be

$$I=\frac{e}{\Sigma R}\qquad T_d=IE_L\frac{0.739}{\Omega}\text{ lb ft sec}$$

and the synchronizing torque

$$T_s=\frac{\Sigma R}{\Sigma L}\,T_d\text{ lb ft}$$

E_L is the armature voltage of the d-c machine, ΣR and ΣL are the sums of the resistances and inductances of the circuit of both d-c machines, Ω is the angular velocity of the DC_1.

The equivalent circuit for the synchronous machine (S_1) and the d-c machine (DC_1) is shown in Figure 4B. L_{S1D1} represents the moment of inertia of the variable-speed set (S_1+DC_1), R_{D1} represents the damping and C_{D1} the synchronizing torque of the DC_1.

Both the d-c machines are coupled by spring and damping in the same manner as the synchronous machines (S_1) and the induction machine (A). For similar reasons the constants R_{D2} and C_{D2} that represent the DC_2 are different from R_{D_1} and C_{D1}: both d-c machines have different speed and different coil fluxes. The ratio of the torques produced by the current I in both machines is

$$\frac{T_{D2}}{T_{D1}}=\frac{n_{D1}}{n_{D2}}\,\frac{(N\phi)_{D2}}{(N\phi)_{D1}}$$

Thus the constants for the DC_2 will be

$$K_{D2}=R_{D1}K_2\qquad C_{D2}=C_{D1}K_2$$

$$K_2=\frac{n_{D1}}{n_{D2}}\,\frac{(N\phi)_{D2}}{(N\phi)_{D1}}$$

The equivalent circuit for the constant-speed set (DC_2+S_2) will be therefore represented by the Figure 4C. $L_{S_2D_2}$ represents the moment of inertia of the constant-speed set (DC_2+S_2), R_{S_2} represents the damping, C_{S_2} the synchronizing torque of the synchronous machine (S_2).

Using the equivalent circuit Figure 4 it is possible to set up the differential equation for the whole set. By solving this equation the resonant frequencies can be found, and the damping factors of the oscillations can be determined. However, it is more convenient to connect the three separate circuits of Figure 4 to one circuit and to set up the differential equation for this circuit. Dividing all constants of

Figure 4B by the factor $(P_{S1}/P_A)^2$ and all constants of Figure 4C by the factor $(P_{S1}/P_A)^2 \times K_2$ the equivalent circuit Figure 5 will be obtained.

SOLUTION OF EQUIVALENT CIRCUITS FOR THE ELECTRICAL ANALOGY

An electrical system such as shown in Figure 5 can readily be solved for a given frequency, however, the determination of the natural frequencies is quite difficult. In the practical problem described in reference 4 a set of simultaneous equations was set up for the branches of the network, and a numerical solution obtained by matrix methods. This involves four complex roots or eight distinct roots and is too laborious for general use. A more practical semigraphical solution with the usual complex number representation of the impedances was obtained by plotting the current for a given applied voltage at different frequencies. Two frequencies between which the phase of the current reversed were taken as an indication that resonance lay between them, and more points were tried near the peak currents to determine the exact resonance. The circuit was set up approximately on the a-c calculating board. Although negative resistance or even the very low positive resistance of some branches could not be represented, the solution did indicate the points of resonance which could then be calculated accurately.

The circuits were checked at the natural frequencies for any net negative damping assuming a small impressed force in the branch containing the negative resistance. Also, the amplitudes for any impressed forces at the propeller at the natural frequencies were checked by these methods.

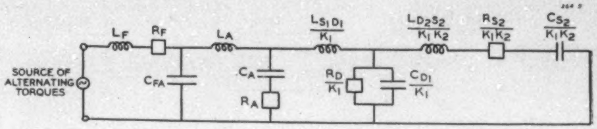

Figure 5. Electrical analogy for the combined mechanical system

Conclusion

Methods have been indicated for determining the stability limits of this system, and the dynamic stability or hunting characteristics have been analyzed. The results of the application of this type of analysis indicate that the machines can readily be designed to be stable. Also providing the motor is operated well below synchronous speed, 8 or 12 per cent below for large machines, it is possible to avoid any tendency to hunt.

List of Symbols

This does not include certain symbols defined as used, or the symbols indicating the machines as shown in Figure 1.

a—Number of parallels
e_1, e_2—Impressed voltage of primary and secondary
E_{L1}, E_{L2}—Induced voltages in primary and secondary
$f=(1+\tau_2)$
f_1, f_2—Frequency of primary and secondary impressed voltage
$h=-r_2/sx_m$
I—Current
I_m—Magnetizing current
$l=[(1+\tau_2)r_1+(1+\tau_1)r_2/s]xs$
l, L—Inductance
$m=\left[x_1+(1+\tau_1)x_2-\frac{r_2}{\tau_1}\frac{r_1}{x_m}\right]xs$
n—Rpm
N—Total number of conductors
r_1, r_2—Stator and rotor resistances
r_{Dd}—Resistance of damper winding on the direct axis
r_{Dq}—Resistance of damper winding on the quadrature axis
r_f—Field resistance
s—Per-unit slip
T—Torque
T_d—Damping torque per radian per second
T_s—Synchronizing torque per radian
x_1, x_2—Stator and rotor leakage reactance
x_{ad}—Reactance of armature reaction on direct axis
x_{aq}—Reactance of armature reaction on quadrature axis
x_{Dd}—Leakage reactance of direct-axis damper winding
x_{Dq}—Leakage reactance of quadrature-axis damper winding
x_f—Leakage reactance of the field
x_m—Magnetizing reactance
Z—Impedance
α—Power-factor angle
β—Angle between machines
δ—Angle between voltages of machines
Δ—Per-unit torsional oscillation frequency
$\tau_2=x_2/x_m$—Leakage coefficient
ϕ—Flux per pole
Ω—Angular velocity

Appendix A. Simplified Solution of Negative Damping for a Doubly Fed Polyphase Machine

In the analysis, the following assumptions are made:

1. The primary resistance is negligible in comparison with primary reactance at the frequencies considered.

2. The three-phase winding of the machine can be replaced by an equivalent two-phase winding.

3. The coefficients of self-inductance of primary and secondary (L_1 and L_2) and of mutual inductance (M) are independent of the relative position of rotor and stator.

4. The primary flux is sinusoidally distributed, and its amplitude is the same at all times.

5. The angular motion of the rotor with respect to that of the stator flux is represented by

$$\theta=\omega_s t+f(t) \tag{1}$$

where ω_s is equal to 2π times the frequency of the a-c voltage applied to the rotor.

6. The amplitude of the angular oscillations represented by $f(t)$ is small, so that $\cos f(t)$ can be taken as unity, and $\sin f(t)$ can be taken as $f(t)$. This is a good approximation up to about 15 electrical degrees. On this basis

$$\cos\theta=\cos\omega_s t-f(t)\sin\omega_s t \tag{2}$$

$$\sin\theta=\sin\omega_s t+f(t)\cos\omega_s t \tag{3}$$

7. The systems providing excitation for stator and rotor are very large, so that they are unaffected by any change in the motor.

The flux linkages in phase a of the secondary due to primary flux are equal to

$$\psi_{a_2}'=-\frac{M\psi_1}{L_1}\cos\theta \tag{4}$$

where ψ_1 represents the primary-flux linkages. Similarly, the flux linkages in secondary phase b are

$$\psi_{b_2}'=-\frac{M\psi_1}{L_1}\sin\theta \tag{5}$$

The voltage in phase a of the secondary is

$$e_{a_2}=p\psi_{a_2}'-E_2\sin(\omega_s t-\phi) \tag{6}$$

where E_2 is the amplitude of the voltage applied to the secondary, and ϕ is the phase angle between this voltage and the voltage $p\psi_{a_2}'$. The voltage in phase b is

$$e_{b_2}=p\psi_{b_2}'+E_2\cos(\omega_s t-\phi) \tag{7}$$

In the operational form the currents in phases a and b of the secondary are

$$i_a=\frac{p\psi_{a_2}'-E_2\sin(\omega_s t-\phi)}{r+pL} \tag{8}$$

$$i_b=\frac{p\psi_{b_2}'+E_2\cos(\omega_s t-\phi)}{r+pL} \tag{9}$$

The total flux linkages of the secondary phases are

$$\psi_{a_2}=-M\frac{\psi_1}{L_1}-i_aL \tag{10}$$

$$\psi_{b_2}=-M\frac{\psi_1}{L_1}-i_bL \tag{11}$$

In equations 7 to 11, r is the resistance and L is the equivalent coefficient of self-inductance of the secondary, which is equal to

$$L=L_2-\frac{M^2}{L_1} \tag{12}$$

From the two-reaction theory of torque the torque in the machine is

$$T = i_a\psi_{b_s} - i_b\psi_{a_s}$$

$$= M\frac{\psi_1}{L_1}\sin\theta\times$$

$$\left[\frac{M\frac{\psi_1}{L_1}p\cos\theta + E_2\sin(\omega_s t-\phi)}{r+pL}\right]+$$

$$M\frac{\psi_1}{L_1}\cos\theta\times$$

$$\left[\frac{-M\frac{\psi_1}{L_1}p\sin\theta + E_2\cos(\omega_s t-\phi)}{r+pL}\right] \qquad (13)$$

This torque equation can be expanded and simplified to equation 14.

$$T = M\frac{\psi_1}{L_1}\left[M\frac{\psi_1}{L_1}\left(\frac{-r\omega_s + f(t)\omega_s^2 L}{r^2+\omega_s^2L^2} - \right.\right.$$

$$\sin\theta\frac{f(t)\omega_s\cos\omega_s t + pf(t)\sin\omega_s t}{r+pL} +$$

$$\left.\cos\theta\frac{f(t)\omega_s\sin\omega_s t - pf(t)\cos\omega_s t}{r+pL}\right)+$$

$$E_2\left(\frac{1}{r^2+\omega_s^2L^2}\right)(r\cos\phi - \omega_s L\sin\phi -$$

$$\left.f(t)\omega_s L\cos\phi - f(t)r\sin\phi)\right] \qquad (14)$$

If the angular oscillation represented by $f(t)$ is set equal to $\theta_\Delta \sin \omega_\Delta t$, where θ_Δ is the amplitude of the fundamental frequency of variation and ω_Δ is 2π times this frequency, the torque is represented by equation 15, in which $\omega_+ = (\omega_s + \omega_\Delta)$, $\omega_- = (\omega_s - \omega_\Delta)$, and the secondary resistance is r_+ at ω_+, r_- at ω_-, and r_s at ω_s.

$$T = \left(M\frac{\psi_1}{L_1}\right)^2\left(\frac{-r_s\omega_s}{r_s^2+\omega_s^2L^2}\right)+$$

$$M\frac{\psi_1}{L_1}E_2\left(\frac{1}{r_s^2+\omega_s^2L^2}\right)(r_s\cos\phi - \omega_s L\sin\phi) -$$

$$\left(M\frac{\psi_1}{L_1}\right)^2\left(\frac{\theta_\Delta}{2}\right)\left(\frac{\omega_+ r_+}{r_+^2+\omega_+^2L^2} - \right.$$

$$\left.\frac{\omega_- r_-}{r_-^2+\omega_-^2L^2}\right)\cos\omega_\Delta t - \left(M\frac{\psi_1}{L_1}\right)\times$$

$$\left[M\frac{\psi_1}{L_1}\left(\frac{\theta_\Delta}{2}\right)\left(\frac{\omega_+^2}{r_+^2+\omega_+^2L^2} + \frac{\omega_-^2}{r_-^2+\omega_-^2L^2} - \right.\right.$$

$$\left.\left.\frac{2\omega_s^2}{r_s^2+\omega_s^2L^2}\right)L + E_2\theta_\Delta\frac{\omega_s L\cos\phi + r\sin\phi}{r_s^2+\omega_s^2L^2}\right]\times$$

$$\sin\omega_\Delta t \qquad (15)$$

The $\sin \omega_\Delta t$ term of equation 15 represents a torque in phase with and opposed to the alternating displacement, and is therefore the synchronizing torque.

The $\cos \omega_\Delta t$ term represents a torque in phase with and aiding the velocity. The nature of this torque is such that it tends to increase any small variation in the motion of the rotor, and thus it provides negative damping in the machine.

This equation indicates that the damping factor at any speed can be determined from the speed-torque curve obtained with the rotor short-circuited. The slope of a line between two points on this curve is the value of the damping coefficient at the speed halfway between those points. Thus if vibration is forced at the frequency corresponding to ω_Δ, and the speed of the machine is $(\omega-\omega_s)$, the damping factor will be the slope of the line connecting points on the curve at the speeds $(\omega-\omega_s)+\omega_\Delta$ and $(\omega-\omega_s)-\omega_\Delta$.

In the practical case the secondary is not connected to a system of infinite capacity, so the secondary voltages induced by the pulsations act through a circuit which includes the impedance of the machine connected in the secondary. Since the pulsations induce oscillation frequency currents in the rotor of the connected synchronous machine, the rotor-circuit resistances must be divided by the per-unit oscillation frequency when this impedance is added into the equivalent circuit.

Appendix B. Tests on Model System

In order to study the stability of the system under consideration, a model has been set up as shown in Figure 6. The induction machine A was loaded by a d-c generator for want of a fan, but a flywheel has been put on the shaft of this d-c machine as a substitute for the high WR^2 of the fan. A variable ohmic resistance and a variable inductance have been put in the armature circuit of both d-c machines DC_1 and DC_2 in order to vary the damping torque as well as the synchronizing torque of these machines. The constant-speed set (DC_2+S_2) consisted of duplicates of the machines of the variable-speed set (DC_1+S_1) with the sole exception that the synchronous machine (S_1) had a damper winding of copper while the synchronous machine (S_2) had a damper winding of material with high resistivity.

The rating and the constants of the different machines were as follows:

Induction motor:

100 horsepower—2,200/440 volts—60 cycles—3 phases—6 poles

$r_1 = 1.2$ $\quad r_2 = 1.19$
$x_1 = 4.5$ $\quad x_2 = 4.0$
$x_m = 147$ $\quad WR^2 = 172$ pound-feet^2
Flywheel $WR^2 = 164$ pound-feet^2

Synchronous machines:

100 kva—2,300 volts—60 cycles—3 phases—6 poles

$r_1 = 1.27$ $\quad x_{ad} = 57.0$ $\quad r_f = 0.161$
$x_1 = 5.0$ $\quad x_{aq} = 54.3$ $\quad x_f = 6.2$
$r_{Dd} = 2.76$ $\quad r_{Dq} = 1.27$
$x_{Dd} = 8.3$ $\quad x_{Dq} = 4.36$
$WR^2 = 195$ pound-feet^2

Transformer between A and S_1:

3×33 kva—2,300/440 volts—60 cycles

$r_1 = 0.32$ $\quad x_1 + x_2 = 1.5$
$r_2 = 0.0734$ (including brushes)

The constants given above are expressed in ohms per phase for 60 cycles; they are reduced to the primary of each machine.

D-c machines:

150 horsepower—230 volts—525 amperes—1,100 rpm—$WR^2 = 305$ pound-feet^2—6 poles—6 parallels —$N = 450$

Armature resistance $r_a = 0.0073$
Armature reactance $x_a = 0.11$ at 25 cycles

As could be expected, the tests have shown that the system is statically stable and that the speed control takes the same course as in the conventional Kramer set, where the machine that absorbs the slip power of the induction machine is a rotary converter. A continuous variation of the speed is possible without any disturbances.

In order to study the dynamic stability, the damping of the system has been artificially weakened by overexcitation of the synchronous motor (S_1) or by putting an inductance in the armature circuit of both d-c machines. Two kinds of conditions under which sustained oscillations have been observed are given in the following:

Condition *A*

Machine	Volts	Amperes	Cos ϕ	Slip
A	2,310	25.5	0.736 leading	26.6%
S_1	626	31.5	0.57 leading	
DC_1	58	75		
S_2	2,310	0		

The frequency of the oscillation was $f_o = 2.5$ cycles per second. The machine (S_1) was overexcited. Between both d-c machines was placed an additional resistance $= 0.024$ ohm.

For these conditions the following damping and synchronizing torques were determined:

Machine	T_d (Pound-Feet-Seconds)	T_s (Pound-Feet)
S_1	−58	3,100
DC_1	62.7	1,590
S_2	9.6	885

The factor K_1 (Figure 4) is here equal to 1, the factor K_2 is equal to $\frac{1{,}200}{320}\times\frac{2.41}{0.512} = 17.6$, thus the equivalent circuit and its constants are as shown in Figure 7.

Substituting the impedances of Figure 7 in the equations, the characteristic differential equation of the system can be found, and by solving this equation the tested frequency of the oscillations can be checked. The substitution leads to a differential equation of seventh degree. For the solution of such an equation Graeffe's method or the matrix method, both of which are rather laborious, has to be used. An examination of Figure 7 shows that this is not necessary in our case, where the machine (S_2) has a much higher rating than it has in the real induction synchronous cascade.

$f_o = 2.5$ cycles per second corresponds to $p = 15.7$. The impedance of the machine (S_2) is so large for this value of p that it has practically no influence on the impedance that represents the two d-c machines. Thus we can omit the tail of Figure 7, and the differential equation will be only of the fifth degree. A further reduction by 1 is possible, if we limit our considerations to a natural frequency around 2.5 cycles per second. It is then possible to replace both impedances in parallel that represent the two d-c machines by one impedance. With this as-

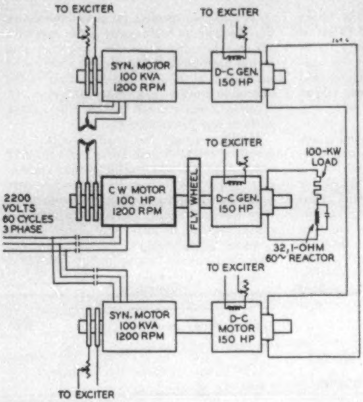

Figure 6 (left). Variable-speed synchronous Kramer-set laboratory - model test of large wind-tunnel drive

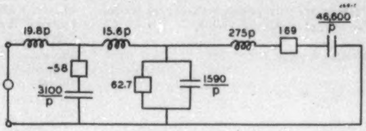

Figure 7 (upper right). Electrical analogy with numerical values corresponding to model test

sumption the following impedances will determine the oscillations:

$$Z_1=19.8 \qquad Z_2=-58+\frac{3,100}{p}$$

$$Z_3=43+15.6p+\frac{490}{p}$$

The characteristic differential equation is with these constants

$$p^4-3.89p^3+379p^2+338p+4,910=0$$

The roots of this equation are

$$p_{1,2}=2.4\pm j19$$
$$p_{3,4}=0.54\pm j3.6$$

To the angular velocity 19 corresponds $f_0=3.02$ cycles per second. Since the real part of this pair of roots is positive, the damping is negative, and the oscillations are sustained.

To the other pair of roots corresponds a slow oscillation with 0.57 cycles per second. A slower oscillation than this has been observed during the tests. It must be noticed that the damping and synchronizing torques depend on the frequency of the oscillations. The values of these torques as given above were determined for $f_0=2.7$ and are not correct for the low oscillations. Thus the second pair of roots is only approximate.

Condition *B*

Machine	Volts	Amperes	Cos ϕ	Slip
A	2,460	17.7	0.71 leading	35.4%
S_1	920	26.7	0.50 leading	
DC_1	80	90		

The frequency of the oscillations was $f_0=2.9$ cycles per second. The second d-c machine had been cut out, and the DC_1 was loaded into a resistor. A large reactance between both d-c machines had given under the same conditions the same frequency of oscillations. The machine (S_1) was overexcited ($e_d=1.95$).

For these conditions the damping and synchronizing torque of the synchronous motor (S_1) are

$T_d=8.8$ pound-feet-seconds
$T_s=3{,}330$ pound-feet

The differential equation of the oscillation is

$$p^2+1.0+382=0$$

and the roots are

$$p=-0.5\pm j19.5$$

To the angular velocity 19.5 corresponds $f_0=3.1$ cycles per second. The calculation gives a small positive damping, while the test has shown sustained oscillations, that is negative damping. The discrepancy is caused by the difficulty of exact determination of the input (and output) current, and so on, at the starting of the oscillations. The figures under conditions *A* and *B* are only approximate.

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Loss-of-Field Protection for Generators

G. C. CROSSMAN
MEMBER AIEE

H. F. LINDEMUTH
MEMBER AIEE

R. L. WEBB
MEMBER AIEE

Synopsis: The features of a new protective relay scheme are described. The scheme is designed to remove underexcited a-c generators from the system, upon occurrence of undervoltage, before loss of synchronism occurs. The relay development and its operating characteristics are described. Calculations required to determine generator behavior after loss of field are also presented to make the application of this scheme easily applied to generators on any system.

The Problem

THOUGH electrical circuits from generator armature windings outward to utilization apparatus have received much attention from a protective viewpoint in the past 20 years, relatively little attention has been given to generator field-winding protection. Calculations show that loss of field in a large generator may cause serious voltage disturbance to the system. A need for some suitable protection method that will initiate disconnection of the troubled machine is indicated.

When loss of field occurs on a loaded generator, the magnetic coupling between the rotor and the stator becomes so weakened that the rotor advances and, after a short period, pulls out of synchronism with the system. The unit continues to carry load of varying magnitude but draws a high component of wattless current from the system.

Continued operation without excitation has harmful effects to both the generator and to the system. The generator, now an asynchronous machine, will be subject to high circulating currents in the face of the field rotor or in the amortisseur winding, and these may cause injurious heating, at least in local areas. Also induced current or voltage will appear in the field winding depending upon whether it is short circuited or open circuited.

The effects of field failure may be much more important on the system, particularly if the generator is a large unit in relation to other operating capacity. For complete loss of field, calculations have indicated that, on most power systems not equipped with automatic-generator voltage regulators, seriously low-system voltages may be reached in not more than 10 to 15 seconds and in some cases in as short a time as one second.

System loads affect the degree of voltage disturbance after loss of field. If a large percentage of the load is induction motors equipped to be disconnected only after the voltage has reached 25 to 50 per cent of normal, many of these will stall as the voltage is reduced to approximately 75 per cent of normal at the bus. This condition becoming cumulative, may cause voltage instability until the voltage has been reduced to a value where most of the motor load does become disconnected.

Since 1927 there have been five serious machine-excitation failures on the system with which the authors are connected. In addition to these, three other cases of field trouble have developed in large machines which might easily have caused loss of field, had the conditions not been discovered and corrected. One of these failures reached the stage of voltage instability, as referred to above, because of motor loads, and the station bus voltage was reduced to 37 per cent of normal before a sufficient number of motors was disconnected to permit recovery to approximately 80 per cent of normal voltage.

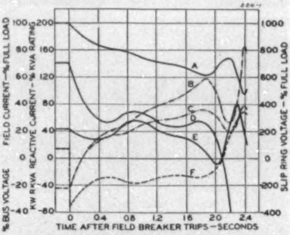

Figure 1. Generator loss-of-field characteristics after accidental tripping of field-supply breaker (large 1,800 rpm unit)

A—Bus voltage
B—Generator reactive current into generator
C—Generator reactive kilovolt-amperes into generator
D—Generator field current
E—Generator output kilowatts
F—Slip-ring voltage

It became evident some years ago that this system in New York required adequate means for main-generator field protection. The results of the development which followed, and a description of the means for applying it, are outlined in sufficient detail to permit practical use of the information by others.

Field protective relays should be sensitive to any reduction of excitation that will, at any generator load, become unsatisfactory for continued operation of the machine. Relays which operate at a specific value of field current or voltage do not give full coverage, since they must be set to operate at values well below no-load excitation conditions and yet be required to function for excitation disturbances under full-load conditions that are within safe operating range so far as the relays can determine. The new relay scheme described herein provides the degree of sensitivity desired and equally satisfies the requirements for field protection at any level of generator loading.

Generator Behavior After Loss of Field

Field loss in a generator may be partial or complete. Types of partial field failure are:

(*a*) Field rheostat trouble.

(*b*) Reductions in excitation voltage.

(*c*) Internal short circuit in a section of the field winding.

(*d*) Operating error.

Complete field failure would include:

(*a*) Winding open circuit.

(*b*) Slip ring or equivalent short circuit.

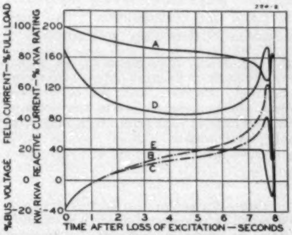

Figure 2. Generator loss-of-field characteristics after loss of field to main exciter (3,600 rpm unit)

A—Bus voltage
B—Generator reactive current into generator
C—Generator reactive kilovolt-amperes into generator
D—Generator field current
E—Generator output kilowatts

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G. C. Crossman, H. F. Lindemuth, and R. L. Webb are all assistant engineers with Consolidated Edison Company of New York, Inc., New York, N. Y.

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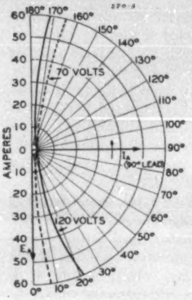


Figure 3. Phase-angle characteristics of reactive-current relay (tripping occurs in area to right of characteristic curves)

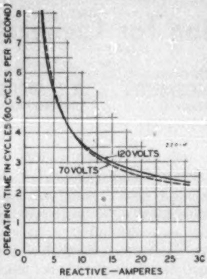


Figure 4. Time-reactive current characteristics of reactive-current relay (voltages are input values to magnetic voltage regulators)

(*c*) Loss of field to main exciter.

(*d*) Accidental opening of field supply breaker.

(*e*) Operating error.

To show the behavior of generators when complete loss of field occurs, two machines on the system with which the authors are connected, were selected. One of these shows conditions for the case of "accidental tripping of field supply breaker," and the other, "loss of field to main exciter." The machine used to show effects of the first condition is one of the largest units on the system and operates at 1,800 rpm. The curves showing the various behaviors are given in Figure 1. The generator used in the second case is a unit of medium size which operates at 3,600 rpm. Its characteristic curves are given in Figure 2.

In both cases the system was considered to be operating with normal capacity for off-peak conditions. No generator automatic voltage regulators were considered to be in service.

In calculating the effects shown in Figures 1 and 2 many varying effects must be taken into consideration. The more important of these may be itemized as:

1. The effects of turbine-governor action are not likely to be great before loss of synchronism. However, after loss of synchronism, violent governor action may occur,[1,2] thereby causing wide fluctuations in generator power output. There may be small power oscillations before loss of synchronism due to the effects of inertia in the rotating parts of the affected generator.

2. Generator and system impedances must be carefully considered since their relative values determine, in large part, the degree of voltage disturbances after loss of field. Obviously the effect on system voltage will be greater as the relative size of the machine increases.

3. The time constant of the generator field circuit determines the rate of decay in bus voltage after loss of field. The field-winding open-circuit time constant as modified by any field circuit external resistance determines, in approximately inverse porportion, the rate of decay in internal generated voltage and bus voltage.

4. Initial power output of the affected generator has an effect on the elapsed time from loss of field to loss of synchronism. Obviously the time will be shorter as the load on the generator becomes larger.

5. Initial reactive loading on the affected generator must be taken into account, since the loss of that output has its effects on the reactive output required of the other system machines. Higher reactive loading tends toward greater reduction of bus voltage after loss of field.

6. Generator voltage regulators were not considered in these calculations, since they are not used generally on the system with which the authors are concerned. If regulators are used on sufficient generating capacity, they will help materially in maintaining bus voltage when one generator has lost its field.

The methods used in calculating the changes occurring after loss of field are given in appendixes I, II, III, which are a part of this paper.

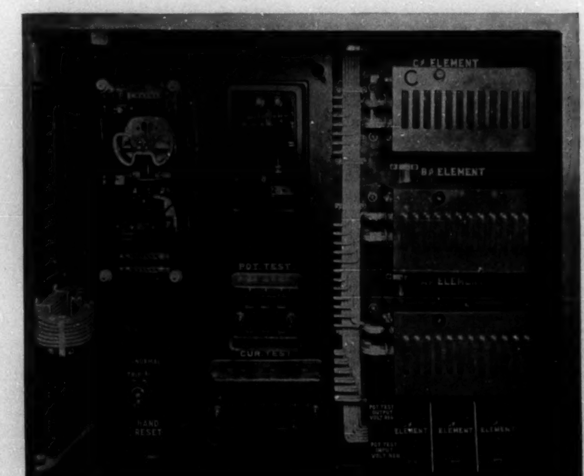
Development of Protective Equipment

The results of Figure 1 show that a relay designed to measure "input" megavars might readily be used to detect generator field excitation disturbances to the degree desired. However, as is shown here, the rate of decay of voltage is so fast for some machines that the rate of increase in "input" megavars is greatly reduced before loss of synchronism occurs. Hence, the torque and operating speed of such a relay is reduced where fast performance is desired. The rate of change in reactive current is not affected. Consequently a relay designed to measure reactive current and be sensitive to its direction would be a better instrument for this purpose. This reactive current relay must have high operating speed if it is to clear the machine before the bus voltage is reduced below the minimum permissible value.

Calculations have indicated that such a relay applied as single-phase elements would be subject to incorrect operations on unbalanced system short circuits. Hence, a polyphase relay with three elements (one per phase) was indicated.

Figure 5. Typical installation of field protective relays

A—Reactive-current relay
B—Undervoltage relay
C—Magnetic voltage regulators



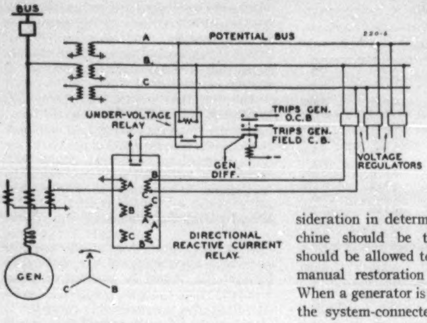


Figure 6. Schematic diagram of relay connections

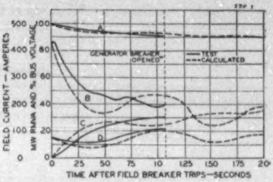


Figure 7. Loss-of-field test on a 50,000-kw generator—field breaker tripped

A—Bus voltage
B—Field current
C—Generator reactive kilovolt-amperes into generator
D—Generator output kilowatts

The relay is a polyphase induction disk, three-element self-reset watt-type device with provisions for time and wattage adjustments. Seven wattage taps varying from 50 to approximately 500 watts permit use of the same relay on any size generator.

To permit measurement of reactive current a magnetic-type voltage regulator was chosen to maintain constant voltage at the relay when variations occur in the bus voltage. The magnetic voltage regulator holds ±1 per cent of normal voltage on the relay coil and permits not more than ±5 degree phase shift while the input voltage varies from 60 per cent to 110 per cent of normal.

Care was required to assure satisfactory performance from zero to 60 cycles and over a corresponding range in voltage on the potential supply to the magnetic regulators and to the relay. Since the generator potential transformers are used for the relays, these may be energized during the generator starting period, thereby producing this wide range of frequency and voltage.

Figure 3 shows the phase-angle characteristics of the relay supplied through the magnetic voltage regulators with input voltage at 120 and at 70 volts. It will be noted that the relay maximum torque appears at about 90 degrees leading current. To obtain this characteristic the relay voltage leads the normal in-phase current by 90 degrees, as would be necessary in applying a watt element for reactive current measurements.

Figure 4 shows the time-reactive current curve of the relay when supplied at 120 and 70 volts through the magnetic voltage regulators.

It will be noted in both Figures 3 and 4 that the relay torque does not change appreciably over this wide voltage range and that the phase shift of the tripping characteristic is not great.

Bus-voltage magnitude is a major consideration in determining whether a machine should be tripped instantly or should be allowed to operate and permit manual restoration of field excitation. When a generator is small compared with the system-connected generation and is linked to the other system generators through relatively low impedance, loss of field in that machine may not cause a serious voltage disturbance. Field-current interruptions may occur or serious field-current reductions may be experienced where quick correction of the trouble is possible and the generator need not be lost from the system.

An undervoltage relay has, therefore, been made a part of the present scheme. It is a standard voltage-regulator type element adjustable to operate at the particular voltage value dictated by the characteristics of an individual machine. Its contacts are connected in series with those of the reactive current relay to prevent tripping unless the bus voltage approaches a value beyond which continued operation of the generator would be unsafe.

Figure 5 shows a typical installation of the relay equipment.

Figure 6 shows a simplified connection diagram of the relays and their associated equipment. Tripping of the generator circuit breaker is first initiated and the field supply is then disconnected by an auxiliary switch on the generator circuit breaker.

System Tests

The reactive current relay was set up for tests on a 50,000-kw machine. The unit was loaded to 14,000 kw at unity power factor. The main-field breaker was then opened, thereby connecting the field winding to its discharge resistor through the main-field rheostat. Oscillographic records were obtained of field current, megawatts and megavars. Bus voltage was obtained with a recording voltmeter. Some of the results of this test, test 1, are shown in Figure 7. In the same illustration, curves are given showing the calculated values for the same conditions. Agreement was close enough to assure that behavior after loss of field can be determined with sufficient accuracy for applying field protective relays.

Altogether four similar tests were made on two machines of the same design and rating. All initial and final behaviors observed on tests 1 and 2 are given in Table I. Complete data were not taken on the last two tests, which were made at higher initial loads.

Calculations show, for this system, that the dividing line in machine rating is about 50 to 60 megawatts beyond which loss of field may produce unstable voltage conditions for the most unfavorable operating and load conditions. Agreement has been reached to apply the protection to all machines on the system of 50-megawatts and larger except for two 60-megawatts, 25-cycle units whose characteristics are such that they could be made the "exceptions."

Conclusions

The relay equipment described makes practicable protective measures which will cover virtually all cases and types of loss of field in generators. It is believed that

Table I. Results of Loss-of-Field Tests on 50,000-Kw Generator

		Generator Armature				Field		
Test	Condition	Current	Volts	Mw	Mvar	Current	Volts	Time to Trip (Sec.)
1	Before field loss	300	26,500	14	0	+430	+ 82	
	At tripping	910	24,000	22	−31	+200	−150	1.1
2	Before field loss	500	27,000	16	+17	+625	+118	
	At tripping	900	23,800	22.5	−29	+200	−110	1.4

Tabel II. Tabulation of Calculations for Appendix III

	Time—t—Seconds				
	0	0.1	0.2	0.3	0.4
$e_{g(t)}$	1.129	0.974	0.855	0.763	0.688
$\Delta e_{g(t)}$	−0.155	−0.119	−0.092	−0.075	−0.064
$e_{s(t)}$	0.916	0.916	0.915	0.914	0.912
$\Delta e_{s(t)}$	0	−0.001	−0.001	−0.002	−0.002
$E_{g(t)}$ per unit	1.508	1.155	0.897	0.723	0.618
$E_{g(t)}$ % full load	70.5	54.0	41.9	33.8	28.9
$E_{s(t)}$	0.836	0.899	0.944	0.979	1.002
$P_{g(t)}$ per unit	0.436	0.358	0.304	0.279	0.278
$P_{g(t)}$ % kva rating	43.6	35.8	30.4	27.9	27.8
$Q_{g(t)}$	0.813	0.300	0.057	−0.039	−0.060
$P_{gi}-P_{g(t)}$	0	0.078	0.132	0.157	0.158
$\Delta\omega_{(t)}$	0	1.3	2.1	2.5	2.5
$\omega_{(t+1/2\Delta t)}$—degrees/0.1 sec.	0	1.3	3.4	5.9	8.4
$\delta_{(t)}$—degrees	30.1	30.1	31.4	34.8	40.7
I	0.612	0.404	0.346	0.389	0.460
Bus voltage—per cent	101.5	95.0	89.8	86.5	84.1
Bus reactive kva *into* generator—% kva rating	−44.4	−13.8	+6.1	+18.9	+26.8
Bus reactive current *into* generator—% kva rating	−43.8	−14.5	+6.8	+21.9	+31.9

All values are per-unit on machine rating, 137,500 kva, and 13,500 volts except where indicated otherwise.

this further improves the reliability of electric service since a thoroughly reliable relay device has been developed which does not of itself create a hazard.

The scheme provides a relay combination which is sensitive to serious excitation reductions, regardless of initial machine load and excitation values.

This new type of field protection has now been applied to all 60-cycle generators rated 50 megawatts and above, excepting two units already provided with undercurrent-undervoltage relays, and to one large 25-cycle machine on the system with which the authors are connected. Though the first installations have been in service over a year no operating experience is yet available.

Appendix I. Calculation Method for Complete Loss of Field

Basis of Calculations

The method used is a step-by-step process and takes into account the decrement of system generator internal voltages as the unexcited machine draws magnetizing current. Although less labor in calculations would result if this decrement were neglected, the authors' calculations for some 15 generators indicate that this decrement is usually about 5 per cent at the relay operating point, and may be as much as 10 to 15 per cent at "pull-out." Consequently, it is felt that the additional labor is justified.

To simplify and reduce this work as far as possible, consistent with reasonable accuracy, several simplifying assumptions have been made. The first of these is neglecting the effects of changes in system load during loss-of-field conditions. Actually some reduction will take place and will cause the bus voltage to be slightly higher than calculated at the relay operating point.

Secondly, oscillations between system generators have been neglected, and these generators have been lumped into one equivalent machine. Since changes in system load have been neglected, this machine is considered to act as motor, absorbing the power output of the unexcited generator. While the actual system generators are in no sense motors, their effects upon the unexcited generator and its bus voltage are much the same. This assumption is often used in transient-stability calculations.[3]

Third, saturation of the magnetic circuits in the unexcited generator and in the system generators has been neglected. While such saturation in the unexcited unit acts to hasten the decay of bus voltage, the saturation in the system generators acts oppositely. Furthermore, the authors' calculations indicate that the unexcited machine flux is usually in the unsaturated region at the relay operating points.

Fourth, constant prime-mover input to the unexcited generator is assumed, since the average output is constant up to "pull-out."

Fifth, to determine the relative swinging of the unexcited generator rotor against the system, system swings are neglected and an equivalent inertia is used for the unexcited machine. This inertia may be calculated by formula 4 of appendix II.

Sixth, effects of system generator voltage regulators have been neglected.

The formulas and symbols which have been used by the authors are for steam driven turbogenerators and are given in appendix II.

Where system conditions will not permit the use of the above assumptions, determination of loss-of-field conditions becomes more complicated. However, practically all cases should be capable of solution quite readily if an a-c calculating board is used.

Determination of System Equivalent-Motor Constants

The first step is to determine which system generators are likely to be appreciably affected by loss of excitation on the unit under consideration.

The system equivalent-motor synchronous reactance, x_{ds}, defined in appendix II, is the reactance from the unexcited machine terminals to the synchronous internal voltages of the selected system generators, such voltages being considered equal and in phase. The transient reactance x_{ds}', is determined in the same way.

The system equivalent motor inertia, H_s, is the sum of the corresponding inertias of the system generators.

The field time constant of the system equivalent motor, T_{sf}', is a weighted average of the system-generator field time constants, with greater weight being given to the units expected to supply the greater portions of the unexcited generator magnetizing current.

Step-by-Step Calculations

After the initial values of all quantities have been determined, the size of the time interval, Δt, should be selected to cause relatively small changes in angle and direct axis flux during this period. For purposes of clarity in the following explanations, it will be assumed that a 0.1-second interval has been selected.

During the first time interval, that is, from time zero to time 0.1 second, the angle δ is held constant, and only the generator direct axis flux, e_g, is changed. The value of this flux at time zero, that is, $e_{g(0)}$, is reduced by the amount $\Delta e_{g(0)}$, the change from time zero to time 0.1 second, determined from either formula 8 or 9, depending upon whether the excitation supply voltage decays gradually or whether it drops to zero instantly, as in a solid short circuit. The value of E_g to be used in this formula will, of course, be that for time zero, that is, $E_{g(0)}$. No change in e_s will occur as may be seen from formula 10.

With $e_{g(0.1)}$, $e_{s(0.1)}$ and $\delta_{(0.1)}$ thus determined for time 0.1 second, corresponding synchronous voltages $E_{g(0.1)}$ and $E_{s(0.1)}$, generator output, $P_{g(0.1)}$, and other required quantities may be determined from the formulas in appendix II.

During the next time interval, that is, from time 0.1 second to time 0.2 second, e_g, e_s, and δ will change. The change $\Delta e_{g(0.1)}$ will be determined as before, using $E_{g(0.1)}$, and will be added to $e_{g(0.1)}$ to obtain e_g at time 0.2 second, that is, $e_{g(0.2)}$. Flux $e_{s(0.2)}$ will be obtained similarly.

To obtain the new angle at time 0.2 second, $\delta_{(0.2)}$, several intermediate steps must be taken. The difference between the assumed constant input to the machine, that is, the initial output P_{gi} neglecting losses, and the output at time 0.1 second, $P_{g(0.1)}$, constitutes an accelerating or decelerating force, depending upon whether this difference is positive or negative. This force is considered as acting from time 0.05 second to time 0.15 second, and during this period will produce a change in the velocity of the generator rotor above system speed. This velocity change is designated by the symbol $\Delta\omega_{(0.1)}$ and is obtained from formula 6. The velocity at time 0.15 second is considered as an average velocity for the time interval 0.1 second to 0.2 second, and is designated by the symbol $\omega_{(0.15)}$. This velocity is equal to the algebraic sum of the above change in speed, $\Delta\omega_{(0.1)}$, and the corresponding average speed at time 0.05 second, designated by the symbol $\omega_{(0.05)}$. Since the angle δ was held constant from time zero to time 0.1 second, $\omega_{(0.05)}$ is zero, and therefore $\omega_{(0.15)}$, is equal to $\Delta\omega_{(0.1)}$.

The average velocity $\omega_{(0.15)}$ for the time interval, 0.1 second to 0.2 second, is measured in the electrical degrees per time interval, that is, degrees per 0.1 second. Consequently, $\omega_{(0.15)}$ is numerically equal to the change in the angle δ during this period. Therefore, δ at time 0.2 second, $\delta_{(0.2)}$, is equal to the angle δ at time 0.1 second, $\delta_{(0.1)}$ plus $\omega_{(0.15)}$.

The angle at time 0.3 second is obtained similarly. Velocity $\omega_{(0.25)}$ is equal to $\omega_{(0.15)}$ plus $\Delta\omega_{(0.2)}$, and $\delta_{(0.3)}$ is equal to $\delta_{(0.2)}$ plus $\omega_{(0.25)}$.

All subsequent step-by-step calculations are made by a continuation of this method.

These steps are shown in detail by the sample calculations of appendix III for the generator of Figure 1.

Other quantities, such as generator terminal voltage and magnetizing current, may easily be obtained for each time interval by the usual vector calculations.

Inasmuch as each step of calculations depends upon the previous step, the accuracy at each step should be checked as far as possible.

Simplifications When T_{gf}' Is Large and H_{gt}' Is Small

When the ratio of the generator-field time constant, T_{gf}', to the generator equivalent inertia, H_g', is around 2.0 or greater, the generator-output power oscillations are small and may be neglected without appreciable error. Consequently, as the flux decays, the machine advances by just enough to hold constant output.

Therefore, the procedure here consists in determining e_g and e_s for each step as before, and then determining by trial-and-error process the angle δ which will produce the constant output. Although two or three trials may be necessary for each step, the number of steps to be calculated will be considerably less than required by the previously outlined method of step increments of velocity and velocity change, because much larger time intervals may be used.

Appendix II. Calculation Nomenclature and Formulas

Nomenclature

E_g = Generator voltage behind synchronous reactance x_{dg} (per-unit field current—saturation neglected).

e_g = Generator voltage corresponding to direct-axis flux (per-unit generator direct-axis flux).

x_{dg} = Generator direct-axis synchronous reactance.

x_{dg}' = Generator direct-axis transient reactance.

I = Generator output current.

δ = Angle between generator and system equivalent motor—electrical degrees.

P_g = Generator power output to system.

Q_g = Generator reactive kilovolt-amperes supplied to system and corresponding to voltage E_g and current I.

E_s = System equivalent-motor voltage behind synchronous reactance x_{ds} (per-unit field current—saturation neglected).

e_s = System equivalent-motor voltage corresponding to direct-axis flux (per-unit motor direct-axis flux).

x_{ds} = Direct-axis synchronous reactance of system equivalent motor plus system impedance between this motor and the generator.

x_{ds}' = Direct-axis transient reactance of system equivalent motor plus system impedance between this motor and the generator.

$x_{gs} = x_{dg} + x_{ds}$.

T_{gdo}' = Open-circuit time constant of generator—seconds.

T_{gf}' = Field-circuit time constant of generator during loss-of-field conditions—seconds.

T_{sf}' = Field-circuit time constant of system equivalent motor during loss-of-field conditions—seconds.

t = Time in seconds.

Δt = Time interval selected for step-by-step calculations—seconds.

H_g = Inertia of generator and turbine rotors—kilowatt seconds of generator and turbine rotors divided by the kilovolt-ampere base used in calculations.

H_s = Inertia of system equivalent motor—kilowatt-seconds of system equivalent motor divided by kilovolt-ampere base used in calculations.

H_g' = Equivalent inertia of generator and turbine rotors relative to an infinite system inertia—kilowatt-seconds divided by kilovolt-ampere base used in calculations.

K_g' = Generator acceleration constant corresponding to H_g'.

E_{si} = Initial value of E_s before generator loss of excitation.

P_{gi} = Initial value of P_g before loss of excitation.

f = System frequency—cycles per second.

$\delta_{(t)} = \delta$ at time t.

$E_{g(t)} = E_g$ at time t.

$E_{s(t)} = E_s$ at time t.

$e_{g(t)} = e_g$ at time t.

$e_{s(t)} = e_s$ at time t.

$\Delta e_{g(t)}$ = Change in e_g during time interval Δt between time t and time $(t+\Delta t)$.

$\Delta e_{s(t)}$ = Change in e_s during time interval Δt between time t and time $(t+\Delta t)$.

$P_{g(t)}$ = Generator power output at time t.

$Q_{g(t)} = Q_g$ at time t.

$\omega_{(t+1/2\Delta t)}$ = Average velocity of generator rotor above system speed during time interval Δt between time t and time $(t+\Delta t)$—electrical degrees per time interval Δt.

$\omega_{(t-1/2\Delta t)}$ = Average velocity of generator rotor above system speed during the time interval Δt between time $(t-\Delta t)$ and time t—electrical degrees per time interval Δt.

$\Delta\omega_{(t)}$ = Change in velocity of generator rotor from the average velocity $\omega_{(t-1/2\Delta t)}$ to the average velocity $\omega_{(t+1/2\Delta t)}$—electrical degrees per (time interval $\Delta t)^2$.

$E_{gf(t)}$ = Average generator per-unit field current corresponding to average exciter output voltage during time interval between time t and time $(t+\Delta t)$, divided by the generator field circuit resistance.

All quantities are per-unit quantities on the selected system kilovolt-ampere base unless otherwise indicated.

Formulas

FIELD CURRENT—DIRECT-AXIS FLUX

$$E_g=\frac{e_g-\dfrac{(x_{dg}-x_{dg}')e_s\cos\delta}{x_{gs}-(x_{ds}-x_{ds}')}}{1-\dfrac{x_{dg}-x_{dg}'}{x_{gs}}\left[1+\dfrac{(x_{ds}-x_{ds}')(\cos\delta)^2}{x_{gs}-(x_{ds}-x_{ds}')}\right]} \tag{1}$$

$$E_s=\frac{e_s-\dfrac{(x_{ds}-x_{ds}')e_g\cos\delta}{x_{gs}-(x_{dg}-x_{dg}')}}{1-\dfrac{x_{ds}-x_{ds}'}{x_{gs}}\left[1+\dfrac{(x_{dg}-x_{dg}')(\cos\delta)^2}{x_{gs}-(x_{dg}-x_{dg}')}\right]} \tag{2}$$

REAL AND REACTIVE POWER

$$P_g+jQ_g=\frac{E_gE_s}{x_{gs}}\sin\delta+ j\left(\frac{E_g^2}{x_{gs}}-\frac{E_gE_s}{x_{gs}}\cos\delta\right) \tag{3}$$

INERTIA

$$H_g'=\frac{H_gH_s}{H_g+H_s} \tag{4}$$

GENERATOR ACCELERATION CONSTANT AND VELOCITY

$$K_g'=\frac{180f(\Delta t)^2}{H_g'} \tag{5}$$

$$\Delta\omega_{(t)}=K_g'(P_{gi}-P_{g(t)}) \tag{6}$$

$$\omega_{(t+1/2\Delta t)}=\omega_{(t-1/2\Delta t)}+\Delta\omega_{(t)} \tag{7}$$

FIELD DECREMENT—GENERATOR

When change in exciter voltage during the time interval Δt must be considered.

$$\Delta e_{g(t)}=\frac{E_{gf(t)}-E_{g(t)}}{T_{gf}'}\,\Delta t \tag{8}$$

When excitation voltage supplied to the generator field may be considered as zero during the time interval Δt,

$$\Delta e_{g(t)}=\frac{-E_{g(t)}}{T_{gf}'}\,\Delta t \tag{9}$$

FIELD DECREMENT—SYSTEM EQUIVALENT MOTOR

$$\Delta e_{s(t)}=\frac{E_{si}-E_{s(t)}}{T_{sf}'}\,\Delta t \tag{10}$$

Appendix III. Sample Calculations for Generator of Figure 1

GENERATOR RATING

137,500 kva 13,800 volts
110,000 kw 60 cycles
80 per cent power factor 1,800 rpm
Full-load field current = 1,540 amperes
Per-unit field current = 720 amperes
Prime mover—medium-pressure steam turbine

Field Investigation of the Characteristics of Lightning Currents Discharged by Arresters

I. W. GROSS
MEMBER AIEE

G. D. McCANN
ASSOCIATE AIEE

EDWARD BECK
MEMBER AIEE

Synopsis: Data have been obtained during the past three years on the magnitude and wave shape of lightning currents discharged by arresters in service on several solidly grounded neutral circuits of the American Gas and Electric Company system. Correlated measurements have been obtained with the cathode-ray oscillograph, the fulchronograph, and the surge-front recorder. The maximum arrester-phase leg current recorded in this investigation was 9,600 amperes with 70 per cent of the currents less than 1,000 amperes. The wave fronts of the low-magnitude currents were, in general, abrupt. For crest magnitudes of over 1,000 amperes they ranged from two to over 25 microseconds to crest. The maximum rate of rise recorded was 2,500 amperes per microsecond.

The components in all discharges were of relatively short duration, with times to half value averaging 25 microseconds and with no measurable durations in excess of 500 microseconds.

Of the 18 arrester-phase legs studied, all but one discharged at least once during the investigation. Nineteen of the 21 separate records of discharges in three-phase arrester banks had currents which, if arresters had not been installed, would have produced voltages in excess of the standard basic impulse level for the voltage class of the apparatus involved, so that failure of unprotected equipment might have occurred.

Purpose and Scope

THIS investigation was undertaken in 1939 to determine the lightning duty imposed on arresters in service. Factors that affect this are frequency of occurrence, magnitude, wave shape, and multiple character of the discharges. Five measuring stations were set up at different three-phase arrester locations, two in substations of The Ohio Power Company in Ohio, and three in substations of the Appalachian Electric Power Company in Virginia. One of the latter locations was moved during the course of the investigation so that in all, a total of six arrester banks was studied.

Description of System and Recording Installations

A summary of the pertinent characteristics of the substations at which the recording installations were made is given in Table I. They are located in regions where lightning was known to be frequent and at arrester banks where past experiences had indicated that a higher than average number of discharges was to be expected. The power sources feeding the circuits on which the study was made are solidly grounded.

The two recording stations on The Ohio Power Company system are at 33-kv delta-connected transformers. The installations on the Appalachian Electric Power company system are made in one similar 33-kv station; in two 12-kv stations, one with grounded-neutral and the other with delta-connected transformers; and in one 132-kv station with delta-connected transformers. The station grounds and earth resistivity are low at all of these locations.

Pictures and a schematic diagram of the recording equipment are shown in Figures 1 and 2. Each installation consists of a high-speed fulchronograph, and surge-crest ammeter links in each arrester phase lead together with a slow-speed fulchronograph, crest ammeter links, and a magnetic surge-front recorder in the common ground lead of the three arrester phases. In two cases (Twin City and Stone Creek), a cathode-ray oscillograph is also connected in the common lead. The cathode-ray oscillograph equipment was designed and built especially for the purpose of automatically recording lightning transients. It consists essentially of a glass-envelope hot-cathode oscillograph tube, the necessary electrical circuits, a special camera to photograph the trace produced by the transient on the fluorescent screen, and a current shunt. These recording instruments have been described in the literature.[1,2]

The field installations were serviced on an average of once a week during the lightning season, and usually after each lightning storm.

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I. W. Gross is electrical research engineer with American Gas and Electric Service Corporation, New York, N. Y. G. D. McCann is transmission engineer, industry engineering department, and Edward Beck is section engineer, switchgear engineering department, both with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

This investigation has been made possible by the co-operative efforts of the two companies with which the authors are associated, as well as a number of individuals. The authors acknowledge the contributions of the field organizations of The Ohio Power Company and the Appalachian Electric Power Company in installing and servicing the field installations, and of O. Ackermann of the Westinghouse Electric and Manufacturing Company for his assistance in the design and supervision of the cathode-ray oscillographs.

GENERATOR AND SYSTEM CONSTANTS

$x_{dg}=0.940$ $T_{gdo}'=6.26$ seconds
$x_{dg}'=0.241$ $T_{gf}'=0.975$ second (corresponding to field resistance of 0.138 ohm and external resistance of 0.747 ohm)
$x_{ds}=0.511$ $T_{sf}'=9.0$ seconds
$x_{ds}'=0.264$ $H_s=53.4$ seconds
$x_{gs}=1.451$ $H_g=7.7$ seconds
$\Delta t=0.1$ second $H_g'=6.73$ seconds

Reactance—generator bus to generator terminals = 0.048

All values are on machine rating 137,500 kva and 13,500 volts. Reactances are per unit.

Formulas

$$E_g=\frac{e_g-0.582e_s\cos\delta}{0.518-0.099(\cos\delta)^2}$$

$$E_s=\frac{e_s-0.329e_g\cos\delta}{0.830-0.159(\cos\delta)^2}$$

$$P_g+jQ_g=0.689E_gE_s\sin\delta+j(0.689E_g^2-0.689E_gE_s\cos\delta)$$

$$\Delta\omega_{(t)}=16.0(0.436-P_{g(t)}) \text{ since } P_{g1}=0.436$$

$$\Delta e_{g(t)}=-0.103E_{g(t)}$$

$$\Delta e_{s(t)}=\frac{0.836-E_{s(t)}}{90} \text{ since } E_{s1}=0.836$$

The values calculated with the preceding formulas are listed in Table II.

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General Discussion of Records

Twenty-one separate sets of records have been obtained involving 49 single-phase arrester-discharge currents with a total of 88 individual components. Records were obtained at all six stations and their distribution is shown in the lower part of Table I and in the top curve of Figure 3. As shown in Figure 3, more discharges per arrester year were obtained than in other previously reported investigations. This is because the locations were chosen from past experience which indicated that they would yield more than the average number of records. The curves in this figure show considerable variation from each other. This is probably due to a number of factors including variance in lightning frequency, the number of arresters in multiple, and the type of line construction.

Of the 21 separate records, ten involved discharges in three phases, eight in two phases, and three in only one phase. Sixteen of the records showed only single-component discharges in each of the individual arrester phases. In the remaining five, which had multiple components in one or more phases, the number of components varied from two to 12. Of the 21 records, all but three had maximum crest magnitudes in the common lead which were substantially equal to the sum of the maximum crests in the individual phases.

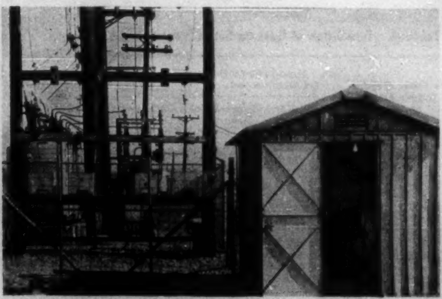
In 12 of the records all of the components were entirely negative. Four records were entirely positive. For two records the components in two of the phases were entirely negative, while the components in the third phase were entirely positive. One record had nonoscillatory components of both polarities in the same phase. Two of the records had oscillatory components. Twenty-three of the 88 individual components, or 26 per cent, were initially positive.

Table I. Summary of Installations

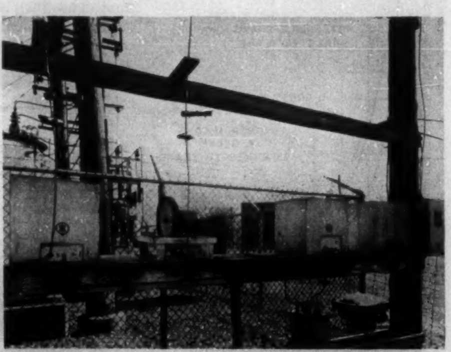
	The Ohio Power Company		The Appalachian Electric Power Company			
	Twin City Substation	Stone Creek Substation	Huntington Court Substation	Christiansburg Substation	Reusens Substation	Rocky Mount Substation
Type of station	Delta	Delta	Grounded wye	Delta	Delta	Delta
Miles to nearest grounded source	20	10	0	0.6	82	25
Circuit voltage	33 kv	33 kv	12 kv	12 kv	132 kv	33 kv
Arrester type	Line	Line	Station	Line	Station	Station
Arrester rating	30 kv	30 kv	12 kv	12 kv	109 kv	30 kv
Arrester ground resistance—ohms	0.5	1.8	0.7		0.4	0.2
Earth resistivity in meter ohms	15-40	15-70	55-220		45-250	120-200
Number of lines	2	2	2	1	2	1
Type of lines	Wood	Wood	Wood	Wood	Steel—1 ground wire	Wood
Number of years of study	2	2	3	2	2	1/2
Number of composite 3-phase records	9	3	2	1	2	4
Apparatus standard basic impulse level, kv	200	200	110	110	650	200
Number of records exceeding basic impulse level if no arresters	9	3	1	1	1	4
Number of discharges per individual arrester phase	21	8	2	3	5	10
Average discharges per year, phase 1	3.0	1.5	0.3	0.5	1.0	8.0
Average discharges per year, phase 2	3.5	1.0	0.3	0.5	0.5	6.0
Average discharges per year, phase 3	4.0	1.5	0	0.5	1.0	6.0

Detailed Discussion of Particular Records

A few of the records which have been obtained are discussed in detail because of their special interest. In considering the fulchronograms, the limit of sensitivity of the fulchronograph should be borne in mind. Its lower range of current sensitivity is about 50 amperes. In addition, the wave shape of arrester-discharge currents with times to half value of less than about 20 microseconds will not be recorded. Therefore, in the case of currents of short time to half value or of short duration, the instrument will indicate that they were short but will not supply accurate information on the times involved. Attention is called to this because most of the components of discharge currents recorded in this investigation were of short duration.



A—General view showing the fulchronographs and cathode-ray oscillograph



B—Close-up of the fulchronographs and cathode-ray oscillograph shunt

Figure 1. Lightning recording station at the Twin City Substation of The Ohio Power Company

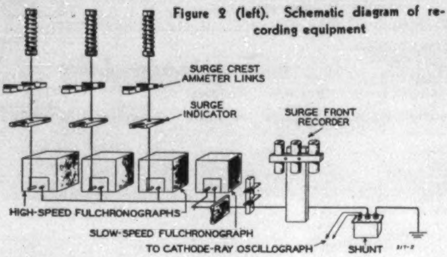

SURGE CREST AMMETER LINKS
SURGE INDICATOR
SURGE FRONT RECORDER
HIGH-SPEED FULCHRONOGRAPHS
SLOW-SPEED FULCHRONOGRAPH
TO CATHODE-RAY OSCILLOGRAPH
SHUNT

Figure 2 (left). Schematic diagram of recording equipment

Record 71, Figure 4. This record represents the first case in which directly comparable simultaneous field records of current were obtained with the fulchronographs and the cathode-ray oscillograph. Since only one arrester phase discharged, the same current passed through one high-speed fulchronograph, the low-speed fulchronograph, and the cathode-ray oscillograph, permitting a direct comparison of their records.

The current rose abruptly in 2.5 microseconds to 250 amperes and reached its crest of 500 amperes in 20 microseconds. It decayed to half value in 33 microseconds. After decaying to 100 amperes in 45 microseconds, it persisted at about this value until it decayed below the recording range of the fulchronograph in 175 microseconds. In 2,750 microseconds it again rose to about 100 amperes and lasted to 2,900 microseconds. This second portion might either be power follow or lightning current.

The cathode-ray oscillogram of this record illustrates the effect of the arrester breakdown on the wave shape of the front of the discharge current. After the gap breaks down, there is a rapid rise of current through the arrester brought about by the change in the surge circuit. This rate of change, measured in the arrester circuit during the period of readjustment, is greater than that which would exist in the line surge if no arrester discharge had occurred. Thus, the wave front of the original surge is more nearly as shown by the broken line of Figure 4b.

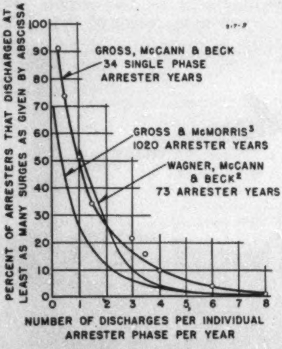


Figure 3. Weighted distribution of lightning discharges through individual arresters

Record 72, Figure 5. Only the cathode-ray oscillogram is shown for this record, since the duration is so short that only one link of each of the high-speed fulchronographs was magnetized. For this record, the surge-crest ammeter links, the slow-speed fulchronograph, and the cathode-ray oscillograph records indicate a crest magnitude of 1,000 amperes in the common ground lead. Surge-crest ammeter links and high-speed fulchronographs in the individual phase legs indicate currents totalling 550 amperes which is considerably less than the recorded neutral current. The reason for this is unknown and as pointed out previously, such a discrepancy has occurred in only a very few cases.

The neutral current has an abrupt front of less than one quarter of a microsecond and a time to half value of 8 microseconds. It decayed to zero in 32 microseconds and then rose to about 30 amperes after 66 microseconds.

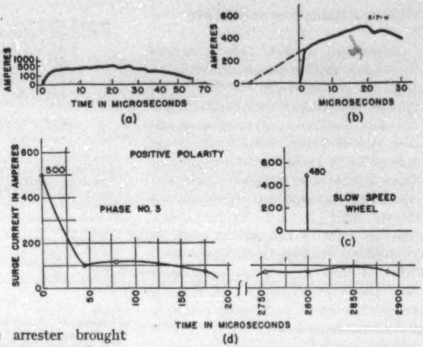


Figure 4. Record 71 obtained at the Twin City substation, May 8, 1941

(a) Cathode-ray oscillogram of current in common ground of lightning arrester

(b) Replot of oscillogram to a linear scale

(c) Slow-speed fulchronogram of current in common ground

(d) High-speed fulchronogram of current in phase 3

Record 77, Figure 6. This record is of particular interest. It gives the first cathode-ray oscillograph record of a lightning-arrester discharge current of considerable magnitude. It shows in detail the entire duration of the discharge. In addition, it is substantiated by fulchronograph records in each of the three poles and the neutral of the arrester. It was obtained during the failure of a defective bushing in the station.

Because of its importance, the original oscillogram shown in Figure 6a has been replotted to linear co-ordinates in Figure 6b. The fulchronograms are plotted in Figure 6c.

An analysis of the record shows that

Table II. Tabulation of Data on Surge Fronts*

Phases Discharged	Record Number	Nominal System Kv	Crest Magnitude in Amperes Average of Instruments	Time to Crest in Microseconds		Average Rate of Rise in Amperes Per Microsecond	
				Cathode-Ray Oscillograph	Surge-Front Recorder	Cathode-Ray Oscillograph	Surge-Front Recorder
3	92	33	−9,600		3.7		2,500
3	77	33	−6,500	3.5	4.4	1,850	1,480
2	87	33	−5,150		8.8		585
3	73	33	−3,650		6.6		555
2	91	132	−3,150		Over 25		Less 125
3	66	33	−2,000		8.3		240
3	55	33	−1,600		2		800

Of 11 additional records of surges with crest magnitudes less than 1,000 amperes, all had surge fronts of less than one microsecond as indicated by seven cathode-ray oscillograms and six surge-front recorded records, except the surge of record 71, Figure 4, which had an effective front of 2.5 microseconds.

* These measurements were made in the common ground lead of three-phase arrester banks.

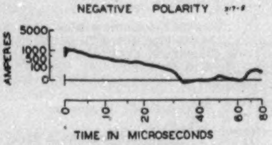
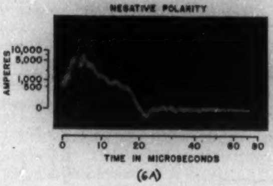


Figure 5. Cathode-ray oscillogram of record 72 obtained in common lead of arrester bank at the Twin City substation, May 22, 1941

The fulchronographs and surge-crest ammeter links both recorded single negative crest magnitudes of zero amperes in phase 1, 50 amperes in phase 2, 500 amperes in phase 3, and 1,000 amperes in the common ground. The high-speed fulchronographs showed no measurable duration



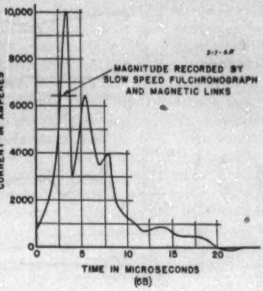


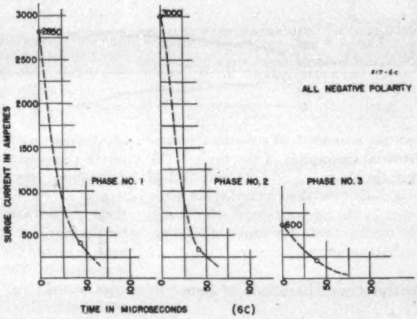
Figure 6. Record 77 obtained at the Stone Creek substation, June 12, 1941

6A Cathode-ray oscillogram of current in common ground lead of arrester bank

6B Replot of oscillogram to a linear scale

6C High-speed fulchronograms of current in each arrester phase

Slow-speed fulchronograph recorded a negative crest of 6,500 amperes and the same crest magnitudes as recorded by the fulchronographs were recorded by the surge-crest ammeter links

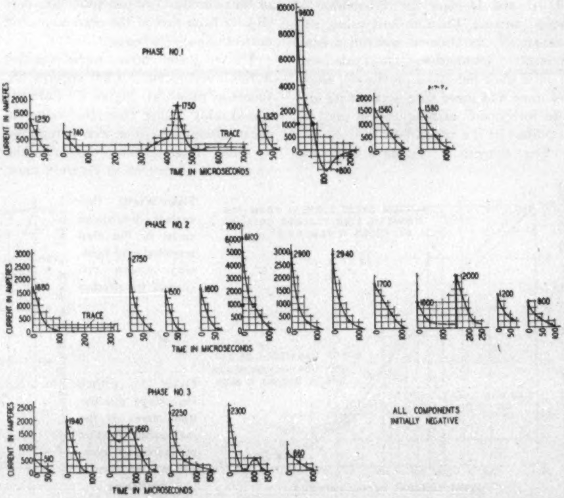


the three-phase legs of the arrester discharged simultaneously for the sum of the crest currents recorded by the fulchronographs and the surge-crest ammeter links in each phase equals the crest magnitude of 6,500 amperes recorded by the fulchronograph and surge-crest ammeter links in the common ground lead. The cathode-ray oscillogram showed a peak current of 10,000 amperes with a high oscillation believed to have been caused by the bushing failure. Its mean value of 6,500 amperes is in complete agreement with the records of the fulchronographs and magnetic links. The shape of the tail of the wave was probably affected by the bushing failure and, therefore, should not be taken as indicative of the normal arrester discharge.

Record 94, Figure 7. This record consists only of the high-speed fulchronograms in the individual phase legs of the arrester, as the slow-speed fulchronograph and the oscillograph in the common ground lead were not in operation. The record is of interest, because of the considerable number of components in all phases, 7 in phase 1, 12 in phase 2, and 6 or 7 in phase 3. The third component in phase 3 may actually be two superimposed on each other. The crest currents ranged from a maximum of 9,600 amperes, the highest recorded in this investigation, to a minimum of 510 amperes. The maximum measurable duration of any component was 400 microseconds. All of the components were totally negative, with the exception of the 9,600 ampere component which was initially negative followed by a positive portion with a crest of 800 amperes.

Record 96, Figure 8. This figure shows cathode-ray oscillograms of current in the common ground lead. The duration as measurable on the fulchronograms was too short to warrant plotting,

Figure 7 (below). High-speed fulchronograms of the current in each lightning arrester phase for record 94 obtained July 18, 1941 at the Twin City substation



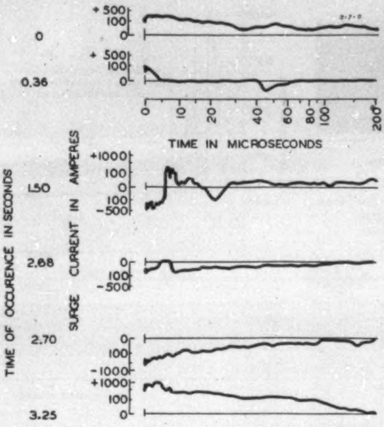


Figure 8 (left). Cathode-ray oscillograms of record 96 obtained in common ground lead of arrester bank at the Stone Creek substation, July 28, 1941

The fulchronographs recorded components of no measurable duration and the following crest magnitudes:

Phase 1	+150	+trace
−100	−200	+600
Phase 2	No current	
Phase 3	+150	+trace
−100	−300	+450
Neutral	+300	+trace
−100	−500	+1000

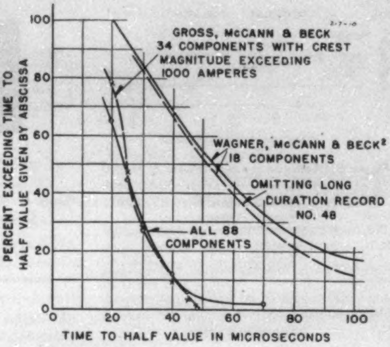


Figure 10. Percentage distribution-curves of times to half value of individual components of lightning currents discharged by arresters

but the recorded crest currents are tabulated in the caption of the figure. The arrester phase leg currents were of low magnitude and short duration, as indicated by the fulchronograph records and the oscillograms. The fronts of all components are abrupt.

Analysis and Discussion of Data

The records obtained have yielded considerable information on the magnitudes and wave shapes of currents discharged by arresters in service. Data on wave fronts are given in Table II and Figures 9, 10, and 11 show the distribution of crest currents, times to half value, and measurable durations of individual components. Comparisons are made with similar data published previously, since, as more and more data accumulate and are integrated, existing curves may be modified by the added data.

Crest Currents. Figure 9 shows the distribution of the crest magnitudes of all 88 components recorded in this investigation. For comparison with data secured in other investigations made with the surge-crest ammeter link only, a curve for the crests of the maximum components in each of the 49 arrester discharges is also shown. There is substantial agreement between this curve and the curve published by Gross and McMorris.[3] The deviations between the curves in the region of the low currents are probably the result of differences in instrument sensitivity. It is of interest that the percentage distribution of the crest magnitudes of the individual components differs only slightly from that of the maximum crest current of each discharge.

The maximum current so far recorded in this investigation is 9,600 amperes, obtained in record 94, Figure 7. Currents considerably higher than this value do occur infrequently in service as reported by other investigators; therefore, the upper limit of current in Figure 9 must be considered with reservations for the time being.

Wave Fronts. Table II lists the data obtained on wave fronts by means of the magnetic surge-front recorder and the cathode-ray oscillograph. They indicate that the low current discharges have abrupt fronts, usually less than a microsecond. This is to be expected, since for a short period following the sudden breakdown of the arrester gap, the current rises quickly as discussed for record 71, Figure 4. The low current discharges thus give little information on the fronts of the surges occurring on the line. For currents in excess of 1,000 amperes the fronts vary between 2 and 25 microseconds with average rates of rise of from less than 125 to 2,500 amperes per microsecond. The highest rate of rise reported by Wagner, McCann, and Beck[2] is 3,200 amperes per microsecond. Considering the number of measurements (29) of wave front reported in these two papers, the percentage of arrester discharges hav-

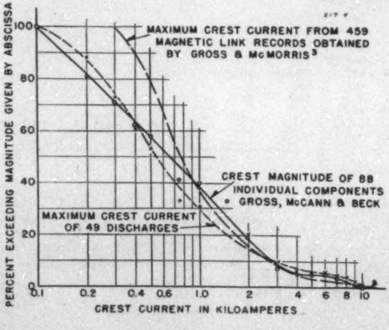


Figure 9 (left). Percentage distribution curves of the crest magnitudes of lightning currents discharged by arresters

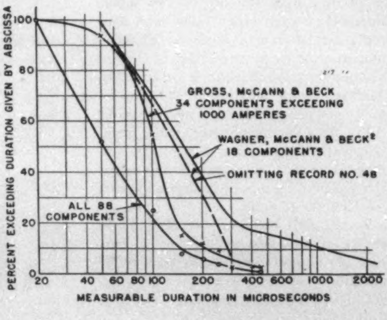


Figure 11 (right). Percentage distribution curves of the measurable duration of individual components of arrester discharges ing rates of rise greater than 3,200 amperes per microsecond will be small.

Times to Half Value. Figure 10 shows the percentage distribution of times to half value plotted on two bases:

1. Considering all 88 components.
2. Considering only the 34 components that exceeded 1,000 amperes.

This was done because the time to half value of the small magnitude components might be influenced by the fulchronograph's lower limit of sensitivity. However, both curves are in good agreement but indicate considerably shorter times to half value than the curves published by Wagner, McCann, and Beck covering measurements on a number of systems prior to 1941. It should be noted that the Wagner, McCann, and Beck curves include only the few records secured on the American Gas and Electric Company system before 1941.

Measurable Duration. Percentage distribution curves of the measurable duration of arrester-discharge currents are given in Figure 11. This duration is the time required for the current in a component to decay below about 50 amperes. Since the measurable duration should be greatly influenced by the crest magnitude of the surge, the data have been plotted both for all 88 components and for the 34 exceeding 1,000 amperes. As would be expected, longer durations are recorded for the higher magnitude discharges. The longest duration obtained in this investigation is below 500 microseconds. This corresponds to the results reported by Wagner, McCann, and Beck if the long-duration surge obtained by them on an ungrounded system is omitted. This was expected since the present study has been conducted entirely on solidly grounded neutral systems. As has been previously pointed out,[2,4] the windings of grounded-neutral transformer banks providing a path to ground in parallel with the arrester, are more likely to absorb the low-magnitude, long-duration portions of a surge than are the arresters.

Arrester Protection. The number of arrester discharges which were associated with surges that would have produced dangerous potentials in the absence of the arresters have been estimated and listed in Table I. If one or more arrester current components in one or more phases of any discharge had a crest magnitude which indicated that the incoming surge on the line would have produced a voltage at the station in excess of the standard basic impulse level, with no arresters present, it was counted as one possible failure. Nineteen of the total of 21 records had currents above the critical value. At Twin City, Stone Creek, and Rocky Mount, all discharges were in the danger zone. At every station, at least one surge appeared that might have caused damage.

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The Fundamentals of Industrial Distribution Systems

D. L. BEEMAN
ASSOCIATE AIEE

R. H. KAUFMANN
MEMBER AIEE

Synopsis: The intensive manufacturing activity accompanying the present defense program has stimulated interest in industrial electric power distribution. The ultimate objective is one of effectively satisfying electric power requirements with reasonable first cost consistent with a fair standard of safety and service reliability.

This paper presents the fundamental aspects of industrial power distribution, with particular reference to low-voltage power supply to distributed electrical machinery, and compares various basic system designs, including large concentrated substations and distributed load-center substations with radial and secondary network modifications, as to safety, service reliability, simplicity, and so on, relative to estimated installed cost. The comparative analysis comprehends the complete electrical system between high-voltage supply bus and utilization terminal of low-voltage feeders. The ideal size of unit substation as influenced by operating voltage and load density is covered.

Principles of system design here disclosed are applicable not only to new plant construction but to expansion or modernization of existing plants as well.

THE intent of this paper is to present the comparative merits of the several typical forms of industrial plant electrical distribution systems presented with a view to the general adoption of that system which will, in the majority of cases, meet the overall requirements most effectively.

General Considerations

The characteristic features on which the evaluation of merit depend are:

First cost.
Safety.
Service reliability.
Simplicity of operation.
Voltage regulation.
Efficiency.
Maintenance cost.
Flexibility in meeting load changes.

Only the *over-all* system performance is significant. This requires simultaneous recognition of all elements between the primary power source and the ultimate utilization machines. These elements are: primary feeder switching and transmission circuits, step-down transformers together with their associated high-voltage and low-voltage switching equipment, low-voltage circuits to secondary distribution centers, secondary distribution panels and branch circuits to individual loads.

Primary Operating Voltage

The primary operating voltage will, in general, be fixed by considerations outside the general factory requirements. The presence of a considerable number of large motors may favor the use of 2,400 or 4,160-volt primary systems. It will otherwise be generally preferable to adopt the incoming utility-system voltage so long as it is not in excess of 15 kv. Common system voltages are 4,160 and 13,200 volts.

Secondary Operating Voltage

The great majority of manufacturing plants with which we are concerned incorporate machine tools powered by relatively small motors, usually not in excess of 25 horsepower. Except for very small fractional horsepower units, the adoption of a nominal 440-volt level is to be preferred to lower voltages. Advantage can thus be taken of substantial reductions in secondary switching cost and size together with corresponding reductions in secondary cable cost.

The use of four-wire system suitable for 440-volt motors offers a promising means of supplying both power and fluorescent lighting from a common system retaining the advantages of 440-volt operation.

Incandescent lighting, while of ever lessening importance for factory lighting, may be encountered, and is suited to 120-volt operation. This operating voltage may be derived from 440-volt system by transformers, or by separate lighting transformers independent of the power system.

A load composed largely of incandescent lights and small motors unsuited to 440-volt service would favor the use of a 208Y/120-volt system.

Load Density

The load densities commonly encountered, including both power and lighting, are in the range of about 8 to 25 volt-amperes per square foot.

Lighting load levels for up-to-date illumination intensity may be expected to be 4 to 6 volt-amperes per square foot for incandescent lamps and 2 to 3 volt-amperes per square foot for high power-factor fluorescent lamps.

Power-load densities will be subject to greater variation ranging between about 6 and 20 volt-amperes per square foot. Areas devoted largely to assembly will show the lower load levels while intensive manufacturing areas will show the higher levels.

Accumulated records indicate a fairly limited load-density range in the order of 8 to 15 volt-amperes per square foot for existing manufacturing plants, and the higher contemplated values apply to proposed new plants. Plants of higher load density are known, but this fact does not affect the following discussion.

Specific System Comparison

To avoid abstract comparisons, a representative distributed load block has been selected totaling 3,600 to 3,750 kva with a load density of 10 volt-amperes per square foot and suited to 440-volt utilization. Individual control of low-voltage radial feeders, averaging 150 kva each, is to be provided in all cases. The primary service is considered to be 4,160-volt, three-phase, 60-cycle, with a short-circuit interrupting requirement of 150,000 kva.

Evaluations of comparative installed cost includes all electrical equipment between the main high-voltage power supply bus and the terminals of the 150 kva low-voltage feeders.

The typical forms of distribution systems have been classified as follows:

A. Single large substation—radial low-voltage feeders.
B. Distributed load-center system.
1. Simple radial (Figure 4).
2. Primary selective (Figure 5).
3. Secondary selective (Figure 7).
4. Secondary network (Figure 8).

Variations and combinations of these systems are used, but this does not alter the basic data presented here.

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D. L. BEEMAN and R. H. KAUFMANN are both of the industrial engineering department, General Electric Company, Schenectady, N. Y.

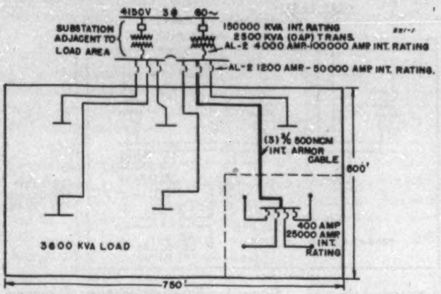

4150V 3φ 60~
SUBSTATION ADJACENT TO LOAD AREA
150000 KVA INT. RATING
2500 KVA (OAP) TRANS.
AL-2 4000 AMP-100000 AMP INT. RATING
AL-2 1200 AMP - 50000 AMP INT. RATING.
(3) 3/c 500MCM INT. ARMOR CABLE
600'
400 AMP 25000 AMP INT. RATING
3600 KVA LOAD
750'

Figure 1. Schematic diagram of conventional distribution system

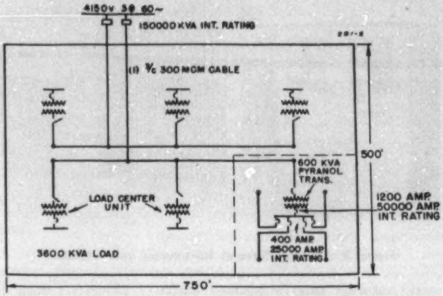


Figure 2. Schematic diagram of load-center distribution system

To simplify the text and aid in clarifying the interpretation of results, the system comparative study has been presented in two sections:

1. A comparison of the single large substation versus the distributed load-center system.
2. Comparison of the several forms of load-center distribution.

I. Single Large Substation Versus Distributed Load-Center System

In the past it has been common practice to distribute power to a relatively large factory area at utilization voltage from a single step-down substation accommodating several thousand kilovolt-amperes. This will be referred to as the conventional method, a typical circuit diagram of which is illustrated in Figure 1.

The modern load-center distribution method incorporates distributed step-down stations of small capacity (600–1,000 kva for 440-volt operation and 300–600 kva for 208Y/120- or 220-volt operation) from which low-voltage power is distributed to the immediately surrounding area as shown in Figure 2. A *load-center unit* is defined as an integrated step-down station consolidating step-down transformer, high-voltage switching unit, and low-voltage switching equipment, as typified by the illustration in Figure 3.

Application of the distributed load-center system not only allows a substantial reduction in total system investment cost, but also provides numerous other advantages.

Cost

The distributed load-center system shows a lower installed cost than the conventional system because:

1. Power is transmitted directly to the utilization area at high voltage. This materially reduces cable investment cost and total I^2R losses.
2. The cost of low-voltage switching equipment is materially less than for the conventional method because of lower short-circuit currents associated with the smaller substations.

Although transformer cost per kilovolt-ampere diminishes with increasing rating, this influence is far overshadowed by the cost reductions enumerated under 1 and 2 for ratings of 600–1,000 kva at 440 volts and 300–600 kva at 220 volts.

For example, the installed cost of these two systems, based on the reference load block of 3,750 kva, would be about $71,000 for the distributed load-center system, Figure 2, compared with about $103,000 for the large single substation type Figure 1.

Reductions in secondary cable and secondary switching-equipment costs for the distributed load-center system overshadow the increase in transformer cost. The necessity of simultaneous recognition of all system parts for determining the lowest *over-all* first cost is evident.

System Planning and Financing

The use of small load-center substations permits electrical capacity to be added in small increments when and where needed; thus large capital layouts are not required, making it easier to finance plant extension. This eliminates the necessity for involved planning; hence, reduces engineering costs and errors. On the other hand, large substations require involved forecasting of location and magnitude of loads and high initial investment, which is not completely put to work until the anticipated load is reached. If future developments do not permit utilizing the capacity of the large substation as planned, then the initial investment is not utilized efficiently or effectively.

A spare transformer for the small load-center unit represents less idle capital than the larger spare unit for the large station.

Installation Considerations

The small substations compared with large ones offer many advantages from the standpoint of handling and installation. For example:

1. No expensive enclosures or foundations are required.
2. The small self-contained metal-enclosed load-center unit can be located within the working area close to the center of the particular load area being served, while the large substation must generally be located at one side of the working area because of size and weight of component parts to be handled.
3. They can be moved more easily from one location to another to cope with changes in electrical demands accompanying changes in manufacturing technique.
4. In the event of a failure, service can be restored more quickly because less time and equipment is required to move the small units. The change can usually be made with equipment and personnel normally available around the average factory.

These advantages are only fully realized with completely metal-enclosed unit-type substations as shown in Figure 2.

Voltage Regulation

Because of the shorter secondary runs in load-center distribution, voltage drop and light flicker are less. This improves the performance of motors and lamps, and hence facilitates more and better production.

System Flexibility

Load-center distribution is ideally adapted to industrial plants, because it may be used in various circuit arrangements to meet varying degrees of service continuity required by manufacturing processes, and because small units are

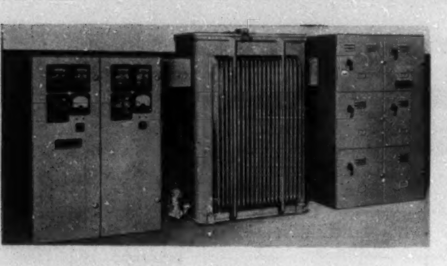


Figure 3. Typical form of load-center unit substation

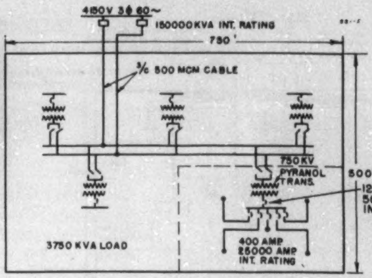


Figure 5. Schematic diagram of primary-selective-circuit arrangement

used, variation may be adapted within small factories.

This comparison indicates the load-center distribution system as being the most desirable for the average industrial plants. The single large substation design is not only more costly, but is deficient in other important respects, such as electrical operating efficiency, voltage regulation, greater initial engineering and planning, higher financing cost, and less flexibility as to future expansion or extensions.

Subsequent analysis of variations in system arrangement will, therefore, be limited to the distributed load-center design.

II. Load-Center-System Circuit Arrangements

The principal reason for considering variations in circuit arrangements is that of service reliability. The various arrangements may be classified into four groups typified by forms 1, 2, 3 and 4, which provide progressively increased service reliability and may be individually characterized as follows:

(All percentage cost figures are based on the typical example previously defined.)

TYPE 1—SIMPLE RADIAL (*See Figure 4*)

This arrangement represents the simplest form of load-center distribution and provides a single direct electrical channel extending to each low-voltage load area. All system elements are considered to be operated at rated capacity.

The lowest possible *installed cost* is obtained due to simplicity of the arrangement and complete absence of secondary power-supply duplication, together with selection of the optimum size of load-center unit. (See appendix for further discussion.)

For cost comparison, this arrangement will be arbitrarily assigned a value of 100 per cent.

The inherent simplicity together with circuit breakers of adequate interrupting capacity results in a high order of safety. The load-center unit-substation primary disconnecting switches are for isolating a load-center unit only when completely deenergized.

All load to a particular area is always delivered by way of a single well-defined route which makes for simplicity in operation.

Voltage regulation is only slightly inferior to the best circuit arrangement considered, the difference being due to normally operating all transformers and cable at full rated capacity.

Service reliability of this arrangement is not the best, for a failure of the primary cable or its associated junctions interrupts power flow to the entire load block pending repair or installation of a temporary high-voltage circuit in place of the faulty section. A failure of a load-center transformer or its associated connections results in an interruption of power to that local area pending repair or replacement of the faulty unit. To deenergize one element for inspection or maintenance (that is, primary feeder breaker, load-center transformer or high-voltage disconnect switch) likewise demands interruption of power flow.

In the great majority of applications, a higher degree of service reliability will be desired and warranted.

TYPE 2—PRIMARY SELECTIVE (*See Figure 5*)

As will be observed from the figure, this circuit arrangement is distinguished by the use of duplicate primary high-voltage feeders, either of which is capable of handling the entire load block in combination with a high-voltage transfer means at each load center.

Under normal operating conditions, approximately equal numbers of load-center unit substations are fed from each primary cable. While operating normally in this condition, operating characteristics are practically the same as the type 1.

The presence of the primary transfer means makes it possible to reestablish service to all load centers with one primary feeder deenergized.

A failure of one primary cable would interrupt service to those load centers which were being fed from this circuit. Service can be restored by changing the position of the transfer means at the respective load-center unit substations. The duration of the outage will depend on the time required to locate the system operator who is authorized to operate the transfer means and the time consumed in visiting those stations at which power interruption occurred.

For maintenance or inspection of pri-

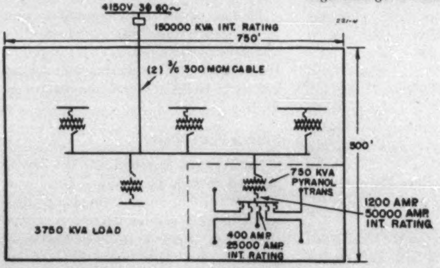


Figure 4. Schematic diagram of simple radial-circuit arrangement

mary circuit equipment, only a short interruption is occasioned since load centers are individually transferred before deenergizing the primary feeder.

It is important to note that, following reestablishment of voltage on a primary feeder, load-center units should again be returned to their normal feeder, which will account for another service interruption of short duration.

The use of a *primary loop circuit* as illustrated in Figure 6 constitutes a variation of the primary selective circuit arrangements. Unless automatic sectionalizing circuit breakers are incorporated, a primary circuit fault results in an extended interruption to the entire load, no part of which can be reenergized until the circuit has been sectionalized to exclude the faulty section. The chief advantages of the loop circuit are:

1. **The ability to combine feeder and tie-circuit functions when primary power-supply stations are located on opposite sides of the plant.**
2. **The ability to isolate a particular load center in the event that fire or explosion should damage both primary cables at one location.**
3. **Primary circuit loading is inherently equalized with both primary switches at each load center closed as they normally would be.**

The *installed cost* is increased due to additional primary switching equipment, high-voltage cable, and the use of primary transfer means on all load-center unit substations. The cost of this arrangement, relative to the cost of the simple radial becomes about 130 per cent.

For a primary circuit-interrupting requirement not exceeding 50,000 kva at circuit voltages below five kilovolts, metal-clad circuit-breaker transfer means, including a two-position structure with one circuit breaker, can be used without increasing the over-all cost level by more than approximately five per cent, as compared with disconnecting switches as the transfer means.

Safety is impaired when the primary transfer means is disconnect switches. Although interlocked to prevent operation except when low-voltage breakers are open, hazards nevertheless exist. The presence of a transformer turn-to-turn coil failures may allow the flow of substantial primary current irrespective of low-voltage breaker position. A major electrical fault at a transformer or its associated primary connections, which produces a primary circuit trip-out, may lead to serious consequences since normal routine procedure provides for transferring all deenergized load-center units to healthy feeders. When the faulty transformer is so transferred, the faulty circuit is *closed* on the healthy feeder by means of the transfer disconnect switch. Of lesser importance but nevertheless disturbing is the fact that a complete shutdown of both primary feeders results, and much confusion may easily exist before service is reestablished.

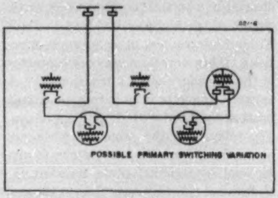


Figure 6. Schematic diagram of primary-selective-circuit arrangements with loop primary feeder

Circuit breakers adequate for high-voltage system short-circuit duty should be considered in all cases, as they eliminate the shortcomings of disconnect switches.

Service reliability is much better than that provided by this circuit arrangement except under unusual circumstances as covered under the topic of safety. It is to be expected that primary circuit failures will be far more numerous than transformer failures, many of which will be accounted for by imperfect cable tap connections or termination.

The service interruption of more or less indeterminate duration following a primary circuit outage is of course objectionable but may, in many instances, be tolerated.

The fact that both primary circuits are usually brought into close proximity at every unit substation represents a distinct hazard to service continuity.

The presence of the transfer means which must be manipulated on a number of load centers for every transfer from normal to emergency operation and again in restoring normal conditions, makes this circuit arrangement slightly more complicated to operate than the simple radial circuit arrangement.

TYPE 3—SECONDARY SELECTIVE (*See Figure 7*)

The secondary selective circuit arrangement differs from those previously described in that the load center incorporates complementary branches, each permanently associated with a particular high-voltage feeder. Transfer from normal to emergency operation is accomplished by means of a low-voltage tie breaker between the two complementary sections. This extends the duplication of supply to the load-center low-voltage bus and thus provides for continued service with either a transformer or a primary feeder out of service.

Under normal conditions the tie breaker is open and both transformer breakers are closed, in which state the system exhibits all the elements of simplicity to be found in the simple radial system.

For emergency operation, the transformer low-voltage breaker associated with the channel which has been or is to be deenergized is opened and the low-voltage tie breaker closed. Loading levels have been selected such that a transformer is subjected to not more than 125 percent rating during emergency operation.

The *installed cost* is increased over that of the primary selective arrangement largely because of reduced normal loading level on transformers. The relative cost level is between 145 and 150 per cent of the cost of the simple radial circuit. However, the reduced normal transformer loading provides additional benefits, aside from service reliability in the form of lower losses, lower transformer temperature and consequent increased life, and better voltage regulation. The cost per kva can be reduced by normally operating the transformers at a load more nearly equal to rating, depending on load curtailment to avoid excessive transformer load during emergency operation.

The same order of *safety* as associated with the simple radial circuits is retained. All transfer switching from normal to emergency conditions and vice versa is performed on full interrupting-capacity low-voltage air circuit breakers. The possibility of producing a complete primary system shutdown by transferring a fault to the healthy feeder is eliminated. The individual transformer primary disconnect switch is used only for isolating purposes and on this basis may be considered to represent only a slight hazard.

In respect to *service reliability*, this circuit arrangement excels the primary-selective circuit. Continued operation is insured with a transformer as well as a primary cable out of service. For very little increased cost, provision can be made for automatic transfer to emergency operation through electrical operation of the tie breaker, by which means restoration of voltage may be accomplished within an interval of about one to two seconds. The normal operation of transformers at considerably less than rating allows an unusual temporary load demand in any area to be met without distress.

Simplicity of operation comparable with that of the simple radial circuits is obtained. The low-voltage tie breaker between complementary bus sections represents the only feature which modifies the operating procedure.

Voltage regulation is reduced under normal operating conditions as a result of reduced transformer loading. Voltage regulation under emergency operating conditions is only slightly inferior to that of the simple radial circuits as a result of transformer operation of 125 per cent rating.

TYPE 4—SECONDARY NETWORK (*See Figure 8*)

The secondary network system is distinguished from others in that continuously connected duplicate sources are provided to each low-voltage load-center bus. The prime source is represented by a transformer tie to the primary system while low-voltage tie circuits interconnected with one or more other prime sources constitute the emergency source. Two or more primary feeders are used, and the emergency source for one load center is derived from prime sources associated with a different primary feeder.

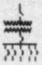
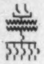
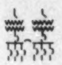
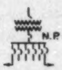
Provision is made for automatically clearing any particular prime source or tie circuit which becomes involved in fault without interrupting other source connections. This requires a network protector in the secondary of each transformer. A network protector is an automatic circuit breaker equipped with directional relays. The relays trip the protector whenever power flows from the low-voltage to the high-voltage terminals of the transformer. It does not trip for power flow in the opposite direction. Automatic reclosing is provided.

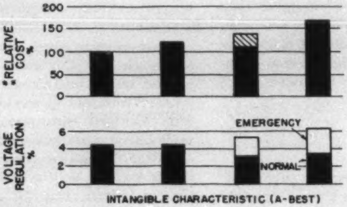
The required reserve capacity in primary cables and transformers is not greatly different than that for the secondary selective circuits. Loss of voltage on one primary feeder immediately results in automatic transfer of the load previously carried by that feeder to adjacent load centers by way of the interconnecting low-voltage tie circuits. Reserve transformer capacity is needed to avoid transformer loading levels in excess of 125 per cent of rating. Two primary feeders, to which the load centers are alternatively connected, would require that the normal load level per transformer be held to 63 per cent of rating. The required transformer capacity may be reduced by about 30 per cent by the use of three or more primary feeders. This can also be accomplished in two-feeder systems by the introduction of primary transfer switches in combination with suitable means for insuring prompt transfer of load-center units to healthy feeders following loss of voltage on one feeder. Transfer devices offer no economic advantage over three-feeder systems, hence are not considered because of the hazard of transfer switches.

It will be immediately apparent that the introduction of permanently connected emergency tie circuits will increase the low-voltage short-circuit current level unless the size of load-center units is simultaneously reduced. Where a number of load-center units are interconnected on the low-voltage side, careful design and specification of tie circuit impedance will be necessary to realize the most favorable economic balance. It will quite often be necessary to insert reactors in the tie circuits to obtain the desired results. The same care in system design is required in incorporating one or more additional load-center units in the network at a future date as dictated by future load conditions.

The *installed cost* of the secondary network referred to the simple radial type will be in the range of 175 to 200 per cent.

Table I. Relative Performance Characteristics, Load-Center Distribution System Design

	NO 1 SIMPLE RADIAL (FIG 4)	NO. 2 PRIMARY SELECTIVE (FIG 5.)	NO. 3 SECONDARY SELECTIVE (FIG 7)	NO. 4 NETWORK (FIG 8)
LOAD CENTER UNIT DETAIL				 N.P
TRANSF. L.V. CIR BRKER	AL-2 1200A (50000 IC)	→	→	1200A NET PROT.
FEEDER CIR BRKER RATING	AE 1B 400A (25000 IC)	→	→	AL-2 400A (50000 IC)
TOTAL NO. FEEDER CIR. BRKERS	25	25	24	24
LOAD CENTER UNITS NO.	5	5	4	6
UNIT TRANSF KVA	750	750	750	750
TOTAL TRANSF KVA	3750	3750	6000	4500
FIRM LOAD CAPACITY	—	*3750	* 3750	*3750



INTANGIBLE CHARACTERISTIC (A-BEST)				
SAFETY	A	C	A	B
SERVICE RELIABILITY	D	C	B	A
EASE OF OPERATION	A	B	B	B
MAINTENANCE	A	C	B	D

*For high-voltage feeder outage only.

**Based on load area defined in text.

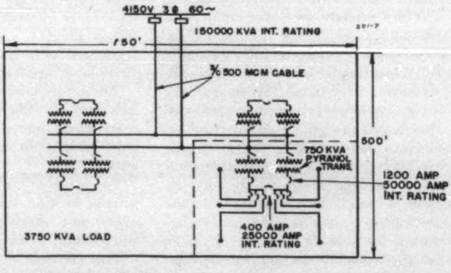


Figure 7. Schematic diagram of secondary-selective-circuit arrangement

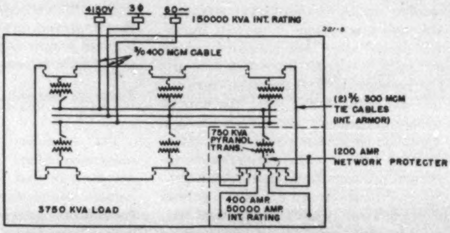

4150V 3Φ 60~
150000 KVA INT. RATING
22/-8
3/c 400 MCM CABLE
(2) 3/c 300 MCM TIE CABLES (INT. ARMOR)
750 KVA PYRANOL TRANS.
1200 AMP NETWORK PROTECTER
400 AMP 50000 AMP INT. RATING
3750 KVA LOAD

Figure 8. Schematic diagram of secondary network

The increased cost is the result of the introduction of low-voltage tie circuits and the more costly low-voltage switching equipment demanded by increased short-circuit current level.

With respect to *safety*, the secondary network, with low-voltage circuit breakers of adequate interrupting capacity, is comparable with the secondary selective, and the simple radial types. The fact that more than one source circuit must be opened to deenergize a particular load-center low-voltage bus represents some additional hazard, unless highly trained personnel is available.

The increased low-voltage short-circuit level to be expected with the secondary network is opposed to the interests of safety. Higher short-circuit current levels mean more violent disruptive effects at the point of fault. It may also jeopardize the safety of nearby branch circuit-switching units and individual load controllers unless the system is carefully checked and designed to avoid such a possibility.

Service reliability is of the highest order to the load-center low-voltage bus. A primary feeder or transformer unit may be deenergized without incurring interruption of service to any load center. Electrical faults in primary feeders or transformers will be automatically removed without service interruption except for a momentary voltage depression during the time the faulty element is being severed from the system.

Operating simplicity is sacrificed in the secondary network. In general any system incorporating multiple paralleled branches introduces operating complications. Should primary feeders originate from different high-voltage bus sections, as they should preferably do, voltage or phase-angle differences between these two sources will cause circulating currents which increase the loading on some load-center transformers and may create many perplexing problems for the operating departments. To deenergize a particular load-center low-voltage bus requires that two or more source circuits be opened. The maintenance and adjustment of network protectors with their associated network relays require greater skill than do conventional air circuit breakers.

Voltage regulation is good but not significantly superior. The normal load regulation may be either above or below the secondary selective circuit, depending on the particular form being considered. The regulation under emergency operation is likely to be inferior to the secondary selective form due to voltage drop in the low-voltage tie circuits.

It excels in the ability to accept heavy motor starting currents without light flicker which is directly the result of higher short-circuit current levels. This feature will be of value, however, only if motor starting currents approach that value which will produce light flicker. For instance, across-the-line starting of a 25-horsepower motor on a 440-volt system having a short-circuit current level of 25,000 amperes will result in a voltage drop of only one per cent. Since this would be judged to represent no noticeable flicker effect even with incandescent lighting, an increase in short-circuit current level could not be credited with improving light flicker. However, when larger motors are to be started on the 480-volt system, the network acquires some additional advantage.

Summary of Comparative Performance

Comparative installed-cost and electrical-performance characteristics of the various forms of distributed load-center systems are condensed in Table I. Qualities which are incapable of numerical evaluation are rated in terms of letters, *A* representing the best, *B* the next best, and so on.

The system cost comparisons are based on the representative load area expressly defined in the early part of this paper. While deviations from this basic load condition will influence the specific values to some degree, all systems will be similarly affected.

In every case, air circuit breakers of adequate interrupting capacity have been used for all low-voltage switching operations. Cascade arrangements[5] are used where it permits savings on breaker cost. All systems have been designed to provide individual control of the same number of secondary feeder circuits. An average unit feeder capacity of 150 kva (about 200 amperes at 440 volts) has been selected as representing a reasonable value commensurate with operating flexibility and ease of installation.

The selection of load-center unit ratings has been made with optimum installed cost as the objective.

Application of Limiters

For the purpose of diminishing the installed first cost, particularly in the case of the secondary network system, the substitution of limiters for air circuit breakers has been proposed.

To realize the service reliability of which the system is capable, of course, demands that the fault-current interrupting ability be unquestionably established under service conditions.

The application of limiters without proper isolating switches seriously impairs safety and operating flexibility. To deenergize any part thereof requires either that the entire network be deenergized or the particular section be isolated by literally cutting the energized conductors. The former would nullify the service continuity rating which the network systems claims as its chief advantage. The latter constitutes a serious hazard to operating personnel.

All repairs, additions or modifications made after the system is initially energized must be worked "hot". Although such practice is quite common in the utility industry, its successful application depends on a maintenance crew unusually skilled in handling electrical circuits and kept in practice by continual experience. The usual industrial plant would be unlikely to maintain a competent group of such skilled men and would afford insufficient practice to maintain proficiency.

The usual limiters incorporate no positive operation indicator. Tie circuits may have been divorced from the system as a result of internal fault without warning to the operating department and thus constitute a hazard to service continuity under emergency operating conditions.

Selection of Circuit Arrangement

The problem of selecting that circuit arrangement which will satisfy application requirements becomes primarily one of balancing service reliability against installed cost. It should, however, be borne in mind that a system, theoretically not perfect, but with the best possible type of equipment, is better than a system more perfect on paper, but with second-rate apparatus, such as a fusible element instead of a breaker.

Service reliability requirements will vary widely for various service conditions. The requirements for warehouses and storerooms will be low, although the ability to restore excitation to the more important circuits is obviously advantageous. General manufacturing plants operating on a piece-rate basis have observed that electrical-service outages of as much as twenty or thirty minutes rarely result in diminished output for that day. A few industrial processes are seriously influenced by service interruption for any significant interval, such as wire enameling and glass working.

In these more exacting applications, the secondary network system may be justified, although even an allowable outage interval of two or three seconds will permit application of the secondary selective circuits with automatic transfer, at considerably less cost.

For all but the most exacting requirements, the secondary-selective-circuit arrangement should find general if not universal application in industry in view of these outstanding merits.

1. Moderate Installed Cost

Based on maintaining full system load during emergency operation, the cost is moderately greater than that of the minimum tolerable system arrangement. The increased cost above the simple radial system is largely invested in active material (primary cables and transformers) which return benefits in the form of improved voltage regulation, reduced operating temperature, lower losses, and the ability to meet unusual temporary peak load demands without distress.

For areas which allow a lower order of service reliability, first cost can be reduced by curtailing load during emergency operation. A 40 per cent load reduction during emergency operation allows system first cost to be reduced to within about 10 per cent of the simple radial circuit. No change in load-center size or design is needed, the result being accomplished merely by assigning a larger normal load block to the particular load center.

2. High Order of Safety

All secondary switching operations affecting service are performed with low-voltage air circuit breakers of adequate interrupting capacity. The disconnecting switches on the primary of the transformers need only be operated when completely deenergized.

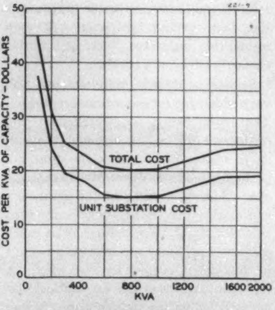


Figure 9. Cost of load-center distribution as a function of transformer size

480 volts

3. Good Service Reliability

Each secondary feeder circuit has access to two low-voltage sources derived from different primary feeders. Automatic transfer may be readily incorporated to limit service interruption to an interval of one or two seconds.

4. Simple to Operate

The operating simplicity of the simple radial system is obtained. There is no question as to the proper switching operation required to meet a particular situation. Each duplex load-center unit is self-contained and independent of others.

5. Voltage Regulation Is of the Best

The normal voltage regulation is entirely comparable with that of any other system, although the difference between any of the distributed load-center systems is actually not significant.

Appendix

Optimum Size of Load-Center Unit

The most economical size of unit substation is determined principally by the cost of the unit substation itself. This point is illustrated in Figure 9, which shows the cost per kilovolt-ampere of the small capacity of a radial load-center distribution system and the cost per kilovolt-ampere of the load-center unit.

The smaller load-center units are more expensive, because small equipment is inherently more expensive per kilovolt-ampere. The larger load-center units become increasingly expensive because of the large switchgear required to handle the high short-circuit current accompanying larger transformer banks. The optimum size of transformers lies between these two limits and for 480-volt secondaries 600-, 750-, and 1,000-kva units are most economical.

The shape of these curves of Figure 9 is accentuated by cable costs. For the smaller units more primary cable but less secondary cable is required. For the larger units less primary cable is required, but there is a very material increase in the amount of secondary cable necessary to transmit the power over the larger load area. This point is illustrated in Figure 10.

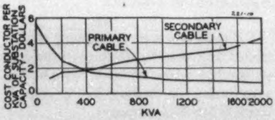


Figure 10. Cable cost as a function of transformer size

Load density affects the over-all system costs, principally because of the effect on cable cost. The total installed cost per kilovolt-ampere of load-center distribution system for load densities 2 volt-amperes to 20 volt-amperes per square foot inclusive are shown in Figure 11. The range of load densities studied should cover most average factory installations. It is interesting to note that, regardless of load densities, the shape of the curves is the same, and the optimum size of units is not affected.

The kilowatt losses per kilovolt-ampere capacity is substantially constant over the range of units from 100 to 1,500 kva.

The above curves have all been for 480-volt secondaries. When lower-voltage secondaries are used, the cost per kilovolt-ampere of the system is higher, because of the great amount of secondary cable required and because of the increased cost of secondary switchgear. This results in most economical sizes of transformers for 240-volt or 120/208-volt circuits, being 300 to 600 kva.

Figure 12 shows the comparative cost of 120/208-volt load-center radial system compared with 480-volt system.

Voltage Regulation and Short-Circuit Current Level

The normal voltage regulation of a distribution system will be controlled primarily by transformer regulation and voltage drop in low-voltage circuits.

Transformer regulation will be in the order of one per cent at 1.0 power factor and three to four per cent at 0.8 power factor at rated current and correspondingly less at lower currents.

The voltage regulation in low-voltage circuits is a function of conductor configuration, current loading and length of run. Using three-conductor or three single-conductor cables in conduit, in sizes ranging from 1/0 to 500,000 at current levels approaching the thermal rating, the voltage regulation, on the basis of 440 volt, three-phase, three or four-wire service, will be in the order of one-half to three-fourths per

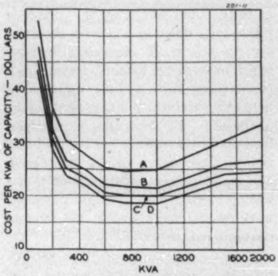


Figure 11. Load-center distribution system costs as affected by load density

480 volts

Curve *A*—2 volt-amperes
Curve *B*—5 volt-amperes
Curve *C*—10 volt-amperes
Curve *D*—20 volt-amperes

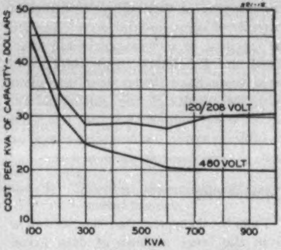


Figure 12. Comparative costs of 120/208-volt and 480-volt load-center distribution systems

cent per hundred feet of transmission, and will be more or less independent of power factor in the range of 0.7 to 0.9 lagging. At lower operating voltages, the per cent voltage regulation will be correspondingly increased.

Separation of conductors increases the reactive voltage drop and increases the voltage regulation by increasingly greater amount as the power factor deviates from unity in the lagging direction.

Incremental voltage or flicker-influence factor goes hand in hand with short-circuit level. The greater the short-circuit current the lower will be the abrupt voltage change resulting from a given low-power-factor abruptly applied load.

However, increased short-circuit current level means increased low-voltage switching-equipment expense, more violent disturbance associated with an electrical fault, and restricted application of conventional motor starters. It is therefore advisable to design for the lowest short-circuit current level commensurate with best overall economy and freedom from light flicker. No benefit can be claimed, as far as light flicker is concerned, for increasing the short-circuit current above that required for freedom from flicker.

The magnitude of abrupt voltage dip which can be permitted if the frequency of the occurrence is of the order of a few per hour is about two per cent for incandescent and about four per cent for fluorescent lighting.

Table II shows the required short-circuit current level to permit full-voltage starting of general-purpose induction motors for two per cent and four per cent voltage dip respectively on a 440-volt, three-phase system.

Table II. Required Short-Circuit Current Level for Limited Voltage Dip With Across-the-Line Motor Starting

Motor Rating (Horsepower)	2% Dip (Amperes)	4% Dip (Amperes)
15	7,500	3,750
30	15,000	7,500
50	25,000	12,500
100	50,000	25,000

Effect of Load-Density Variation

Variation in load density influences the mean length of run (*MLR*) between the load-center bus and the utilization machine. The *MLR* directly affects the investment cost in low-voltage distributing circuits (about two to four dollars per kilovolt-ampere per 100 foot at 440 volts) and the voltage regulation (about one-half to three-fourths per cent per 100 foot at 440 volts with closely spaced conductors).

The area associated with a given load center will be directly proportional to load-center rating and inversely proportional to load density. It follows that the *MLR* will vary in like manner except as the square root of load-center rating and load density, since the length of run is governed by the lineal dimension of the sides. Expressed mathematically, this becomes

$$MLR = K\sqrt{\frac{\text{L.C. rating}}{\text{load density}}}$$

assuming, of course, that the shape of the load area remains unchanged.

Low-voltage circuit runs generally follow a rectangular course coinciding with the building form rather than a direct diagonal path to the subdistribution point which increases the length of run by 40 per cent for square load areas.

To enable rapid evaluation of low-voltage *MLR*, the attached Figure 13 has been prepared, giving the horizontal *MLR* as a function of load density for various load-center ratings, based on rectangular configuration. To this must be added the vertical lengths almost invariably involved. For floor or underfloor installation, a 10-foot allowance (5 feet at each end) is reasonable. For overhead installation, a 15- to 30-foot allowance would generally be expected.

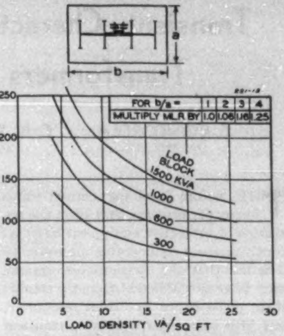


Figure 13. Mean length of secondary run versus load density

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Transient Characteristics of Current Transformers During Faults

C. CONCORDIA
MEMBER AIEE

C. N. WEYGANDT
ASSOCIATE AIEE

H. S. SHOTT
ASSOCIATE AIEE

THE factors involving current-transformer performance that affect the operation of protective equipment have recently received considerable attention.[1–3] The fact that the transient performance may be greatly different from the steady-state performance has been recognized, but little quantitative data on transient performance have been published. Data from oscillograph records of tests and from approximate analytical methods have definitely indicated the magnitudes of the expected steady-state errors and at the same time conveyed the thought that the transient errors would be very much larger. This paper presents some of the results of a study of current-transformer transient performance that has been made on the differential analyzer with the effects of transformer saturation more accurately considered.

While the information presented herewith is neither a complete coverage of the subject nor even a complete summary of what has been done to date by the authors, it has seemed desirable to publish it now for the general benefit of the interested engineers. More specifically the data presented apply to current transformers such as might be encountered in bus-differential protection. The magnitude of expected error under various conditions of application was studied for a wide range of transformers of the conventional bar-type construction and of the bushing type, but it is possible to present only a small part of these results here because of space limitations. Most of the data presented here on the bushing-type transformer was for a transformer physically larger than would be normally used, in order that the data would show what improvement can be affected through increase in size.

Certain broad assumptions have been made regarding the distribution of fault current between circuits, the degree of similarity of the various transformers, and so on, since all combinations of possible operating conditions cannot be considered in a study of this type. However, the data will be useful as a basis for judging the expected performance of existing and contemplated installations, particularly since the effects of variations of such important factors as the magnitudes of fault current, size of transformer, number of transformers, and time constant of the d-c component of fault current have been studied. In addition to the factors involving the current, the data have been referred to a base involving a factor which is called the transformer size constant. This size constant depends upon the physical construction of the transformer and the total secondary resistance both of the winding and of the burden. With this information the data already presented can be translated to apply to other transformers if the physical dimensions and the secondary loading are known.

The results were recorded as reproductions of the error current in the differential-relay circuit versus time during periods of large through-fault currents, and a portion of these results is summarized here as a function of the governing factors mentioned above, with respect to the application possibilities of instantaneous overcurrent and of time-delay overcurrent relays. For other types of relays, such as those having harmonic restraint,[4] d-c restraint, percentage differential restraint, and so on, it will probably be necessary to consider each application on its own merits, at least until a time when the response of these relays to transient currents of nonsinusoidal wave form can be specified in a more generalized form. Field and laboratory tests which have been made have shown good agreement with the results of the differential-analyzer study, although it has not been possible to cover the complete range by test.

Conclusions

From the data presented here, as well as from the large amount of data taken which could not be included, the following conclusions may be drawn:

1. The concept of transformer size constant has been introduced. The transient performance of two transformers having the same size constants and saturation characteristics will be practically identical when the primary current, in ampere turns per unit of core length, is the same for both. Of two transformers having the same size constant the one having the longer magnetic circuit will give better performance for the same value of primary current.

2. Data have been obtained to show the calculated differential-circuit error currents, that appear as a result of transformer saturation, for a wide range of magnitudes of through-fault current, transformer size constant, d-c time constant of the primary circuit, and number of transformers in the group.

3. When conventional current transformers are used, the magnitude of differential-error current with faults of ten or more times rated current can be from 30 per cent to 75 per cent or larger, depending on the size, even at a high-voltage bus where the d-c time constant of the fault current may be only two or three cycles.

4. From a study of the differential-error current curves obtained, it is apparent that time-delay settings of only three or four cycles will not insure proper operation, when simple time-delay relays are used for bus-differential protection; very much greater delays may be necessary.

5. The differential-error currents are particularly severe at a bus where the fault current has a very long d-c time constant. For these applications it is not practical to design conventional current transformers large enough to insure that they will not saturate at the larger values of fault current. Therefore, there is a definite field for the new air-gap current transformer recently introduced.[5]

6. The form and magnitude of the dif-

Table I

Transformer	Size Constant Turns^2 Inch/Ohms	Type	Rating: Amperes	Rating: Ampere-Turns per Inch Core Length	Core Area Sq In.	Mean Length of Core, In.	Secondary Turns	Secondary Resistance, Ohms
A	16,700	Bar	4,000/5	160	1.30	25.0	800	1.99*
B	50,000	Bushing	4,000/5	107.5	9.90	37.3	800	3.40*

* Including 1 ohm lead resistance.

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C. CONCORDIA and H. S. SHOTT are both of central station engineering department, General Electric Company, Schenectady, N. Y. C. N. WEYGANDT is instructor in the Moore School of Electrical Engineering, University of Pennsylvania, Philadelphia, Pa.

This paper is the result of a co-operative effort of the authors and S. B. Crary, L. F. Kennedy, C. D. Hayward, A. T. Sinks, G. Camilli, and F. J. Maginniss of the General Electric Company, and of Dr. Irven Travis of the University of Pennsylvania. The differential analyzer at the Moore School of Electrical Engineering, University of Pennsylvania, was used to solve the equations.

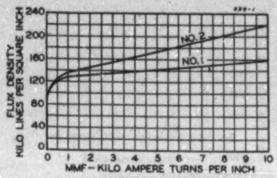

Figure 1. Saturation characteristic of the mutual flux path

ferential-error current depends greatly on the number of transformers in the group.

Analysis of Current-Transformer Performance

The performance of a current transformer can be determined from the voltage equation of its secondary circuit

$$10^{-8}\frac{d\psi}{dt}=i_2r_2+L_2\frac{di_2}{dt} \qquad (1)$$

where

$\psi=N_2\phi$ is the flux linkages of the secondary due to the mutual flux
N_2 = the number of secondary turns
ϕ = the number of flux lines mutual to both the primary and secondary windings
i_2 = the secondary current in amperes
r_2 = resistance of the secondary circuit, including that of the burden, in ohms
L_2 = secondary leakage inductance plus the inductance of the burden in henrys
t = time in seconds

Equation 1 is integrated to give

$$\frac{\phi}{10^8}=\int\frac{r_2}{N_2}\,i_2\,dt+\frac{L_2}{N_2}\,i_2+\frac{\phi_0}{10^8} \qquad (2)$$

where the constant of integration is proportional to the residual flux ϕ_0.

Another relation is that the magnetomotive force acting on the core is proportional to the primary ampere turns less the secondary ampere turns, or, for a transformer having only one turn in the primary

$$N_2i_2=i_1-\frac{hl}{0.4\pi} \qquad (3)$$

where

i_1 = primary current in amperes
$h/0.4\pi$ = magnetomotive force acting on the core in ampere turns per inch of core length
l = mean length of core in inches

The flux mutual to the primary and secondary windings can be written as

$$\phi=A\beta \qquad (4)$$

where

A = area of core in square inches
β = flux density in the core in lines per square inch

Upon substituting the expressions for the secondary current i_2 and the mutual flux ϕ of equations 3 and 4 into equation 2, there is obtained a general equation for determining the performance of a single transformer, including the effects of its burden, when the primary current is known.

$$\frac{A\beta}{10^8}=\int\frac{r_2}{N_2^2}\left(i_1-\frac{hl}{0.4\pi}\right)dt+\frac{L_2}{N_2^2}\left(i_1-\frac{hl}{0.4\pi}\right)+\frac{A\beta_0}{10^8} \qquad (5)$$

Dividing through by the factor r_2l/N_2^2, there is obtained a more convenient form,

$$C\beta=10^8\int\left(\frac{i_1}{l}-\frac{h}{0.4\pi}\right)dt+10^8\frac{L_2}{r_2}\left(\frac{i_1}{l}-\frac{h}{0.4\pi}\right)+C\beta_0 \qquad (6)$$

where the factor, $C=AN^2/r_2l$ turns² inch ÷ ohms, is defined as the transformer size constant.

If the secondaries of current transformers applied to bus differential protection are connected in parallel, through a differential-relay circuit of negligible impedance, the above equations can be used to determine the performance of each transformer separately. Furthermore, previous work has shown that the transformer errors due to the residual flux and to the flux associated with L_2 are small compared with the errors caused by the flux associated with R_2. For a through fault at a bus of $n+1$ feeders and transformers, transformer 1 carries total fault current and in view of the above assumptions its performance is given by:

$$C\beta_1=10^8\int\left(\frac{i_1}{l}-\frac{h_1}{0.4\pi}\right)dt \qquad (7)$$

It is further assumed that all transformers have identical size constants and saturation characteristics and that the fault current is equally divided between the other n transformers. The equation for each of the n transformers is

$$C\beta_n=10^8\int\left(\frac{i_1}{nl}-\frac{h_n}{0.4\pi}\right)dt \qquad (8)$$

The current through the differential-relay circuit is equal to the secondary current of transformer 1 less the sum of the secondary currents of the other n transformers. Referred to the primary this error current is:

$$\frac{i}{l}=\left(\frac{i_1}{l}-\frac{h_1}{0.4\pi}\right)-n\left(\frac{i_1}{nl}-\frac{h_n}{0.4\pi}\right) \qquad (9)$$

$$=\left(\frac{nh_n}{0.4\pi}-\frac{h_1}{0.4\pi}\right)\text{ amperes per inch of core length}$$

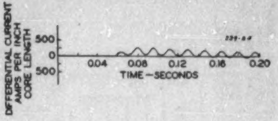

Figure 2a. Symmetrical component of fault current equal to 269 rms amperes per inch core length

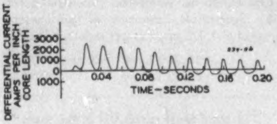

Figure 2b. Symmetrical component of fault current equal to 988 rms amperes per inch core length

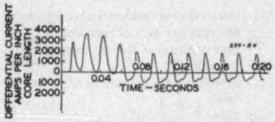

Figure 2c. Symmetrical component of fault current equal to 1,438 rms amperes per inch core length

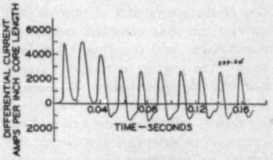

Figure 2d. Symmetrical component of fault current equal to 1,975 rms amperes per inch core length

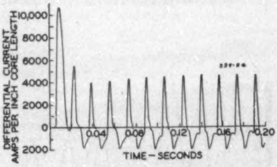

Figure 2e. Symmetrical component of fault current equal to 3,950 rms amperes per inch core length

Figure 2. Error current in the differential circuit as a function of the magnitude of through-fault current

Fault current completely offset. One transformer versus five. Saturation curve 1

C = 16,700 turns² inch/ohms
T = 0.265 second

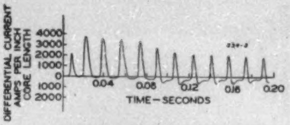

Figure 3. Differential-error current for a through-fault current having a d-c component one-half the crest value of the a-c component

One transformer versus five. Saturation curve 1. Symmetrical component of fault current equal to 1,975 amperes per inch core length

$C = 16{,}700$ turns^2 inch/ohms
$T = 0.265$ second

The total fault current is of the form

$$\frac{i_1}{l} = \frac{I_1}{l}[(\cos\alpha)\epsilon^{-t/T} - \cos(\omega t+\alpha)] \qquad (10)$$

where

I_1/l = crest value of symmetrical component, amperes per inch of core length
T = time constant of the d-c component in seconds
α = determines the magnitude of the initial offset
$\omega = 2\pi f$

Equations 7–10 were solved on the University of Pennsylvania's differential analyzer[6,7] to determine the error current in the differential-relay circuit as a function of the magnitudes of through-fault current, d-c time constant, number of transformers, and transformer size constant. The primary current i_1/l (equation 10), was generated by the machine and formed one component of the secondary current. This secondary current was integrated to determine the mutual flux density β according to equation 8, and β in turn moved an index in the β direction of the saturation curve as plotted on an analyzer input table. An operator manually controlled the index in the direction of magnetizing force H so as to keep the index on the saturation curve and, at the same time, move the exciting current component of the secondary current in proportion. The primary and exciting currents were added to form the integrand of equation 8. Two saturation characteristics were used as shown on Figure 1. Curve 1 is for silicon steel core material and was used for most of the cases considered. Curve 2 is for a transformer in which air forms a considerable part of the mutual core area in parallel with the iron. These two curves cover an extreme range of saturation characteristics for conventional current transformers. For the very high degrees of saturation which cause the large errors observed, the effects of hysteresis are negligible, and were not considered in this study. Other studies of similar transient circuit performance with iron saturation which have been made have also indicated that consideration of hysteresis is not necessary to explain the fundamental phenomena.

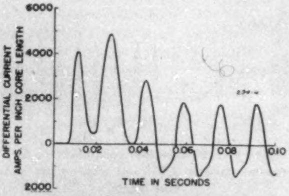

Figure 4. Differential-error current for transformers having saturation characteristic 2

Through-fault current, completely offset. Symmetrical component of fault current equal to 1,975 amperes per inch core length. One transformer versus five.

$C = 16{,}700$ turns^2 inch/ohms
$T = 0.265$ second

Results

The differential-relay error currents have been determined for a wide range of through-fault currents, transformer size, and d-c time constant. One set of results shows the error currents for the case in which the total through-fault current in one transformer is equally divided between two other transformers. The differential analyzer was set up so that the results for the case of one transformer versus three, one versus five, and one versus an infinite number, were also obtained at the same time. The data obtained for the case of one transformer versus an infinite number show in addition to the differential current which might flow in this case the absolute error current of the transformer under study. The constants for the bar-type current transformer, and for the bushing-type transformer, studied are tabulated in Table I. Data have also been obtained for very large and very small transformers having size constants $C = 200{,}000$ and $C = 4{,}170$, respectively.

The following sections describe that part of the data which is presented in this paper. The effects of variations of such important factors as the magnitudes of fault current, the d-c time constant, the number of transformers, and the transformer size constant, are shown.

MAGNITUDE OF FAULT CURRENT

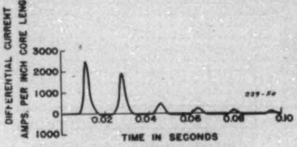

Figure 5a. $T = 0.0106$ **second**

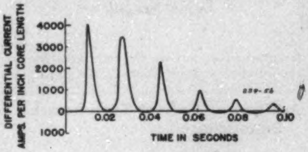

Figure 5b. $T = 0.0212$ **second**

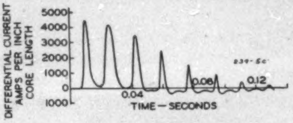

Figure 5c. $T = 0.0424$ **second**

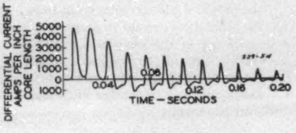

Figure 5d. $T = 0.0848$ **second**

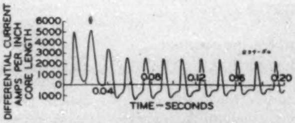

Figure 5e. $T = 0.1696$ **second**

Figure 5. Differential-error current as affected by the magnitude of the time constant of the d-c component of fault current

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 1,975 amperes per inch core length. One transformer versus five

$C = 16{,}700$ turns^2 inch/ohms

Figure 2 shows the error currents (referred to the primary) as obtained with transformer A (Table I). The total through fault current in one transformer (which will be designated as transformer 1) is equally divided between five other transformers. The fault current is completely offset and has a relatively long d-c time constant of $T = 0.265$ second. The magnitudes of fault currents are given in terms of the rms value of its symmetrical

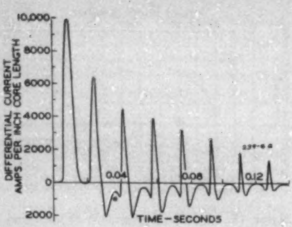

Figure 6a. $C=16{,}700$ turns^2 inch/ohms

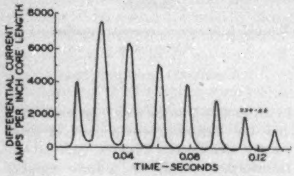

Figure 6b. $C=50{,}000$ turns^2 inch/ohms

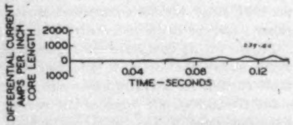

Figure 6c. $C=200{,}000$ turns^2 inch/ohms

Figure 6. Effect of transformer size constant

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 3,950 amperes per inch core length. One transformer versus five

$T = 0.0424$ second

a-c component. This value multiplied by $2\times\sqrt{2}$ is equal to the maximum instantaneous fault current for a completely offset wave.

For a small fault current having a symmetrical component of 269 rms amperes per inch core length (Figure 2a) the differential-error current is proportional to the saturation ampere turns of transformer 1, since the other transformers do not saturate when subjected to only one fifth of this value of total fault current. It is seen that several cycles are required for the d-c component of core flux to build up to the point where the saturation is appreciable. The error current has a large d-c component and the a-c component is primarily of fundamental frequency.

The other curves of Figure 2 show the effects of larger fault currents. With these larger currents all the transformers saturate, and the differential-error current is equal to the saturation ampere turns of transformer 1 less the sum of the saturation ampere turns of the other five transformers. As the magnitude of fault current is increased, the d-c component of core flux reaches a higher value in less time. That is, all the transformers lose their ability to transform the d-c component of fault current in a shorter time. This is demonstrated by the curves of Figure 2 in that the d-c component of differential-error current decays at a greater rate for the larger values of fault current. The wave form of a current through the differential relay becomes more distorted due to the greater degree of saturation associated with the larger fault current.

The maximum possible error, equal to the total fault current, occurs when the accuracy of transformer 1 collapses, and the others transform perfectly. Even with the large fault current of Figure 2e the maximum possible error is almost attained during the first cycle, because transformer 1 saturates at a much greater rate than the others, and its accuracy is destroyed before the saturation in the other transformers becomes appreciable. Another interesting fact is that the differential current is of relatively good wave form although completely or nearly completely offset during this period, but that the wave form becomes greatly distorted after the other current transformers saturate.

SMALLER D-C TIME CONSTANTS

Error currents obtained with completely offset fault currents having smaller d-c time constants are given on Figure 5. The transformer size constant, $C=16{,}700$ turns^2 inch/ohms, is the same as used before. Maximum instantaneous fault current, neglecting the decay of d-c component in the first half-cycle, is equal to 5,590 amperes per inch core length. Even with the very short time constant of $T=0.0106$ second, the maximum error is approximately 90 per cent of the maximum a-c component of fault current. The error current does disappear at a faster rate for the smaller d-c time constants. As a matter of interest, the value of rms symmetrical component of fault current is 12.4 times rated for the particular transformer listed in Table I as representative of this size constant. However, another transformer of identical size constant might have a different rating.

EFFECT OF TRANSFORMER SIZE CONSTANT

The curves of Figure 6 give an interesting relation, showing the comparative results for three installations operating under similar conditions but having transformers of different size constants. Although the assumed fault current is the same in amperes per inch core length for all three cases it may not be the same in actual amperes or in times rated current. For the installation having transformers of smaller size constant, $C=16{,}700$, all six transformers become highly saturated. Therefore, the d-c component of error current decays quite rapidly, and then the harmonic content of the a-c component

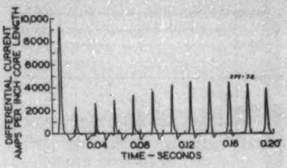

Figure 7a. One transformer versus two

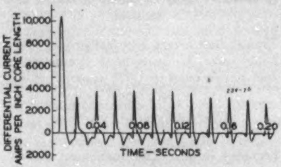

Figure 7b. One transformer versus three

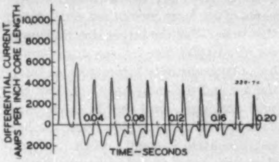

Figure 7c. One transformer versus five

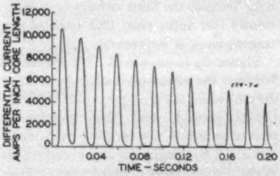

Figure 7d. One transformer versus an infinite number

Figure 7. Differential-error current as affected by the number of transformers carrying fault current

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 3,950 amperes per inch core length

$C=16{,}700$ turns^2 inch/ohms
$T=0.0848$ second

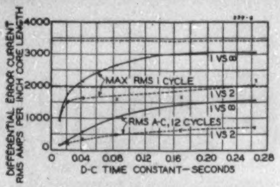

Figure 8. Summary curve showing the effect of variations in the magnitude of the d-c time constant

Through-fault current, completely offset. Saturation curve 1. Symmetrical component of fault current equal to 1,975 amperes per inch core length

C = 16,700 turns² inch/ohms

becomes large. The maximum instantaneous fault current is large for the first cycle or two, but the harmonic components of a-c error current are small during this time. For the larger size transformers, $C = 50{,}000$, five of the transformers are only moderately saturated. As before the a-c and d-c components of error current are large, but the d-c component does not decay so rapidly, and the harmonic content is small. With the hypothetically large transformer installation, $C = 200{,}000$, the error current is small only because the fault current is of sufficiently low value such that none of the transformers is appreciably saturated.

Figure 6a is compared with Figure 5c to show the effect of doubling the fault current when the d-c time constant is $T = 0.0424$ second, and the transformer size constant is $C = 16{,}700$ turns² inch/ohms. The maximum error current is slightly more than doubled. Again, the higher degree of saturation obtained in the other five transformers with the larger fault current causes the d-c component of error current to decay more rapidly, and the harmonic content becomes greater.

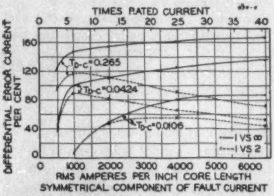

Figure 9. Maximum rms error current over one-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

C = 16,700 turns² inch/ohms

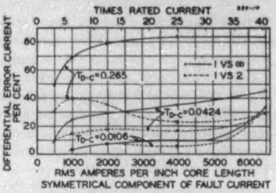

Figure 10. Rms a-c component of error current over 12-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

C = 16,700 turns² inch/ohms

Figure 13 shows the maximum differential-error currents (rms for one cycle) for a range of fault current and transformer size. The effect of variation in the saturation characteristic is also shown for one size (Curves C and C'). All of the error currents are large except for the largest transformer size with rather small fault currents.

Different Numbers of Transformers

Most of the data presented above were for a condition of one transformer versus five. Figure 7 shows the error currents for one transformer versus two, one versus three, one versus five, and one versus an infinite number. The results are all for the transformer size constant $C = 16{,}700$ and a reasonably small d-c time constant of $T = 0.0848$ second. The rms symmetrical component of fault current, 3,950 amperes per inch core length, is quite large being 24.7 times normal for the typical transformer of this size constant as listed in Table I. For one transformer versus two, all transformers are highly saturated. As the number of transformers is increased, the saturation in all except transformer 1 becomes less, and the harmonic component of error current decreases. For the case of one transformer versus an infinite number (which is the same as one versus a perfect transformer), the a-c and d-c components are both large, the d-c component decays slowly, and the harmonics are very small.

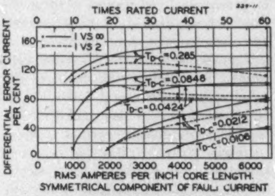

Figure 11. Maximum rms error current over one-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

C = 50,000 turns² inch/ohms

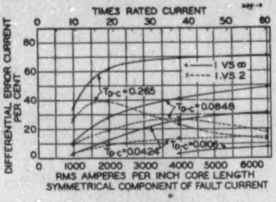

Figure 12. Rms a-c component of error current over 12-cycle interval as a function of the magnitude of fault current and of the d-c time constant

Through-fault current, completely offset. Saturation curve 1

C = 50,000 turns² inch/ohms

Figure 7c, as compared to Figure 5d, again shows the effect of doubling the fault current for a transformer size constant $C = 16{,}700$, one transformer versus five, but with a d-c time constant of 0.0848 second.

Figures 6a, 7c, and 2e show the effects of different d-c time constants in the same manner as the curves of Figure 5, and under the same conditions except for a larger fault current.

Degree of Offset

The error current of Figure 3 is for the same conditions as Figure 2d except that the d-c offset is only one half. The symmetrical component of fault current is the same in the two cases, but the maximum instantaneous fault current (and maximum possible error current) is 4,193 amperes per inch core length for Figure 3 as compared to 5,590 for Figure 2d. Although the magnitudes of the a-c and d-c components of error current are appreciably smaller, the d-c component decays at a slower rate because of less saturation. The percentage harmonic content appears to be about the same.

Saturation Characteristic

Figure 4 is also for the same conditions as Figure 2d, except that the transformers were assumed to have saturation characteristic 2, Figure 1. The maximum error currents are about the same, but the a-c component is somewhat smaller, particularly after the first two cycles. In general, the conclusions arrived at with either of these two very different saturation curves were about the same, so that they should also be valid for most other saturation curves obtainable with conventional current transformers.

Application

The character of the differential-relay current varies widely depending upon the size constant and number of transformers involved, and upon the magnitude of the fault current and its d-c time constant. For this reason it is difficult to summarize the accumulated data for all the various types of relays used, especially since the response of these relays to transient currents of this nature is not very well known.

Results can be plotted in a form which will serve to indicate the expected performance of either instantaneous or time-delay relays. For instance, Figure 8 summarizes the effect of variation of the d-c time constant for the transformer size $C=16{,}700$ for fully offset through current having a symmetrical rms component of 1,975 amperes per inch core length. The rms value of the completely offset wave is therefore equal to $\sqrt{3}\times 1{,}975$. The maximum rms differential-error current over a one-cycle interval can be taken as a measure of the current which determines the operation of an instantaneous relay. The rms a-c component of error current over the 12 cycles interval that gives the largest value is taken as a measure of the current that would determine the operation of an induction-type time-delay relay with the time setting of approximately 0.2 second. The curves were plotted for one transformer versus two and for one transformer versus an infinite number thus covering the entire expected range of operation. The data taken from Figure 5, for one transformer versus five, would lie between the two plotted curves.

Figure 9 gives more complete data for the instantaneous error currents of the same size transformer. The effects of the magnitude of fault current over a wide range are shown for three different values of d-c time constant. The fault current in rms amperes per inch core length is perfectly general. The times-rated current applies only to the particular transformer selected as representative of this size constant $C=16{,}700$. It is seen that the error current is small only in a range of very small d-c time constant and reasonably small fault current. Likewise, Figure 10 gives similar data applicable to time-delay relays. The time delay is quite effective for the smaller d-c time constants.

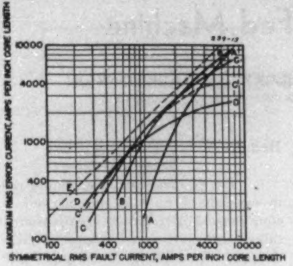

Figure 13. Differential-error current (rms over one cycle)

One transformer versus three. Through fault—fully offset. D-c time constant=0.265 second

Curve *A*—Saturation characteristic 1, $C=200{,}000$ turns^2 inches/ohms

Curve *B*—Saturation characteristic 1, $C=50{,}000$ turns^2 inches/ohms

Curve *C*—Saturation characteristic 1, $C=16{,}670$ turns^2 inches/ohms

Curve *C′*—Saturation characteristic 2, $C=16{,}670$ turns^2 inches/ohms

Curve *D*—Saturation characteristic 1, $C=4{,}170$ turns^2 inches/ohms

Curve *E*—Current for 100 per cent error

The data of Figures 11 and 12 are similar to that of Figures 9 and 10, except that they are for the larger transformer size constant $C=50{,}000$ turns^2 inch/ohms. The times-rated fault current applies only to the particular transformer listed in Table I. The advantages of using larger transformers are apparent.

Admittedly this summary is not perfectly accurate and may not be the correct measure for determining the response of these relays. It is valuable as being indicative of definite trends.

For the high-speed relays that depend upon percentage, harmonic, or d-c restraint for proper operation, the analysis is more complicated. Their characteristics are such that each application requires special consideration and the accumulated data are valuable as a basis for selecting the proper relay characteristics.

AIR-GAP-CORE CURRENT TRANSFORMERS

The ideal solution of using current transformers large enough to insure almost perfect accuracy up to the maximum value of through-fault current is not practical because of the excessive size and cost. A new current transformer, as described by Kennedy and Sinks,[5] has been developed for high-speed bus-differential protection. This transformer, which has air gaps in the mutual flux path, has constant ratio and phase-angle errors up to the point where the iron paths saturate. By selecting transformers that maintain a linear characteristic up to the maximum value of fault current, the a-c component of primary current is always reproduced with the same degree of accuracy. Very little of the d-c component is transformed, but this is not required for differential protection. Simple overcurrent relays can be used since differential-error currents appear only because of improper matching of the linear characteristics. The size and cost is not much greater than that of the standard current transformers ordinarily used.

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1. TRANSIENT CHARACTERISTICS OF CURRENT TRANSFORMERS, **A. T. Sinks.** AIEE miscellaneous paper 40-70, January 1940.

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4. HARMONIC-CURRENT RESTRAINT RELAY FOR BUS DIFFERENTIAL PROTECTION, **L. F. Kennedy and C. D. Hayward.** AIEE TRANSACTIONS, volume 57, 1938, May section, pages 262–71.

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The Doubly Fed Machine

C. CONCORDIA
MEMBER AIEE

S. B. CRARY
MEMBER AIEE

GABRIEL KRON
ASSOCIATE AIEE

SYNCHRONOUS machines, operating with a-c excitation on both stator and rotor are used in many applications, for example, as induction frequency converters, as power and instrument Selsyn drives, and as variable speed power drives. Reference 1 has mentioned particularly the variable speed fan drive, and presented equations for the small oscillations of one such doubly fed machine. Reference 2 has also previously given the equations of hunting of the doubly fed machine (part XIV, section IV) in connection with the general study of oscillations of rotating machines. However, since the present authors have been using in their own work equations which seem to them to be more convenient and simpler in form for calculations, and since it now seems desirable to present not only general equations but also some of the more fundamental and significant performance characteristics of these machines, it is thought that this paper may now be appropriate. The form of the equations developed possesses the additional novelty of facilitating the setting up of equivalent circuits for hunting on the a-c network analyzer, and allowing the quick determination of the damping and synchronizing torques directly by wattmeter readings.

In this paper there are presented:

1. A general analysis of the doubly fed machine, in a form believed to be particularly convenient for the study of rotor hunting and for the interconnection of two or more machines.

2. An example showing the transient electrical characteristics under three-phase short circuit.

3. Examples showing the characteristic damping and synchronizing torques during hunting at various speeds and loads.

4. Equivalent electrical circuits for hunting which have been found to be of considerable help in the determination of the machine performance during small oscillations by means of the a-c network analyzer.

Results

1. The general equations are derived in appendix I and are summarized in equations 14, 23, and 24. Attention is called particularly to the compact derivation and the simplicity of the final equations made possible by the simultaneous use of complex variables in the general transient analysis and also a complex angular variation.

2. Equations 26, 27, 29, and 30, appendix II, give the transient three-phase short-circuit currents of a doubly fed machine or induction frequency converter. It is of interest here that

(*a*). The steady-state short-circuit currents are determined principally by the short-circuit or transient reactance.

(*b*). The d-c component is of the same form as that for short circuit of a synchronous machine.

(*c*). There is no transient component of fundamental frequency short-circuit current, such as would be observed in a synchronous machine; instead, the corresponding transient current is of rotor speed frequency and is moreover very small.

3. Figures 1, 2, and 3 show curves of damping and synchronizing torques for the hunting of a particular doubly fed motor. Some of the curves were calculated and some measured on the network analyzer, using the equivalent circuits of appendix IV. The damping torque is seen to be negative up to very small slips, and indeed becomes most negative at a slip somewhat greater than the frequency of hunting. It is evident that, because of the inherent negative damping characteristic of these machines, either load damping must be depended upon, or special precautions must be taken, if stable operation is to be obtained.

4. Figure 3 shows the equivalent circuits and a tabulation of the direct power and reactive kva measurements which determine the damping and synchronizing torque coefficients during hunting. The circuits are derived in a companion paper "Equivalent Circuits for the Hunting of Electrical Machinery"[3] by one of the authors.

References

1. Damping and Synchronizing Torque of the Double-Fed Asynchronous Machine, **M. M. Liwschitz.** AIEE Transactions, volume 60, 1941, October section, pages 923–4.

2. The Application of Tensors to the Analysis of Rotating Electrical Machinery, **G. Kron.** *General Electric Review*, April 1935 to March 1938, inclusive. (Available also in book form.)

3. Equivalent Circuits for the Hunting of Electrical Machinery, **G. Kron.** AIEE Transactions, volume 61, 1942, May section, pages 290–6.

4. Relations Among Some of the Transformations Used in Electrical Engineering Problems, **C. Concordia.** *General Electric Review*, volume 41, July 1938, page 323.

Appendix I. Derivation of Transient and Hunting Equations

(*a*). The equations of a two-phase rotating machine (see Figure 4, and see also reference 4, equations 10 and 11, for the relations between these two phase quantities and the usual three-phase machine quantities) referred to axes connected to the stator and rotor, are:

$$e_{as}=r_s i^{as}+p\psi_{as} \qquad e_{dr}=r_r i^{dr}+p\psi_{dr}$$
$$e_{bs}=r_s i^{bs}+p\psi_{bs} \qquad e_{qr}=r_r i^{qs}+p\psi_{qr}$$

or,

$$\boldsymbol{e}=\boldsymbol{R}\cdot\boldsymbol{i}+p\boldsymbol{\psi} \tag{1}$$

where the flux-linkage vector is $\boldsymbol{\psi}=\boldsymbol{L}\cdot\boldsymbol{i}$. In a machine with smooth air gap the self and mutual inductances are:

$$\boldsymbol{L}=\begin{array}{c|c|c|c|c} & \boldsymbol{a_s} & \boldsymbol{b_s} & \boldsymbol{d_r} & \boldsymbol{q_r} \\ \hline \boldsymbol{a_s} & L_s & & M\cos\theta_2 & -M\sin\theta_2 \\ \boldsymbol{b_s} & & L_s & M\sin\theta_2 & M\cos\theta_2 \\ \boldsymbol{d_r} & M\cos\theta_2 & M\sin\theta_2 & L_r & \\ \boldsymbol{q_r} & -M\sin\theta_2 & M\cos\theta_2 & & L_r \end{array} \tag{2}$$

where θ_2 is the angle of the machine rotor. Hence the equations are $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$, where $\boldsymbol{Z}=\boldsymbol{R}+p\boldsymbol{L}$, or

$$\boldsymbol{Z}=\begin{array}{c|c|c|c|c} & \boldsymbol{a_s} & \boldsymbol{b_s} & \boldsymbol{d_r} & \boldsymbol{q_r} \\ \hline \boldsymbol{a_s} & r_s+L_s p & & pM\cos\theta_2 & -pM\sin\theta_2 \\ \boldsymbol{b_s} & & r_s+L_s p & pM\sin\theta_2 & pM\cos\theta_2 \\ \boldsymbol{d_r} & pM\cos\theta_2 & pM\sin\theta_2 & r_r+L_r p & \\ \boldsymbol{q_r} & -pM\sin\theta_2 & pM\cos\theta_2 & & r_r+L_r p \end{array} \tag{3}$$

(*b*). Multiply the second and fourth of equations 3 by *j* and add them to the first and third respectively, to obtain a new set of equations

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C. Concordia and S. B. Crary are in the central station engineering department and Gabriel Kron is consulting engineer with General Electric Company, Schenectady, N. Y.

$$\mathbf{e}=\mathbf{Z}\cdot\mathbf{i} \tag{4}$$

where now

$$\mathbf{Z}=\begin{array}{c|c|c} & s & r \\ \hline s & r_s+L_s p & Mp\epsilon^{j\theta_2} \\ \hline r & Mp\epsilon^{-j\theta_2} & r_r+L_r p \end{array} \tag{5}$$

and

$$e_{as}+je_{bs}=e_s \qquad i^{as}+ji^{bs}=i^s \tag{6}$$
$$e_{dr}+je_{qr}=e_r \qquad i^{dr}+ji^{qr}=i^r$$

Hence the four equations with real coefficients may be expressed as two equations with complex coefficients. These equations check with reference 2, page 74, equation 23. (For the process see reference 2, page 147.)

Torque

(*a*). The electromagnetic torque upon the rotor of an electrical machine is

$$T_e=i^{dr}\psi_{qr}-i^{qr}\psi_{dr} \tag{7}$$

If $i^{dr}+ji^{qr}=i_r$, and $\psi_{dr}+j\psi_{dr}=\psi_r$, then

$$T_e=\text{real part of } \left[(i^{dr}-ji^{qr})(-j)(\psi_{dr}+j\psi_{qr})\right]$$
$$=-ji^{r*}\psi_r=i^{r*}B_r \tag{8}$$

where i^{r*} is the conjugate of i^r and $B_r=-j\psi_r$ = flux-density wave of the rotor. Then from equation 5,

$$\psi_r=M\epsilon^{-j\theta}i^s+L_r i^r \tag{9}$$

Therefore, for a smooth machine:

$$T_e=\text{real part of } (-jM\epsilon^{-j\theta}i^s i^{r*}) \tag{10}$$

(*b*). The same expression for the torque may also be found from the relation:[2]

$$T=\text{real part of } \mathbf{i}^*\cdot\mathbf{B}=\mathbf{i}^*\cdot\mathbf{G}_s\cdot\mathbf{i}$$

where the torque tensor is

$$\left.\begin{aligned} &\mathbf{G}=\begin{array}{c|c|c} & s & r \\ \hline s & & \\ \hline r & -jM\epsilon^{-j\theta} & -jL_r \end{array} \\ &\mathbf{G}_s=\begin{array}{c|c} & s \\ \hline r & -jM\epsilon^{-j\theta} \end{array} \end{aligned}\right\} \tag{11}$$

Transformation to Axes on Stator Flux

The stator flux rotates with a velocity $p\theta_1$ with respect to the stator (Figure 5) and the rotor flux rotates with $p\theta_3$ with respect to the rotor. The rotor itself rotates with $p\theta_2$. Hence the applied voltages are:

$$e_s=E_s\epsilon^{j\theta_1} \quad \text{and} \quad e_r=E_r\epsilon^{j\theta_3} \tag{12}$$

Let two new axes s' and r' be introduced, both rotating with the stator flux. That is, let

$$\left.\begin{aligned} &i^s=i^{s'}\epsilon^{j\theta_1} \\ &i^r=i^{r'}\epsilon^{j\theta_1}\epsilon^{-j\theta_2}=i^{r'}\epsilon^{j(\theta_1-\theta_2)} \\ &\mathbf{C}=\begin{array}{c|c|c} & s' & r' \\ \hline s & \epsilon^{j\theta_1} & \\ \hline r & & \epsilon^{j(\theta_1-\theta_2)} \end{array} \end{aligned}\right\} \tag{13}$$

By $\mathbf{C}_t^*\cdot\mathbf{Z}\cdot\mathbf{C}$ (where the p in $\mathbf{Z}$ refers to $\mathbf{C}$ but not to $\mathbf{C}_t^*$), $\mathbf{C}_t^*\cdot\mathbf{G}\cdot\mathbf{C}$, and $\mathbf{C}_t^*\cdot\mathbf{e}$, or by $\mathbf{Z}'=\mathbf{C}_t^*\cdot\mathbf{Z}\cdot\mathbf{C}+\mathbf{C}_t^*\cdot\mathbf{L}\cdot p\mathbf{C}$

$$\mathbf{Z}'=\begin{array}{c|c|c} & s' & r' \\ \hline s' & r_s+L_s(p+jp\theta_1) & M(p+jp\theta_1) \\ \hline r' & M[p+j(p\theta_1-p\theta_2)] & r_r+L_r[p+j(p\theta_1-p\theta_2)] \end{array} \qquad \mathbf{e}'=\begin{array}{c|c} s' & E_s \\ \hline r' & E_r\epsilon^{j\delta} \end{array} \tag{14}$$

$$\mathbf{G}_s'=\begin{array}{c|c|c} & s' & r' \\ \hline s' & & \\ \hline r' & -jM & \end{array} \qquad \delta=\theta_3+\theta_2-\theta_1$$

Or, writing out the equations

$$[r_s+L_s(p+jp\theta_1)]i^{s'}+M(p+jp\theta_1)i^{r'}=E_s$$
$$M[p+j(p\theta_1-p\theta_2)]i^{s'}+$$
$$\{r_r+L_r[p+j(p\theta_1-p\theta_2)]\}i^{r'}=E_r\epsilon^{j\delta} \tag{15}$$

$$\text{Torque} = \text{real of } (-jMi^{s'}i^{r'*})$$

where

$$E_s=ji^{f_1}M_1p\theta_1=jE_1 \qquad E_r=ji^{f_3}M_3p\theta_3=jsE_3$$

Steady State

In the steady state $p=0$, $p\theta_1=\omega$, $p\theta_3=p\theta_1-p\theta_2=s\omega$, and $\omega L=X$. Equation 15 then becomes:

$$\left.\begin{aligned} &\mathbf{Z}'=\begin{array}{c|c|c} & s' & r' \\ \hline s' & r_s+jX_s & jX_m \\ \hline r' & jsX_m & r_r+jsX_r \end{array} \\ &\mathbf{e}'=\begin{array}{c|c} s' & jE_1 \\ \hline r' & jsE_3\epsilon^{j\delta} \end{array} \end{aligned}\right\} \tag{16}$$

The steady-state currents i^s and i^r required in the hunting equations of the next section, as well as the steady-state torque-angle characteristic, can be calculated from equation 16. In terms of the applied voltages, the currents are:

$$\left.\begin{aligned} &i^{s'}=\frac{jE_1\left(\dfrac{r_r}{s}+jX_r\right)+X_mE_3\epsilon^{j\delta}}{(r_s+jX_s)\left(\dfrac{r_r}{s}+jX_r\right)+X_m^2} \\ &i^{r'}=\frac{j(r_s+jX_s)E_3\epsilon^{j\delta}+X_mE_1}{(r_s+jX_s)\left(\dfrac{r_r}{s}+jX_r\right)+X_m^2} \end{aligned}\right\} \text{(17}$$

However, in many cases the stator voltage, current, and power factor, instead of both applied voltages and the angle may be assigned. Then either equation 16, or more simply the equivalent circuit shown later, can be used to calculate δ, i^r and E_3.

Hunting Equations—Small Changes in the Complex Variables

(*a*). In setting up the equations of hunting of polyphase machines, two different methods may be followed:

1. The polyphase complex equations are first changed to real equations and the latter are subjected to small changes in the variables.
2. The polyphase equations are left unchanged in complex form and are subjected to small changes in the complex variables.

The second method will be followed in this paper, as it is a more compact analytical procedure.

(*b*). Let $p\theta_2=p\theta_{20}+\Delta p\theta_2$ and so forth; then equation 15 or 14 with the use of the compound tensor (reference 2, page 116, equation 32),

$$\begin{array}{c|c|c} & \Delta\mathbf{i} & \Delta\theta \\ \hline \Delta\mathbf{e} & \mathbf{Z} & \mathbf{G}\cdot\mathbf{i}p-\dfrac{\partial\mathbf{e}}{\partial\theta} \\ \hline \Delta T & -\mathbf{i}^*\cdot(\mathbf{G}+\mathbf{G}_t^*) & \end{array} \tag{18}$$

give the equations of hunting as:

$$\mathbf{Z}'=\begin{array}{c|c|c|c} & s & r & \theta \\ \hline s & r_s+L_s(p+jp\theta_1) & M(p+jp\theta_1) & \\ \hline r & M[p+j(p\theta_1-p\theta_2)] & r_r+L_r[p+j(p\theta_1-p\theta_2)] & -j(Mi^s+L_ri^r)p+sE_3\epsilon^{j\delta} \\ \hline \theta & jMi^{r*} & -jMi^{s*} & \end{array} \tag{19}$$

where the real part of the last row is $-\Delta T_e$.

Steady Hunting

During steady hunting let $p=kh\omega$, where h is the per unit oscillation frequency and k plays the same role as j. That is $k^2=-1$, but kj cannot be combined. Then equations 19 become:

$$\mathbf{Z}'=\begin{array}{c|c|c|c} & s & r & \theta \\ \hline s & r_s+X_s(kh+j) & X_m(kh+j) & \\ \hline r & X_m(kh+js) & r_r+X_r(kh+js) & -j(X_mi^s+X_ri^r)kh+sE_3\epsilon^{j\delta} \\ \hline \theta & jX_mir^* & -jX_mi^{s*} & \end{array} \tag{20}$$

or, writing the equations out:

$$[r_s+X_s(kh+j)]\,\Delta i^s+X_m(kh+j)\,\Delta i^r=0$$

$$X_m(kh+js)\,\Delta i^s+[r_r+X_r(kh+js)]\,\Delta i^r=$$
$$jkh(X_mi^s+X_ri^r)\,\Delta\theta_2-sE_3\epsilon^{j\delta}\,\Delta\theta_2-$$
$$jX_mi^{r*}\,\Delta i^s+jX_mi^{s*}\,\Delta i^r=\Delta T_e \tag{21}$$

where

$s=1-v=$ per unit rotor average slip
$\delta=\theta_2+\theta_3-\theta_1$

Only the real part of the last equation (ΔT) is taken.

In calculating ΔT, first the j component is discarded as usual after rationalization

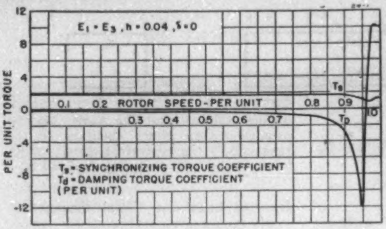


Figure 1. Damping- and synchronizing-torque coefficients of a typical doubly fed machine at no load

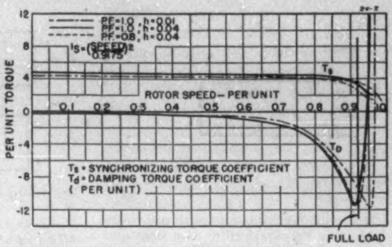


Figure 2 (right). Damping- and synchronizing - torque coefficients of a typical doubly fed machine

Effect of oscillation frequency and power factor. Load torque proportional to speed squared

so that ΔT is expressed in terms of k only (and not j). Then the $k-s$ in the denominator are rationalized, so that the synchronizing and damping torque coefficients are given by:

$$\Delta T = T_s + khT_D \tag{22}$$

Two separate symbols j and k are used in this procedure, in order to distinguish between the complex numbers introduced by the original use of the complex j currents, voltages, and fluxes as variables, and the further complex k numbers introduced by the use of the complex (exponential) rotor angular variation. It may readily be shown that at any stage one may revert to real transient voltage equations and have only the complex numbers due to the complex angular variation.

Eliminating Δi^s

If the row and column of s is eliminated from equation 20, or the stator current change Δi^s is eliminated from 21, then

$$Z' = \begin{array}{c|c|c} & r & \theta \\ \hline r & \boxed{r_r + r_s''s + khX_r'' + j(sX_r'' - khr_s'')} & kh B_r + sE_3\epsilon^{j\delta} \\ \theta & -B_r^* + i^{r*}(r_s'' + jX_r'') & \end{array} \tag{23}$$

where

$$B_r = -j(X_m i^s + X_r i^r) = -j\psi_r$$

$$r_s'' = \frac{r_s X_m^2}{D'}$$

$$X_r'' = X_r - \frac{X_m^2}{D'}X_s(1-h^2) - khr_s''$$

$$D' = (r_s + khX_s)^2 + X_s^2$$

If again the rotor current Δi^r (row and column of r in equation 23) is eliminated, then the final equation for the torque change is:

$$\Delta T = \text{real of} \left[-\frac{(khB_r + sE_3\epsilon^{j\delta})[-B^* + i^{r*}(r_s'' + jX_r'')]}{r_r + r_s''s + khX_r'' + j(sX_r'' - khr_s'')} \right] \tag{24}$$

The phrase "real" and the asterisk (the conjugate symbol) refer to j and not to k. Both r_s'' and X_r'' are independent of the slip. First the ks are treated as algebraic symbols and the js are eliminated. Afterward the ks are treated as the complex operator k (where $k^2 = -1$). After rationalizing, equation 22 holds for the torque change of the doubly fed machine. Also, if the rotor impressed voltage E_3 is zero, the equations apply to the small oscillations of a standard polyphase induction motor.

Appendix II. Three-Phase Short Circuit

In order to illustrate the characteristics of the doubly fed machine under transient conditions, three-phase short circuits on both stator and rotor in turn, neglecting initial loading, will be considered.

(a). Stator Short Circuit

Applying, by superposition, a stator voltage E_s, to equations 14 or 15, we obtain for the stator three-phase short-circuit current the operational expression:

$$i^{s'} = \frac{E_s}{r_s + L_s(p + j\omega_1) - \left\{ \dfrac{M^2(p + j\omega_1)(p + j\omega_3)}{r_r + L_r(p + j\omega_3)} \right\}} \tag{25}$$

where ω_1, ω_2, ω_3 are the constant stator frequency, rotor speed, and rotor frequency, respectively.

Taking for simplicity the stator resistance $r_s = 0$, this current may be written as:

$$i^s = i^{s'}\epsilon^{j\omega_1 t} = \frac{E_s\epsilon^{j\omega_1 t}}{j\omega_1\left[L_s - \dfrac{j\omega_3 M^2}{r_r + j\omega_3 L_r}\right]} - \frac{E_s}{j\omega_1\left[L_s + \dfrac{j\omega_2 M^2}{r_r - j\omega_2 L_r}\right]} + \frac{r_r(L_r - L_r')E_s\epsilon^{-\frac{r_r}{L_r'}t + j\omega_2 t}}{L_s(j\omega_2 L_r' - r_r)(j\omega_2 L_r' + r_r)} \tag{26}$$

where $L_r' = L_r - \dfrac{M^2}{L_s}$ = transient inductance viewed from the rotor terminals.

If the stator resistance r_s is considered, but rotor resistance $r_r = 0$, the short-circuit current becomes:

$$i^s = i\epsilon^{s' j\omega_1 t} = \frac{E_s}{r_s + j\omega_1 L_s'}\left(\epsilon^{j\omega_1 t} - \epsilon^{-\frac{r_s}{L_s'}t}\right) \tag{27}$$

where

$$L_s' = L_s - \frac{M^2}{L_r} \qquad \omega_1 L_s' = x_s'$$

In general, there are three components of current, stator fundamental frequency (ω_1), speed frequency (ω_2), and direct current. Note that with zero rotor resistance the speed frequency term disappears, leaving only the fundamental and d-c components in equation 27. The "d-c component" decays exponentially to zero, just as in case of a synchronous machine short circuit, but the usual transient component of alternating current is replaced by the speed frequency term.

(b). Rotor Short Circuit

Similarly applying a rotor voltage E_r in equations 14 or 15, the rotor short-circuit current becomes:

$$i^r = \frac{E_r}{r_r + L_r(p + j\omega_3) - \left\{ \dfrac{M^2(p + j\omega_3)(p + j\omega_1)}{r_s + L_s(p + j\omega_1)} \right\}} \tag{28}$$

Whence, with $r_{\text{rotor}} = 0$

$$i^r = i^{r'}\epsilon^{j\omega_3 t} = \frac{E_r\epsilon^{j\omega_3 t}}{j\omega_3\left[L_r - \dfrac{j\omega_1 M^2}{r_s + j\omega_1 L_s}\right]} - \frac{E_r}{j\omega_2\left[L_r - \dfrac{j\omega_2 M^2}{r_s + j\omega_2 L_s}\right]} - \frac{r_s(L_s - L_s')E_r\epsilon^{-\frac{r_s}{L_s'}t - j\omega_2 t}}{L_r(r_s + j\omega_2 L_s')(r_s + j\omega_1 L_s')} \tag{29}$$

and with $r_{\text{stator}} = 0$, but $r_{\text{rotor}} \neq 0$,

$$i^r = i^{r'}\epsilon^{j\omega_3 t} = \frac{E_r}{r_r + j\omega_3 L_r'}\left(\epsilon^{j\omega_3 t} - \epsilon^{-\frac{r_r}{L_r'}t}\right) \tag{30}$$

The three components of current are again the fundamental (ω_3), d-c, and speed frequency (ω_2) components, but now the presence of the speed frequency term depends on the stator resistance, and the d-c decrement depends on the rotor resistance.

In case of an induction frequency converter connected between two parts of a power system, it is evident that the converter may be approximately represented simply as a reactance (the machine short-circuit reactance). Since $E_r/E_s \cong \omega_3/\omega_1$ and $L_s' \cong L_r'$ on a one-to-one turn ratio basis, the rotor and stator short-circuit currents will be nearly equal in amperes, and will also be equal in per unit if the kva base is taken proportional to the frequency of the current being considered. Thus in a network diagram involving a 25 by 60 cycle converter, all the 25-cycle system reactances may be expressed on a kva base 25 by 60

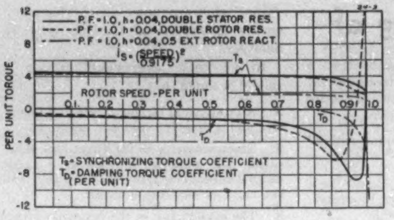


Figure 3. Damping- and synchronizing-torque coefficients of a typical doubly fed machine

Effect of change in stator and rotor resistance and in rotor reactance in machine of Figure 2

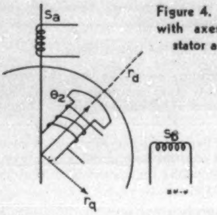


Figure 4. Machine with axes fixed in stator and rotor

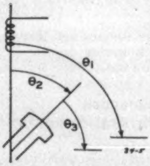


Figure 5. Machine with rotating reference axes

times as great as that for the 60-cycle system, to obtain a common diagram suitable for short circuit calculations on either part of the system.

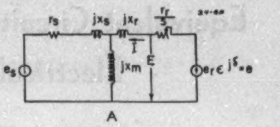
Appendix III. Damping and Synchronizing Torque—Example

Several more or less typical curves of damping and synchronizing torque coefficients as a function of rotor speed are shown in Figures 1, 2, and 3. The machine of Figure 1 has all reactances and the stator resistance about twice as great as those of Figure 2, so that with an appropriate change of scale the effect of a change in rotor resistance may be estimated. Figure 2 shows the rather slight effect of a change of stator terminal power factor, and also the effect of oscillation frequency h in shifting the point at which maximum negative damping occurs. Figure 3 shows, for the machine of Figure 2 with an oscillation frequency of $h=0.04$, the effects of doubling the stator and rotor resistances in turn, and also the effect of a greatly increased rotor leakage (or added external) reactance.

Appendix IV. Equivalent Circuits for Hunting

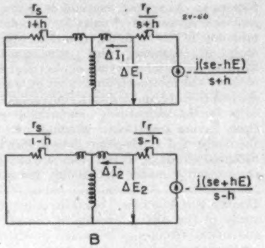
Figure 6 gives equivalent circuits[8] which represent the doubly fed machine during rotor hunting as well as in the steady state. In using these equivalent circuits as set up on the a-c network analyzer, the required voltages are first read from the steady-state (upper) circuit and used as indicated to compute the voltages to be applied in the hunting (lower) circuit. The total steady-state torque as well as the components of the synchronizing (T_s) and damping (T_d) torque coefficients may then be read directly on a watt and reactive voltampere meter, according to the table of formulas shown under the circuits. Note that the formulas are based on the use of a watt- and varmeter which reads e^*i to form the complex expression $W+jQ$, where W is the wattmeter reading, and Q is the varmeter reading, both here and in Figure 6. As a simple example, consider a single generator supplying a reactive load unit. If generated voltage and current flowing out of the generator are measured, the watts will be positive and the vars will be negative.

The equivalent circuits are based on the stationary axes equations given previously by one of the authors in reference 2, page 146, rather than on the rotating axes equations derived in the paper. The reason for this is that the rotating axes formulas are simpler for hand calculations, while the stationary axes equations are simpler for equivalent circuit representation. The use of rotating axes leads to d-c steady-state quantities, so that only hunting frequency terms arise during the entire calculation of either the current or torque changes, while with stationary axes the steady-state quantities are of fundamental frequency, and fundamental plus hunting and fundamental minus hunting frequency currents and voltage changes must be separately considered. On the other hand, the rotating axis equations require four separate equivalent circuits (with two meshes each) in place of three. It should be remarked that in general the choice of proper reference axes is very important if the simplest method of solution is to be had, and that the best axes depend on not only the apparatus being analyzed, but also on the aspect or particular problem being considered and on the method of solution to be used (that is, hand or network analyzer).



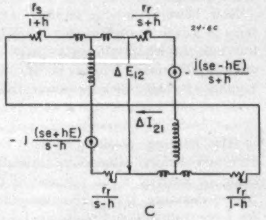
A—Steady-state network

$$IE^* = W+jQ$$
$$T = W$$



B—Hunting-frequency networks

$$I\Delta E_1^* = W_1+jQ_1$$
$$\Delta I_1E^* = W_2+jQ_2$$
$$I\Delta E_2^* = W_3+jQ_3$$
$$\Delta I_2E^* = W_4+jQ_4$$
$$T_s = \frac{W_1+W_2+W_3+W_4}{2}$$
$$T_D = \frac{(Q_2+Q_3)-(Q_1+Q_4)}{2h}$$



C—Hunting-frequency networks to simplify measurement of T_D

$$I\Delta E_{12}^* = W_a+jQ_a$$
$$\Delta I_{12}E^* = W_b+jQ_b$$
$$T_D = \frac{Q_b-Q_a}{2h}$$

Figure 6. Equivalent circuits for determination of steady-state and hunting torques

Equivalent Circuits for the Hunting of Electrical Machinery

GABRIEL KRON
ASSOCIATE AIEE

Synopsis: A general method is given to establish equivalent circuits for the determination of the hunting characteristics—such as damping and synchronizing torques—of standard types of electrical machines. The method is illustrated by setting up steady-state and hunting equivalent circuits for the salient-pole synchronous machine having amortisseur windings and for the doubly fed single-phase Selsyn with unbalanced windings, special cases of which are the capacitor motor and doubly fed polyphase motor. A companion paper, "The Doubly Fed Machine," contains a detailed study of the characteristics of one of the equivalent circuits as measured on the a-c network analyzer.

UNTIL recently the study of damping torque has been confined to synchronous machines and rotary converters. Operation of these machines without hunting has been obtained by the use of properly designed amortisseur or pole-face windings.

Now, however, a large number of systems of rotating machines are being put into use, in which altogether new and critical problems of hunting occur. In a number of cases, such as power Selsyn systems, and variable speed wind tunnel drives, exact and thorough going analysis of the hunting possibilities has been necessary before satisfactory operation could be secured. The purpose of this paper, therefore, is to present methods for more complete and ready analysis of these modern systems of interconnected rotating apparatus, so that hunting difficulties can be predicted and provided for in advance of installation.

The determination of the damping and synchronizing torques with standard methods involves an inordinate amount of analysis and calculation. The following equivalent circuits not only offer a clear physical picture of the interrelated phenomena taking place during small oscillations, but also enable one to get a quick numerical answer, either by the use of the a-c network analyzer, or by standard circuit methods.

The only practical equivalent circuits hitherto available were those of induction machines having symmetrical windings and running at a constant speed.[1,2] Recently steady-state equivalent circuits have been established for machines with asymmetrical windings, such as the capacitor motor,[4] also for machines with asymmetrical magnetic structure such as the salient-pole synchronous machine.[5] The extension in this paper from steady-state to hunting performance involves chiefly the application of a more complex voltage expression upon the network, and not any significant change in the network itself.

By a practical equivalent circuit is understood here one that allows the determination of not only the currents flowing and voltages appearing in every winding of the machine, but also the torques, as the speed or angle varies. The circuit must also allow the approximate consideration of the effect of saturation and iron loss.

Results

The equivalent circuits of the salient-pole alternator of Figure 1 and the necessary measurements to be made are shown in Figure 2, while those of the doubly fed single-phase Selsyn of Figure 3 are shown in Figure 4. Similar networks and measurements of other standard machines are shown on Figures 5 to 9. A detailed study of Figure 8 is undertaken in a companion paper, "The Doubly Fed Machine," showing numerous performance curves measured on the a-c network analyzer.

In all these equivalent circuits it should be noted that:

1. All equivalent circuits consist of a positive and a negative sequence network.
2. The asymmetry in the direct and quadrature axis windings appears as a single mutual impedance between the positive and the negative sequence network.
3. The asymmetry in the magnetic structure (the saliency) appears as a single mutual impedance, not only between the sequence networks, but also between the stator and rotor windings as well.

Also it should be noted that:

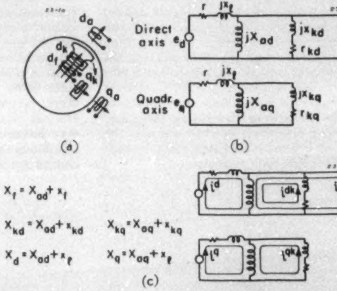
1. The hunting networks are the same as the steady-state networks, except that the resistances are divided by different constants.
2. Where two hunting networks are needed in place of one, the second differs from the first only in having the frequency of hunting h replaced by $-h$.
3. All impressed voltages on the hunting networks are measured off the steady-state network.
4. The steady-state torques and also the damping and synchronizing torques are measured directly by a wattmeter.

A Principle to Establish Models for Physical Systems

In setting up steady-state and hunting equivalent circuits for the various types of machines in a systematic manner, it has been found, as was to be expected, that only such collection of terms in the equations could be physically reconstructed or measured by instruments that formed a tensor. Geometric objects and other nontensor invariants could not be physically represented. Vice versa, it was also found that an equivalent circuit always gave a set of equations that formed a tensor equation.

It can be stated as an engineering principle that: A set of equations expressing

Figure 1. Reference axes and constants of the synchronous machine



(a) Reference axes

(b) Mutual and leakage reactances

(c) Assumed meshes and their self-reactances

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GABRIEL KRON is consulting engineer in the engineering general department of General Electric Company, Schenectady, N. Y.

the performance of a physical system (be it electrical, mechanical, thermal, or any other system) may be represented by a model(equivalent circuit)only if the equations are tensor equations. That is, only tensors have physical existence, and only tensors can be measured. Vice versa, measurable physical entities always appear in the mathematical analysis as tensors.

This principle is only a consequence of previous statements that to explain the behavior of a physical system in terms of actually existing measurable physical entities, the system must be analyzed in terms of tensors. The equations of performance of simpler systems, such as stationary networks, automatically come out in a tensor form, whether they are so recognized or not, but that is not the case in more complex phenomena, such as the hunting of rotating machines. Tensors appear automatically only in special cases, while in others measurable concepts must be introduced either by experience or by the methods of tensor analysis.

Appendix I. Rules to Establish Equivalent Networks

The Determination of Torque on a Network

(*a*). When the rotor of an electrical machine rotates, at any one instant, four voltages appear in each of its windings (if all reference axes rotate with the same speed, including zero speed)

$$\begin{aligned}\mathbf{e}&=\boldsymbol{R}\cdot\boldsymbol{i}+p\boldsymbol{\phi}+p\theta\boldsymbol{B} &\quad\Big|\quad e_{\bar\alpha}&=R_{\bar\alpha\beta}i^\beta+p\phi_{\bar\alpha}+p\theta B_{\bar\alpha}\\ \mathbf{e}&=\boldsymbol{R}\cdot\boldsymbol{i}+p\boldsymbol{L}\cdot\boldsymbol{i}+p\theta\mathbf{G}\cdot\boldsymbol{i} &\quad\Big|\quad e_{\bar\alpha}&=R_{\bar\alpha\beta}i^\beta+pL_{\bar\alpha\beta}i^\beta+p\theta G_{\bar\alpha\beta}i^\beta\end{aligned}\qquad(1)$$

where

$\boldsymbol{e}$ = impressed voltages
$\boldsymbol{R}\cdot\boldsymbol{i}$ = resistance drops
$p\boldsymbol{\phi}$ = induced voltages
$p\theta\boldsymbol{B}$ = voltage generated by cutting the rotor flux-density wave $\boldsymbol{B}$

The instantaneous torque on the rotor is the real part of

$$T=\boldsymbol{i}^*\cdot\boldsymbol{B}=\boldsymbol{i}^*\cdot\mathbf{G}\cdot\boldsymbol{i}\ \Big|\ T=i^\alpha B_{\bar\alpha}=i^\alpha G_{\bar\alpha\beta}i^\beta\qquad(2)$$

(*b*). The voltage equation 1 may be written

$$\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}\ \Big|\ e_{\bar\alpha}=Z_{\bar\alpha\beta}i^\beta\qquad(3)$$

where the transient impedance tensor is defined as

$$\boldsymbol{Z}=\boldsymbol{R}+\boldsymbol{L}p+p\theta\mathbf{G}\quad\Big|\quad Z_{\bar\alpha\beta}=R_{\bar\alpha\beta}+L_{\bar\alpha\beta}p+p\theta G_{\bar\alpha\beta}\qquad(4)$$

In rotating machinery **G**, hence $\boldsymbol{Z}$ is asymmetrical.

If the impedance tensor $\boldsymbol{Z}$ is symmetrical it is often possible to establish a stationary network whose performance is also $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$. In all standard electrical machines it is possible to introduce a transformation $\boldsymbol{C}$ that changes the asymmetrical $\boldsymbol{Z}$ to a symmetrical $\boldsymbol{Z}'$, thereby allowing the establishment of an equivalent network.

The selection of the form of $\boldsymbol{C}$ depends on the desired reference frame to be employed and the desired form of the equivalent circuit. Hence $\boldsymbol{C}$ is different for different types of machines and interconnected systems.

(*c*). Since the equivalent network—when found with the aid of $\boldsymbol{C}$—corresponds to the equation of voltage 1 all the individual voltages $\boldsymbol{R}\cdot\boldsymbol{i}$ or $p\theta\boldsymbol{B}$ can be found on the network. Hence on all equivalent networks the rotor flux-density vector $\boldsymbol{B}$ appears as a set of differences of potential $\boldsymbol{E}$ (to a suitable scale) and can be easily traced on the network by reading the components of $\mathbf{G}\cdot\boldsymbol{i}$.

The torque then is given (in synchronous watts) by the wattmeter readings

$$\text{Torque}=T=\text{real part of }\boldsymbol{i}^*\boldsymbol{B}=\boldsymbol{i}^*\cdot\boldsymbol{E}\qquad(5)$$

where

$$\boldsymbol{E}=\mathbf{G}\cdot\boldsymbol{i}\qquad(6)$$

It has to be remembered that:

1. If in a product i and E are of different frequencies, then iE^* is divided by two, giving the peak of the oscillating torque.
2. If both i and E are of the same frequency (including zero frequency) or if only one of them is of zero frequency, then iE^* is not divided by two.

The Equations of Small Displacements

(*a*). When the rotor oscillates for any cause, θ becomes $\theta_0+\Delta\theta$, $\boldsymbol{i}$ becomes $\boldsymbol{i}_0+\Delta\boldsymbol{i}$, and so on, so that the above two equations become

$$\boldsymbol{e}+\Delta\boldsymbol{e}=\boldsymbol{R}(\boldsymbol{i}+\Delta\boldsymbol{i})+p(\boldsymbol{\phi}+\Delta\boldsymbol{\phi})+p(\theta+\Delta\theta)(\boldsymbol{B}+\Delta\boldsymbol{B})\qquad(7)$$

$$T+\Delta T=\text{real part of }-(\boldsymbol{i}+\Delta\boldsymbol{i})^*\cdot(\boldsymbol{B}+\Delta\boldsymbol{B})+Mp^2(\theta+\Delta\theta)\qquad(8)$$

(where now T represents applied torque, hence the minus sign).

Subtracting the steady-state equations and neglecting second-order infinitesimals, the equations of small displacements are

$$\Delta\boldsymbol{e}=\boldsymbol{R}\cdot\Delta\boldsymbol{i}+p\,\Delta\boldsymbol{\phi}+p\theta\,\Delta\boldsymbol{B}+\Delta p\theta\boldsymbol{B}\qquad(9)$$

$$\Delta T=-(\boldsymbol{i}^*\cdot\Delta\boldsymbol{B}+\Delta\boldsymbol{i}^*\cdot\boldsymbol{B})+Mp^2\Delta\theta\qquad(10)$$

(The phrase "Real part of" will be left out in the following.)

Or in terms of constants

$$\begin{aligned}\Delta\boldsymbol{e}&=\boldsymbol{R}\cdot\Delta\boldsymbol{i}+\boldsymbol{L}\cdot p\,\Delta\boldsymbol{i}+p\theta\mathbf{G}\cdot\Delta\boldsymbol{i}+\Delta p\theta\mathbf{G}\cdot\boldsymbol{i}\\ \Delta T&=-(\boldsymbol{i}^*\cdot\mathbf{G}\cdot\Delta\boldsymbol{i}+\Delta\boldsymbol{i}^*\cdot\mathbf{G}\cdot\boldsymbol{i})+Mp^2\Delta\theta\end{aligned}\qquad(11)$$

(*b*). The equation of small displacement may be written as

$$\Delta\boldsymbol{e}=\boldsymbol{Z}\cdot\Delta\boldsymbol{i}+\Delta p\theta\mathbf{G}\cdot\boldsymbol{i}$$

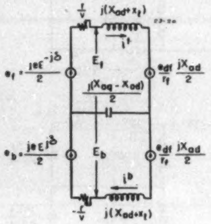
or

$$\boxed{\boldsymbol{Z}\cdot\Delta\boldsymbol{i}=\left(\frac{\partial\boldsymbol{e}}{\partial\theta}-\boldsymbol{B}p\right)\Delta\theta}\qquad(12)$$

where $\boldsymbol{Z}$ is defined in equation 4.

Hence during small oscillations the transient impedance tensor $\boldsymbol{Z}$ of any machine is the same as the $\boldsymbol{Z}$ during constant speed. However, two sets of impressed voltages appear:

1. $-\boldsymbol{B}\,\Delta p\theta$ representing the internal generated voltages due to cutting the steady-state rotor flux-



$$i^fE_f^*=W'\qquad i^bE_b^*=W''\qquad T=W'+W''$$

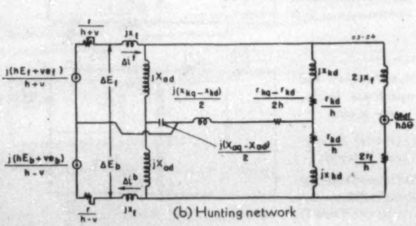
$$i^f\Delta E_f^*=W_1+jQ_1\qquad \Delta i^fE_f^*=W_3+jQ_3$$

$$i^b\Delta E_b^*=W_2+jQ_2\qquad \Delta i^bE_b^*=W_4+jQ_4$$

$$T_s=W_1+W_2+W_3+W_4$$

$$T_D=\frac{(Q_3+Q_4)-(Q_1+Q_2)}{h}$$

(a) (left) Steady-state network (c) Torque formulas



(b) Hunting network

Figure 2. The salient-pole synchronous machine on infinite bus

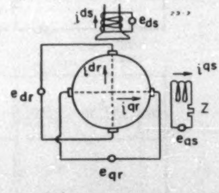
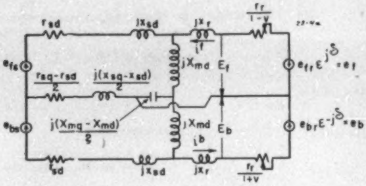
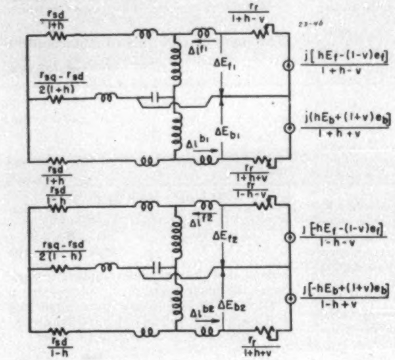


Figure 3. Reference axes of the doubly fed single-phase motor



r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1-v}$
i^f
e_{fs}
jX_{md}
E_f
$e_{fr}\epsilon^{j\delta}=e_f$
$\frac{r_{sq}-r_{sd}}{2}$
$\frac{j(x_{sq}-x_{sd})}{2}$
$j\frac{(X_{mq}-X_{md})}{2}$
e_{bs}
jX_{md}
E_b
$e_{br}\epsilon^{-j\delta}=e_b$
i^b
r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1+v}$

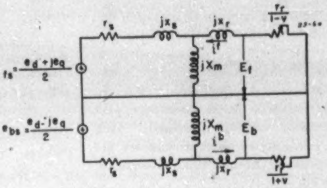


$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h-v}$
Δi^{f_1}
ΔE_{f_1}
$\frac{j[hE_f-(1-v)e_f]}{1+h-v}$
$\frac{r_{sq}-r_{sd}}{2(1+h)}$
ΔE_{b_1}
$\frac{j(hE_b+(1+v)e_b]}{1+h+v}$
Δi^{b_1}
$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h+v}$
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{j[-hE_f-(1-v)e_f]}{1-h-v}$
$\frac{r_{sq}-r_{sd}}{2(1-h)}$
Δi^{b2}
ΔE_{b2}
$\frac{j[-hE_b+(1+v)e_b]}{1-h+v}$
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1+h+v}$

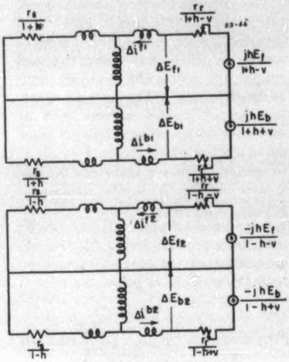
Figure 4 (left). The single-phase instrument Selsyn

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)



r_s
jx_s
jx_r
$\frac{r_r}{1-v}$
i^f
$e_{fs}=\frac{e_d+je_q}{2}$
jX_m
E_f
$e_{bs}=\frac{e_d-je_q}{2}$
jX_m
E_b
i^b
r_s
jx_s
jx_r
$\frac{r_r}{1+v}$

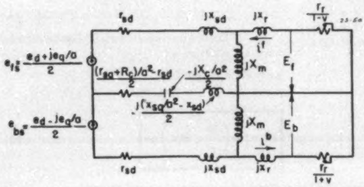


$\frac{r_s}{1+h}$
$\frac{r_r}{1+h-v}$
Δi^{f_1}
ΔE_{f_1}
$\frac{jhE_f}{1+h-v}$
ΔE_{b_1}
$\frac{jhE_b}{1+h+v}$
Δi^{b_1}
$\frac{r_s}{1+h}$
$\frac{r_r}{1+h+v}$
$\frac{r_s}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{-jhE_f}{1-h-v}$
ΔE_{b2}
$\frac{-jhE_b}{1-h+v}$
Δi^{b2}
$\frac{r_s}{1-h}$
$\frac{r_r}{1-h+v}$

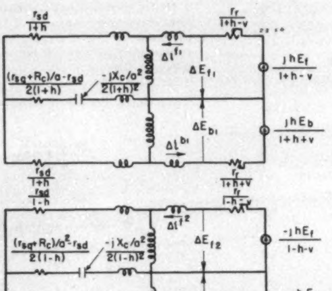
Figure 6 (right). The induction motor on unbalanced voltages

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)



r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1-v}$
i^f
$e_{fs}=\frac{e_d+je_q/a}{2}$
jX_m
E_f
$\frac{(r_{sq}+R_c)/a^2-r_{sd}}{2}$
$\frac{-jX_c/a^2}{2}$
$\frac{j(x_{sq}/a^2-x_{sd})}{2}$
$e_{bs}=\frac{e_d-je_q/a}{2}$
jX_m
E_b
i^b
r_{sd}
jx_{sd}
jx_r
$\frac{r_r}{1+v}$

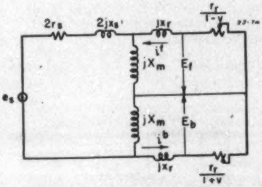


$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h-v}$
Δi^{f_1}
ΔE_{f_1}
$\frac{jhE_f}{1+h-v}$
$\frac{(r_{sq}+R_c)/a-r_{sd}}{2(1+h)}$
$\frac{-jX_c/a^2}{2(1+h)^2}$
ΔE_{b_1}
$\frac{jhE_b}{1+h+v}$
Δi^{b_1}
$\frac{r_{sd}}{1+h}$
$\frac{r_r}{1+h+v}$
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{-jhE_f}{1-h-v}$
$\frac{(r_{sq}+R_c)/a^2-r_{sd}}{2(1-h)}$
$\frac{-jX_c/a^2}{2(1-h)^2}$
ΔE_{b2}
$\frac{-jhE_b}{1-h+v}$
Δi^{b2}
$\frac{r_{sd}}{1-h}$
$\frac{r_r}{1-h+v}$

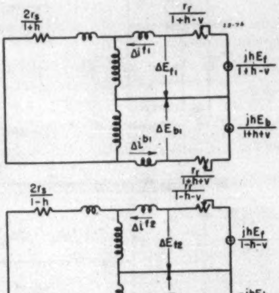
Figure 5 (left). The capacitor motor

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)



$2r_s$
$2jx_s$
jx_r
$\frac{r_r}{1-v}$
i^f
jX_m
E_f
e_s
jX_m
E_b
i^b
jx_r
$\frac{r_r}{1+v}$



$\frac{2r_s}{1+h}$
$\frac{r_r}{1+h-v}$
Δi^{f_1}
ΔE_{f_1}
$\frac{jhE_f}{1+h-v}$
ΔE_{b_1}
$\frac{jhE_b}{1+h+v}$
Δi^{b_1}
$\frac{r_r}{1+h+v}$
$\frac{2r_s}{1-h}$
$\frac{r_r}{1-h-v}$
Δi^{f2}
ΔE_{f2}
$\frac{jhE_f}{1-h-v}$
ΔE_{b2}
$\frac{-jhE_b}{1-h+v}$
Δi^{b2}
$\frac{r_r}{1-h+v}$

Figure 7 (right). The single-phase induction motor

(a) (above) Steady-state network

(b) (below) Hunting networks (for torque formulas see Table I)

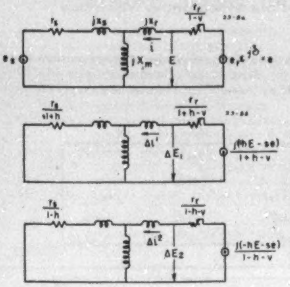


Figure 8. The doubly fed polyphase induction motor

(a) (left) Steady-state network
(b) (left) Hunting networks
(c) (right) Torque formulas

$$iE^* = W \qquad T = W$$

$$i\Delta E_1^* = W_1 + jQ_1 \qquad \Delta i^1 E^* = W_3 + jQ_3$$

$$i\Delta E_2^* = W_2 + jQ_2 \qquad \Delta i^2 E^* = W_4 + jQ_4$$

$$T_s = \frac{W_1 + W_2 + W_3 + W_4}{2} \qquad T_D = \frac{(Q_2+Q_3)-(Q_1+Q_4)}{2h}$$

density B by the oscillating speed change $\Delta p\theta$. This voltage appears in every machine.

2. $\partial e/\partial\theta$ representing the oscillation of the steady-state impressed voltage e. This voltage appears only in slip ring machines where e is a function of θ.

Also small voltage changes Δf may be impressed from outside.

Steady Hunting

(*a*). When the frequency of oscillations—say in spontaneous hunting or in driving a reciprocating load—is $h\omega$, and p is to be replaced by $jh\omega'$, care has to be exercised if the transient Z contains the complex operator j. Such cases can be analytically treated by introducing an additional complex operator k for $p = kh\omega'$ as shown in the companion paper "The Doubly Fed Machine." However to establish equivalent networks it is advisable to use a physical reference frame that does not introduce j in the transient Z, so as to avoid using equivalent networks with two sets of frequencies.

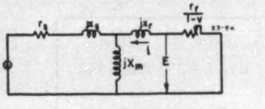
(*b*). In replacing p by $jh\omega'$ (assuming no j in the transient Z) two cases have to be distinguished (reference 7, page 119) depending on the reference frame used:

1. The steady-state currents i are constant. Then all p in equation 12 are replaced by $jh\omega$.

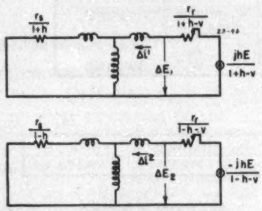
2. The steady-state currents i are of fundamental frequency. In such a case two sets of hunting equations are established:

(*a*). The currents Δi are of fundamental plus hunting frequency. Then all p in Z are replaced by $j(1+h)\omega$ and p in $Bp\,\Delta\theta$ by $jh\omega$.

(*b*). The currents Δi are of fundamental minus hunting frequency. Then all p in Z become $j(1-h)\omega$ and p in $Bp\,\Delta\theta$ becomes $-jh\omega$. That is h assumes a negative value.



(a) Steady-state network



(b) Hunting networks (for torque formulas see Figure 8c)

Figure 9. The polyphase induction motor

(*c*). The hunting-frequency electrical torque ΔT comes out as a complex number

$$\Delta T = \frac{\partial T}{\partial\theta}\Delta\theta = (T_s + jhT_D)\,\Delta\theta \tag{13}$$

where T_s is the synchronizing-torque coefficient and T_D the damping-torque coefficient. If one of them is negative, the system is unstable. In calculating T_s and T_D only $\Delta T/\Delta\theta$ is needed, hence in the impressed voltage $\Delta\theta$ may be left out or assumed to be any convenient constant.

$$C = \frac{1}{2}\begin{array}{c|cc} & f & b \\ \hline d & 1 & 1 \\ q & -j & j \end{array} \qquad \gamma = \begin{array}{c|cc} & d & q \\ \hline d & & -1 \\ q & 1 & \end{array}$$

If $\Delta\theta$ is given, the oscillating-frequency torque is

$$\Delta T = \Delta\theta\sqrt{T_s^2 + (hT_D)^2} \tag{14}$$

(*d*). Once p is replaced by $jh\omega$, and so on, it is permissible to introduce a C containing j.

Steps to Reduce Z to a Symmetrical Form

(*a*). In machines with sinusoidal space waves the torque tensor G may be expressed along any reference frame as

$$G = \gamma\cdot L \quad \Big| \quad G_{\alpha\beta} = \gamma_\alpha{}^\gamma L_{\gamma\beta} \tag{15}$$

where γ is the rotation tensor. (Reference 7, page 62.) Hence the impedance tensor may be written as

$$Z = R + (pI + p\theta\gamma)\cdot L \tag{16}$$

where I is the unit tensor. The tensors R, I, and L are symmetrical in any reference frame, but γ is not. It is assumed that Z contains no j.

(*b*). To establish an equivalent network it is necessary to bring Z to a symmetrical form. Three steps are to be made:

1. Replacing p by $j\omega$ and $p\theta$ by $v\omega$,

$$Z = R + (I - jv\gamma)\cdot jX = R + n\cdot jX \tag{17}$$

where n contains the speed v as a parameter

$$n = I - jv\gamma \tag{18}$$

2. Bringing Z to a symmetrical form consists chiefly of discovering a transformation C that changes the rotation tensor γ to a diagonal form. When the physical reference axes have been judiciously selected and also are at right angles in space, one such C is that of the method of symmetrical components

$$\gamma' = \begin{array}{c|cc} & f & b \\ \hline f & j & \\ b & & -j \end{array} \tag{19}$$

where $\gamma' = C_t^*\cdot\gamma\cdot C$. This step brings γ, hence n to a diagonal form. Except for the scalars n, the matrix of Z is now symmetrical.

Since the sequence axes are hypothetical axes, their introduction does not change the frequency of any quantity, and it is immaterial whether first the ps are replaced by $jh\omega$ and then are the sequence axes introduced or first the sequence axes are introduced, and then the ps are replaced by jhw.

3. Dividing the equation of voltage $e = Z\cdot i$ by n (or each equation by an appropriate scalar)

$$e' = n^{-1}\cdot e = (n^{-1}\cdot R + jX)\cdot i = Z'\cdot i \tag{20}$$

where Z' now is symmetrical. The resistances and the impressed voltages are functions of the speed v.

To find the torque, not only Z but also G must be transformed by C. Then $\omega G\cdot i = E$ is traced on the network. The torque is the real part of $i\cdot E^*$, that is the sum of the wattmeter readings.

Steps to Establish the Steady-State Networks

1. The transient Z of the machine is first set up along a reference frame that gives a symmetrical Z.

2. The torque tensor G and impressed voltage vector e are also found along the same reference frame.

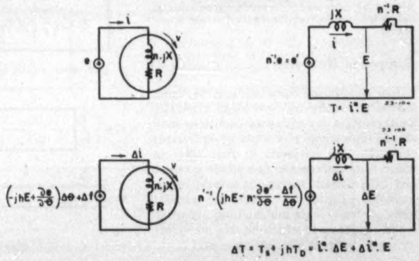

i
v
e
$n.jX$
R
jX
$n^{-1}.R$
$n^{-1}.e = e'$
i
E
$T = i^*.E$
Δi
v
$\left(-jhE + \frac{\partial e}{\partial\theta}\right)\Delta\theta + \Delta f$
$n'.jX$
R
jX
$n'^{-1}.R$
$n'^{-1}.\left(jhE - n\frac{\partial e'}{\partial\theta} - \frac{\Delta f}{\Delta\theta}\right)$
Δi
ΔE
$\Delta T = T_s + jhT_D = i^*.\Delta E + \Delta i^*.E$

Figure 10. Compound machines and their compound networks

(a) Steady-state machine and its equivalent network

(b) Hunting machine and its equivalent network

3. Replace p in $\boldsymbol{Z}$ by $j\omega$ (where ω may be zero).

4. Divide $\boldsymbol{Z}$ by $\boldsymbol{n}$.

5. Establish the steady-state network.

6. The impressed voltages are $\boldsymbol{e}'=\boldsymbol{n}^{-1}\cdot\boldsymbol{e}$.

7. The differences of potential $\boldsymbol{E}=\omega\boldsymbol{G}\cdot\boldsymbol{i}$ are indicated on the equivalent circuit.

8. The steady-state torque is the real part of $\boldsymbol{i}^*\cdot\boldsymbol{E}$.

Steps to Establish the Hunting Networks

1. Replace p in $\boldsymbol{Z}$ by $j\omega'$ (where ω' is $h\omega$ or $(1\pm h)\omega$ as indicated in a previous section).

2. Divide $\boldsymbol{Z}$ by $\boldsymbol{n}'$.

3. Establish the corresponding network or networks. They are the same as the steady-state network except that the resistances are divided by different constants $\boldsymbol{n}'$.

4. The voltages impressed on the hunting networks are

$$\Delta\boldsymbol{e}'=\boldsymbol{n}'^{-1}\cdot\left(jh\boldsymbol{E}-\boldsymbol{n}\cdot\frac{\partial\boldsymbol{e}'}{\partial\theta}-\frac{\Delta\boldsymbol{f}}{\Delta\theta}\right) \qquad (21)$$

where $\boldsymbol{E}$ and $\boldsymbol{e}'$ appear on the steady-state network, $\Delta\boldsymbol{f}$ is any outside impressed voltage change and h may be plus or minus. Also $\boldsymbol{n}\cdot\partial\boldsymbol{e}'/\partial\theta=\partial\boldsymbol{e}/\partial\theta$.

5. On the hunting network the differences of potential $\Delta\boldsymbol{E}=\omega\boldsymbol{G}\cdot\Delta\boldsymbol{i}$ are determined (they exist across the same junction-pairs as $\boldsymbol{E}$).

6. The following watts W and vars Q are measured

$$\Delta T/\Delta\theta=W+jQ=\boldsymbol{i}\cdot\Delta\boldsymbol{E}^*+\Delta\boldsymbol{i}\cdot\boldsymbol{E}^*=T_s+jhT_D \qquad (22)$$

7. The sum of the wattmeter readings is the synchronizing torque T_s and the sum of the varmeter readings (divided by h) is the damping torque T_D.

It should be remembered that:

1. By convention the varmeter reading $W+jQ$ is $\boldsymbol{i}\cdot\boldsymbol{E}^*$ and not $\boldsymbol{i}^*\cdot\boldsymbol{E}$; hence a negative Q gives positive T_D.

2. While i and E in the actual machine are rotating vectors $i=(A+jB)\epsilon^{j\omega t}$; ΔT, also i and E in the networks are single-phase quantities, $i=$real of $(C+jD)\epsilon^{j\omega t}$. When the frequency of a ΔT expression is $-\omega_h$ it represents a negative T_D.

Hence if the frequency of ΔT is $-\omega_h$ the varmeter reading Q keeps its sign. If ΔT is $+\omega_h$, the sign of Q is reversed.

3. If a ΔT expression is the product of two sinusoidal waves, one half of the product is the torque change.

Compound Networks

Just as ordinary equations may be represented physically by equivalent networks, analogously a set of tensor equations may also be represented physically by equivalent networks (see reference 6, page 480) in which each coil represents a whole network and each current represents several mesh-currents. Figure 10 shows the general form of steady-state and hunting networks for all machines in which no relative velocity exists between the reference axes.

Appendix II. The Salient-Pole Synchronous Machine

The Primitive Machine

Let the constants of the synchronous machine of Figure 1a be assumed as shown in Figures 1b and c. Its $\boldsymbol{Z}$, $\boldsymbol{G}$, and $\boldsymbol{e}$ tensors (or those of the primitive machine with five axes) have been given in reference 7, page 42 as (using X for inductance L in per unit as is customary in the per-unit system)

$$\boldsymbol{Z}=\begin{array}{c|ccccc} & d_f & d_k & q_k & d_a & q_a \\ \hline d_f & r_f+X_fp & X_{ad}p & & X_{ad}p & \\ d_k & X_{ad}p & r_{kd}+X_{kd}p & & X_{ad}p & \\ q_k & & & r_{kq}+X_{kq}p & & X_{aq}p \\ d_a & X_{ad}p & X_{ad}p & -X_{aq}p\theta & r+X_dp & -X_qp\theta \\ q_a & X_{ad}p\theta & X_{ad}p\theta & X_{aq}p & X_dp\theta & r+X_qp \end{array} \qquad (23)$$

$$\boldsymbol{G}=\begin{array}{c|ccccc} & d_f & d_k & q_k & d_a & q_a \\ \hline d_a & & & -X_{aq} & & -X_q \\ q_a & X_{ad} & X_{ad} & & X_d & \end{array} \qquad (24)$$

$$\boldsymbol{e}=\begin{array}{ccccc} d_f & d_k & q_k & d_a & q_a \\ \hline e_{df} & & & p\theta e\sin\delta & p\theta e\cos\delta \end{array} \qquad (25)$$

where $\delta=\theta-\theta_{\text{bus}}$. All the five reference axes are rigidly attached to the field and rotate with it. (Note the change in sign of $p\theta$.)

The machine is assumed to be connected to an infinite bus running at the same speed $p\theta$ as the field.

Reduction to Symmetrical Form

Let symmetrical components be introduced by the following transformation

$$\begin{aligned} i^{df}&=i^{df}\\ i^{dk}&=(i^{fk}+i^{bk})/2\\ i^{qk}&=-j(i^{fk}-i^{bk})/2\\ i^{da}&=(i^{fa}+i^{ba})/2\\ i^{qa}&=-j(i^{fa}-i^{ba})/2 \end{aligned} \qquad (26)$$

$$\boldsymbol{C}=\frac{1}{2}\begin{array}{c|ccccc} & d_f & f_k & b_k & f_a & b_a \\ \hline d_f & 2 & & & & \\ d_k & & 1 & 1 & & \\ q_k & & -j & j & & \\ d_a & & & & 1 & 1 \\ q_a & & & & -j & j \end{array} \qquad (27)$$

If now:

1. $\boldsymbol{C}_t^*\cdot\boldsymbol{Z}\cdot\boldsymbol{C}=\boldsymbol{Z}'$, $\boldsymbol{C}_t^*\cdot\boldsymbol{G}\cdot\boldsymbol{C}=\boldsymbol{G}'$ and $\boldsymbol{C}_t^*\cdot\boldsymbol{e}=\boldsymbol{e}'$ are calculated,

2. p is replaced by jk and $p\theta$ by v where v is the per-unit velocity of the machine,

3. $\boldsymbol{Z}'$ is multiplied by $\boldsymbol{n}'^{-1}$ where the value of $\boldsymbol{n}'$ is given in equation 28.

$$\boldsymbol{n}'=\begin{array}{c|ccccc} & d_f & d_k & q_k & d_a & q_a \\ \hline d_f & h & & & & \\ f_k & & h & & & \\ b_k & & & h & & \\ f_a & & & & h+v & \\ b_a & & & & & h-v \end{array} \qquad (28)$$

The results are

$$\boldsymbol{n}'^{-1}\cdot\boldsymbol{Z}'=1/4\times$$

$$\begin{array}{c|ccccc} & d_f & f_k & b_k & f_a & b_a \\ \hline d_f & 4\left(\dfrac{r_f}{h}+jX_f\right) & 2jX_{ad} & 2jX_{ad} & 2jX_{ad} & 2jX_{ad} \\ f_k & 2jX_{ad} & \begin{array}{c}(r_{kd}+r_{kq})/h+\\ j(X_{kd}+X_{kq})\end{array} & \begin{array}{c}(r_{kd}-r_{kq})/h+\\ j(X_{ad}-X_{aq})\end{array} & j(X_{ad}+X_{aq}) & j(X_{ad}-X_{aq}) \\ b_k & 2jX_{ad} & \begin{array}{c}(r_{kd}-r_{kq})/h+\\ j(X_{ad}-X_{aq})\end{array} & \begin{array}{c}(r_{kd}+r_{kq})/h+\\ j(X_{kd}+X_{kq})\end{array} & j(X_{ad}-X_{aq}) & j(X_{ad}+X_{aq}) \\ f_a & 2jX_{ad} & j(X_{ad}+X_{aq}) & j(X_{ad}-X_{aq}) & \begin{array}{c}2r/(h+v)+\\ j(X_d+X_q)\end{array} & j(X_d-X_q) \\ b_a & 2jX_{ad} & j(X_{ad}-X_{aq}) & j(X_{ad}+X_{aq}) & j(X_d-X_q) & \begin{array}{c}2r/(h-v)+\\ j(X_d+X_q)\end{array} \end{array} \qquad (29)$$

The same result is found if first p is replaced by $jh\omega$, then $\boldsymbol{C}$ is introduced.

$$\boldsymbol{G}=\frac{1}{4}\begin{array}{c|ccccc} & d_f & f_k & b_k & f_a & b_a \\ \hline f_a & 2jX_{ad} & j(X_{ad}+X_{aq}) & j(X_{ad}-X_{aq}) & j(X_d+X_q) & j(X_d-X_q) \\ b_a & -2jX_{ad} & -j(X_{ad}-X_{aq}) & -j(X_{ad}+X_{aq}) & -j(X_d-X_q) & -j(X_d+X_q) \end{array} \qquad (30)$$

$$\boldsymbol{e}=\begin{array}{ccccc} d_f & f_k & b_k & f_a & b_a \\ \hline e_{df} & & & jve\epsilon^{-j\delta}/2 & -jve\epsilon^{j\delta}/2 \end{array} \qquad (31)$$

$$=\begin{array}{ccccc} d_f & f_k & b_k & f_a & b_a \\ \hline e_{df} & & & ve_f & -ve_b \end{array} \qquad (32)$$

where e_f and e_b appear on the steady-state network of Figure 2a derived in reference 5.

The Hunting Equivalent Network

(a). The equivalent circuit of hunting (with per-phase constants) corresponding to $\boldsymbol{Z}'$ is shown in Figure 2b. It is established in the same manner as the steady-state network shown in reference 5.

It should be noted that at least two negative resistances are needed, one for each network. Such negative resistances may be used in conjunction with the a-c network analyzer. If no negative resistances are at hand, the networks of Figure 2 may be changed in a manner similar to that shown in reference 5, Figures 3b, 6, and 7.

(b). The components of $\Delta\boldsymbol{E}=\boldsymbol{G}'\cdot\Delta\boldsymbol{i}'$ are shown in Figure 2b.

$$\Delta\boldsymbol{E}=\frac{1}{4}\begin{array}{c|c|}
f_a & 2jX_{ad}\,\Delta i^{df}+j(X_{ad}+X_{aq})\,\Delta i^{fk}+j(X_{ad}-X_{aq})\,\Delta i^{bk}+ \\
 & \qquad j(X_d+X_q)\,\Delta i^{fa}+j(X_d-X_q)\,\Delta i^{ba} \\
\hline
b_a & -[2jX_{ad}\,\Delta i^{df}+j(X_{ad}-X_{aq})\,\Delta i^{fk}+j(X_{ad}+X_{aq})\,\Delta i^{bk}+ \\
 & \qquad j(X_d-X_q)\,\Delta i^{fa}+j(X_d+X_q)\,\Delta i^{ba}]
\end{array} \qquad (33)$$

$$=\begin{array}{c|c|c|c|c|}
d_f & f_k & b_k & f_a & b_a \\
\hline
 & & & \Delta E_f & \Delta E_b
\end{array} \qquad (34)$$

(c). To find the impressed voltages $\Delta\boldsymbol{e}$ on the hunting network, let equation 31 be differentiated

$$\frac{\partial\boldsymbol{e}}{\partial\theta}=\begin{array}{c|c|c|c|c|}
d_f & f_k & b_k & f_a & b_a \\
\hline
 & & & -jve_f & -jve_b
\end{array} \qquad (35)$$

Hence the resultant $\Delta\boldsymbol{e}$ is

$$\Delta\boldsymbol{e}=\boldsymbol{n}'^{-1}\left(jh\boldsymbol{E}-\frac{\partial\boldsymbol{e}}{\partial\theta}-\frac{\Delta\boldsymbol{f}}{\Delta\theta}\right) \qquad (36)$$

$$\Delta\boldsymbol{e}=\begin{array}{c|c|c|c|c|}
d_f & f_k & b_k & f_a & b_a \\
\hline
-\dfrac{\Delta e_{df}}{h\,\Delta\theta} & & & \dfrac{j(hE_f+ve_f)}{h+v} & \dfrac{j(hE_b+ve_b)}{h-v}
\end{array} \qquad (37)$$

On the field an impressed hunting-frequency voltage Δe_{df} may exist.

(d). By measuring the watts of $\boldsymbol{i}^*\cdot\boldsymbol{E}$ and the watts and vars of $\boldsymbol{i}^*\cdot\Delta\boldsymbol{E}+\Delta\boldsymbol{i}^*\cdot\boldsymbol{E}$ as shown on Figure 2c, the steady-state torque (per phase), also the damping and synchronizing torques are found. (Note the change in sign of T_D and T_s due to that of $\Delta p\theta$.)

In per unit on Figure 2a the impressed voltages $je/2$ and $e_{df}jX_{ad}/2r_f$ are $1/\sqrt{2}$.

Appendix III. The Single-Phase Instrument-Selsyn

The Primitive Machine

Let a single-phase induction motor be considered (Figure 3a) in which the ratio of the cross-phase to the main-phase turns is a. Let it be assumed that the impedances of the two stator windings differ by $Z=R+Lp+1/pC$. Also on both stator and rotor windings let unbalanced voltages be impressed. (To simplify the equations the saliency of the stator will be neglected here, but is considered in Figure 4.)

The transient $\boldsymbol{Z}$, $\boldsymbol{G}$, and $\boldsymbol{e}$ of such a machine is [the primitive machine with four windings (reference 7, page 43, or reference 4)].

$$\boldsymbol{Z}=\begin{array}{c|c|c|c|c|}
 & d_s & q_s & d_r & q_r \\
\hline
d_s & r_s+L_sp & & Mp & \\
\hline
q_s & & a^2(r_s+L_sp+Z) & & aMp \\
\hline
d_r & Mp & aMp\theta & r_r+L_rp & L_rp\theta \\
\hline
q_r & -Mp\theta & aMp & -L_rp\theta & r_r+L_rp \\
\hline
\end{array} \qquad (38)$$

$$\boldsymbol{G}=\begin{array}{c|c|c|c|c|}
 & d_s & q_s & d_r & q_r \\
\hline
d_r & & aM & & L_r \\
\hline
q_r & -M & & -L_r & \\
\hline
\end{array} \qquad (39)$$

$$\boldsymbol{e}=\begin{array}{c|c|c|c|}
d_s & q_s & d_r & q_r \\
\hline
e_{ds} & e_{qs} & e_{dr} & e_{qr}
\end{array} \qquad (40)$$

where (if $r_s=r_{sd}$ and $l_s=l_{sd}$=leakage reactance)

$$Z=R+Lp+\frac{1}{pC}=\left(\frac{r_{sq}}{a^2}-r_{sd}+\frac{R_c}{a^2}\right)+\left(\frac{l_{sq}}{a^2}-l_{sd}\right)p+\frac{1}{pa^2C} \qquad (41)$$

Reduction to Symmetrical Form

Let symmetrical components be introduced by the transformation

$$i^{ds}=(i^{fs}+i^{bs})/2$$
$$i^{qs}=-j(i^{fs}-i^{bs})/2$$
$$i^{dr}=(i^{fr}+i^{br})/2$$
$$i^{qr}=-j(i^{fr}-i^{br})/2$$

$$\boldsymbol{C}=\frac{1}{2}\begin{array}{c|c|c|c|c|}
 & f_s & f_r & b_s & b_r \\
\hline
d_s & 1 & & 1 & \\
\hline
q_s & -j/a & & j/a & \\
\hline
d_r & & 1 & & 1 \\
\hline
q_r & & -j & & j \\
\hline
\end{array} \qquad (42)$$

By $\boldsymbol{C}_t^*\cdot\boldsymbol{Z}\cdot\boldsymbol{C}$, $\boldsymbol{C}_t^*\cdot\boldsymbol{G}\cdot\boldsymbol{C}$, and $\boldsymbol{C}_t^*\cdot\boldsymbol{e}$

$$\boldsymbol{Z}=\frac{1}{2}\begin{array}{c|c|c|c|c|}
 & f_s & f_r & b_s & b_r \\
\hline
f_s & r_s+L_sp+Z/2 & Mp & -Z/2 & \\
\hline
f_r & M(p-jp\theta) & r_r+L_r(p-jp\theta) & & \\
\hline
b_s & -Z/2 & & r_s+L_sp+Z/2 & Mp \\
\hline
b_r & & & M(p+jp\theta) & r_r+L_r(p+jp\theta) \\
\hline
\end{array} \qquad (43)$$

$$\boldsymbol{G}=\frac{1}{2}\begin{array}{c|c|c|c|c|}
 & f_s & f_r & b_s & b_r \\
\hline
f_r & -jM & -jL_r & & \\
\hline
b_r & & & jM & jL_r \\
\hline
\end{array} \qquad (44)$$

If the rotor is connected to the rotor of another Selsyn with infinite inertia, or with assumed constant speed

$$\boldsymbol{e}=\begin{array}{c|c|c|c|}
f_s & f_r & b_s & b_r \\
\hline
e_{fs} & (1-v)e_{fr}\epsilon^{j\delta} & e_{bs} & (1+v)e_{br}\epsilon^{-j\delta}
\end{array}$$

$$=\begin{array}{c|c|c|c|}
f_s & f_r & b_s & b_r \\
\hline
e_{fs} & (1-v)e_f & e_{bs} & (1+v)e_b
\end{array} \qquad (45)$$

Since the reference axes are stationary the frequency of the hunting currents is $(1+h)\omega$ and $(1-h)\omega$.

1. Replacing first p by $j(1+h)\omega$ $p\theta$ by $v\omega$, and dividing $\boldsymbol{Z}_1'$ by $\boldsymbol{n}_1'$

$$\boldsymbol{n}_1'=\begin{array}{c|c|c|c|c|}
 & f_s & f_r & b_s & b_r \\
\hline
f_s & 1+h & & & \\
\hline
f_r & & 1+h-v & & \\
\hline
b_s & & & 1+h & \\
\hline
b & & & & 1+h+v \\
\hline
\end{array} \qquad (46)$$

$$\boldsymbol{Z}_1'=\frac{1}{2}\begin{array}{c|c|c|c|c|}
 & f_s & f_r & b_s & b_r \\
\hline
f_s & \dfrac{r_s}{1+h}+jX_s+\dfrac{Z_1}{2} & jX_m & -\dfrac{Z_1}{2} & \\
\hline
f_r & jX_m & \dfrac{r_r}{1+h-v}+jX_r & & \\
\hline
b_s & -\dfrac{Z_1}{2} & & \dfrac{r_s}{1+h}+jX_s+\dfrac{Z_1}{2} & jX_m \\
\hline
b_r & & & jX_m & \dfrac{r_r}{1+h+v}+jX_r \\
\hline
\end{array} \qquad (47)$$

where the mutual impedance of the sequence axes Z_1 may have the form

$$Z_1=\frac{R}{1+h}+jX_L-\frac{jX_c}{(1+h)^2} \qquad (48)$$

2. Replacing p by $j(1-h)\omega$ and dividing $\boldsymbol{Z}_2'$ by $\boldsymbol{n}_2'$, the resultant $\boldsymbol{Z}_2'$ is the same as $\boldsymbol{Z}_1'$, except $+h$ everywhere is replaced by $-h$.

The same result is found if first p is replaced by $j(1\pm h)\omega$ then $\boldsymbol{C}$ is introduced.

The Hunting Equivalent Networks

The corresponding two equivalent networks are given in Figure 4b showing also $\Delta E_1 = \omega G' \cdot \Delta i_1$ and $\Delta E_2 = \omega G' \cdot \Delta i_2$. The saliency of the d axis is taken care of by the addition of a condenser $j(X_{mq} - X_{md})$ common to all four meshes, as in the case of the salient-pole synchronous machine, Figure 2b.

The voltages impressed on the hunting networks are by

$$\Delta e' = n'^{-1} \cdot \left(Ep - \frac{\partial e}{\partial \theta} \right) \qquad (49)$$

$$\Delta e_1' = \begin{array}{cccc} f_s & f_r & b_s & b_r \\ \hline \multicolumn{1}{|c|}{} & \multicolumn{1}{c|}{\dfrac{j[hE_f-(1-v)e_f]}{1+k-v}} & \multicolumn{1}{c|}{} & \multicolumn{1}{c|}{\dfrac{j[hE_b+(1+v)e_b]}{1+h+v}} \\ \hline \end{array} \qquad (50)$$

$$\Delta e_2' = \begin{array}{cccc} f_s & f_r & b_s & b_r \\ \hline \multicolumn{1}{|c|}{} & \multicolumn{1}{c|}{\dfrac{j[-hE_f-(1-v)e_f]}{1-h-v}} & \multicolumn{1}{c|}{} & \multicolumn{1}{c|}{\dfrac{j[-hE_b+(1+v)e_b]}{1-h+v}} \\ \hline \end{array} \qquad (51)$$

The torques are the real parts of

$$\Delta T = i^* \cdot (\Delta E_1 + \Delta E_2) + (\Delta i_1 + \Delta i_2)^* \cdot E \qquad (52)$$

By measuring watts and vars, the torques per phase are found by the formulas shown in Table I.

Table I. Torque Formulas for the Single-Phase Motors of Figures 4–7

$i^f E_f^* = W'$	$i^b E^{b*} = W''$	$T = W' + W''$
$i^f \Delta E_{f1}^* = W_1 + jQ_1$	$i^b \Delta E_{b1}^* = W_5 + jQ_5$	
$i^f \Delta E_{f2}^* = W_2 + jQ_2$	$i^b \Delta E_{b2}^* = W_6 + jQ_6$	
$\Delta i^{f1} E_f^* = W_3 + jQ_3$	$\Delta i^{b1} E_b^* = W_7 + jQ_7$	
$\Delta i^{f2} E_f^* = W_4 + jQ_4$	$\Delta i^{b2} E_b^* = W_8 + jQ_8$	
$T_s = \dfrac{W_1+W_2+W_3+W_4+W_5+W_6+W_7+W_8}{2}$	$T_D = \dfrac{(Q_2+Q_3+Q_6+Q_7)-(Q_1+Q_4+Q_5+Q_8)}{2h}$	

Special Cases

1. *The Capacitor Motor.* The rotor-impressed voltages e_{fr} and e_{br} are zero, Figure 5.

2. *Polyphase Induction Motor on Unbalanced Voltages.* $Z=0$, Figure 6.

3. *The Single-Phase Induction Motor.* The rotor-impressed voltages are zero and $Z=\infty$, Figure 7.

4. *The Doubly Fed Induction Motor.* $Z=0$ and $a=1$. Also $e_{bs}=e_{br}=0$ and the negative-sequence networks are missing, Figure 8. This case is illustrated in detail in the companion paper.[3]

5. *The Polyphase Induction Motor.* $Z=0$, the negative-sequence networks are missing and the rotor-impressed voltage e_{fr} is also zero, Figure 9.

Appendix IV. More General Cases of Hunting

Machines With Arbitrary Reference Frames

When a relative velocity $p\theta'$ exists between the stator and rotor reference axes (as in interconnected slip ring machines such as Selsyns) the equation of voltage has an additional term $p\theta' V \cdot i$ in it, so that

$$e = (R + Lp + p\theta G + p\theta' V) \cdot i = Z \cdot i \qquad (53)$$

The equation of hunting then is

$$Z \cdot \Delta i = \left[\frac{\partial e}{\partial \theta} - (G+V) \cdot ip \right] \Delta\theta = \left(\frac{\partial e}{\partial \theta} - E'p \right) \Delta\theta \qquad (54)$$

However V is not a tensor and it cannot be physically represented.

In some cases (in machines with symmetrical structures) V may be changed into a tensor by assuming all reference axes to rotate at the same speed. But in case of interconnected synchronous machines or instrument Selsyns that assumption cannot be made.

Tensorial Form of the Hunting Equation

It is well-known that while the equation of Lagrange or its generalization, equation 53

$$e_\alpha = R_{\alpha\beta} i^\beta + a_{\alpha\beta} \frac{di^\beta}{dt} + \Gamma_{\beta\gamma,\alpha} i^\beta i^\gamma \qquad (55)$$

is a tensor equation, the equation of small oscillations (derived from it by replacing x^α by $x^\alpha + \Delta x^\alpha$ and i^α by $i^\alpha + \Delta i^\alpha$) equation 54, or

$$\Delta e_\alpha = R_{\alpha\beta} \Delta i^\beta + a_{\alpha\beta} \frac{d(\Delta i^\beta)}{dt} + \Gamma_{\beta\gamma,\alpha} \Delta i^\beta i^\gamma + \Gamma_{\beta\gamma,\alpha} i^\beta \Delta i^\gamma + \frac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta} i^\beta i^\gamma \Delta x^\delta \qquad (56)$$

is no longer a tensor equation. That is Δi^β itself is not a vector (tensor of rank one) nor can the various terms be grouped to form tensors. Hence, no physical model (equivalent circuit) can be established to represent these equations in the general case.

The tensorial form of the equation of small oscillations of dynamical systems in general and of electrical machinery in particular is[9]

$$\delta e_\alpha = R_{\alpha\beta} \delta i^\beta + a_{\alpha\beta} \frac{\delta(\delta i^\beta)}{dt} + K_{\delta\gamma\beta\alpha} i^\delta i^\beta dx^\gamma + R_{\gamma\beta\alpha} i^\beta dx^\gamma \qquad (57)$$

where $K_{\delta\gamma\beta\alpha}$ is the Riemann-Christoffel curvature tensor and δi^β represent "absolute" or "covariant" differentials. This is the hunting equation whose equivalent circuit has to be established and this is the equation that fits the hunting equivalent circuits of machines having general reference axes.

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Saturated Synchronous Machines Under Transient Conditions in the Pole Axis

REINHOLD RÜDENBERG
MEMBER AIEE

1. Introduction

THE performance of synchronous machines in steady-state operation, as well as in the transient state, is substantially determined by magnetic saturation in their iron circuits.[13,14,20] It is well-known that high initial and sustained short-circuit currents develop under widely different conditions of saturation and that large capacitive loading is unstable but for the effect of saturation.

The transition from one state of operation to another under the influence of changing saturation will be considered in this paper, and we will develop the transient phenomena from the magnetic characteristic of the machine. We fix our attention on the direct or pole axis since the fields in the quadrature axis of the rotor act essentially independently, according to Blondel's theorem.[6] The solution becomes obvious if we concentrate the saturation at the pole cores of the rotor and confine ourselves at first to symmetrical three-phase conditions in the stator and to constant speed of the machine. The analysis is simple[9] if the rotor consists of laminated cores surrounded only by exciting coils. It becomes more involved if damper windings or solid steel rotors are used having paths for eddy-current formation.

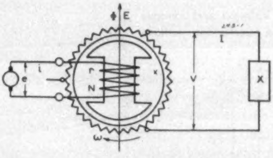

Figure 1. Constants of stator, rotor, and magnetic circuits of synchronous machine

2. Change With Time of the Main Flux

We consider first the stator circuit in Figure 1. The terminals with voltage V are loaded by an impedance X which, for simplicity, is shown with one phase only and may be purely reactive so that we have to consider only the pole axis of the machine. Consequently, the stator resistance may be neglected. Since higher harmonics are of secondary importance, we consider the phenomena of fundamental stator frequency only. With leakage reactance x, the external current I requires an internal electromotive force

$$E = V + xI = (X + x)I \tag{1}$$

This relation holds only if the amplitude variation of the current with time is slow compared with the harmonic variation due to the frequency ω,

$$\frac{dI}{dt} \ll \omega I \tag{2}$$

an assumption which will be verified later. With internal resistance and external active load, equation 1 remains unchanged in form, except that it then has a vector significance.

Secondly, we consider the rotor circuit in Figure 1. The external excitation voltage e is given either as constant or as a known function of time. With resistance r, number of turns N, and rotor pole flux Φ, the exciting current i in the transient state is determined by

$$e(t) = ri + N\frac{d\Phi}{dt} \tag{3}$$

This expression can be transformed by introducing the electromotive force E rather than the flux Φ, both always being proportional,

$$\Phi/\Phi_o = E/E_o \tag{4}$$

If we denote the rated values of flux Φ_o, stator voltage E_o and rotor voltage e_o, the last term of equation 3 equals either of the two following expressions:

$$N\frac{d\Phi}{dt} = T_m\frac{dE}{dt} = e_oT_p\frac{d(E/E_o)}{dt} \tag{5}$$

Herein

$$T_m = N\Phi_o/E_o \tag{6}$$

is a time constant of the complete machine which has the advantage of being constant entirely independent of the magnetic saturation, while

$$T_p = N\Phi_o/e_o \tag{7}$$

is a time constant of the rotor poles with their field windings, which has a simpler physical significance and is constant so long as the rated rotor values remain unchanged.

The rotor circuit-equation 3 now becomes, using equation 7,

$$e(t) = ri + e_oT_p\frac{d(E/E_o)}{dt} \tag{8}$$

It is expedient to take for Φ in equation 3 the total pole-core flux which is linked completely with the exciting winding. The electromotive force E in equation 4 corresponds to this flux, which is regarded, as usual, as the main flux of the machine to which also the magnetic characteristic is related. Hence the difference between electromotive force E and terminal voltage V is caused by the leakage fluxes of both stator and rotor and thus x in equation 1 defines the sum of stator and rotor

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Reinhold Rüdenberg is professor of electrical engineering in the graduate school at Harvard University, Cambridge, Mass.

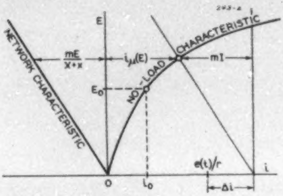


Figure 2. Internal and external excitation characteristics

leakage reactances,[7] both related to the stator circuit.

Thirdly, we consider the magnetic circuit of the machine in the direct or pole axis. With open stator terminals, the correlation of electromotive force E and exciting current i is given as "no-load characteristic" by the functions

$$E=E(i) \text{ or } i=i(E) \tag{9}$$

The rotor leakage at no load may be included herein.

With loaded machine the armature reaction of the current I causes a change of the resultant excitation. We can express this change by mI if m is a numerical factor expressing the effective turn ratio of stator and rotor windings, including the correction due to leakage and the effect of phase displacement in case of active load. If we consider the demagnetizing effect of inductive current as positive, the magnetic characteristic under load[1] is given by the function

$$E=E(i-mI) \tag{10}$$

which can be inverted to

$$i=i_\mu(E)+mI \tag{11}$$

This relation for the total exciting current i is represented by Figure 2. Herein $i_\mu(E)$ is that part of the exciting current which is needed for the magnetization of the internal main synchronous flux and mI is that part which is necessary for the magnetization of the electric stator circuit and which is transferred through the machine to the external load.

Not only the first but also the second part of the current in equation 11 is dependent solely on the electromotive force E, corresponding to the main flux Φ. We see this immediately by equation 1 since

$$I=\frac{E}{X+x} \tag{12}$$

This is represented at the left-hand side of Figure 2, and we will denote this line as the "network characteristic." With constant external and internal impedances this characteristic is a straight line through the origin, as shown in Figure 2. Its slope in terms of stator voltage and current is given merely by

$$X+x$$

If now we insert the stator current of equation 12 in equation 11, and the rotor current of equation 11 in equation 8, and define the rated exciting current i_o by

$$e_o=ri_o \tag{13}$$

we obtain the final relation for our problem

$$T_p\frac{d(E/E_o)}{dt}=\frac{e(t)/r-[i_\mu(E)+mE/(X+x)]}{i_o} \tag{14}$$

Thus, the change with time of the relative stator voltage E/E_o of the machine is determined by the difference of the fictitious steady-state or ohmic rotor current, namely e/r, under the impressed voltage $e(t)$, and the sum of the actual internal and external magnetizing currents necessary to produce the voltage E. This difference, indicating a current of unbalance, is denoted by Δi in Figure 2 and is obtained by drawing a parallel to the network characteristic through the working point on the no-load characteristic. If the difference Δi is positive, the voltage E increases; if Δi is negative, the voltage decreases. In both cases the rate of change is inversely proportional to the time constant T_p of the rotor.

Since the right-hand side of equation 14 depends only on the two variables t and E, we can solve this differential equation graphically for many problems. It is convenient to write equation 14 in a purely numerical form, namely

$$\frac{d(E/E_o)}{d(t/T_p)}=\frac{e(t)}{e_o}-\frac{\Sigma i(E)}{i_o} \tag{15}$$

where the last term on the right-hand side represents the excitation current of the entire internal plus external magnetic characteristic, as shown in Figure 3a. In Figure 3b and 3c there are derived by use of equation 15 the changes with time of voltage and exciting current when the load current of the generator is suddenly decreased. Under the influence of a voltage regulator a series resistance in the shunt excitation of the exciter may be switched on and off when the voltage E passes through the rated value. The voltage curve $e(t)$ of the exciter is given in Figure 3c, and the difference at every instant between this voltage and the total exciting current, taken from the characteristic in Figure 3a, both given in relative values, determines the rate of change of the voltage E, as indicated in Figure 3b. Step by step, the complete set of curves can be derived in this way, and experiments show that such curves agree well with actual oscillograms.[5,8]

For many important phenomena, the excitation voltage e remains constant during the transient process and this simplifies the problem materially. The exciting current immediately before the instant of switching is

$$i_1=e_1/r \tag{16}$$

e_1 denoting the constant rotor voltage. The first term on the right-hand side of equations 14 and 15 now is independent of time and this relation can be written

$$T_p\frac{(dE/E_o)}{dt}=\frac{i_1}{i_o}-\frac{\Sigma i(E)}{i_o}=\frac{\Delta i(E)}{i_o} \tag{17}$$

where the difference current Δi is now dependent on the voltage E only. From Figure 4a we see that this value

$$\Delta i=i_1-i_\mu-mI \tag{18}$$

is given by the difference shown shaded between the internal magnetic characteristic of the machine and the external network characteristic, drawn backwards

Figure 3. Point-by-point derivation of voltage- and current-time curves after sudden drop of load

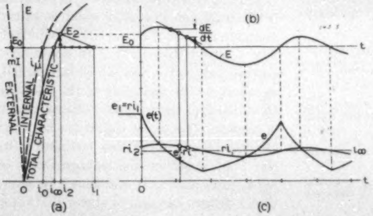
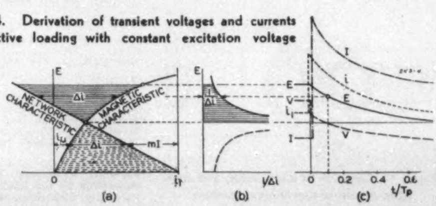


Figure 4. Derivation of transient voltages and currents at inductive loading with constant excitation voltage



from the original exciting current i_1. For every voltage E the change with time of the voltage is determined, therefore, by the horizontal distance between the two characteristics, and the entire E curve can easily be derived graphically. We can integrate equation 17 rigorously by separation of the variables and obtain the time elapsed since the instant of switching

$$t = T_g \int \frac{d(E/E_o)}{\Delta i/i_o} \tag{19}$$

This constitutes a simple quadrature and Figure 4b and c show how the E curve plotted against time can be derived by graphical integration of an auxiliary curve $1/\Delta i$ dependent on E.

The electromotive force E departs from the initial value which is determined by the previous operation of the machine, decreases at a rate determined by the value of Δi and approaches asymptotically the intersection of the internal and external characteristics, indicating a state of equilibrium. This new steady-state value may be approached from above or from below, depending upon the initial magnitude of the voltage E.

3. Transient Voltages and Currents

The other parameters of operation, as terminal voltage, exciting current, and stator current, can easily be determined graphically. According to equation 1, the terminal voltage V is the difference between electromotive force E and leakage reactance voltage xI. If, therefore, we draw in Figure 5 through the steady-state exciting current i_1 a straight line representing the leakage characteristic of the synchronous machine, with slope x rather than $X+x$ as for the network characteristic, this line subdivides the entire voltage E into two parts, namely, the terminal voltage V and the leakage voltage xI. Thus we can take the value of V for every value of E and transfer it to the time diagram, Figure 4c. While the electromotive force E is continuous at the instant of switching, the terminal voltage V jumps by an amount equal to the change of leakage voltage at the instant of switching.

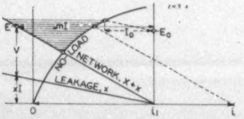


Figure 5. Three characteristics which are constant during inductive transient performance

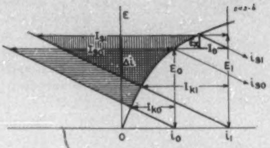


Figure 6. Unbalance of currents at sudden short circuit under two previous machine conditions

The value mI for the stator current I is given in Figure 5 by the horizontal distance between the characteristic point at the value E on the network characteristic and the vertical through the exciting current i_1. This follows directly from the definition of the network characteristic. Thus the stator current I can also be plotted in the time diagram, Figure 4c. The exciting current given by equation 11 is, according to Figure 5, the value cut off the i axis at a given instant by a line through the E point at the no-load characteristic and parallel to the network characteristic. It also is transferred to Figure 4c. Stator current and rotor current behave discontinuously at the instant of switching and jump suddenly to their new values, decreasing subsequently with time to their steady-state values, which for the exciting current coincides with the magnitude prior to the switching process.

In every case it is easy to determine the correct scale for the stator current. We need only to consider the Potier triangle in Figure 5, which by its magnitude for zero power factor gives a direct measure of the rated stator current. Thus we can measure I directly, rather than mI in the rotor scale.

A significant case of operation is the sudden short circuit of a synchronous machine at its terminals. Since now there is no external reactance X, the network characteristic becomes identical with the leakage characteristic and, therefore, drops in position. This is shown in Figure 6 for two different prior exciting currents, corresponding to no-load and to full-reactive-load conditions. The equilibrium of the currents in the machine is now heavily disturbed, and thus a large difference current Δi occurs. The voltage E, therefore, decreases rapidly, and very large currents are built up, constituting in the stator an alternating current I and in the rotor a direct current i, both decreasing continuously from their initial values to the final sustained values. Hence we have developed a method for determining numerically the change with time of these currents with any given saturation of the synchronous machine. Figure 6 shows that the initial short-circuit stator currents I_s for prior no-load and full-load conditions differ only slightly, this difference being equal to the magnitude of the prior load current, though the sustained currents I_k are widely different. In any case, the value of the initial alternating short-circuit current, given by the similarity of the triangles, is

$$I_s/I_o = E/E_x \tag{20}$$

where E denotes the electromotive force at the instant of short circuit and E_x the total leakage voltage of the machine at rated current I_o.

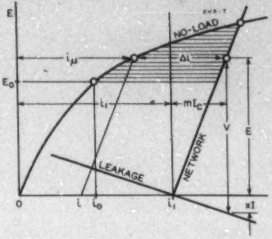


Figure 7. Three characteristics which are constant during capacitive transient performance

Another interesting case is the capacitive loading of a synchronous generator. The load current now is, by equation 1

$$I = \frac{E}{-1/\omega C + x} = \frac{\omega C}{1 - \omega C x} E = -I_c \tag{21}$$

and thus changes sign. The network characteristic therefore must be drawn from the exciting current i_1 toward the right-hand side, as in Figure 7, the slope being determined by the coefficient of E in equation 21. The increment of the capacitive current due to the combined action of external capacitive reactance and internal leakage inductance is given by the denominator of this coefficient. Resonance would be approached only for very large values of the capacitance. Equation 18 can now be written in the form

$$\Delta i = i_1 - i_\mu + mI_c \tag{22}$$

and we see from Figure 7 that Δi is again the horizontal difference between the no-load characteristic and the capacitive characteristic of the stator network. However, since Δi has changed its sign compared with the inductive loading of Figure 4, the voltage now increases with time toward the final value, again deter-

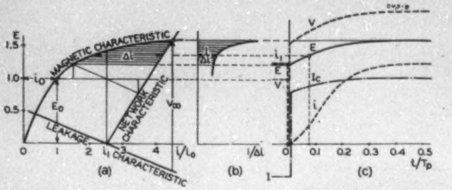


Figure 8. Derivation of transient voltages and currents at capacitive loading with constant excitation voltage

mined by the intersection of the two characteristics.

The terminal voltage V is always the vertical distance between network and leakage characteristics. If we lengthen the leakage characteristic in Figure 7 beyond the current i_1, we see that V becomes larger than the electromotive force E by reason of the inductive voltage rise produced by the capacitive current. A parallel line to the network characteristic through the E point on the no-load characteristic cuts off on the abscissa the magnitude of the exciting current i, as shown in Figure 7. The transient exciting current here is temporarily fairly small and even may become negative.

Figure 8 represents the sudden capacitive loading of a synchronous generator, previously under full inductive load, when the capacitance is so chosen that it would give full load under normal voltage. By drawing the inverted difference current $1/\Delta i$ and integrating this, we obtain in Figure 8c the change of voltage E with time, according to equation 19, and by graphical correlation we can add the other curves for terminal voltage V, stator current I_c and exciting current i. At the instant of switching all magnitudes, except the electromotive force E, jump to their new transient values. The exciting current returns finally to its original value i_1, and electromotive force, stator current and, particularly, terminal voltage attain high magnitudes.

We now can state the general rule for any transient change in synchronous machines with constant excitation[9] voltage. Up to the initiation of the switching process the machine may have worked, as in Figure 9, with an exciting current i_1 and an electromotive force E, both determined by the intersection of the no-load machine characteristic with the prior load network characteristic. When the network characteristic suddenly changes by a definite amount, turning to the left-hand side with increase of inductive load or to the right-hand side with decrease of inductive load or increase of capacitive load, the working point of the machine moves along the magnetic characteristic toward the new intersection at a rate given by equation 17 or 19, which is proportional to the current difference Δi of the shaded areas in Figure 9.

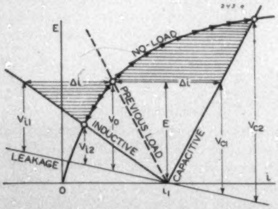


Figure 9. Unbalance of currents at sudden change of general reactive load

The phenomena occurring with removal of load of a synchronous machine are also easy to describe. If, for example, the sustained terminal short-circuit current of a generator is suddenly interrupted, as in Figure 10, the exciting current jumps temporarily to a very small value, i' or i'' according as the load changes to zero or to a finite value. The electromotive force E increases with a rate determined by the width of the shaded areas, and the momentary values of terminal voltage V and stator current I for any instant can easily be taken from the diagram by projecting from the load characteristic to the no-load and short-circuit lines. If, on the other hand, a capacitive load is switched off[4,12] as in Figure 11, the exciting current jumps temporarily to the larger value i' or i'', according to the final-load or no-load condition. Stator voltages and current can also be taken from the diagram, using the shaded areas and following the same scheme as before.

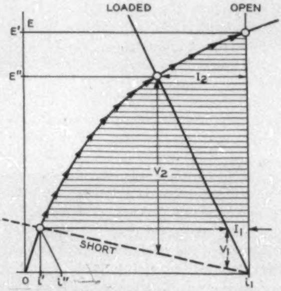


Figure 10. Interruption of short circuit and recovery toward loaded or open-circuited condition

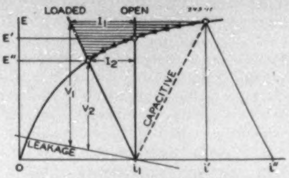


Figure 11. Interruption of capacitive load and recovery toward loaded or open-circuited condition

The only magnitude of our main equations 17 and 19 which is not contained in the diagram of characteristics is the time constant T_p of the rotor pole flux. Calculations according to equation 7 and oscillographic experiments have shown that for machines of the usual design for 50–60 cycles per second, with no additional resistance in the excitation circuit, the time constant T_p varies mainly with the synchronous power generated by each pole. It is:

for 100 1,000 10,000 kva per pole

in the order of

$T_p =$ 3 6 12 seconds

With numerical values of the integral of equation 19 such as are given on the axis of abscissae in Figures 4 and 8, we realize that the actual change with time of currents and voltages is always very slow compared with the change due to the frequency 50 or 60 cycles per second. Our assumption in equation 2 is verified therefore.

However, this is true only during the transient state following the instant of switching, but is not true for the time $t=0$ at which the switching occurs. Our curves for the stator currents and voltages represent only the amplitudes of the harmonically varying magnitudes, so that a jump of these amplitudes at $t=0$ does not mean necessarily an actual discontinuity but merely a rapid variation compared to the slower succeeding change. It is well-known that other transient currents may appear which prevent a discontinuous transition between the values immediately before and immediately after $t=0$. These additional transient currents flow without impressed voltage, suffer a rapid decay, and bridge the jump of the amplitudes only at the instant of switching. Their initial values, therefore, are given directly by the jumps of the currents and voltages in our time diagrams.

If the synchronous machine is loaded by self-inductance, either external or only internal, the superposed transient current is a decaying direct current, its time constant being that of the stator circuit. If, however, the load consists of capacitance, the superposed current is a damped oscillating current, the natural frequency being determined by capacitance and self-inductance of the machine and the load. It is well-known that the actual currents or voltages, by the additional effects of these free intermediate currents, may rise temporarily to about double the value of the original discontinuous magnitudes.

The rate of change of any curve can be expressed significantly by the value of the subtangent S. This is shown in Figure 12b with curve E which is derived from the characteristics in Figure 12a of the magnetic and electric circuits of a synchronous machine. By using equation 17 we can express the subtangent, as cut off on the asymptotic axis,

$$S=\frac{\Delta E}{dE/dt}=T_p\,\frac{\Delta E/E_o}{\Delta i/i_o} \tag{23}$$

if we denote by ΔE the difference of the momentary and final voltages, corresponding to the current difference Δi. Hence the ratio of the subtangent to the time constant of our problem is given by the ratio of the height to the width of the shaded characteristic area in Figure 12a, both measured in relative values.

Let us consider two extreme examples with different asymptotic points of equilibrium. With no load of a nonsaturated machine, we see from Figure 13a that

$$\left.\begin{aligned}&\Delta E/\Delta i=E_o/i_o\\ &\text{hence}\quad S_o=T_p\end{aligned}\right\} \tag{24}$$

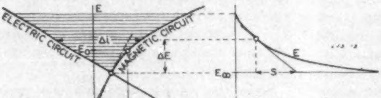


Figure 12. Subtangent of the voltage curve defining the character of change with time

or the subtangent equals the time constant. With saturation, corresponding to the dashed curve in Figure 13a, ΔE for the same Δi is smaller than before and, therefore, $S < T_p$.

With terminal short circuit of a nonsaturated machine, Figure 13b shows, with I_s for the sudden and I_k for the sustained short-circuit current, that

$$\left.\begin{aligned}&\Delta i=mI_s,\quad \Delta E=E_o\,\frac{mI_k}{i_o}\\ &\text{hence}\quad S_k=T_p\,\frac{I_k}{I_s}\end{aligned}\right\} \tag{25}$$

and the subtangent is a fraction only of the rotor-pole time constant T_p. This means that the equivalent self-inductance of the field winding is reduced by the reaction of the short-circuited armature. Thus the decay of the electromotive force and of the main flux of a machine under short-circuit conditions is more rapid than under no-load conditions in the ratio of sustained to sudden short-circuit current. With saturation, corresponding to the dashed curve of Figure 13b, Δi becomes larger for the same ΔE, and therefore S is smaller than before.

So we see that the subtangent S of the E curve is constant only for nonsaturated machines, and therefore only these machines can show an exponential decay of the voltages and also of the load- and short-circuit currents. Actual saturated machines, however, have a subtangent S which, corresponding to equation 23 and Figure 12, may be small during the initial stages and may increase toward a constant value at the final stages only, where the magnetic characteristic approaches the steady-state point linearly in the direction of its tangent. With capacitive load this rule is inverted, the subtangent decreases with higher saturation. All influences which by smaller armature reaction elevate the network characteristic, such as ohmic resistance or one- or two-terminal loading, increase the magnitude of the subtangent toward the no-load value of equation 24.

4. Effect of Damper Circuits

If the poles of a synchronous generator are equipped with damper circuits, these form for the most part a squirrel cage at the surface of the pole faces, both with salient-pole and cylindrical rotors. The total flux in the damper winding thus is identical with the flux entering the stator surface, if we neglect the very small circumferential flux in the air gap, including the damper slot flux. Between damper and field windings, however, a circumferential flux can pass to the adjacent poles, as in Figure 14. This constitutes the rotor leakage flux, in the transient part of which we are interested. A lumped damper circuit rather than a distributed winding is shown in Figure 14 in order to simplify the problem with regard to the direct or pole axis.

We denote by σ the leakage coefficient between damper and excitation windings due to the rotor leakage flux, by T_d the magnetic time constant of the damper winding, given as quotient of self-inductance and resistance, while T_p is the time constant of the exciting pole winding as before. It is well-known that, without regard to saturation, two exponential transient currents may flow in the two magnetically coupled windings, the time constants T of which are given as roots of the quadratic equation[2]

$$T^2-T(T_p+T_d)+\sigma T_pT_d=0 \tag{26}$$

Hence for small leakage, as in actual machines, the time constants are very nearly

$$T_1=T_p+T_d;\quad T_2=\frac{\sigma}{1/T_p+1/T_d}\cong\sigma T_d \tag{27}$$

where the last term on the right-hand side is a further approximation for T_2 for relatively small T_d. The primary time con-

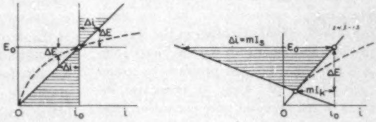


Figure 13 (right). No-load and short-circuit conditions of nonsaturated and saturated machines

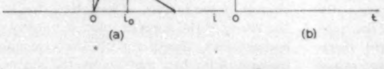


Figure 14 (lower left). Damper circuits and rotor leakage in synchronous machine

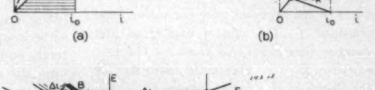


Figure 15 (right). Behavior of slow-transient main flux and rapid-transient rotor-leakage flux at inductive loading stant T_1, given by the sum of T_p and T_d, represents the change with time of the main flux, and the secondary time constant, T_2, given by the product of two small quantities, represents the change with time of the leakage flux. Thus we can distinguish between a slow-transient main flux and a rapid-transient leakage flux. To both fluxes are correlated the respective voltages and currents in the windings.

An actual medium-sized machine may have an excitation time constant of the poles $T_p=5$ seconds, a damper time constant $T_d=1$ second, and a leakage coefficient between both rotor windings $\sigma=$ 15 per cent. Then we have the two resultant time constants

$$T_1=5+1=6 \text{ seconds}$$

$$T_2=0.15\,\frac{5\cdot 1}{5+1}=0.125 \text{ second}$$

T_2 being only two per cent of T_1. For current of 60 cycles per second the exponential damping for half a cycle is

$$\epsilon^{-\frac{t}{T_1}}=0.999;\quad \epsilon^{-\frac{t}{T_2}}=0.933$$

The rapid-transient rotor leakage flux flows for the most part in air, as seen by Figure 14. Hence this flux, constituting a free magnetic field which acts in addition to the main pole flux, is proportional or nearly so to its exciting currents. Therefore, it will be only slightly influenced by saturation and thus will change exponentially with time in actual machines. The leakage time constant T_2 is, therefore, a true or nearly true exponential time constant. It may vary, however, to some extent with different loading of the stator, caused by armature reaction on the damper circuit.[21]

The slow-transient main flux, on the other hand, flows through the armature and poles of the machine and is, therefore, greatly influenced by saturation of these steel parts. Thus, we cannot expect that the main time constant T_1 will affect the phenomena exponentially, but we must use it with actual machines in the way expressed graphically in our previous solution for the saturated problem by equations 17 and 19. Only we must use, instead of the previous pure excitation time constant T_p, the sum of excitation and damper time constants given by the first equation 27, T_1. Hence the main flux, or the electromotive force, is given by

$$T_1\,\frac{d(E/E_0)}{dt}=\frac{\Delta i}{i_0} \tag{28}$$

all other notations remaining unchanged.

Both fluxes, together, the slow-transient main-pole flux with the large saturated time constant T_1 and the rapid-transient rotor-leakage flux with the small exponential time constant T_2, constitute the armature flux entering the surface of the stator and producing in combination with the stator-leakage flux the terminal voltage and the stator current. At the instant of switching, the field winding keeps the pole flux constant, the damper keeps the air-gap armature flux constant, and thus only the stator-leakage flux can change instantaneously.

The initial values of all magnitudes can be determined by a diagram of characteristics as in Figure 15, which shows the case of sudden inductive loading of the synchronous machine. If no damper were present, there would be effective from the beginning the total network characteristic, formed by the reactances $X+x_s+x_r$, the last two terms denoting the leakage reactances of stator and rotor separately. Point B on this characteristic then would represent the current and would move down at a rate given by our previous considerations. With damper winding at the pole surfaces, however, not only the main-pole flux but also the rotor-leakage flux remains constant during the instant of switching, since the leakage flux is linked with the damper winding. Only the external field and the stator-leakage flux can vary, therefore, with the stator current, and this is represented by the line $X+x_s$ in Figure 15. This line intersects with the voltage of the constant-pole flux at point A, and this point is, therefore, the actual starting point. If the damper were as strong as the field winding, it would sustain the rotor-leakage flux for some time, and point A would gradually move down along the stator line $X+x_s$. Actually, however, the rotor-leakage flux varies with the relatively small time constant T_2 and, therefore, the effective network characteristic turns rapidly from the initial line $X+x_s$ to the final position $X+x_s+x_r$ in an exponential manner, corresponding to ϵ^{-t/T_2}. If T_2 is known, the intermediate characteristics for successive times can easily be drawn into the diagram.

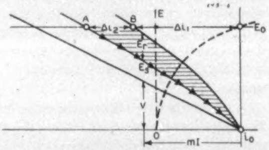


Figure 16. Effect of magnetic saturation in path of leakage flux

The turning of this effective network characteristic corresponds to the rapid decay of the transient rotor-leakage flux. During this time, which is usually of the order of a tenth of a second, the main flux and its electromotive force E, also shown by Figure 15, have just started to move down over the shaded area at a rate given by the time constant T_1 and the current difference Δi. The smaller the external load, the steeper is the external characteristic X and the smaller is the current difference Δi to which, according to equation 28, the change of voltage with time is proportional. Thus, with known time constants and known characteristics, it is easy to trace the intermediate curve between stator and rotor lines in Figure 15 on which the point A gradually moves down toward the final intersection of network and magnetic characteristics.

If the leakage circuits of stator and rotor are saturated in any way,[15,16] we can include this effect in our considerations. Instead of straight lines, we have merely to draw curved additional stator- and rotor-leakage lines, as in Figure 16. The total network characteristic now is composed of the external reactive voltage V and the internal leakage voltages E_s and E_r, both depending on the current I with different effects of saturation. Corresponding to our previous consideration of saturated transients, Δi_2 in Figure 16 represents a current difference sustained by the damper circuit, which for some time keeps alive the rotor-leakage flux. The saturated rapid-transient variation therefore is given, corresponding to equation 17, by

$$T_2\,\frac{d(E/E_0)}{dt}=\frac{\Delta i_2}{i_0} \tag{29}$$

which is easy to evaluate graphically. According to this relation point A moves rapidly down to zero in a nonexponential manner, and the additional damper current Δi_2 can be determined for any time t. Meanwhile the pole flux in the main magnetic circuit decreases slowly according to equation 28 with the large time constant T_1. Its current difference Δi_1, in Figure 16, thus changes only slightly while Δi_2 drops to zero.

Since the rotor-leakage flux does not change at the instant of switching due to the effect of the damper, only the stator-leakage flux, together with the external reactive flux, can vary with the stator current, the total armature flux remaining constant. As shown in Figure 17a, the stator current I jumps, therefore, to the characteristic point A and is built up to such a magnitude that it balances not only the increase of the exciting current but also the damper current. Thus the initial

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value I_A of the stator current is given in Figure 17a by the horizontal distance between the previous exciting current i_1 and the point A. The terminal voltage V is always given by the height of the external characteristic X in Figure 17a under the working point on the actual network characteristic. At the initial moment, V_A is found as the section under point A, the difference from the electromotive force E consisting in the stator-leakage voltage only, since the rotor-leakage flux does not vary at that instant. After some time, however, when the effective network characteristic has turned to its final position, the difference $E-V$ is given always by the combined stator- and rotor-leakage voltage. The slow transient part of the terminal voltage originates at the value V_B under the point B, the difference V_A-V_B being equal to the rotor-leakage voltage, and decreases toward the steady-state value V_∞.

In Figure 17b the slow transient drop of the electromotive force E and the slow and rapid transient decay of the terminal voltage V are shown following sudden loading of the machine. The electromotive force E, corresponding to the rotor-pole flux, decreases continuously from the no-load value, finally attaining the steady-state value at the intersection of both main characteristics. The terminal voltage, V, however, jumps from the no-load value, identical with the electromotive force, by an amount caused by the suddenly appearing stator-leakage drop and reaching instantaneously V_A. The rotor-leakage drop, on the other hand, does not occur suddenly but builds up gradually with the time constant T_2, and thus effects a rapid decrease of the terminal voltage to the asymptotic curve beginning with V_B and caused by the decay of the main-pole flux. The stator current follows a similar curve taken from the horizontal distances in Figure 17a and also shown in Figure 17b.

Since at the first instant the damper acts merely as a supplement to the field winding, excluding any effect of the rotor leakage, we obtain the total rotor current at the instant of switching by drawing through the working point on the magnetic characteristic, as in Figure 15, a parallel to the stator line $X+x_s$. Later, however, after the decay of the rapid-transient magnitudes, we must draw the parallel to the total network characteristic $X+x_s+x_r$. Thus the total rotor current starts with a high value i_A and approaches rapidly the slow transient current decreasing from the initial value i_B and finally reattaining the previous current i_1. Hence by the presence of the damper winding, stator current, as well as rotor current, acquires a superposed rapid-transient peak which decays exponentially with the time constant T_2, while the excitation winding causes a subsequent slow-transient decrease determined in its shape by the shaded area. The main-pole flux and the electromotive force E decrease continuously without superposed initial peak.

The total transient rotor current consists of exciting current and damper current. During the slow-transient period both are produced by the same decreasing main flux, therefore inducing the same voltage per turn in the respective windings. The subdivision of the slow-transient current into its components is, therefore, determined by the resistances or by the time constants of the exciting winding T_p and the damper winding T_d, as shown in Figure 15. The rapid-transient supplementary current Δi_2, on the other hand, induced by the decaying rotor-leakage flux, flows entirely in the damper circuit. It is understood that the damper current is measured here in the scale of the field current and may be converted to its proper value by the turn ratio.

5. Initial Conditions Under Load

If the synchronous machine at the prior condition is not open-circuited but is under load, the initial fluxes in stator and rotor are not equal. In Figure 18 the Potier-Sumec load triangle, or rather quadrangle,[3] is shown in which point a marks the magnitude of the stator flux, and b that of the rotor flux, which is larger, because of the increased rotor leakage flux under load. Figure 18 represents the phenomena occurring under a sudden short circuit near the machine terminals, so that the external characteristic vanishes completely, and only the rotor- and stator-leakage characteristics remain, both of the same order of magnitude. Without a damper, the pole flux corresponding to point b would remain constant at the instant of switching, and the current difference between B and b would move down the shaded area, according to equation 17.

This simple process is altered in two respects by the presence of a damper. First, the saturated time constant for the rotor-pole flux is increased to the value of the first equation 27, and thus the current difference Δi moves more slowly according to equation 28. Second, not only the pole flux but also the rotor-leakage flux, and therefore the total armature flux, remain constant at the instant of switching. Since this stator flux for the prior load condition is given by the point a in Figure 18, the corresponding point on the stator leakage characteristic up to which the flux can vary instantaneously is A, and, therefore, the stator current jumps from the prior value I_o to the large value I_A. This is the instantaneous value of the stator short-circuit current and its magnitude is

$$I_A=\frac{E_o+E_s}{x_s}=\frac{E_o}{x_s}+I_o \tag{30}$$

It exceeds the no-load instantaneous current merely by the value of the prior load current, as was also the case without damper action.

With the decay of the rapid-transient rotor-leakage flux, the characteristic line x_s turns, with an exponential time constant T_2, to the total leakage characteristic x_s+x_r. Meanwhile the main-pole flux with the electromotive force E moves down, producing a slow-transient stator current beginning at point B with the value

$$I_B=\frac{E_o+E_s+E_r}{x_s+x_r}=\frac{E_o}{x_s+x_r}+I_o \tag{31}$$

Thus both the initial short-circuit currents are determined mainly by the prior terminal voltage E_o and the stator- and rotor-leakage reactances.

The peak of the rapid-transient current, on the initial value of the slow-transient current, Figure 17, is given by the difference of equations 30 and 31 and is large with terminal short circuit. With distant short circuits, however, where the denominators of equations 30 and 31 contain, in addition, the external impedance, the peak is, without prior load,

$$\frac{I_A-I_B}{I_B}=\frac{x_r}{X+x_s} \tag{32}$$

a ratio indicated in Figure 17, and this excess value becomes less significant the larger the external impedance.

When evaluating the stator currents from such diagrams, it should be noted that the scales for the two currents I_A and I_B are slightly different, due to the fact that the ratio of stator current to rotor current is given mainly by the turn ratio, but is also slightly dependent on the leakages. I_A, however, is determined only by the stator leakage, while I_B is determined by both stator and rotor leakages.

We can now abandon our conception of a lumped damper winding as in Figure 14, which gives a fixed value of secondary time constant. It is well-known that multiple circuits, extended in space, as they are used with squirrel-cage dampers and also with solid cylindrical rotors, have not one single time constant but a series of

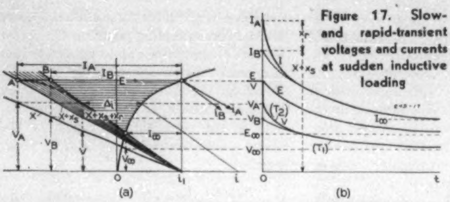

Figure 17. Slow- and rapid-transient voltages and currents at sudden inductive loading

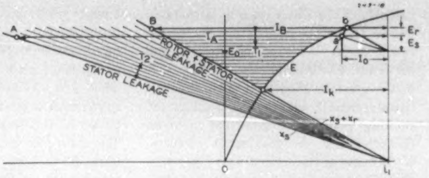

Figure 18. Influence of stator and rotor leakage at sudden short circuit of previously loaded synchronous machine with damper

exponential time constants, each belonging merely to a partial amplitude of the current. This means that the flux through distributed dampers does not decay exponentially but according to a more complicated time function, beginning rapidly and ending slowly. Thus we have to turn the effective leakage characteristic from the stator line to the rotor line in Figures 15–18, not exponentially, but quickly at first and more slowly finally, approaching the speed of the lowest rapid-transient time constant. Hence we see that the starting and ending lines of the rapid-transient period always go through the points *A* and *B*, and that only slight alterations result in the intermediate position of the effective characteristic. The initial and final values of the rapid- and of the slow-transient voltages and currents also remain unchanged.

It may be noted that the slow-transient and rapid-transient phenomena of this investigation are not identical with the transient and subtransient phenomena commonly used.[10,11,18] Figure 19 shows for a sudden short circuit the difference between the two conceptions. The "subtransient" current is usually defined as the difference between the actual change of the current with time and the "transient" exponential asymptote, which has a time constant equal to the longest subtangent. This latter curve, drawn backwards as dashed in Figure 19, lies considerably deeper than the slow-transient curve which has a smaller subtangent in the beginning due to the magnetic saturation. The "transient reactance," defined by the start I' of the slowest exponential asymptote, is therefore with saturated machines materially greater than the sum of stator- and rotor-leakage reactances, the sum giving the beginning I_B.

Let us consider some further cases which are of interest in the operation of synchronous machines. Figure 20 shows the phenomena at the clearing of a short circuit near the terminals after which the machine again supplies a certain load. The diagram contains the stator- and rotor-leakage characteristics for the prior short-circuit operation, and the network characteristic for the final operation, including stator and rotor leakages separately. At the instant of interrupting the short circuit, the rotor-pole flux starts on the magnetic characteristic at a point *b* given by stator-plus rotor-leakage characteristic. The pole flux and electromotive force change, according to the shaded area, with the difference current Δi between magnetic characteristic and total network characteristic. This construction is the same as would be used without damper action except that the time constant is somewhat larger, namely T_1. This gives the initial slow-transient terminal voltage V_B and the subsequent change with time, as shown in Figure 20b, up to the final voltage V_∞.

However, the actual armature flux at the instant of switching is given by the stator-leakage flux only, corresponding to point *a*, and this leakage flux alone can jump at the instant of switching. Thus only the stator-leakage characteristic jumps suddenly to the network stator line $X+x_s$ through point *A* in Figure 20a. The initial terminal voltage immediately after interruption of the short circuit is, therefore, given by V_A as the vertical difference between the two stator lines, under load and under short circuit. The difference between the two voltages V_B and V_A, corresponding to the initial rotor-leakage flux, decays rapidly with the small time constant T_2, as shown in Figure 20b. Hence, the terminal voltage, being zero during the time of short circuit, jumps with the interruption instantaneously to a value determined by the prior stator-leakage drop, then increases rapidly due to the vanishing rotor-leakage flux sustained for a short time by the damper winding,[17] and finally creeps slowly upward due to the reappearing main-pole flux of the machine.

This change with time of the recovery voltage is important in the operation of circuit breakers interrupting a short-circuit current. We see that this voltage consists of three parts, the rate of ascent of which for full interruption of the current we will compute. The first step is the instantaneous appearance of the prior stator-leakage voltage V_s at the terminals. The rate of increase is V_s/O, and this indicates that an overshooting of voltage can occur if capacitance is present in the circuit. The second step is constituted by the prior rotor leakage voltage V_r and its rate of change is determined by the small time constant as V_r/T_2. The third step, following the magnetic saturation curve with the time constant T_1, is given by equation 28. Thus the total initial rate of increase of the recovery voltage is

$$\left(\frac{dV}{dt}\right)_o=\frac{V_s}{O}+\frac{V_r}{T_2}+\frac{E_0}{T_1}\,\frac{\Delta i}{i_0} \tag{33}$$

Usually the second term is five to ten times as large as the third term, and, therefore, the subsequent increase of the recovery voltage is determined mainly by the rotor-leakage voltage and the rotor-leakage time constant.

The stator current flowing into the load, given by the horizontal distance between the network characteristic and the vertical through the steady-state excitation, starts with relatively small magnitudes I_A and I_B and increases slowly to its final value. The transient rotor current is again cut off on the axis of abscissae by a parallel to the final network characteristic and divides into a larger exciting current and a smaller damper current in the ratio of the time constants T_p and T_d.

The sudden capacitive loading of a synchronous generator is shown in the diagram Figure 21. An extension of our previous Figure 8, this figure shows the transition from full inductive load to full capacitive load under the influence of magnetic

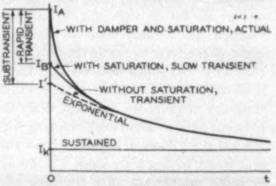

Figure 19. Comparison of slow-transient and rapid-transient phenomena with division into transient and subtransient curves

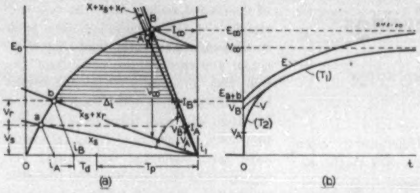

Figure 20. Interruption of terminal short circuit of synchronous machine with damper

saturation and damper circuits. The slow-transient voltage is determined by the large time constant T_1 and the shaded current difference, Δi beginning between the complete network characteristics under full inductive and full capacitive load. Superposed are the rapid-transient magnitudes given by the turning of the stator line to the rotor line of the final capacitive characteristic. The terminal voltage starts with a jump from E_o to the vertical height V_A through point A of the subsequent stator line, indicating unchanged stator flux, and approaches the rotor line through point B at constant rotor flux.

In Figure 21b the change with time of stator voltage and rotor current is represented. The voltage increase due to stator leakage is instantaneous, the increase due to rotor leakage is rapid, and the increase due to the main flux is relatively slow. The exciting current jumps to a small value and regains its prior value gradually. The stator current also jumps suddenly from the inductive to the capacitive direction and increases further to its final value, I_∞, given by the intersection of the characteristics. Actually, all discontinuities of voltage and current are smoothed by a superposed transient oscillation between the capacitance and the inductance. Since the natural period of these oscillations is always small compared even with the time constant of the damper winding, only the stator leakage is effective and determines the natural frequency.

We have assumed in the beginning that the magnetic saturation is concentrated in the rotor poles, a condition under which the Potier-Sumec quadrangle of Figure 18, for example, is a straight-line figure. If, however, the stator is also saturated in yoke and teeth, the increased rotor-leakage flux needs additional excitation by rotor saturation alone, and thus point a approaches the main magnetic characteristic. In every case, characteristics for the total stator flux and total rotor flux can be plotted separately[19] and used in an equivalent manner, as the corners of the straight-line quadrangle have been used for rotor-pole saturation only.

Although for the sake of simplicity our deductions have been confined to symmetrical, three-phase, reactive load having a straight-line external characteristic, there is no restriction in principle against using our graphical method for other conditions of load. Any active load also gives a definite correlation of voltage and stator current, except that we must consider, with respect to the armature reaction within the machine, the internal reactive component only. This can be done by methods well-known in the two-reaction theory of synchronous machines. With any kind of constant-impedance load, the external characteristic, and, therefore, the total network characteristic, including leakages, remains linear. Even unbalanced loads, as under line-to-neutral or line-to-line short-circuit conditions, can be treated in the same way except that only their positive-sequence component enters the graphical analysis. The remaining negative-sequence and zero-sequence components, on the other hand, do not interact materially with the synchronous fields of the machine if damper circuits are present.

If, finally, the load consists of other synchronous or induction motors which generate a counterelectromotive force and, therefore, prevent the external characteristic from passing through the origin, or if the load consists of transformers with magnetizing currents increasing rapidly with the voltage, the network characteristic may become flat-topped or markedly curved. Since, however, the linearity of the network characteristic does not play any part in our derivations, we see that our general method of solution remains unchanged even in such more complicated cases.

6. Summary

By reducing to the same parameter with saturation the voltage-current equations for stator, rotor, and magnetic circuits of synchronous machines, a differential equation in time for the main-pole flux and the induced electromotive force

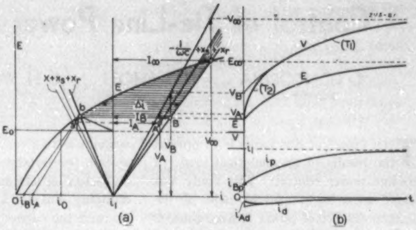

Figure 21. Capacitive loading of synchronous machine with damper

is derived, a relation easy to evaluate graphically. The difference of current between the magnetic characteristic of the machine and the electric characteristic of the stator circuit determines the rate of change of the electromotive force from which all the other slow-transient magnitudes can be derived.

For constant excitation voltage, a closed solution for the variation of flux and electromotive force is given, valid for any sudden change of load, including short-circuit current formation or capacitive superexcitation. The combined effect of damper circuits and rotor leakage causes a superposed rapid-transient variation manifested by an additional current peak at sudden short circuits and by a rapid initial voltage rise at sudden interruptions of the circuit.

The method of solution is in principle independent of the distribution of the saturation on rotor and stator, as well as of the character of loading of the machine, be it by constant or variable impedance, by symmetrical or unbalanced currents, by active or reactive power. The common subdivision into transient and subtransient phenomena is not identical with the separation into slow- and rapid-transient effects, the physical significance of which is derived in this paper.

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6. AN EXTENSION OF BLONDEL'S TWO-REACTION

Control of Tie-Line Power Swings

C. CONCORDIA
MEMBER AIEE

H. S. SHOTT
ASSOCIATE AIEE

C. N. WEYGANDT
ASSOCIATE AIEE

THE object of this paper is to present the results of an analytical study of tie-line power control. This study was made as a logical development of the general subject of power system control. Previous work[1,2] has indicated the general requirements of the prime-mover speed governors and the desirability of supplementary control to insure proper load division, frequency, and time. The general requirements of supplementary controls are given in the companion paper[3] by Crary and McClure. In most cases these controls are satisfactorily obtained as rather slow corrective adjustments to the speed-governor mechanisms, but for certain types of load a more active tie-line load controller, which will tend to suppress transient load swings also, may be required. An example of this is a rapidly varying load, such as a strip mill supplied from local generation as well as from a tie to a larger power system, in which it may be desirable to keep the load variations off the tie line as much as possible.

The characteristics and response of this kind of more active tie-line load controller have been studied in order to determine

1. The best tie-line controller and speed-governor characteristics (that is, the best controller characteristics for a given governor, and conversely the best governor characteristics for a given controller) to give the most effective tie-line power control.

2. The maximum effectiveness of the optimum controller in reducing the magnitude of tie-line power swings.

Basis of Study

The approximate torque equations for two interconnected speed-governed power systems were given in reference 2. Each system was represented as a single machine having an equivalent inertia, load-damping, and speed-governor characteristic, and the capacity of the tie line was specified by synchronizing torque and tie-line damping torque coefficients. In the present analysis the equation for one of the systems includes the additional effects of a tie-line power controller and (for a few cases) a speed droop-correction mechanism. The other system is considered to be infinitely large, that is, its electrical angle is fixed with respect to the standard frequency reference. The torque equations used and the definitions of the various system parameters are given in the appendix.

Governor Mechanism

Speed-change and tie-line controller indications are applied to an idealized governor-control mechanism[2] consisting of a two-stage amplifier with time lags T_1 and T_2 and an over-all amplification factor inversely equal to the governor regulation. The prime-mover input torque change is equal, therefore, to the output of the second stage. Since the degree of stability of this idealized system tends to be somewhat greater than that of an actual governor having more than two time lags, practically all of the results have been obtained under the least stable condition of equal time lags.[2] In certain cases, the effects of droop correction have been simulated by allowing the regulation to decrease from the transient value, R, to the steady-state value, r, at a rate defined by the time constant T_3 of the droop compensation mechanism.[1]

Tie-Line Control

In the usual type of tie-line controller, the indications are transmitted intermittently at equal time intervals to the governing system through the synchronizing motor which is operated from a constant potential source. During each of the equal time intervals, usually of the order of two second period, the motor circuit is closed a length of time proportional to the deflection of the controller galvanometer or load sensitive device at the beginning of the interval. The average effect of these impulses is to change the synchronizing-motor position, and, consequently, the magnitude of the prime-mover input correction, at a rate proportional to the deviation in tie-line power from the scheduled value. Some of the results obtained from the differential-analyzer solution of the problem include the effects of this intermittent controller.

It is assumed that when using the corresponding continuously acting controller, the type principally considered in this analysis, the control circuit will be arranged to operate the synchronizing motor at a speed proportional to the tie-line power deviation. The rate of correction is inversely proportional to the controller regulation R_t'; or, R_t' is a measure of the time required to change the synchronizing-motor position an amount equivalent

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C. Concordia and H. S. Shott are with the central station engineering department, General Electric Company, Schenectady, N. Y. C. N. Weygandt is instructor in the Moore School of Electrical Engineering, University of Pennsylvania, Philadelphia, Pa.

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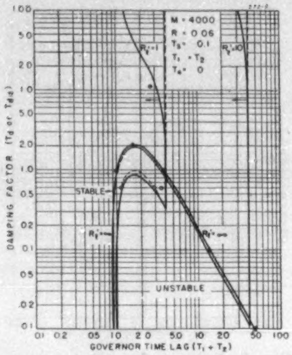


Figure 1. System stability—effects of droop compensation and of the floating type of tie-line control

—— T_d versus T_1+T_2, $T_{d12}=0$, $T_3=\infty$
– – – T_{d12} versus T_1+T_2, $T_d=0$, $T_3=\infty$
△ $r=0.03$, $T_3=5(T_1+T_2)$, $R_t'=\infty$
⊙ $r=0.03$, $T_3=5(T_1+T_2)$, $R_t'=1$

to a prime-mover torque change ΔP, when the controller is actuated by a fixed tie-line power deviation ΔP. The tie-line controller time constant T_4 is a measure of the time lag in the tie-line controller and synchronizing-motor response. Control circuits based on these principles are sometimes referred to as the "floating type" in that the correction is applied at a *rate* proportional to the change in the controlled variable, and the adjustment continues until the variable is restored to its normal value.

A "proportional type" of control is one that exerts a correcting force proportional to the change in the controlled quantity, as would be obtained, for example, with a tie-line controller that actuates a torque motor connected to the governor mechanism. The corresponding controller regulation, R_t, specifies that a tie-line power change ΔP results in a corrective indication equivalent to a prime-mover torque change $\Delta P/R_t$. A time lag T_5 is associated with this control system in the analysis.

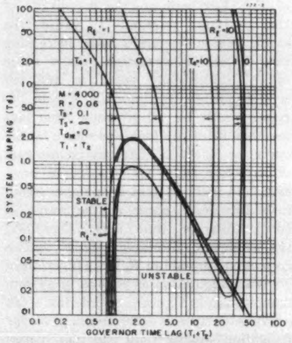


Figure 2. System stability—effect of time lag in the tie-line controller

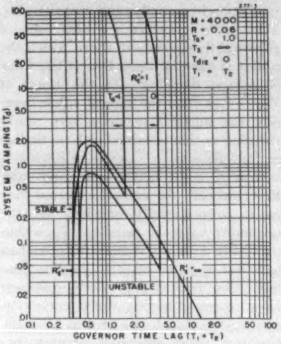


Figure 3. System stability—effects of floating type of tie-line control

Larger tie-line synchronizing power coefficient than that of Figures 1 and 2

Load-Torque Change

The distribution of load increments between two systems is determined at the first instant by the electrical characteristics. In this study the entire load-torque change ΔT has been applied directly to the smaller system; a condition that would be realized if the impedance to the smaller system from the point of load application were negligibly small with respect to the impedance to the larger system. This assumption is justified, since most of the data have been taken with a tie line of small capacity with respect to even the smaller system.

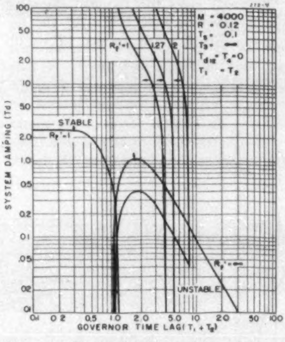


Figure 4. System stability—effects of floating type of tie-line control

Larger governor regulation than that of Figures 1 and 2

Maximum Rates of Response

In order to prevent large tie-line power swings the prime-mover power input to each system must vary with its load. The over-all power input to a group of generating units is limited, however, to a maximum rate depending upon the steam storage or hydraulic capacities of the various units. In some cases this rate may be of the order of ten per cent of the regulated capacity per minute.

Although these practical limitations have an important bearing upon the general problem of tie-line control, the ideal condition of an infinite prime-mover energy source has been assumed for the purpose of evaluating the desirable characteristics of the controller and governing systems.

Discussion of Results

One criterion for determining the optimum governor and tie-line controller characteristics is that the over-all system performance should be as stable as possible. Therefore, the effects of the various parameters upon system frequency stability have been studied. The direct calculations of stability limits are necessarily limited to those cases involving continuously acting (linear) tie-line control equipment.

Another desirable condition is that the tie-line power swings should be reduced to a minimum. The tie-line power changes for given load changes, calculated by means of the differential analyzer at the Moore School of Electrical Engineer-

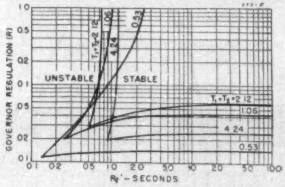


Figure 5. Permissible governor regulation and tie-line controller correcting time—effect of governor time lag

$M=4{,}000$ $T_5=0.1$ $R=0.06$
$T_d=2$ $T_1=T_2$ $T_4=0$
$T_{d12}=0$ $T_3=\infty$

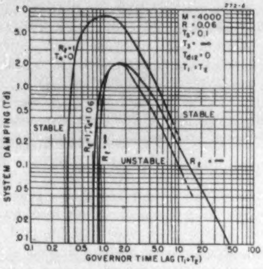


Figure 6. System stability—effect of proportional tie-line control

ing, University of Pennsylvania, are used as a basis for the conclusions regarding this phase of the problem.

STABILITY

Figure 1 shows the required load or tie-line damping as a function of total governor time lag, with various tie-line controllers, when the tie-line capacity is relatively small ($T_s=0.1$). The effect of the tie-line controllers may be seen by comparing the corresponding curves with the one obtained for a system having no tie-line control, $R_t'=\infty$. Also shown with this reference curve, which is similar to those of Figures 3 and 4 of reference 2, are the comparable points for a system having the same transient regulation, $R=0.06$, but a steady-state regulation of $r=0.03$ following the operation of a droop-correction mechanism with a time constant five times that of the governor control system. The stability limits are not appreciably changed provided the time lag T_3 is sufficiently long.

1. "Floating" Control. The effect of a floating type of tie-line controller (Figure 1) with relatively slow correcting rate, $R_t'=10$ seconds, and zero time lag, $T_4=0$, is to introduce a critical value of governor time constant above which the system is unstable regardless of the value of system damping. In the range of T_1+T_2 less than the critical value, however, the unstable region is very nearly the same as that obtained with no tie-line control, $R_t'=\infty$.

With a much faster correcting rate, $R_t'=1$ second, the critical governor time lag is proportionally smaller, but the maximum value of required T_d within the stable region is reduced to less than half that required without tie-line control. The comparable points obtained for a governor having droop correction give a somewhat lower critical governor time lag. This difference appears, because R_t' has been taken as a measure of the equivalent correcting time when the controller indication is amplified only according to the transient governor regulation R, whereas the actual amplification of the adjustments increases as droop correction is applied.

The dashed curves of Figure 1 give the required tie-line damping, T_{d12}, with zero system damping, $T_d=0$, and are compared with the corresponding solid curves of required T_d with $T_{d12}=0$. With both types of damping present the curve of required total damping, T_d+T_{d12}, would lie between the two.

Introduction of time lag, T_4, in the tie-line control system (for example, in the synchronizing motor) results in a lower critical value of T_1+T_2, Figure 2, but otherwise the unstable region is not appreciably changed. Moreover, the percentage decrease of the critical governor time constant is more nearly a function of the ratio T_4/R_t' than of T_4 alone.

Figure 3 gives the required damping versus governor time lag when the tie-line capacity is much greater ($T_s=1.0$).

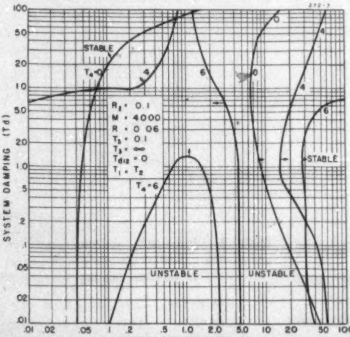


Figure 7. System stability—effect of proportional tie-line control

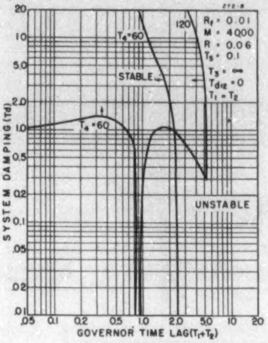


Figure 8. System stability—effect of proportional tie-line control

As shown in reference 2, and also by comparison of Figures 2 and 3, the maximum required T_d is unaffected, but the unstable region occurs in a range of lower values of T_1+T_2, when T_s is increased. The critical governor time lag introduced as a result of tie-line control is practically independent of T_s, and is approximately equal to $4R_t'$ for zero controller time lag and small system damping.

Results calculated with a larger regulation ($R=0.12$) and small tie-line capacity ($T_s=0.1$), Figure 4, are compared with those of Figures 1 and 2 for $R=0.06$. Without tie-line control ($R_t'=\infty$) the maximum required T_d is inversely proportional to the governor regulation, and the comparison of the unstable areas is similar to that of Figure 3, reference 2.

The other curves of Figure 4 indicate that the critical governor time lag is practically independent of R as well as of T_s. Furthermore, the magnitude of the maximum required T_d within the stable region decreases with increasing rate of tie-line correction. At a value of R_t' numerically equal to $MR/2\pi f$ seconds correcting time, the system becomes stable for all governor time lags less than its critical value as shown by the curve for $R_t'=1.27$. At any greater rate of correction ($R_t'=1$ is an example) however, and if the system damping is small, instability may occur in the range of small governor time lags. Therefore, the maximum allowable rate of tie-line correction (minimum R_t') is a function of governor regulation as well as of system damping and of governor time lag. Or, conversely, there is an optimum governor regulation smaller than one that causes instability, because of the above mentioned condition, and greater than one

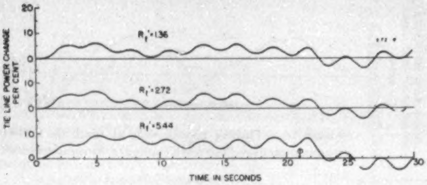


Figure 9. Floating control—magnitudes of tie-line power swings as affected by the rate of correction

$M = 4{,}096 \quad T_s = 0.1 \quad R = 0.0625$
$Td = 2 \quad T_1 = T_2 = 0.53 \quad T_4 = 1.06$
$T_{d12} = 0 \quad T_3 = \infty$
$\Delta T = t/42.4$ for $0 < t < 21.2$
$\Delta T = 0.5$ for $t < 21.2$

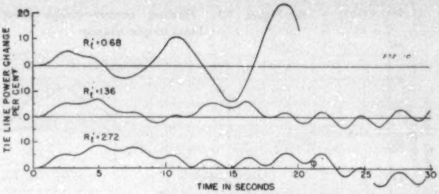


Figure 10. Floating control—magnitudes of tie-line power swings as affected by the rate of correction and by droop compensation

$M = 4{,}096 \quad T_s = 0.1 \quad R = 0.0625$
$T_d = 2 \quad T_1 = T_2 = 0.53 \quad r = 0.03125$
$T_{d12} = 0 \quad T_3 = 5.44 \quad T_4 = 1.06$
$\Delta T = t/42.4$ for $0 < t < 21.2$
$\Delta T = 0.5$ for $t < 21.2$

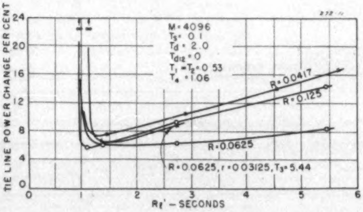
that causes instability, because of the large required T_d under normal conditions. One general rule for determining the maximum allowable rate of tie-line correction is, therefore, that R_l' should be greater than $MR/2\pi f$; that is, the possibility of an unstable region similar to the one for $R_l' = 1$, Figure 4, should be avoided.

If the time lag of the governor is relatively large, instability is approached as the critical governor time lag approaches the actual time constant of the particular governing system. Under these conditions the minimum allowable R_l' is practically independent of the governor regulation, and with zero controller time lag it is approximately equal to four times the total governor time constant.

The above conclusions are substantiated by the curves of Figure 5 showing the permissible governor regulation versus correcting time of the tie-line controller for several different governor time constants and with $T_d = 2$, $T_4 = 0$. The minimum allowable R_l' is in all cases a function of governor time constant, but for large values of governor time constant ($T_1 + T_2$ equal to 2.12 and 4.24 seconds) it is practically independent of governor regulation.

Figure 11. Floating control—tie-line power deviation as affected by the governor regulation and by the rate of tie-line correction

$\Delta T = t/42.4$ for $0 < t < 21.2$
$\Delta T = 0.5$ for $t < 21.2$



2. *"Proportional" Control.* When the proportional type of tie-line controller is used, the steady-state change in tie-line power resulting from a load-torque change ΔT is (equation 1 of the appendix with $p = 0$),

$$\Delta P = \frac{\Delta T}{1 + \dfrac{1}{R_l}} = \frac{R_l \Delta T}{1 + R_l}$$

The effects of a proportional controller with a very broad regulation, $R_l = 1.0$, upon the stability limitations of the system having six per cent governor regulation and small tie-line capacity, $T_s = 0.1$, are shown on Figure 6. With zero controller time lag, $T_s = 0$, the region of instability and the maximum required T_d are both considerably greater than those calculated with no tie-line control. Satisfactory results are obtained, however, by adding a small amount of time lag as demonstrated by the results obtained with $T_s = 1.06$ seconds. This value of controller regulation allows wide swings of tie-line power in that the load changes are equally divided between the two systems.

A controller regulation of $R_l = 0.1$ is more nearly desirable in that the steady-state tie-line power change is limited to approximately nine per cent of the load change. With zero controller time lag, Figure 7, the required system damping is excessively large over the entire practical range of governor time lags. As the controller time lag is increased, the stable region in the range of small governor time lags is progressively changed until it acquires a form similar to the curves calculated with the R_l' type of controller. This effect is shown by the curves for T_5 equal to four and six seconds, Figure 7.

The response of a proportional controller with a time lag T_5 is given in the appendix as,

$$\frac{D}{R} = \frac{\Delta P}{pR_l T_5 + R_l}$$

as compared to the response of the floating-type controller without time lag,

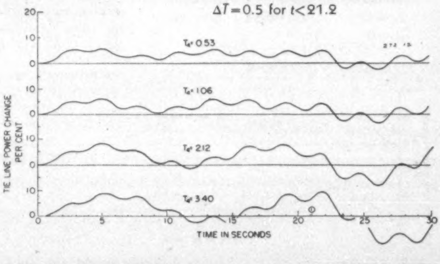
$$\frac{D}{R} = \frac{\Delta P}{pR_l'}$$

That is, for small R_l and large T_5 the product of the two is the governing factor as regards controller response, and this factor corresponds to the R_l' type of regulation.

Figure 8 is a good illustration in that the results for $R_l = 0.01$ and T_5 equal to 60 and 120 seconds are similar to those of Figure 4 for the other type of control.

Figure 12. Effect of time lag in the floating type of tie-line control

$M = 4{,}096 \quad T_s = 0.1 \quad R = 0.0625$
$T_d = 2 \quad T_1 = T_2 = 0.53 \quad R_l' = 1.36$
$T_{d12} = 0 \quad T_3 = \infty$
$\Delta T = t/42.4$ for $0 < t < 21.2$
$\Delta T = 0.5$ for $t < 21.2$



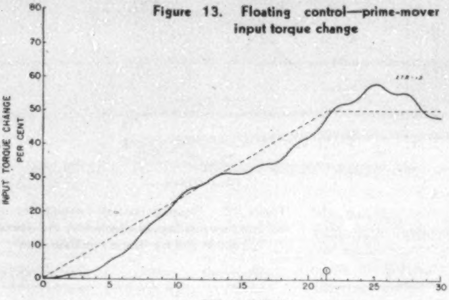


Figure 13. Floating control—prime-mover input torque change

——— Actual - - - - - Ideal

$M=4{,}096$ $T_s=0.1$ $R=0.0625$

$T_d=2$ $T_1=T_2=0.53$ $R_t'=1.36$

$T_{d12}=0$ $T_3=\infty$ $T_4=2.12$

$\Delta T=t/42.4$ for $0<t<21.2$

$\Delta T=0.5$ for $t<21.2$

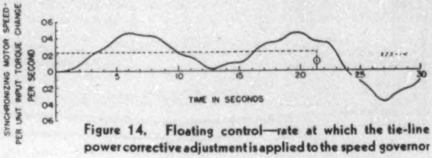


Figure 14. Floating control—rate at which the tie-line power corrective adjustment is applied to the speed governor

——— Actual - - - - - Ideal

$M=4{,}096$ $T_s=0.1$ $R=0.0625$

$T_d=2$ $T_1=T_2=0.53$ $R_t'=1.36$

$T_{d12}=0$ $T_3=\infty$ $T_4=2.12$

$\Delta T=t/42.4$ for $0<t<21.2$

$\Delta T=0.5$ for $t<21.2$

One disadvantage of proportional control is that it may require a more complicated mechanism for use with most governors. Furthermore, provision for obtaining a wide range of time lags may be required depending upon the required range of tie-line regulation.

TIE-LINE POWER SWINGS

The differential analyzer results were recorded in the form of curves of tie-line power, frequency, prime-mover torque, and controller response versus time during periods of large variable loads. A severe type of variable load, such as that furnished by a strip mill, is one in which a total variation as great as 50 per cent of the local generation capacity occurs in cycles of the order of one-minute periods. As an approximation to this condition, one set of runs was taken with a smooth load cycle in which ΔT increases at a uniform rate from zero to 0.5 per unit in 21.2 seconds, remains constant for another 10.6 seconds, decreases at a uniform rate from 0.5 to zero in another 21.2 seconds, and so on. Because the decreasing ΔT was applied from a transient condition, the magnitudes of the power swings during this period were occasionally greater than those of the increasing ΔT period. For the purposes of comparing the effects of various control characteristics on a common basis, therefore, the results presented in this paper give only the magnitudes of the swings up to the time at which the decreasing ΔT was applied. Another set of runs was taken with continuously increasing ΔT applied at a fixed rate.

1. *Floating Control.* Governor- and tie-line-controller time lags each equal to 1.06 seconds, system damping of $T_d=2.0$, and small tie-line capacity ($T_s=0.1$), were assumed for one set of conditions. The curves of tie-line power versus time, Figure 9, show the relative effects of three different rates of controller response for a system having a governor regulation of $R=0.0625$. The load torque ΔT was increased uniformly from zero to 0.5 per unit in 21.2 seconds (point 1 on the time scale) and was then held constant for the remainder of the time interval shown. Of the three values of R_t' used, the smallest is most effective in limiting the power deviation, but this does not mean that R_t' should be decreased further. Although the system inertia and regulation are not exactly the same as those used in the stability calculations, the minimum allowable R_t' for stable operation may be estimated by considering the location of this operating point ($T_1+T_3=1.06$ and $T_d=2.0$) within the stable regions of Figures 1 and 2.

The tie-line power swings shown in Figure 10 are for a system with the same transient regulation ($R=0.0625$), but with a steady-state regulation of $r=0.03125$ obtained by means of a droop-correction mechanism having a time lag $T_3=5.44$ seconds. The run taken with $R_t'=0.68$ second demonstrates the instability resulting from too rapid tie-line power correction. As stated previously, R_t' is a measure of the equivalent correcting time when the amplification is inversely equal to the transient regulation R.

The maximum power swings of these runs, as well as those taken with governor regulations of 0.125 and 0.0417, are compared on Figure 11. The optimum controller response is nearly equal to that corresponding to the minimum allowable R_t', and under the assumed conditions the optimum governor regulation is of the order of six per cent. The tie-line power swings are not improved by adding droop correction.

Using the approximate optimum values indicated above, $R_t'=1.36$ seconds and $R=0.0625$, the controller time lag was varied. As shown by the curves of Figure 12, T_4 should be made as small as

Figure 15. Floating control—effect of governor regulation on the tie-line power deviation for a system having larger damping factor ($T_d=10$)

$M=4{,}096$ $T_s=0.1$ $R_t'=1.36$

$T_d=10$ $T_1=T_2=0.53$ $T_4=1.06$

$T_{d12}=0$ $T_3=\infty$

$\Delta T=t/42.4$ for $0<t<21.2$

$\Delta T=0.5$ for $t<21.2$

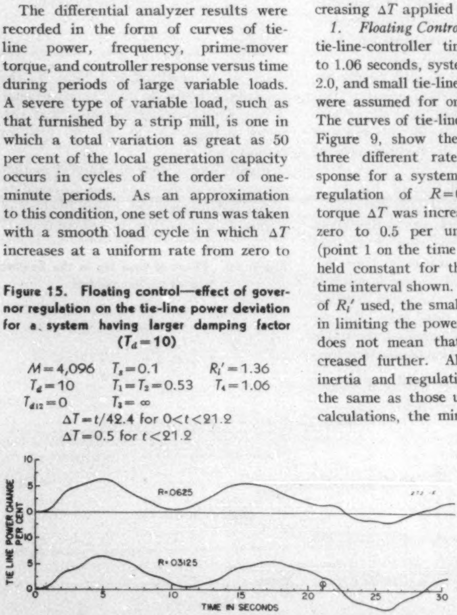
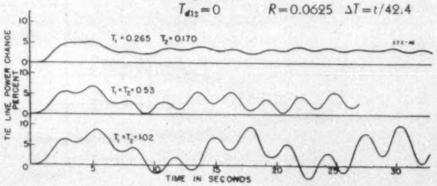


Figure 16. Floating control—effect of governor time lag

$M=4{,}096$ $T_s=0.1$ $R_t'=1.36$

$T_d=2$ $T_3=\infty$ $T_4=1.06$

$T_{d12}=0$ $R=0.0625$ $\Delta T=t/42.4$



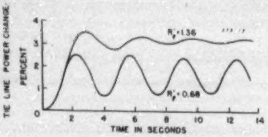


Figure 17. Floating control—effect of the rate of tie-line correction when there are no governor or controller time lags

$M=4{,}096$ $T_g=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0$ $T_4=0$
$T_{d12}=0$ $T_3=\infty$ $\Delta T=t/42.4$

possible. This conclusion is compatible with the results of the stability calculations.

Figures 13 and 14, curves of prime-mover input torque change versus time and of synchronizing motor speed versus time, respectively, are for the same conditions as the power swing of Figure 12 with $T_4=2.12$. All of these quantities depend, of course, upon the rate of load-torque change. In actual practice a maximum correcting rate may be imposed because of boiler limitations, and so forth, previously discussed. The dashed curves of Figures 13 and 14 may be interpreted as showing the expected response of an idealized control system having no controller or governor time lags and zero correcting time.

Natural frequency components of tie-line power swings are effectively reduced when the system damping is increased to $T_d=10$, Figure 15. The curve for $R=0.0625$ is compared with that of Figure 9 for $T_d=2$ and $R_l'=1.36$. It is observed that the maximum power swings are approximately equal. Although the larger system damping factor allows the use of a smaller governor regulation ($R=0.03125$), no appreciable over-all improvement is realized.

All of the remaining differential-analyzer runs were taken with a continually increasing load torque. For example, the effects of variations of the governor time constant are given on Figure 16 with ΔT applied at the rate of full torque change in 42.4 seconds. The system damping is $T_d=2$, and the governor regulation is $R=0.0625$. The rate of tie-line correction ($R_l'=1.36$) is the approximate optimum value (Figure 11) when the governor and controller time constants are each equal to 1.06 seconds. The curves are of interest in that an increase of governor time constant results in greater tie-line power swings. At a critical governor time lag of 2.04 seconds, the system becomes unstable.

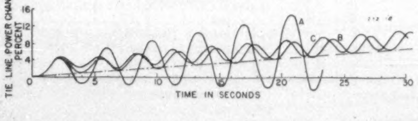


Figure 18. Proportional control—tie-line power deviation as affected by the controller time lag

Curve A—$T_5=5.44$
Curve B—$T_5=8.15$
Curve C—$T_5=10.88$

$M=4{,}096$ $T_g=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l=0.10$
$T_{d12}=0$ $T_3=\infty$ $\Delta T=t/42.4$

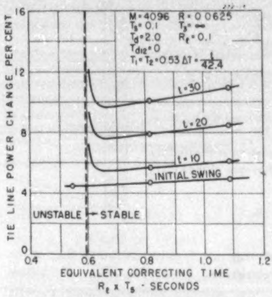


Figure 19. Summary of Figure 18 in terms of the equivalent controller correcting time ($R_l \times T_5$)

The optimum rate of tie-line correction depends upon the time constants of the control system. In the limiting case of zero controller and governor time lags, Figure 17, the maximum tie-line power change is less with $R_l'=0.68$ than with $R_l'=1.36$, although the natural frequency oscillations are not damped out as rapidly. The smaller R_l' is the one equal to $MR/2\pi f$ seconds correcting time (see section on stability).

2. *Proportional Control.* The curves of Figure 18 include the effects of a proportional tie-line controller having a regulation of $R_l=0.1$ and various amounts of time lag, T_5. The theoretical tie-line regulation is shown by the dashed line. Because of this drift of tie-line power the magnitudes of the initial swing and those at the end of 10, 20, and 30 seconds are summarized on Figure 19 as a function of the equivalent correcting time $R\times T_5$. The optimum correcting time is, as before, not much greater than the minimum value required to insure stable operation.

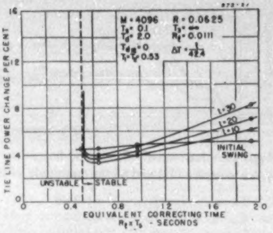


Figure 21. Summary of Figure 20 in terms of the equivalent controller correcting time ($R_l \times T_5$)

Figures 20 and 21 give similar information for a controller regulation of $R_l=0.0111$. The drift of tie-line power is very much less in this case, and with optimum controller time lag the power deviation at the end of 30 seconds is even less than that of the initial swing. The required controller time lag is much larger, however, being approximately inversely proportional to R_l. The system constants and the rate of load-torque change are the same as those used during the runs of Figure 16 for the floating type of control, and the results are, therefore, directly comparable. With the optimum proportional controller, Figure 21, the maximum tie-line power change is 4.6 per cent as compared to 6.8 per cent for the floating controller, Figure 16, with $R_l'=1.36$ and $T_4=1.06$. This difference would be even smaller if the optimum ($T_4=0$) floating-type controller had been used (see Figure 12 for effect of T_4).

3. *Intermittent Floating Control.* In applying the intermittent adjustments, the tie-line power deviation was read at intervals of 2.12 seconds. Following each reading a corrective indication was ap-

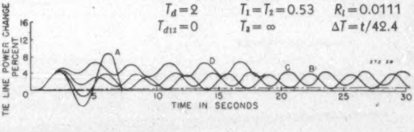


Figure 20. Proportional control—tie-line power deviation as affected by the controller time lag

Curve A—$T_5=$ 43 seconds
Curve B—$T_5=$ 58 seconds
Curve C—$T_5=$ 87 seconds
Curve D—$T_5=$174 seconds

$M=4{,}096$ $T_g=0.1$ $R=0.0625$
$T_d=2$ $T_1=T_2=0.53$ $R_l=0.0111$
$T_{d12}=0$ $T_3=\infty$ $\Delta T=t/42.4$

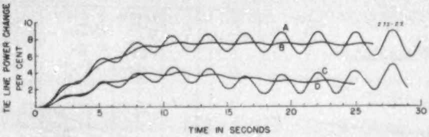


Figure 22. Intermittent control—tie-line power deviation as affected by system damping (T_d) and by the magnitude of load-torque change (ΔT)

Curve A—$T_d=2$, $\Delta T=t/84.8$
Curve B—$T_d=10$, $\Delta T=t/84.8$
Curve C—$T_d=2$, $\Delta T=t/169.6$
Curve D—$T_d=10$, $\Delta T=t/169.6$
$M=4{,}096$ $T_1=T_2=0.53$ $\Delta P_m=0.064$
$T_{d12}=0$ $T_3=\infty$ $k_1=1/42.4$
$T_s=0.1$ $T_4=1.06$ $R_l'(\text{eq.})=5.44$
$R=0.0625$

plied for a length of time proportional to the deviation ΔP at the beginning of the interval, and at a constant rate equivalent to k_1 per unit prime-mover torque change per second. The maximum duration of the impulse was limited to 1.06 seconds (half the period) for a power change equal to or greater than ΔP_m. For this controller there is an equivalent R_l' as given by (see appendix):

$$R_l'(\text{eq.})=\frac{2\,\Delta P_m}{k_1}$$

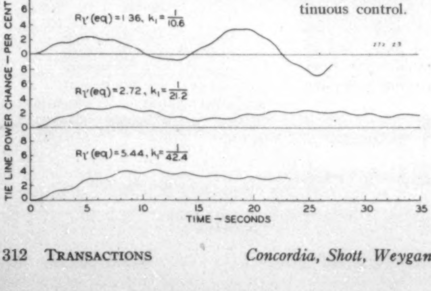
The tie-line power swings of Figure 22 were obtained with a controller having $T_4=1.06$, $\Delta P_m=0.064$ per unit, $k_1=1/42.4$, and $R_l'(\text{eq.})=5.44$ seconds. Curve A is compared with the curve $T_1=T_2=0.53$, Figure 16, for the continuous controller having the constants $R_l'=1.36$ and $T_4=1.06$. It is observed that the intermittent controller causes system instability, although its equivalent correcting rate is only one-fourth that of the continuous controller. For a system having a larger damping factor $T_d=10$, curve B of Figure 22, the results are satisfactory. Curves C and D give similar results with the load torque applied at one-half the rate. The magnitudes of the tie-line power swings are not proportional to the rate of load-torque change, because of the maximum correction rate imposed by this controller.

With $\Delta P_m=0.064$ and a large system damping, $T_d=10$, the effects of variations of the constant k_1 are shown on Figure 23. Likewise, Figure 24 gives the effects of variations of ΔP_m with $k_1=1/42.4$. The maximum power swings are summarized on Figure 25 as a function of the equivalent correcting time, R_l' (eq.). The optimum correcting time is of the order of 2.72 seconds under these conditions, that is, it is approximately twice that of the continuous controller. Furthermore, the effectiveness of a controller having the constants $\Delta P_m=0.032$ and $k_1=1/42.4$ is about the same as one with $\Delta P_m=0.064$ and $k_1=1/21.2$ since their equivalent correcting times are both equal to 2.72 seconds.

The power swings of Figures 23 and 24 for $R_l'(\text{eq.})=2.72$ are compared with that of Figure 15, $R=0.0625$, for the optimum continuous controller. After allowance has been made for the fact that the rates of applied load-torque change are different by a factor of four to one in the two cases, it appears that the tie-line power deviation obtained with the intermittent control is about 75 per cent greater than that obtained with the continuous control.

Figure 23. Intermittent control—effect of varying the equivalent controller correcting time by changing k, with ΔP_m constant

$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=10$ $T_1=T_2=0.53$ $\Delta P_m=0.064$
$T_{d12}=0$ $T_3=\infty$ $T_4=1.06$
$\Delta T=t/169.6$



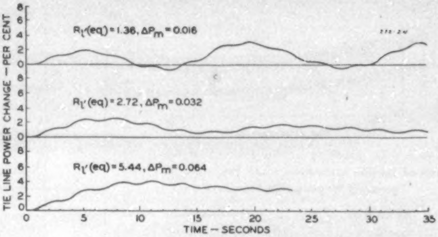


Figure 24. Intermittent control—effect of varying the equivalent controller correcting time by changing ΔP_m with k, constant

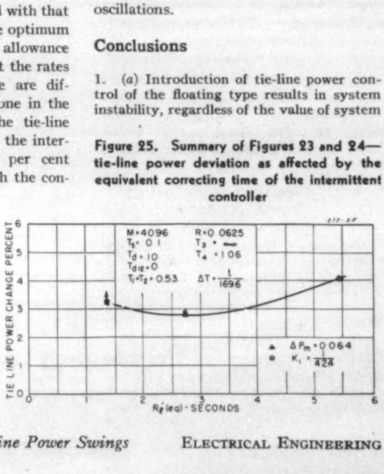
$M=4{,}096$ $T_s=0.1$ $R=0.0625$
$T_d=10$ $T_1=T_2=0.53$ $k_1=1/42.4$
$T_{d12}=0$ $T_3=\infty$ $T_4=1.06$
$\Delta T=t/169.6$

4. *Summary.* The control systems have been compared with respect to their effects upon the prime-mover governor response and tie-line power deviation, when the system is subjected to smooth variable loads. The recorded data also give a measure of the power deviation for the particular load cycles and system constants considered. It is to be expected, however, that the magnitude of the swings depends upon the period as well as the magnitude of the variable load, particularly if the period is very short. The shape of the load cycle may also be of considerable importance. Instantaneous load changes initiate natural-frequency power oscillations. As an example of the extreme condition, successive build up of tie-line power swings can occur if instantaneous load changes are applied at regular time intervals equal to the period of the natural-frequency oscillations.

Conclusions

1. (*a*) Introduction of tie-line power control of the floating type results in system instability, regardless of the value of system

Figure 25. Summary of Figures 23 and 24—tie-line power deviation as affected by the equivalent correcting time of the intermittent controller



damping, if the controller correcting time (R_l') is less than about one fourth of the governor time lag (Figures 1–4). The inherent instability ($T_1+T_2>4R_l'$) is not appreciably affected by variations of the tie-line synchronizing power coefficient T_s or the governor regulation R, but it does appear at an even smaller value of governor time lag if there is time lag (T_4) in the tie-line controller.

(*b*) Instability may also occur in the range of small governor time lags as a result of a large rate of tie-line correction (small R_l'). The appearance of this unstable region is a function of governor regulation, that is, it appears when R_l' is less than $MR/2\pi f$ seconds correcting time (Figure 4). In general, therefore, the minimum allowable R_l', as regards system stability, is a function of the governor time lag and is also a function of governor regulation if the governor time lag is small (Figure 5). Accordingly, there is an optimum governor regulation dependent upon the governor time lag, the system damping factor, and the rate of tie-line correction.

(*c*) With respect to the maximum effectiveness of the floating control in reducing the magnitude of tie-line power swings, the optimum correcting time is not much greater than the minimum allowable value required to insure stable operation (Figure 11). The controller time lag should be reduced to a minimum (Figure 12).

2. The use of a proportional tie-line controller having small regulation and zero time lag will cause instability unless the system damping is very large. Stability is obtained by introducing controller time lag T_4. With small regulation (R_l) and large time lag (T_4), the controller response is primarily a function of the factor $R_l \times T_4$ and is not much different from that of floating control since the above factor is similar to the R_l' type of regulation. On the basis of simplicity, the floating control may be preferable for use with most governors.

3. The impulses of an intermittent control having about two second period and a fast rate of response may result in system instability, if the operation is near the limit of stability without tie-line control. Otherwise, satisfactory results can be obtained, and the optimum equivalent correcting time [R_l'(eq.)] is of the order of two times the optimum R_l' of the continuous controller.

4. The optimum rates of response of tie-line controllers for maximum effectiveness are very much higher than those ordinarily used in power-system control.

5. The optimum continuously acting controllers limit the tie-line power deviation to approximately 15 per cent of the total load variation if the tie-line synchronizing power coefficient is small ($T_s=0.1$), and if the load variation occurs in smooth cycles of about one minute period. With an intermittent controller of the type considered, the tie-line power deviation may be limited to approximately 25 per cent of the load variation.

6. Previous considerations[1,2] have indicated that a turbine-governor regulation of the order of six per cent may be desirable. Although the optimum value depends to some extent upon the governor time lag, system damping, rate of tie-line correction, and so on, the present analysis has shown that six per cent regulation is satisfactory (Figures 11 and 15) when automatic tie-line control is also used. A smaller steady-state regulation is obtained without appreciable change in the degree of system stability by adding a droop-compensation mechanism having a relatively large time constant, but the tie-line power swings are not reduced in magnitude (Figures 1 and 11).

Appendix. Equations and Definitions of Terms

The approximate torque equations for two interconnected power systems were given in reference 2. If it is assumed that one system is infinitely large the equation for the smaller system is,

$$[Mp^2+(T_d+T_{d12})p+T_s]\delta+T_g=\Delta T \tag{1}$$

where,

$M=4\pi fH$ is the effective inertia of the system and is equal to the time in electrical radians required to reach full rated speed from standstill with rated torque applied to the rotor.

T_d is the system damping factor equal to the slope of the net prime mover plus load torque versus speed curve with fixed prime-mover input valve.

T_{d12} is the tie-line damping-torque coefficient, or the change in torque per unit change in relative system speeds.

T_s is the synchronizing-torque coefficient, or the change in torque per electrical radian change in relative system angles.

T_g equals the change in prime-mover torque resulting from the movement of the input valves.

ΔT is that part of the load-torque change applied to this system.

All of the above quantities are in per unit on a kilovolt-ampere base equal to the system capacity.

δ is the effective system angle in radians with respect to the normal-frequency reference; or in this case with respect to the angle of the infinite system.

$p\delta=d\delta/dt$ is a measure of speed or frequency change.

t equals time in electrical radians; 377 radians = one second for a 60-cycle system.

The governor control mechanism has been represented as a two-stage amplifier having time lags T_1 and T_2.[2] The hydraulic-relay system of an actual governor might be considered as the first stage and the steam-storage capacity as the second. Speed response indications and tie-line control adjustments at the governor head

$$A=p\delta+D \tag{2}$$

are transmitted to the first stage through a set of levers. The effective leverage of this connection may vary because of the action of a droop-compensation mechanism, and if the variation is at an exponential rate defined by the time constant, T_3, the motion transmitted to the first stage is

$$F=\frac{(T_3p+1)}{\left(T_3p+\frac{r}{R}\right)}A \tag{3}$$

At the first instant ($p=\infty$) following a change at the governor head, the indications are directly impressed upon the governor control system, but in the steady-state condition following droop correction ($p=0$) they are amplified by a factor equal to the ratio (R/r) of the transient to steady-state governor regulations.

In passing through the governor control system this correcting force is modified by the time lags T_1 and T_2 and is amplified by a factor inversely equal to the transient regulation of the governor. Thus the outputs of the first and second stages are, respectively:

$$\left.\begin{aligned} Q&=\frac{1}{(T_1p+1)}F \\ T_g&=\frac{1}{R(T_2p+1)}Q \end{aligned}\right\} \tag{4}$$

The complete expression for the change in prime-mover torque is, therefore,

$$T_g=\frac{1}{(T_2p+1)(T_1p+1)}\frac{(T_3p+1)}{\left(T_3p+\frac{r}{R}\right)}\times \left(\frac{p\delta}{R}+\frac{D}{R}\right) \tag{5}$$

For a governor without a droop-correction mechanism, $T_3=\infty$ and R is the regulation. Otherwise, R and r are the transient and the steady-state governor regulations, respectively. T_3 is the time constant of the droop-compensation mechanism.

As used in equation 5, the terms $p\delta/R$ and D/R are a measure of the turbine speed response and tie-line control adjustments, respectively, expressed as the equivalent prime-mover torque changes that would result if there were no governor time lag ($T_1=T_2=0$) and if the adjustments were amplified only according to the transient governor regulation $R(T_3=\infty)$. The characteristics of the tie-line control system are interpreted in this paper by evaluating the equivalent response D/R in this manner. For instance, the response of one type of controller has been expressed as

$$\frac{D}{R}=\frac{\Delta P}{R_l'p(T_4p+1)} \tag{6}$$

where

$\Delta P=(T_s+pT_{d12})\delta$ is the tie-line power change, and

T_4 is the time lag of the controller.

With zero controller time lag ($T_4=0$) the first derivative of this equation

$$p\left(\frac{D}{R}\right)=\frac{\Delta P}{R_l'} \tag{7}$$

shows that the equivalent correction is applied at a rate inversely proportional to the constant R_l'.

A D-C Telemeter or D-C Selsyn for Aircraft

R. G. JEWELL
ASSOCIATE AIEE

H. T. FAUS
MEMBER AIEE

Synopsis: This paper describes a d-c telemeter which is particularly adapted for transmitting to the instrument board indications of pressure, temperature, liquid level and of the position of the various controlling members of the airplane. The description comprises the principle, variations, characteristics, and application of this telemetering system. Although the applications described are primarily in connection with aircraft, the versatile nature of this device makes it suitable for many other applications.

The large number of indications which must be transmitted accurately to the instrument board of an airplane has created a demand for a telemetering system of small size and light weight. For many years the a-c Selsyn telemetering system has given excellent service in marine applications. Variations of this system are used successfully on aircraft. A search for a simpler and lighter system which will operate on direct current has led to the development of the d-c Selsyn telemetering system.

Principle

A SIMPLIFIED concept of the operation of the d-c Selsyn may be obtained by referring to Figure 1 which shows a coil wound on a toroidal iron core. This is a continuous single layer winding with two brushes supplying direct current at diametrically opposite points. It is evident that a magnetic pole will be set up in the core under each of the brushes, and that the magnetic field inside the core will be as shown. This field will follow the brushes as they are rotated. If a polarized permanent magnet rotor is placed inside the core, it will revolve so as to keep its direction of magnetization in line with the brushes.

The next step in the development is shown in Figure 2. The circular rheostat transmitter is tapped at intervals of 120 degrees and these taps are connected to similarly spaced taps on the receiver. This gives a result similar to that obtained in Figure 1 so that, within the accuracy limits to be defined later, the permanent magnet rotor will turn to a position having the same relation to the taps on the receiver winding as the brushes have to the corresponding taps on the transmitter winding. The windings shown are similar to delta connected three-phase windings. Connections similar to other polyphase windings may also be used and the windings may be concentrated in coils rather than being uniformly distributed on the core.

The use of a permanent magnet of high coercive force in combination with a copper damping shell fixed in the air gap between the rotor and the stator gives effective damping combined with high torque. Damping by a permanent magnet used in this manner involves no power loss as would be the case if an alternating field were used. The torque is a function of the product of the rotor and stator fluxes. Since the rotor field is of much higher magnetomotive force than the stator field, and since its excitation involves no electrical losses, it is evident that the inherent torque producing effectiveness is high. The high ratio of torque to weight obtainable with this system is one of its outstanding advantages. This makes it possible to use a comparatively light moving element which requires neither ball bearings nor jewel bearings, since the frictional errors are inappreciable with steel and bronze bearings. Since these bear-

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R. G. Jewell and H. T. Faus are development engineers in the West Lynn Works of General Electric Company, Lynn, Mass.

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For another type of controller response

$$\frac{D}{R}=\frac{\Delta P}{R_l(T_3p+1)} \quad (8)$$

the equivalent correction is directly proportional to the tie-line power change, but it is delayed by the time lag T_3.

The effects of an intermittent controller are introduced as follows. The tie-line power change, ΔP, is read at regular time intervals. Within the period Δt following each reading a corrective indication is applied at a constant rate k_1 and for a length of time proportional to ΔP. This rate of correction is again expressed as an equivalent prime-mover torque change per unit of time. If the correction is applied through the synchronizing motor then k_1 is also a measure of the synchronizing-motor speed.

It is assumed that the impulse starts after an elapsed time t_1 dependent upon the value of ΔP at the beginning of the interval, and ends after a fixed time t_e. If the reading of tie-line power deviation is equal to or greater than ΔP_m, the full correction is applied, that is, t_1 is equal to some fixed time t_m. Then for any smaller power deviation ΔP the impulse is applied at the time t_1 given by

$$\frac{t_e-t_1}{t_e-t_m}=\frac{\Delta P}{\Delta P_m} \quad (9)$$

The correcting adjustment applied at the governor head is of the form

$$\frac{D}{R}=\frac{kt}{(T_4p+1)} \quad (10)$$

where (with t_0 = time at beginning of interval Δt, and $t_e<t_0+\Delta t$)

$k=0$ for $t_1>t>t_0$
$k=k_1$ for $t_e>t>t_1$
T_4 = time lag of the controller

At a fixed power deviation equal to ΔP_m, and neglecting the controller time lag ($T_4=0$), there is an average correcting rate over the entire interval equal to

$$p\left(\frac{D}{R}\right)=\frac{k_1(t_e-t_m)}{\Delta t} \quad (11)$$

This average rate can be defined by an equivalent R_l' similar to that of equation 7:

$$p\left(\frac{D}{R}\right)=\frac{\Delta P_m}{R_l'(\text{eq.})} \quad (12)$$

or from the above two equations:

$$R_l'(\text{eq.})=\frac{\Delta P_m\,\Delta t}{k_1(t_e-t_m)} \quad (13)$$

The effects of variations of the constants k_1 and ΔP_m were obtained from the differential analyzer solutions. The following time intervals were used:

$\Delta t = 2.12$ seconds
$t_m = 0.265$ second
$t_e = 1.325$ seconds

All time constants and the factors R_l' and $1/k_1$ are given in units of electrical radians in the above equations but in other parts of the paper they are given in seconds for a 60-cycle system. Otherwise the notation is consistent throughout.

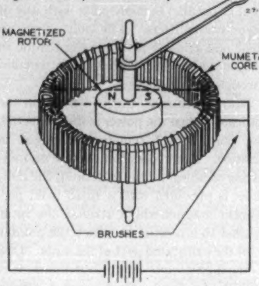
References

1. Prime-Mover Speed Governors for Interconnected Systems, R. J. Caughey and J. B. McClure. AIEE Transactions, volume 60, 1941, April section, pages 147–51; discussions: pages 728–31.

2. Effect of Prime-Mover Speed-Governor Characteristics on Power-System Frequency Variations and Tie-Line Power Swings, C. Concordia, S. B. Crary, and E. E. Parker. AIEE Transactions, volume 60, 1941, pages 559–67.

3. Supplementary Control of Prime-Mover Speed Governors, S. B. Crary and J. B. McClure. AIEE Transactions, volume 61, 1942, April section, pages 209–14.

Figure 1. Simplified d-c Selsyn


27-1
MAGNETIZED ROTOR
MUMETAL CORE
N
S
BRUSHES

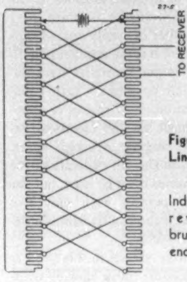

27-5
TO RECEIVER

Figure 5 (left). Linear transmitter for d-c Selsyn

Indicator makes three revolutions as brushes move from end to end of transmitter

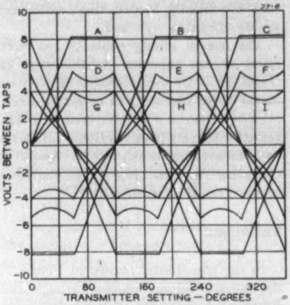


Figure 8. Curves showing voltage variations at taps on the transmitter with various loads connected to the transmitter and 12 volts applied

A—Taps 1–2, no load
B—Taps 2–3, no load
C—Taps 3–1, no load
D—Taps 1–2, one indicator
E—Taps 2–3, one indicator
F—Taps 3–1, one indicator
G—Taps 1–2, two indicators
H—Taps 2–3, two indicators
I—Taps 3–1, two indicators

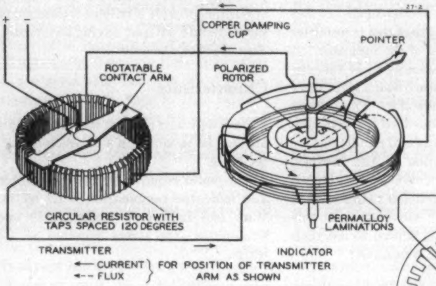

27-2
+
−
COPPER DAMPING CUP
POINTER
ROTATABLE CONTACT ARM
POLARIZED ROTOR
CIRCULAR RESISTOR WITH TAPS SPACED 120 DEGREES
PERMALLOY LAMINATIONS
TRANSMITTER
INDICATOR
← CURRENT
←-- FLUX
FOR POSITION OF TRANSMITTER ARM AS SHOWN

Figure 2 (left). D-c Selsyn telemetering system

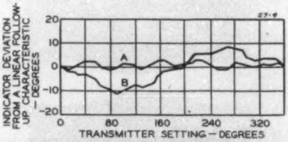


Figure 9. Deviation of d-c Selsyn from a linear follow-up characteristic

A—Instrument without power-failure indicator

B—Instrument with power-failure indicator

Either instrument will repeat its calibration curve within plus or minus 0.1 degree

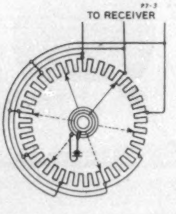

27-3
TO RECEIVER

Figure 3 (left). D-c Selsyn transmitter

Indicator makes three revolutions per revolution of transmitter

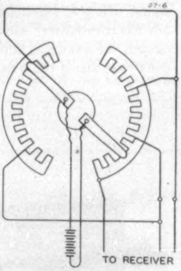
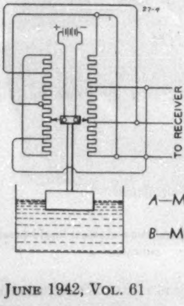

27-6
TO RECEIVER

Figure 6 (above). D-c Selsyn transmitter

Indicator makes one revolution as brushes traverse entire length of respective resistors

Figure 4 (below). Linear transmitter for d-c Selsyn

Indicator makes one revolution as brushes move from end to end of transmitter


27-4
+
−
TO RECEIVER

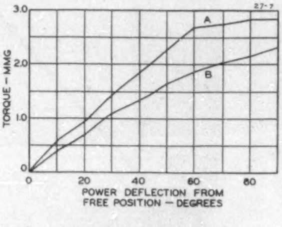


Figure 7 (above). Torque characteristics of d-c Selsyn

A—Maximum-torque condition with transmitter set with one brush on a tap

B—Minimum-torque condition with both brushes set 30 degrees from a tap

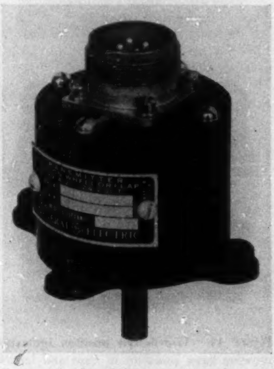

TRANSMITTER
GENERAL ELECTRIC

Figure 10. D-c Selsyn position transmitter

ings are lightly loaded they withstand severe vibration very well.

The use of sliding contacts has been cited as an objection to this type of telemeter. By using brushes of precious metal alloys operating at carefully determined pressures, a reliable contact is obtained with very little friction. Life tests have shown these brushes to be capable of operating for over 40,000,000 complete revolutions of the transmitter.

Variations

In the transmitter shown in Figure 2, the output voltage passes through 360 electrical degrees for each revolution so that the ratio of angular motions of the transmitter and indicator is unity. By making the number of evenly spaced taps equal to $3n$, where n is the number of indicator revolutions per transmitter revolution, it is possible to obtain a ratio equal to any whole number. Brushes of opposite polarity must be 180 electrical degrees ($360/(2n)$ angular degrees) apart. In order to utilize the full capacity of the winding, $2n$ brushes must be used, but the transmitter is operable with only two brushes. Figure 3 shows a transmitter designed for a ratio of 3.

The transmitter shown in Figure 4 is designed to transmit linear motion without converting it to circular motion by mechanical means. Motion of the brushes for the complete length of this transmitter gives one revolution of the indicator. A multi-revolution modification of this circuit is shown in Figure 5 which gives three revolutions of the indicator. This scheme can obviously be modified to give any number of revolutions of the indicator for a given linear motion of the brushes. Figure 6 is a modification of this circuit which will give fractional ratios with a circular transmitter. Obviously with this circuit, the motion is limited to less than one revolution of the transmitter.

Two or more indicators may be operated in parallel in connection with any of these transmitters. The only effect on their operation is to lower the torque in proportion to the reduction in current through each indicator.

Since there is no torque acting on the indicator when the power is disconnected, the pointer will remain unchanged in position. For applications where an indication of power failure is desired, the indicator is provided with a small fixed permanent magnet which attracts the rotor magnet to a position at which the pointer is off the calibrated part of the scale. Due to the high torque of the instrument, it is possible to use a fixed magnet strong enough to provide a reliable indication of power failure without having any significant effect on the accuracy.

Characteristics

Each indicator element weighs 25 grams and can be mounted in a $1^{1}/_{4}$-inch-diameter circle.

The power required by the transmitter and indicator combination is 1.8 watts. With two indicators connected to one transmitter, the power required is 2.2 watts.

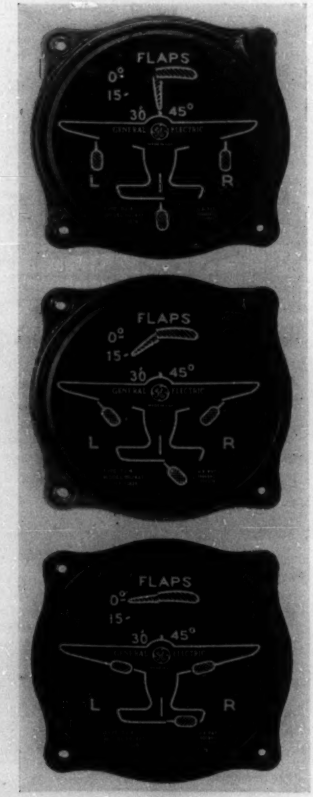


Figure 11. D-c Selsyn position indicators, showing three positions of flaps and landing gear

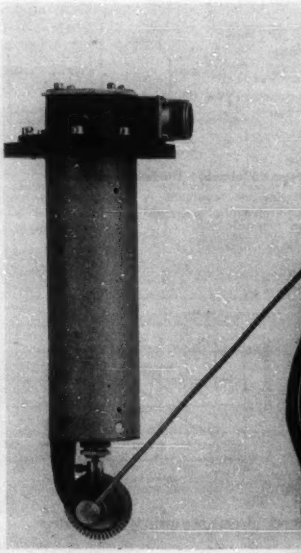


Figure 12. Typical liquid-level transmitter

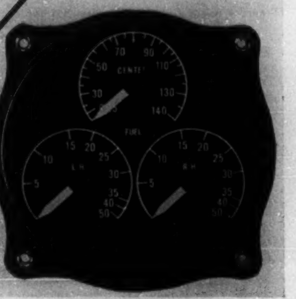


Figure 13. Typical three-element liquid-level indicator

Varying the voltage of the supply does not affect the indication provided no power failure indicator is used. With the power failure indicator a variation of ten per cent in the voltage will cause a maximum error of one degree.

The torque obtained is shown by the curves in Figure 7. The slope of the torque curve at zero is important since a steep slope at this point gives less frictional error. The torque curves show that the steepest part is at zero.

Temperature effect on indications is negligible because of the symmetry of the circuit.

The wave form of the voltage between taps in the transmitter is shown in Figure 8. At no load the form is triangular with the tops cut off flat. With load, the sides and tops of the form bend inward. The change in the form is of such nature that at any given point the ratio of voltage between one pair of taps to that between another pair of taps does not change with load so that an indicator connected to the transmitter will not change its calibration when another indicator is placed in parallel with it.

The peculiar shape of the voltage wave does prevent the indicator from following the transmitter linearly. This inherent deviation has been determined to have a maximum peak of 1.3 degrees. The peaks occur at six points above the axis, and six points below the axis. Figure 9 shows a typical distribution curve *A* which consists of the 1.3 degree deviation and additional deviation due to manufacturing tolerances. Curve *B* of the same figure shows how the distribution curve is changed by the power failure indicator.

Applications

One of the first applications of the d-c Selsyn was for the remote indication of the positions of the landing gear and wing flaps on an airplane. For this purpose four elements were mounted in a single case, one element being used for each of the three landing wheels and one for the flaps.

The remote indication of oil and fuel pressure is accomplished by connecting a bellows to the oil or fuel pressure line and utilizing the motion of the bellows to rotate the transmitter brushes.

The temperature transmitter has a bimetal helix which rotates the transmitter brushes as the temperature changes.

The manifold pressure transmitter has a partially evacuated sealed bellows in a pressure tight compartment and the manifold pressure is applied to the outside of the bellows. Motion of the bellows is transmitted mechanically through a flexible wall to the d-c Selsyn transmitter. Temperature compensation is obtained by having the right amount of air in the sealed bellows.

The differential fuel pressure transmitter is similar to the manifold pressure except the bellows is not evacuated. The fuel pressure is connected to the inside of the bellows while the surrounding chamber is connected to the manifold. The bellows deflection is then proportional to the differential pressure.

The liquid level measurement utilizes the motion of a float to actuate the transmitter brushes. Magnetic coupling is used between the transmitter outside the tank and the float mechanism inside the tank so as to provide a positive gasoline seal.

Conclusions

The d-c Selsyn has been shown to be particularly suited to applications requiring high accuracy, light weight, and rapid and stable indications. These qualities make it particularly suitable for aircraft use. The fact that it can be operated on any source of low-voltage direct current without auxiliary equipment is an advantage, not only for aircraft use but for other applications where special power sources are not available.

Design of Long-Scale Indicating Instruments

A. J. CORSON
ASSOCIATE AIEE

R. M. ROWELL
ASSOCIATE AIEE

S. C. HOARE
ASSOCIATE AIEE

I. Introduction

FROM the viewpoint of utility, the scale length of an indicating instrument bears somewhat the same relationship to its dimensions as does the output of a motor to its frame size. The end product of an instrument is quantitative information. The longer and more legible the scale, the more definite the resulting scale readings, through reduction of observational errors both in the initial calibration and in subsequent use. Thus, instrument designers have endeavored to produce maximum scale lengths within a given space, examples of which are 5-inch scales of 6-inch rectangular switchboard instruments, and the $3^1/_2$-inch scales of 4-inch rectangular instruments. In general, however, the movements of such instruments have been restricted to an angular deflection of about 90 degrees, with consequent limitation in scale lengths as compared to the scale lengths of nonelectrical instruments such as steam and pressure gauges. A study of these fundamentals leads the authors, who attacked the problem of designing a new line of switchboard instruments, to the conclusion that a definite contribution to measurement technique could best be made by designing such instruments for long-range indication, and the smallest practicable panel space.

A study of prior art[1-6] showed that the importance of scale length had been appreciated, although no complete and coordinated group of instruments had resulted. Among the long-scale instruments produced were wattmeters, voltmeters, and ammeters operating on the induction disk principle, also permanent-magnet moving-coil voltmeters and ammeters. The induction instruments, while perhaps satisfactory for restricted operating conditions, hardly met modern performance requirements as to frequency range, wave-form variation, and other conditions. There is very little published information on these instruments, and their manufacture appears to have been discontinued, at least in America. It was therefore concluded that no complete group of long-scale instruments, meeting modern installation and performance requirements, was available.

A distinguishing feature of switchboard instruments which cannot be overemphasized is the variety of measurements which must be accomplished in devices having uniform appearance and outline dimensions. These include the measurement of d-c volts, d-c amperes, a-c volts, a-c amperes, watts, vars, power factor, cycles, synchronism, and such nonelectrical quantities as temperature and speed. It is impractical to satisfactorily measure all these quantities with one design of instrument mechanism, a minimum of four being required as shown in Table A.

Table A

Group	Kind of Mechanism	Quantities Measured
A	Permanent-magnet moving-coil	D-c amperes D-c volts Temperature Speed
B	Repulsion – attraction with fixed and moving vanes	A-c amperes A-c volts
C	Iron-cored electrodynamic	A-c watts A-c vars A-c cycles
D	Electromagnet with rotating vane	Power factor Synchronism

In the following sections, the method of attack in designing long-scale elements for each of these groups will be outlined. A panel dimension of $4^1/_4$ inches square was required, and a scale length of 6.8 inches representing 240 angular degrees was selected. The problem in each case was to obtain (*a*) mechanical freedom of the movement and (*b*) satisfactory torque characteristics, both with respect to magnitude and gradient, over this scale angle. After determining each basic design, much analytical work was necessary to reduce it to practice, but, with one or two exceptions, design details will be omitted in the interest of brevity.

II. The Permanent-Magnet Moving-Coil Instrument for Measurement of D-C Potential and Current

The design of the permanent-magnet moving-coil instrument was definitely simplified by using Alnico as a magnet material. The high coercive force of this material adapted it to short lengths of large cross section, as illustrated in Figure 1. The sector-shaped magnet was cast integrally with a soft-iron pole face for uniform distribution of the field in the air gap, and the magnet was arranged immediately adjacent to the gap to minimize leakage fluxes. The moving coil was pivoted about one of its sides and thus only one of the coil sides was effective in production of torque. The use of two coil sides appeared to involve mechanical complications such as to be thoroughly impracticable. It was found that satisfactory results could be obtained by full utilization of the magnetic material as described above and by using a minimum of weight and inertia in the moving element. The millivoltmeter moving coil was designed for a power input of 0.34 milliwatt, since the instrument will be used with 50-millivolt shunts. This construction also has the advantages of shelf-shielding, mechanical simplicity, and reproducibility. Damping was obtained by adding a short-circuited winding to the moving coil, the wire size and number of turns being determined by the required damping constant of the instrument involved. A uniform scale distribution is obtained as shown in Figure 2. The characteristics are shown in Table II.

Since instruments for the measurement of the temperatures of electrical machinery are required for modern switch-

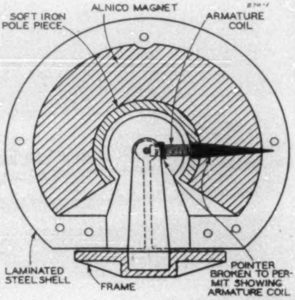


Figure 1. D-c ammeter and voltmeter mechanism

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A. J. CORSON, R. M. ROWELL and S. C. HOARE are engineers of the instrument engineering department, General Electric Company, West Lynn, Mass.

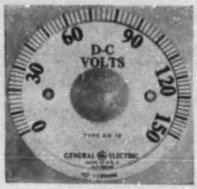

D-C
VOLTS
0
30
60
90
120
150
GENERAL ELECTRIC

Figure 2. D-c voltmeter-scale distribution

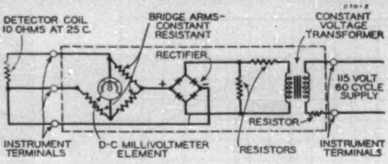

270-3
DETECTOR COIL
10 OHMS AT 25 C
BRIDGE ARMS-CONSTANT RESISTANT
CONSTANT VOLTAGE TRANSFORMER
RECTIFIER
115 VOLT 60 CYCLE SUPPLY
RESISTOR
INSTRUMENT TERMINALS
D-C MILLIVOLTMETER ELEMENT
RESISTORS
INSTRUMENT TERMINALS

Figure 3. Temperature-meter circuit

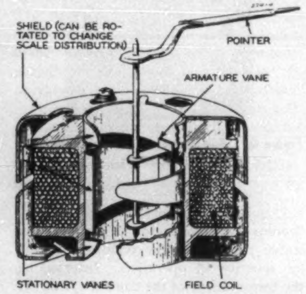

270-4
SHIELD (CAN BE ROTATED TO CHANGE SCALE DISTRIBUTION)
POINTER
ARMATURE VANE
STATIONARY VANES
FIELD COIL

Figure 4. A-c ammeter and voltmeter mechanism

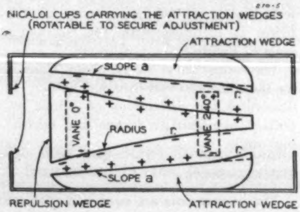

270-5
NICALOI CUPS CARRYING THE ATTRACTION WEDGES (ROTATABLE TO SECURE ADJUSTMENT)
ATTRACTION WEDGE
SLOPE a
VANE 0°
RADIUS
VANE 240°
SLOPE a
REPULSION WEDGE
ATTRACTION WEDGE

Figure 5 (above). Development of repulsion–attraction magnetic system

boards, such instruments have been included in this group. A circuit, illustrated in Figure 3, was thus designed to adapt the d-c milliammeter to temperature measurement using standard 10-ohm resistance temperature detectors. This circuit is essentially a Wheatstone bridge, energized by a constant potential of 6 volts direct current through a saturating transformer and rectifier designed for use on a standard 115-volt, 60-cycle circuit. The characteristics are given in Table II, and the scale distribution is shown in Figure 23.

III. The Repulsion–Attraction Instrument for Measurement of A-C Potential and Current

In their usual forms, the soft-iron torque-producing mechanisms are inherently limited to angular deflections of about 100 degrees of arc. Serious difficulties arise in matters of scale constriction and increased power consumption if the scales of these simple instruments are extended beyond their natural limits.

In the new element, these limitations are removed by utilizing dual forces of electromagnetic repulsion and attraction which act on a single movable vane as shown in Figures 4 and 5. The main repulsion mechanism is essentially similar to wedge-repulsion mechanisms now in use, in which a rapidly decreasing torque–displacement curve is obtained beyond the usual 0–90 degree scale range. The auxiliary irons exert a force of attraction which increases progressively as the repulsion force decreases. With suitable shapes and spacings for the soft-iron members, the two forces are well blended, resulting in a smooth open scale. In addition to the increase of scale angle, the element possesses a higher "torque-per-watt" effectiveness than any known soft-iron or even electrodynamic instrument mechanism, as illustrated in Table I.

The middle repulsion wedge is moulded integrally with the coil form. The attraction irons are mounted in magnetic contact with soft-iron cups which can be rotated manually through small angles. The rotatable cups serve as a means of instrument adjustment and also as a partial return path for the magnetic circuit. The cups are slotted radially to eliminate induced circulatory currents. Figure 4 is a sectional view of the assembly.

The rotatable cups (and attraction irons) permit of a rather wide range of control in scale distribution. The preferred scales are shown in Figure 9, where the voltmeter scale is expanded over the normal 110–120-volt interval. The ammeter scale is essentially uniform over the working range.

Table I. Sensitivity Comparisons of Various Instrument Mechanisms of Comparable Size

(Milliwatts Per Unit Torque for 90-Degree Deflection)

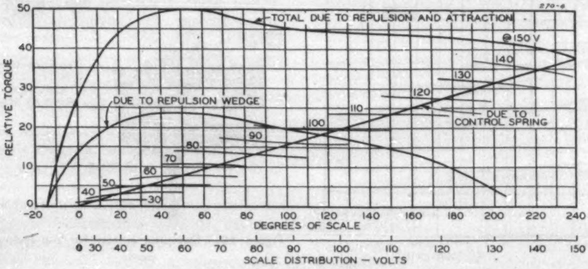
Repulsion–attraction	(soft-iron)	114
Wedge-repulsion	(soft-iron)	400
Inclined-coil	(soft-iron)	240
Electrodynamic	(air-coils)	216
Sector-coil repulsion	(soft-iron)	346

The Coil

In proportion, an air coil of the greatest electromagnetic effectiveness (that is, maximum flux per watt input) will have a square cross section and a mean diameter equal to three times a side of the square as shown in the curves of Figure 7. The authors' curve of proportions is in agreement with Shawcross and Wells'.[2] The variance with Maxwell's classic proportions[3] of $d = 3.7c$ is due, no doubt, to neglect of higher order terms of the calculations. The two criteria are, however, high up on the curves so the variation is more of academic than practical interest.

The magnetic circuit of a repulsion-attraction mechanism of this kind permitted of little more than a rough mathematical treatment which served as a guide to methodical experimentation in determining the best shapes and spacings for the soft-iron members. From computations and from actual torque curves for

Figure 6 (below). Repulsion–attraction instrument torque characteristics along the scale



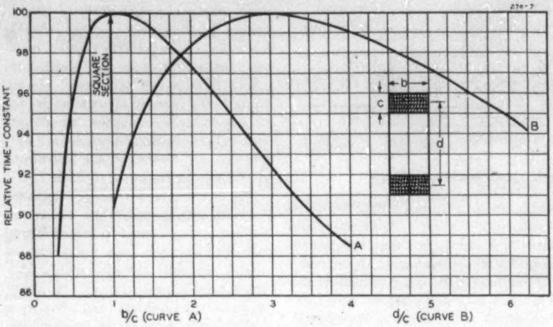
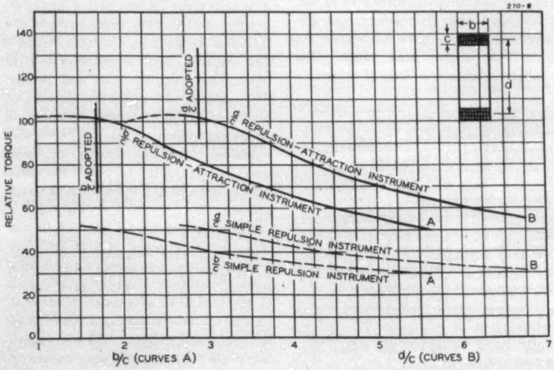


Figure 7. Field-coil proportions as a function of reactance/resistance

basic iron shapes, one could predict, quite closely, the ultimate results of iron shapes when used in combination. The best fixed repelling wedge to operate in conjunction with the ideal vane was one that best met the three criteria of steep initial torque-gradient, large angular deflection, and maximum torque. The initial steep gradient was especially desirable in ammeters for improved readability with low values of current. The wedge of Figure 5 met the requirements—the curved edges giving the steep gradient; the developed length, the long-scale angle; and the over-all dimensions, the maximum of torque.

The final and most important design problem was the selection of suitable attraction irons to extend the scale angle without increased power consumption. It was also necessary to avoid close spacings of adjacent magnetic parts, with their resulting manufacturing hazard. For adjustment purposes, the attraction irons permit of small rotational movements and are provided with means for securely locking them in the final position. The shape and slope of the irons result in a smoothly distributed scale, open to the end of pointer travel. The short curved lengths at the upscale ends of the attraction irons provide a greater degree of scale control. The curves (Figure 8) indicate that the coil system with irons, in its final form, has proportions differing very little from the requirements of ideal air coils.

Figure 8. Field-coil proportions with addition of iron members



Torque Characteristics

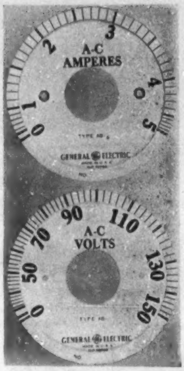


Figure 9. A-c ammeter- and voltmeter-scale distributions

The torque values of the complete system over the scale, and for comparison, the torque of simple repulsion, is shown in Figure 6. The completely drawn, uppermost curve gives the torque at various angular scale positions with a constant application of full-scale voltage (150 volts). The negative angle of −15 degrees corresponds to the assigned unstable position of the pointer—selected for security of operation. Intersecting the torque curve of the control spring are sectional lengths of torque curves of intermediate voltages. From the intersectional points the actual scale distribution is obtained by projection. The projected scale at the lower edge of the figure is identical with the voltmeter scale of Figure 9.

The torque curve for repulsion alone is for the same 150-volt constant value. It illustrates the limitations of simple repulsion in regard to torque and operating angle.

Damping System

The instruments are equipped with an eddy-current damper of unique construction as shown in Figure 10. It consists of a series of ten uniformly spaced, bipolar magnets of Alnico steel, embedded in two

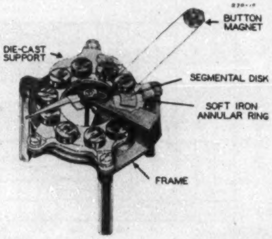


Figure 10. Damping system for a-c instruments

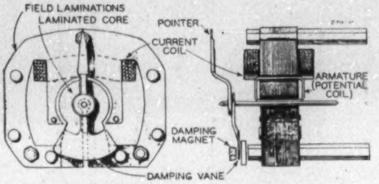


Figure 11. Wattmeter and varmeter mechanism

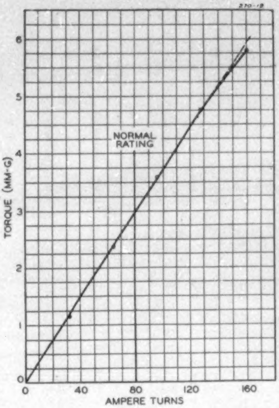


Figure 12 (left). Saturation curve of wattmeter field

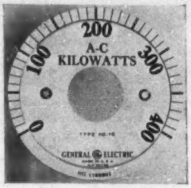


Figure 14. Wattmeter-scale distribution

die-cast half-ring supporting members. The two half-ring groups are assembled on the instrument in a circular arrangement directly over a small segmental aluminum disk, which is carried by the moving element. The magnetic circuit is completed through the disk to a soft-iron split annular-shaped ring mounted directly beneath the disk. The soft-iron ring serves also to shield the actuating element from the field projected by the damper magnets. This system provides an adequate and uniform damping torque throughout 360 degrees of disk movement. It is used on all a-c instruments.

Performance Characteristics

The characteristics of the a-c voltmeters and ammeters are shown in Table II. The efficient iron-clad mechanism has a high reactance-resistance ratio which is reflected in the instrument when constructed as a voltmeter. The effect of this reactance-resistance ratio on frequency influence was, however, overcome by capacitance compensation.

IV. The Iron-Cored Electrodynamic Instrument for the Measurement of Power

The long-scale instruments previously described deal with measurements of single quantities. Since there are important electrical measurements that involve the product or ratio of two electrical quantities, instruments must be provided for performing these mathematical operations. The measurement of the product of two electrical quantities suggested the use of the electrodynamic construction.

Since the conventional type of electrodynamometer instrument mechanism may be used for only a comparatively short-scale range, a modified construction was required for obtaining a scale length of 240 degrees. In order to obtain coordination of design and appearance, the general construction used in the permanent-magnet moving-coil instrument was adopted. In this instrument, however, the permanent magnet was replaced by a magnetic core shaped to permit insertion of a field winding and was designed for a uniform air gap between its pole face and the inner core, as illustrated in Figure 11. Since the magnetic field produced by the coil on the outer field structure is proportional to the current in that coil, the total torque produced by the instrument is proportional to the product of the ampere turns in the field and armature windings. The instrument is magnetically damped in the same manner as described for the iron-vane instruments.

The design of this mechanism presented several problems, particularly with respect to the magnetic circuit:

1. To produce a long scale, the use of magnetic material with attendant iron losses was required to obtain a long air gap.
2. The field circuit had to be designed for a linear flux-current relationship, requiring operations at densities well below the knee of the saturation curve at all possible operating currents.
3. The phase angle between the current and potential fluxes required adjustment to agree with the phase angle between the current and the voltage.
4. Losses in the instrument had to be kept low and voltage errors reduced to a minimum.

Several nickel-iron alloys were considered and tested for this application, and the best combination for low hysteresis and a high saturation density was obtained by the use of an alloy having 49 per cent nickel. The complete field structure, including the central core, was made of thin insulated laminations to reduce eddy currents to a minimum. Figure 12 shows the field density to be well below the saturation value for normal operating ranges.

The use of iron in the magnetic circuit naturally causes the current in the potential coil to lag slightly behind the applied voltage. This is more than canceled by the effect of the iron losses which cause the field flux to lag the line current by a still greater amount. Therefore, the

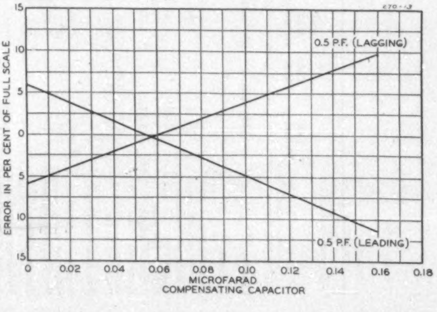


Figure 13. Determination of wattmeter compensation by capacitance

Table II. Long-Scale Instruments

Panel Space, 4¹/₄ by 4¹/₄ Inches Scale Length, 6.8 Inches

	D-C Voltmeter	ASA Stds.	D-C Milli-voltmeter	ASA Stds.	A-C Voltmeter	ASA Stds.	A-C Ammeter	ASA Stds.	Single-Phase Watt-meter	ASA Stds.	Polyphase Wattmeter	ASA Stds.	Polyphase Power-Factor Meter	ASA Stds.	Fre-quency Meter	ASA Stds.	Syn-chro-scope	ASA Stds.	A-C Tempera-ture Meter	ASA Stds.
Capacity	150. volts		50. millivolts		150. volts		5. amperes		115. volts 5. amperes 500. watts		115. volts 5. amperes 1,000. watts		115. volts 5. amperes 0-1-0 P.F.		115. volts 55-60-65 cycles		115. volts 60. cycles		115. volts 20-120 C	
Element																				
Full-scale torque (milli-meter-grams)	3.76		2.14		3.80		3.80		3.47		5.4		37.9		4.0		16.7		1.52	
Weight of movement (grams)	1.49		1.41		2.62		2.62		2.30		3.9		8.2		4.2		8.2		1.65	
Ratio: full-scale torque/weight	2.52		1.52		1.45		1.45		1.51		1.38		4.62*		1.9*		2.04*		0.92*	
Response time (seconds)	1.8	2.0	1.6	2.0	1.6	2.5	1.6	2.5	1.6	2.5	1.3	2.5	1.3	2.5	1.8	2.5			1.5	
Damping-factor	10.	5.	50.	5.	17.8	5.	20.	5.	16.7	5.	19.0	5.	7.7	5.	10.	5.			49.	
Potential Circuit																				
Resistance (ohms)	15,000.		2.10		3,046.				5,800.		7,137.		3,725.						2,470.	
Ohms per volt	100.		42.		20.3				50.5		62.									
Burden (volt-amperes)	1.5		0.0012		7.4	9.			2.28	7.	1.85	7.	2.04**	10.	4.7	15.	1.92	(running)	3.76	
Current Circuit																				
Resistance (ohms)							0.050		0.023		0.023		0.103							
Burden (volt-amperes)							2.32	8.	2.55	5.	2.55	5.	3.6	8.			6.30	(incoming)		
Performance †																				
Full-load self-heating (ultimate)	Nil		Nil		−0.3		Nil		Nil		Nil		±0.4		±0.05				Nil	
Self-heating temperature rise (ultimate)	41.8 C	55 C	Nil	55 C	44 C	55 C	16 C	55 C	28 C	55 C	28 C	55 C	44 C	55 C	24 C	55 C	35 C	55 C	20 C	55 C
Ambient temperature coefficient (per cent per degree centigrade	+0.005	0.035	−0.09	0.10	−0.012	0.025	+0.013	0.025	+0.02	0.025	+0.02	0.025	±0.03	0.05	+0.005	0.035			Footnote ●	
Permanent effect of 65 C (at 25 C)	±0.3		±0.3		±0.3		±0.2		±0.4		±0.4		±0.5		±0.083		Nil		±0.3	
Stray-field influence																				
(*a*) At 5 oersteds (greatest effect)	±0.28	1.0	±0.25	1.0	±2.	4.	±2.	4.	±0.1	4.	±0.1	4.	1.0	3.	±0.012	2.5			0.2°	
(*b*) At 5 oersteds (normal to axis)	±0.28	1.0	±0.25	1.0	±0.2	4.	±0.25	4.	±0.1	4.	±0.1	4.	1.0	3.	Nil	2.5			0.2°	
Voltage influence																				
(*a*) 90–130 volts															±.08					
(*b*) 110–120 volts (at scale ends)																			±0.8°	
(*c*) 105–130 volts (60–100 C)																			±0.8°	
(*d*) 105–130 volts (at scale ends)																			±1.4°	
(*e*) Over a ±10 per cent deviation									±0.2	0.5	±0.2	0.5	±0.5	0.5		0.25				

*240° Equivalent.

† Errors in per cent of full-scale value for voltmeters, millivoltmeters, ammeters, and wattmeters.
Errors in per cent of center-scale value for power-factor meter and frequency meter.
Errors in degrees centigrade for temperature meter.

** Per phase (three-phase winding).

● Temperature coefficient = ±0.064 C on scale per degree centigrade ambient rise at 20 and 120 C scale points. −0.015 C per degree centigrade at 80 C scale point.

Table II (continued). Long-Scale Instruments

Panel Space $4^1/_4$ by $4^1/_4$ Inches. Scale Length, 6.8 Inches

	D-C Voltmeter	ASA Stds.	D-C Milli-voltmeter	ASA Stds.	A-C Voltmeter	ASA Stds.	A-C Ammeter	ASA Stds.	Single-Phase Wattmeter	ASA Stds.	Polyphase Wattmeter	ASA Stds.	Polyphase Power-Factor Meter	ASA Stds.	Frequency Meter	ASA Stds.	Synchroscope	ASA Stds.	A-C Temperature Meter	ASA Stds.
Performance†																				
Frequency influence																				
(a) Mean reversed direct current					+0.5		+2.													
(b) D-c hysteresis (max. effect)					±0.8		+2.													
(c) At 125 cycles					−0.7		−1.0		−0.3‡		−0.3‡									
(d) At 25 cycles					+0.7		+0.5		+0.5		+0.5								Footnote◐	
(e) Over a ±10 per cent deviation						0.5		0.5		0.5		0.5	±1.0⊗	0.5						
Permanent effect of 120 per cent load maintained for 6 hours			±0.3	0.35	0.3			0.35	±0.2	0.35	±0.1	0.35								
Permanent effect of momentary applications of 100 times full-scale current value							±0.4													
Power-factor influence																				
(a) 80 per cent lagging (rated volt-amperes)									±0.2		±0.2									
(b) 80 per cent leading (rated volt-amperes)									−0.3		±0.2									
(c) 50 per cent lagging (rated volt-amperes)									−1.0		−1.0									
(d) Zero power factor (rated volt-amperes)									±1.0	1.0	±1.0	1.0								
Current influence at 50 per cent power-factor																				
(a) $3^1/_2$ amperes													0	1.0						
(b) 5 amperes													±0.9	1.0						
Lead resistance influence																				
Permissible resistance change for 1 C error (ohms)																				
(a) 20 C scale indication																			0.26	
(b) 80 C scale indication																			0.84	
(c) 120 C scale indication																			0.26	
Pull-in frequencies																				
(a) Ascending frequency																	58.0			
(b) Descending frequency																	62.0			
Drop-out frequencies																				
(a) Ascending frequency																	65.0			
(b) Descending frequencies																	56.0			

‡ At 80 cycles.

⊗ Over a range of 15 to 80 cycles.

◐ Error per cycle change in frequency: 1.0 C at 20 C scale point; 0.25 C at 80 C; 1.0 C at 120 C.

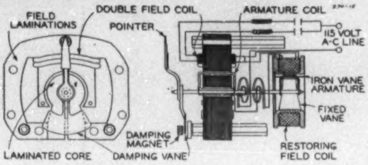


Figure 15 (above). Frequency-meter construction

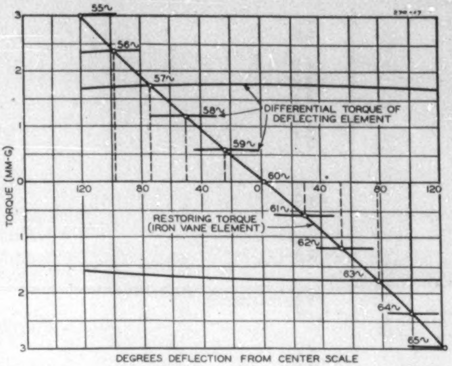


Figure 17 (right). Frequency-meter torque characteristics

potential-circuit current and the field flux are separated by a greater angle than the phase angle of the circuit, and, if uncompensated, would cause the instrument to read low on lagging power factors.

The instantaneous field flux then becomes proportional to

$$I_m \sin(\omega t - \theta - \alpha_2)$$

where α_2 is the angle by which the field flux lags the line current due to iron losses, and θ, the phase angle between current and voltage. The instantaneous armature current is proportional to

$$E_m \sin(\omega t - \alpha_1)$$

where α_1 is the phase angle by which the armature current lags the applied voltage. Without compensation, therefore, the torque would be—

$$T = KEI \cos(\theta - \alpha_1 + \alpha_2)$$

Therefore, a capacitor is shunted across the armature and a portion of the series resistor, providing sufficient compensation to make the angle α_1 equal to angle α_2 giving correct indication at any power factor. Figure 13 shows curves taken at leading and lagging power factors indicating the effect of capacitance on compensation for power-factor errors. The curves show that the selection of a value of capacitance corresponding to the point where the two curves intersect, provides sufficient compensation to make the instrument operate with satisfactory accuracy over a wide range of power factors.

It has been mentioned that potential flux was reduced to a minimum to prevent voltage errors. With a high value of potential flux, the moving coil tends to deflect upon the application of potential alone, due to extraneous flux through the field iron and back across the air gap. While this potential creep can be calibrated out and become of little consequence, if maintained at low value, it would result in voltage errors if the extraneous torque so produced became an appreciable part of the total torque of the instrument. To minimize such errors, the armature turns were kept low, and high resistance was used to secure a minimum armature current consistent with high operating torque. The magnetic flux path is also broken by two gaps, the ratio of which determines the magnetic balance of the circuit and consequently the potential-circuit errors. They are, therefore, set to give the best overall performance.

Because of the uniform gap the scale distribution is nearly linear as shown by Figure 14.

The same type of mechanism is used for polyphase wattmeters, two elements being rigidly fastened together to the supporting base. In the polyphase instrument it is possible to still further increase the potential-circuit resistance in order to decrease voltage errors as the torque to weight ratio of the single-phase construction can be maintained without doubling the total instrument torque. Compensation for power-factor errors is made in the same manner as previously described using a double capacitor for this purpose.

When used for the measurement of vars (reactive volt-amperes), the instruments are provided with external phase-shifting auto transformers or are cross-phased to obtain the proper phase angle in the manner used on conventional switchboard instruments. Single-phase varmeters require an external impedance

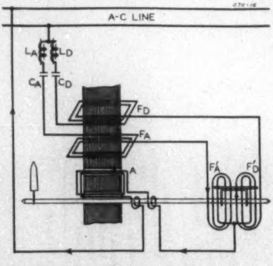


Figure 16. Frequency-meter circuit

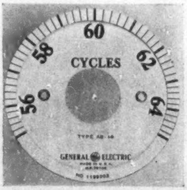


Figure 18. Frequency-meter scale distribution

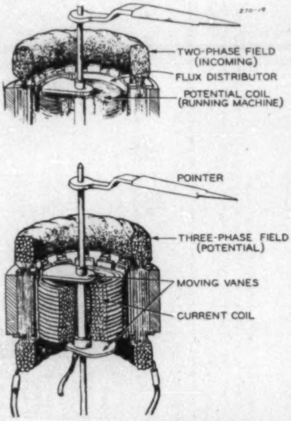

TWO-PHASE FIELD (INCOMING)
FLUX DISTRIBUTOR
POTENTIAL COIL (RUNNING MACHINE)
POINTER
THREE-PHASE FIELD (POTENTIAL)
MOVING VANES
CURRENT COIL

Figure 19. Power-factor meter and synchroscope mechanisms

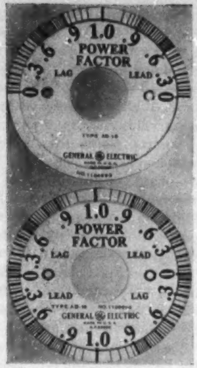


Figure 20. Power-factor-meter scale distribution

network to lag the current in the potential circuit by 90 degrees.

MEASUREMENT OF FREQUENCY

The iron-cored electrodynamic instrument mechanism was applied to the measurement of frequency by using a differential field coil,[4] each side of which was connected in a resonant circuit, one resonant at a frequency below the scale range, and the other, at a frequency above scale range. Since these two field coils are connected in opposition, an auxiliary element is added to provide restoring torque. The deflecting element has a uniform air gap and provides substantially uniform torque over its operating range. Thus the restoring element must also produce approximately uniform torque to obtain a scale as uniform as possible. Obviously with a 240-degree scale, a unidirectional and linear value of torque must be provided for 120 degrees each side of the center-scale position. Since it is evident that such a torque cannot be furnished by a simple dynamometer system, or any of the conventional iron-vane systems, a restoring torque has been provided by utilizing a long-range iron-vane repulsion mechanism similar to that used in the a-c ammeter and voltmeter. The construction is shown in Figure 15. It will be noted that the deflecting element is identical in construction with that used in the wattmeter, and that the restoring element is provided with a fixed iron vane which is wide at scale ends and narrow at center scale.

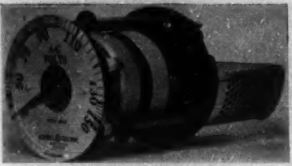


Figure 22. Interior view of a-c voltmeter

The connection diagram, Figure 16, shows the two resonant circuits and the differential connection of the deflecting and restoring fields, the resultant current passing through the armature coil. Variations of these connections are possible. For example, the connection of the restoring field coil with additive polarity, which somewhat changes the shape of the restoring torque curve, provides a scale which is expanded in the center and constricted toward the ends. With the subtractive polarity used, one circuit is resonant at about 34 cycles, while the other is resonant at about 97 cycles, both points being far outside the normal scale range of 55–65 cycles.

The curve in Figure 17 shows that approximately uniform restoring torque has been provided, its intersection with the differential torque curves at cardinal points indicating the angular displacement at these points.

It was necessary to give careful attention to several important points of construction. In the first place, the deflecting and restoring torque curves, extended beyond the scale ends, must be of such shape that the instrument will be stable at both ends of the scale with any condition of normal mechanical variation or changes in the resonant circuits. On a normal 60-cycle frequency meter with a scale of 55–65 cycles, stable operation is provided for about three times the normal frequency range above and below scale ends. The mechanical construction of the restoring field coil is likewise important in maintaining a scale which is essentially uniform and symmetrical. The spacing between the fixed and moving vanes is uniform, and the field winding is

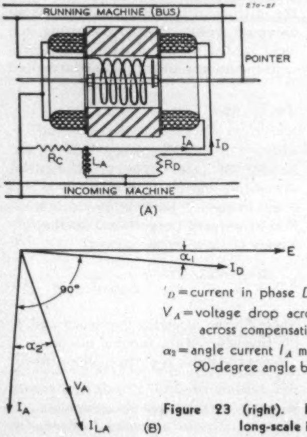


Figure 21 (left). Schematic connections and phase relations of synchroscope

A—Synchroscope connections

B—Phase relations

E = line voltage (incoming machine)

I_{LA} = current through reactance coil

I_A = current in stator coil A

α_1 = phase angle in circuit D

I_D = current in phase D (resistance circuit)

V_A = voltage drop across stator coil A (also across compensating resistance R_c)

α_2 = angle current I_A must lag V_A to produce 90-degree angle between I_A and I_D

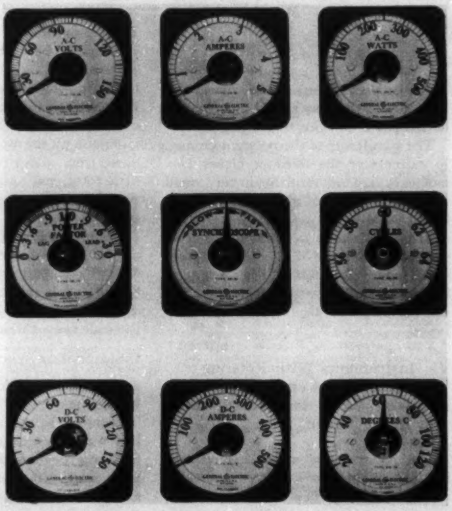


Figure 23 (right). Panel arrangement of long-scale instruments

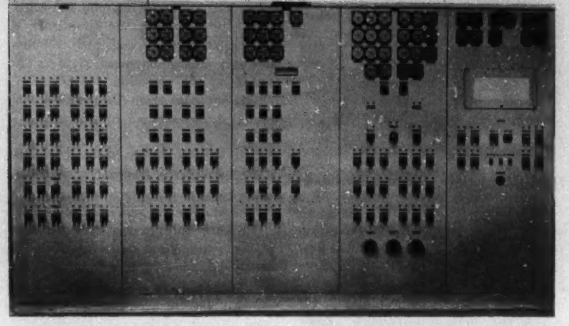


Figure 24. Typical installation of long-scale instruments

concentric with the stationary vane. Greater accuracy is obtained by winding the two coils at the same time, using two insulated wires in multiple.

In the frequency meter, the chief consideration was to make the instrument responsive to frequency changes without being influenced by any ordinary voltage fluctuations and with negligible disturbance from such operating variations as temperature and wave-form changes. It may be shown that the torque of both the deflecting and restoring elements is proportional to the square of the voltage and the deflection dependent on the ratio of currents in the two branches.

$$\text{Angle of deflection } \alpha = \kappa \frac{1-r^2}{1+r^2-2r\cos\theta}$$

(where r = the ratio of the currents in the two resonant branches and θ = the phase angle between them). The instrument indication is thus essentially independent of changes in the operating voltage.

The admittance of the resonant circuits to currents of the third or higher harmonics is very low, resulting in very small accuracy variations when used on distorted wave forms.

Figure 18 shows a standard 60-cycle scale with a range of 55–65 cycles. Other scale ranges are obtained by selecting impedance networks to provide the same current ratios over the required frequency range.

V. Instruments With Rotating Magnetic Field and Polarized Moving Vanes for Measurement of Power Factor

The power-factor meter has a field winding consisting of a motor-type stator with a laminated field structure and a distributed winding. This provides a rotating field when connected to a polyphase circuit. The armature is of the moving-vane type, the vanes of which are magnetically connected by a nickel-iron sleeve and polarized by an axially mounted stationary coil inside the stator. The construction is clearly shown in Figure 19. This system is not new, having been in use for a number of years in other forms,[5] but refinements in mechanical construction and improvements in operation make the instrument worthy of description.

When used to measure power factor, the field winding (stator) is connected through a high resistance to a polyphase line, while the vane is polarized by a stationary coil energized by the current in the circuit.

An analysis of this system will show that the instrument measures directly the phase angle between the current and the voltage and may be calibrated either in terms of power factor or phase angle. Hysteretic drag usually common to this construction has been reduced to a minimum by the use of carefully annealed nickel-iron alloys which produce very little rotational torque, and thus provide accurate readings over a wide range of current.

The movement is free to rotate throughout 360 degrees, and scales of either 180 degrees or 360 degrees can be provided, the latter where operation with reversed current is necessary. Figure 20 shows the scale ranges which may be obtained.

INDICATION OF SYNCHRONISM

When designed for use as a synchroscope, the physical construction of the instrument is the same as the power-factor meter since the instrument actually measures the phase difference between the potentials of the bus and the incoming generator. The polarizing coil, however, is wound for potential instead of current and is connected directly across the line.

Since most synchroscopes are designed for single-phase operation, the stator circuit (connected to the incoming machine) was provided with a phase-splitting arrangement. This consists of an external impedor box containing a resistance and an adjustable reactance coil. The stator is of two-phase construction, one phase being connected in the resistance circuit and the other in series with the reactance. (See Figure 21.) The resistance of the A circuit and the inductance in the D circuit will produce a phase angle somewhat less than 90 degrees. The quadrature phase relation and a symmetrical rotating field is obtained by shunting a non-inductive resistance R_c across the stator coil in the reactive circuit A. This produces a voltage drop V_A in phase with the lagging current I_{LA} through the reactor. The resistance R_c is of such a value that the inherent inductance in the stator causes the current through it (I_A) to lag the voltage drop V_A by an angle giving a 90-degree angle between I_A and I_D, the currents through the two stator branches. Final adjustment is made by a magnetic shunt on the reactance coil L_A.

Both "incoming" and "running" circuits were designed for low-power consumption, and the instrument, therefore, may be used for remote indication of synchronism. Uniform speed of rotation was obtained by the use of steel flux distributor rings as shown in Figure 19. These practically eliminated the slot effect of the stator and prevented spasmodic movement of the pointer at speeds approaching synchronism.

Since the armature vanes are polarized, the instrument becomes, in effect, a synchronous motor. If the polarizing coil were excited by direct current, synchronous operation would be obtained at a speed

$$S = \frac{120f}{P} \text{ (rpm)}$$

where f is the frequency and P the number of poles. The application of alternating current excitation of a different frequency, however, produces rotation at a speed proportional to the difference of these frequencies or

$$S = \frac{120(f_i - f_r)}{P}$$

where f_i = the incoming frequency and f_r the frequency of the running machine.

Since the stator is of two-pole construction

$S = 60(f_i - f_r)$ or 60 rpm for every cycle difference in frequency

Formulas for the Magnetic-Field Strength Near a Cylindrical Coil

HERBERT B. DWIGHT
FELLOW AIEE

THE magnetic-field strength at any given point near a circular cylindrical coil or solenoid in a nonmagnetic medium requires various formulas for its determination, depending on the shape of the solenoid and the position of the point. Formulas are required for the axial component of the magnetic field, called H_x in this paper, and other formulas are needed for the radial component, H_r.

A number of the formulas were given in "Absolute Measurements in Electricity and Magnetism,"[1] by Andrew Gray, editions of 1893 and 1921. Others have been published in other articles, as indicated in the footnotes and references of this paper. A collection of formulas for this problem was published by the writer in "The Magnetic Field of a Circular Cylindrical Coil,"[2] *Philosophical Magazine*, volume 11, April 1931, page 948.

In this paper, an additional group of formulas is presented, suitable particularly for points close to the coil section and for short coils. A device is also given (see paragraphs following equation 3) by which formulas for the flux density in the end plane only of a solenoid are listed, and this greatly increases the range in which the flux density of solenoids under all conditions can be precisely and quickly calculated. It is shown in Figure 5 that the formulas listed in this paper cover the entire field of a solenoid.

All of the published series formulas for solenoids of which the writer is aware, have been rearranged in this form and are given in this paper. Thus this paper contains the complete equipment for computing the magnetic field of a solenoid of any shape and at any point, far or near, including points within the cross section of the winding. The effect of insulation space between conductors, however, is not considered, but the rectangular cross section of the solenoid is assumed to have uniform current distribution.

One application of the formulas and methods listed in this paper is in finding the mutual inductance of a solenoid and a comparatively small coil, particularly when the latter is irregular in its shape or position. In many cases the mutual inductance is equal to the magnetic field of the solenoid at the center of the small coil, multiplied by the projected area of the small coil, crossing the field. This is of use in problems of electromagnetic interference and shielding.

Logarithmic Formulas

The group of logarithmic formulas for points close to the coil section may be derived from a mutual-inductance formula for two circles which was published by T. H. Havelock[3] in 1908 and was extended by E. B. Rosa and F. W. Grover in equation 16, reference 4. It is given in equation 16A, reference 5.

If we let a circle of radius $y = a + c$ on the same axis as the circle of radius a, pass through P, Figure 1, then by differentiating equation 16A, reference 5, for the mutual inductance of the two circles, with respect to y or c, the change in mutual inductance for an increase in y is found. This is the change in flux passing through the circle of radius y when y is increased, caused by a continuous current in the circle of radius a. The increment in flux passes through the ring of width ∂y and circumference $2\pi y$.

If a certain amount of flux φ, for 1 centimeter perpendicular to the paper, passes through an area of width ∂y, at an angle θ to the normal to ∂y, as in Figure 2, then the flux density, or force on a unit magnetic pole is $\varphi/(\partial y \cos\theta)$. The horizontal component of the flux density is obtained by multiplying by $\cos\theta$, and is $\varphi/\partial y$.

Therefore, in the case of Figure 1, the axial component of flux density at radius y, or at P, is

$$\frac{1}{2\pi y}\frac{\partial M}{\partial y} \tag{1}$$

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HERBERT B. DWIGHT is professor of electrical machinery, Massachusetts Institute of Technology, Cambridge, Mass.

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Mason F. Miller, work on the derivation of equation 36 and other formulas, derivation of the first few terms of equation 8 by expansion of equation 13 in series, and comparison of calculated values of field strength with laboratory measurements of solenoids.

A. C. Louie, work on the derivation of equation 9, on the determination of boundaries of ranges in Figure 5 and comparisons with laboratory measurements.

A. L. Carpentier, work on properties of the formulas and determinations of boundaries of ranges in Figure 5.

Thus, the instrument will make one complete revolution for each cycle difference in frequency, the direction of rotation determining whether the incoming generator is slow or fast. Synchronism is indicated when the pointer reaches a vertical position.

VI. Conclusion

These instruments have been designed to meet existing American Standards Association standards of performance, as shown in Table II, and, in general, the characteristics are equivalent to those of 6-inch rectangular instruments. An accuracy rating of 1 per cent of the full-scale reading has been applied. The mechanical durability as evaluated by shipping, vibration, and impact tests is also fully equal to the standards established by present practice. The application of these instruments to a typical switchgear unit is shown in Figure 24. The group represents a potential panel-space saving of 42 per cent with an average weight saving of 16 per cent and an increased scale length of 33 per cent as compared to 6-inch rectangular instruments. It is concluded, therefore, that 90-degree scale angles need no longer be regarded as an inherent limitation of electrical indicating instruments, and instruments having long scales with satisfactory accuracy may be produced.

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Putting $2a=d$ and $2y=D$, as in Figure 1, in order to make the numerical coefficients smaller, the following expression is obtained for the axial component of flux density at P due to a current of I amperes in a circular coil of N turns and of diameter d, assuming that the dimensions of the cross section of the coil are so small that they may be neglected:

$$H_{x(\text{circle})}=\frac{NI}{10}\times\frac{1}{2\pi y}\frac{\partial M}{\partial y}$$

$$=\frac{NI}{10}\frac{\sqrt{d}}{D\sqrt{D}}\left[\left\{\text{logn}\frac{16Dd}{u^2}\right\}\times\left\{1-\frac{3}{4}\frac{u^2}{Dd}+\frac{45}{64}\frac{u^4}{D^2d^2}\cdots+\frac{c}{d}\left(\frac{3}{2}-\frac{15}{16}\frac{u^2}{Dd}\cdots\right)\right\}-\frac{2CD}{u^2}-\left(2-2\frac{u^2}{Dd}+\frac{123}{64}\frac{u^4}{D^2d^2}\cdots\right)+\frac{c}{d}\left(\frac{5}{2}-\frac{77}{32}\frac{u^2}{Dd}\cdots\right)\right]$$

lines per square centimeter (2)

where $u^2=x^2+c^2$ and where logn denotes natural logarithm.

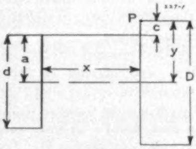

Figure 1. Magnetic field at *P* near a circle of radius *a*

Dimensions throughout this paper are in centimeters and electromagnetic centimeter-gram-second units are used, except where otherwise stated.

The current is assumed to be in such a direction around the circle that the flux density at the center of the circle is to the right. If the flux density given by equation 2 is a minus quantity, as it usually is when y is greater than a in Figure 1, that indicates that the direction of the flux at P is to the left. If y is less than a in Figure 1, the flux density is given by equation 2 but c has a negative numerical value.

Equation 2 and practically all the formulas of this paper, are infinite series, and they should be used for a given case only if the last term used in each series is almost negligibly small and is smaller than the preceding term. Otherwise, the succeeding terms which are neglected are probably of importance and the formula should not be used for such a case.

To obtain the axial component of flux density at P due to an infinitely thin solenoid, multiply expression 2 by ndx/N and integrate from x_1 to x_2, taking these lengths to be positive values. See Figure 3. The quantity n is the number of turns per centimeter of axial length of the solenoid. The expression is 0 when x is 0, and so by putting $x_1=0$ and $x_2=x$ there is obtained the following formula 3 for the axial field at a point P in the end plane of a solenoid of length x, which is the only type of formula that needs to be listed for solenoids whose length is given:

$$H_{x(s)}=\frac{nI}{10}\left[\left\{\text{logn}\frac{32yd}{x^2+c^2}\right\}\times\left\{\frac{x}{d}\left(1-\frac{3}{2}\frac{c}{d}+\frac{9}{4}\frac{c^2}{d^2}-\frac{55}{16}\frac{c^3}{d^3}+\frac{345}{64}\frac{c^4}{d^4}\cdots\right)-\frac{x^3}{d^3}\left(\frac{1}{4}-\frac{15}{16}\frac{c}{d}+\frac{75}{32}\frac{c^2}{d^2}\cdots\right)+\frac{9}{64}\frac{x^5}{d^5}\cdots\right\}-2\tan^{-1}\frac{x}{c}+\frac{x}{d}\left(\frac{1}{2}\frac{c}{d}-\frac{1}{2}\frac{c^2}{d^2}-\frac{3}{32}\frac{c^3}{d^3}+\frac{115}{64}\frac{c^4}{d^4}\cdots\right)+\frac{x^3}{d^3}\left(\frac{1}{2}-\frac{61}{32}\frac{c}{d}+\frac{151}{32}\frac{c^2}{d^2}\cdots\right)-\frac{21}{64}\frac{x^5}{d^5}\cdots\right]$$

lines per square centimeter (3)

The letter s in $H_{x(s)}$ denotes an infinitely thin solenoid or current sheet. This formula has been shortened somewhat by expressing D in terms of d, the mean diameter of the coil.

It is evident from Figure 3 that the magnetic flux density at P due to the actual solenoid of length (x_2-x_1) is equal to the difference between the field densities of two solenoids of lengths x_2 and x_1, respectively, of the same thickness, for both of which P is in the end plane. A physical meaning is thus given to the value of expression 3 for x_1 and x_2 separately, when integration is carried between these limits for the case shown in Figure 3.

It is, therefore, possible to calculate the value for x_1 by one formula, suitable for short coils, and that for x_2 by another formula if desired, suitable for long coils. The use of this device greatly increases the capability of calculating the flux density due to a solenoid under various conditions, with ease and precision.

A similar device for mutual inductance of solenoids was given in reference 5 (see paragraphs following equation 9 of that paper) and it produced a corresponding increase in the capacity to calculate the mutual inductance of coils of various shapes, in various positions.

A correction for the thickness, t, of the coil is desirable. Following Maxwell, Electricity and Magnetism,[6] paragraph 700, and noting that the differential of d is twice the differential of a radical distance

$$H_{(\text{coil})}=H_{(s)}+\frac{t^2}{3!}\frac{\partial^2H_{(s)}}{\partial d^2}+\frac{t^4}{5!}\frac{\partial^4H_{(s)}}{\partial d^4}\cdots \qquad (4)$$

where $H_{(s)}$ is the magnetic-field density, either axial or radial, at a given point P in the end plane due to the infinitely thin central solenoid whose diameter is d (see Figure 3).

Expressing equation 3 in terms of $D=2y$ instead of d, so that c is the only variable, and differentiating, we obtain the term in t^2 of equation 4.

$$H_{x(\text{coil})}=H_{x(s)}+\Delta H_x$$

where

$$\Delta H_x=\frac{nI}{10}\frac{t^2}{D^2}\left[\left\{\text{logn}\frac{16D^2}{x^2+c^2}\right\}\times\left\{\frac{x}{D}\left(\frac{1}{12}+\frac{1}{16}\frac{c}{D}-\frac{7}{32}\frac{c^2}{D^2}\cdots\right)-\frac{9}{32}\frac{x^3}{D^3}\cdots\right\}-\frac{xD}{(x^2+c^2)}\left\{\frac{1}{3}+\frac{1}{2}\frac{c}{D}+\frac{5}{12}\frac{c^2}{D^2}+\frac{7}{48}\frac{c^3}{D^3}-\frac{21}{64}\frac{c^4}{D^4}\cdots-\frac{x^2}{D^2}\left(\frac{1}{12}+\frac{9}{16}\frac{c}{D}+\frac{45}{32}\frac{c^2}{D^2}\cdots\right)+\frac{3}{64}\frac{x^4}{D^4}\cdots\right\}-\frac{xcD^2}{(x^2+c^2)^2}\left\{\frac{2}{3}-\frac{2}{3}\frac{c}{D}-\frac{1}{3}\frac{c^2}{D^2}-\frac{1}{6}\frac{c^3}{D^3}-\frac{1}{24}\frac{c^4}{D^4}+\frac{7}{96}\frac{c^5}{D^5}\cdots+\frac{x^2c}{D^3}\left(\frac{1}{6}+\frac{3}{8}\frac{c}{D}+\frac{9}{16}\frac{c^2}{D^2}\cdots\right)-\frac{3}{32}\frac{x^4c}{D^5}\cdots\right\}-\frac{x}{D}\left(\frac{1}{2}+\frac{121}{96}\frac{c}{D}+\frac{175}{96}\frac{c^2}{D^2}\cdots\right)+\frac{33}{32}\frac{x^3}{D^3}\cdots\right] \qquad (5)$$

Equation 5 should be used only for thin solenoids where t is small and where the correction ΔH_x is a small percentage. For somewhat thicker coils, the following formula may be used, obtained by putting equation 3 in terms of $D=2y$ and then integrating over the cross section of the coil:

$$H_{x(\text{coil})}=\frac{nI}{10}\left[\left\{\frac{c}{t}\text{ logn}\frac{16D^2}{x^2+c^2}\right\}\times\left\{\frac{x}{D}\left(1+\frac{1}{4}\frac{c}{D}+\frac{1}{12}\frac{c^2}{D^2}+\frac{1}{64}\frac{c^3}{D^3}-\frac{7}{320}\frac{c^4}{D^4}\cdots\right)-\frac{x^3}{D^3}\left(\frac{1}{4}+\frac{9}{32}\frac{c}{D}+\frac{9}{32}\frac{c^2}{D^2}\cdots\right)+\frac{9}{64}\frac{x^5}{D^5}\cdots\right\}-\left\{\frac{x}{t}\text{ logn}\frac{x^2+c^2}{D^2}\right\}\left\{1+\frac{1}{4}\frac{x^2}{D^2}-\frac{19}{64}\frac{x^4}{D^4}\cdots\right\}+\left\{\tan^{-1}\frac{c}{x}\right\}\left\{\frac{2c}{t}-\frac{x}{t}\left(2\frac{x}{D}-\frac{2}{3}\frac{x^3}{D^3}+\frac{4}{5}\frac{x^5}{D^5}\cdots\right)\right\}-\frac{c}{t}\left\{\pi-\frac{x}{D}\left(2-\frac{1}{2}\frac{c}{D}-\frac{4}{9}\frac{c^2}{D^2}-\frac{59}{192}\frac{c^3}{D^3}-\frac{917}{4{,}800}\frac{c^4}{D^4}\cdots\right)-\frac{x^3}{D^3}\left(\frac{1}{6}-\frac{1}{6}\frac{c}{D}-\frac{103}{120}\frac{c^2}{D^2}\cdots\right)+\frac{151}{320}\frac{x^5}{D^5}\right\}\right]_{c=c_1}^{c_2}$$

lines per square centimeter (6)

If y is less than a_1, then c_1 and c_2 are negative. If y lies in value between a_1 and a_2, then c_1 is negative and c_2 is positive.

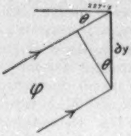

Figure 2. Flux passing through an area of width ∂y

Corresponding formulas are required for the radial component of magnetic field. Using equation 16, reference 4, for the mutual inductance of two circles, the radial field at P, Figure 1, due to a circle or coil of N turns of very small cross section, is

$$-\frac{NI}{10}\times\frac{1}{2\pi y}\,\frac{\partial M}{\partial x} \tag{7}$$

$$= H_{r(\text{circle})}=\frac{NI}{10D}\,\frac{\sqrt{d}}{D\sqrt{D}}\left[\frac{2xD}{u^2}+\frac{x}{d}\left(\frac{5}{2}-\frac{77}{32}\,\frac{u^2}{Dd}+\frac{141}{64}\,\frac{u^4}{D^2d^2}-\frac{17{,}165}{8{,}192}\,\frac{u^6}{D^3d^3}\cdots\right)-\left\{\operatorname{logn}\frac{16Dd}{u^2}\right\}\left\{\frac{3}{2}\,\frac{x}{d}\left(1-\frac{5}{8}\,\frac{u^2}{Dd}+\frac{35}{64}\,\frac{u^4}{D^2d^2}-\frac{525}{1{,}024}\,\frac{u^6}{D^3d^3}\cdots\right)\right\}\right]$$

lines per square centimeter (8)

where $u^2=x^2+c^2$.

In equation 8, the dimension D, which is twice the radial distance to the point P, Figure 1, may be either larger or smaller than d, the diameter of the circle. Only even powers of the quantity c are involved.

The minus sign occurs in equation 7 since the mutual inductance of the two circles in Figure 1 decreases as x increases.

For the radial field at a point P in the end plane of an infinitely thin solenoid of diameter d, multiply equation 8 by ndx/N and integrate from 0 to x, where n is the number of turns per centimeter of axial length.

$$H_{r(s)}=\frac{nI\sqrt{d}}{10\sqrt{D}}\left[\left\{\operatorname{logn}\frac{16Dd}{c^2}\right\}\times\left\{1+\frac{3}{4}\,\frac{c^2}{Dd}-\frac{15}{64}\,\frac{c^4}{D^2d^2}+\frac{35}{256}\,\frac{c^6}{D^3d^3}-\frac{1{,}575}{128^2}\,\frac{c^8}{D^4d^4}\cdots\right\}-\left\{\operatorname{logn}\frac{16Dd}{u^2}\right\}\times\left\{1+\frac{3}{4}\,\frac{u^2}{Dd}-\frac{15}{64}\,\frac{u^4}{D^2d^2}+\frac{35}{256}\,\frac{u^6}{D^3d^3}-\frac{1{,}575}{128^2}\,\frac{u^8}{D^4d^4}\cdots\right\}+\frac{u^2-c^2}{2Dd}-\frac{31}{64}\,\frac{u^4-c^4}{D^2d^2}+\frac{247}{768}\,\frac{u^6-c^6}{D^3d^3}-\frac{7{,}795}{128\times256}\,\frac{u^8-c^8}{D^4d^4}\cdots\right] \tag{9}$$

where $u^2=x^2+c^2$. Here also, $D=2y$ may be greater or less than d.

Note that the radial field close to the end of an infinitely thin solenoid becomes infinitely great, because c approaches 0 and logn c is involved.

By equation 4

$$H_{r(\text{coil})}=H_{r(s)}+\Delta H_r$$

where

$$\Delta H_r=\frac{nI}{10}\left[\frac{t^2}{D^2}\left\{\operatorname{logn}\frac{x^2+c^2}{c^2}\right\}\times\left\{\frac{1}{12}+\frac{1}{4}\,\frac{c}{D}+\frac{17}{32}\,\frac{c^2}{D^2}+\frac{95}{96}\,\frac{c^3}{D^3}\cdots\right\}-\frac{t^2}{D^2}\left\{\operatorname{logn}\frac{16D^2}{x^2+c^2}\right\}\left\{\frac{x^2}{D^2}\left(\frac{7}{32}+\frac{15}{32}\,\frac{c}{D}\cdots\right)\right\}+\frac{t^2}{x^2+c^2}\left\{\frac{1}{3}-\frac{c}{D}+\frac{5}{12}\,\frac{c^2}{D^2}+\frac{7}{12}\,\frac{c^3}{D^3}+\frac{51}{64}\,\frac{c^4}{D^4}+\frac{247}{96}\,\frac{c^5}{D^5}\cdots+\frac{x^2}{D^2}\left(\frac{1}{4}+\frac{3}{4}\,\frac{c}{D}+\frac{35}{32}\,\frac{c^2}{D^2}+\frac{35}{32}\,\frac{c^3}{D^3}\cdots\right)-\frac{x^4}{D^4}\left(\frac{5}{64}+\frac{45}{64}\,\frac{c}{D}\cdots\right)\right\}-\frac{t^2c^2}{(x^2+c^2)^2}\left\{\frac{2}{3}-\frac{2}{3}\,\frac{c}{D}+\frac{1}{6}\,\frac{c^2}{D^2}+\frac{1}{6}\,\frac{c^3}{D^3}+\frac{17}{96}\,\frac{c^4}{D^4}+\frac{19}{96}\,\frac{c^5}{D^5}\cdots+\frac{x^2}{D^2}\left(\frac{1}{2}+\frac{1}{2}\,\frac{c}{D}+\frac{7}{16}\,\frac{c^2}{D^2}+\frac{5}{16}\,\frac{c^3}{D^3}\cdots\right)-\frac{x^4}{D^4}\left(\frac{5}{32}+\frac{15}{32}\,\frac{c}{D}\cdots\right)\right\}+\frac{t^2}{c^2}\left\{\frac{1}{3}+\frac{1}{3}\,\frac{c}{D}-\frac{1}{4}\,\frac{c^2}{D^2}-\frac{5}{12}\,\frac{c^3}{D^3}-\frac{119}{192}\,\frac{c^4}{D^4}-\frac{57}{64}\,\frac{c^5}{D^5}\cdots+\frac{x^2}{D^2}\left(\frac{89}{96}\,\frac{c^2}{D^2}+\frac{101}{32}\,\frac{c^3}{D^3}\cdots\right)\right\}\right] \tag{10}$$

Since equation 10 is applicable only to thin solenoids with a small value of t^2/c^2 because terms in t^4 and higher powers of t have been omitted, the following formula, obtained by integration of equation 9 after it was expressed in terms of D, is given for use with thicker coils:

$$H_{r(\text{coil})}=\frac{nI}{10}\left[\frac{x^2}{Dt}\left\{\operatorname{logn}\frac{16D^2}{x^2+c^2}\right\}\times\left\{\frac{1}{2}-\frac{3}{4}\,\frac{c}{D}-\frac{3}{8}\,\frac{c^2}{D^2}-\frac{7}{32}\,\frac{c^3}{D^3}-\frac{15}{128}\,\frac{c^4}{D^4}\cdots-\frac{x^2}{D^2}\left(\frac{5}{16}-\frac{15}{64}\,\frac{c}{D}-\frac{45}{128}\,\frac{c^2}{D^2}\cdots\right)+\frac{161}{384}\,\frac{x^4}{D^4}\cdots\right\}+\frac{c}{t}\left\{\operatorname{logn}\frac{x^2+c^2}{c^2}\right\}\left\{1-\frac{1}{2}\,\frac{c}{D}+\frac{1}{12}\,\frac{c^2}{D^2}+\frac{1}{16}\,\frac{c^3}{D^3}+\frac{17}{320}\,\frac{c^4}{D^4}+\frac{19}{384}\,\frac{c^5}{D^5}\cdots\right\}+\frac{2x}{t}\left(\tan^{-1}\frac{c}{x}\right)\times\left(1+\frac{2}{3}\,\frac{x^2}{D^2}-\frac{2}{5}\,\frac{x^4}{D^4}\cdots\right)-\frac{c}{t}\,\frac{x^2}{D^2}\left(\frac{5}{6}-\frac{11}{16}\,\frac{c}{D}-\frac{49}{60}\,\frac{c^2}{D^2}-\frac{145}{192}\,\frac{c^3}{D^3}\cdots\right)+\frac{c}{t}\,\frac{x^4}{D^4}\times\left(\frac{101}{320}-\frac{13}{24}\,\frac{c}{D}\cdots\right)\right]_{c=c_1}^{c_2}$$

lines per square centimeter (11)

If y is less than a_1, then c_1 and c_2 are negative. If y lies in value between a_1 and a_2, then c_1 is negative and c_2 is positive.

Note that if the current is assumed to flow in a mathematically exact rectangular cross section, the radial field is not infinite at the corners or elsewhere. The limit of c logn c is 0 when c approaches 0. See reference 9, numbers 72 and 605.

Elliptic Integral Formulas for Circles

The following general formulas for the flux density in any position whatever, relative to a circle which carries a current, have been published by Alexander Russell[7] and give the same results as equations 2 and 8:

$$H_{x(\text{circle})}=\frac{2NI}{10r_1}\left\{\frac{2a(a-y)}{r_2^2}E+(K-E)\right\} \tag{12}$$

$$H_{r(\text{circle})}=\frac{2NI}{10r_1}\left\{\frac{2ax}{r_2^2}E-\frac{x}{y}(K-E)\right\} \tag{13}$$

where the dimensions are as in Figure 1 and where

$$r_1^2=(a+y)^2+x^2 \tag{14}$$

$$r_2^2=(a-y)^2+x^2=c^2+x^2 \tag{15}$$

K and E are complete elliptic integrals of the first and second kinds of modulus k, where

$$k^2=1-r_2^2/r_1^2 \tag{16}$$

Values of K and E may be taken from tables, as for instance, reference 8, pages 199 and 204.

It is seen from Figure 1 that r_2 is the distance from P to the nearest part of the circumference of the circle carrying current. If this distance is very small, k^2 approaches 1 and the value of the elliptic integral K approaches infinity. When this occurs, the values in any table become far apart so that interpolated values cannot be obtained with precision and it becomes better to use a series involving logarithms than to use a table of values. That, however, is equivalent to using formulas 2 and 8, which involve logarithms. It is always possible to find the precise value of the logarithm of any given number, however large or small, one way being to find first the logarithm to base 10.

Since equations 2 and 8 are power series in r_2^2/ay, they have greater precision, the closer the point P approaches to the circumference of the circle and the greater becomes the difficulty of obtaining the value of K from a table.

Zonal Harmonic Formulas for Circles

Other formulas will now be listed. The formulas for solenoids are put in the more useful and usually more concise form giving the flux density at a point in the end plane of the solenoid.

For points near the center of the circle,

$$H_{x(\text{circle})}=\frac{2\pi NI}{10a}\left[1-\frac{3}{2}\frac{r^2}{a^2}P_2\left(\frac{x}{r}\right)+\frac{3\times5}{2\times4}\frac{r^4}{a^4}P_4\left(\frac{x}{r}\right)-\ldots\right] \tag{17}$$

where

$$r^2=x^2+y^2 \tag{18}$$

$P_2(x/r)$, $P_4(x/r)$ and so on, are surface zonal harmonics which may be defined by

$$P_n(\mu)=\frac{1}{2^n n!}\frac{\partial^n}{\partial\mu^n}(\mu^2-1)^n$$

Values are tabulated in reference 8, page 188, reference 10, and elsewhere, or they may be calculated from series (see reference 9, page 169 and equation 46 of this paper).

$$H_{r(\text{circle})}=\frac{\pi NI}{10}\frac{yr}{a^3}\left[P_2'\left(\frac{x}{r}\right)-\frac{3}{4}\frac{r^2}{a^2}P_4'\left(\frac{x}{r}\right)+\frac{3\times5}{4\times6}\frac{r^4}{a^4}P_6'\left(\frac{x}{r}\right)-\ldots\right] \tag{19}$$

where $P_n'(\mu)=\frac{\partial}{\partial\mu}P_n(\mu)$

Values are tabulated in reference 10 or reference 8, page 196, or they may be calculated from series, as above.

For points at a considerable distance from the center of the circle,

$$H_{x(\text{circle})}=\frac{2\pi NI}{10r}\left[\frac{a^2}{r^2}P_2\left(\frac{x}{r}\right)-\frac{3}{2}\frac{a^4}{r^4}P_4\left(\frac{x}{r}\right)+\frac{3\times5}{2\times4}\frac{a^6}{r^6}P_6\left(\frac{x}{r}\right)-\ldots\right] \tag{20}$$

$$H_{r(\text{circle})}=\frac{\pi NIy}{10r^2}\left[\frac{a^2}{r^2}P_2'\left(\frac{x}{r}\right)-\frac{3}{4}\frac{a^4}{r^4}P_4'\left(\frac{x}{r}\right)+\frac{3\times5}{4\times6}\frac{a^6}{r^6}P_6'\left(\frac{x}{r}\right)-\ldots\right] \tag{21}$$

See reference 11, equations 3 and 4.

For points not far from the axis of the circle

$$H_{x(\text{circle})}=\frac{2\pi NIa^2}{10\rho^3}\left[P_1'\left(\frac{x}{\rho}\right)-\frac{1}{2}\frac{y^2}{\rho^2}P_3'\left(\frac{x}{\rho}\right)+\frac{1\times3}{2\times4}\frac{y^4}{\rho^4}P_5'\left(\frac{x}{\rho}\right)-\ldots\right] \tag{22}$$

$$H_{r(\text{circle})}=\frac{2\pi NIa^2y}{10\rho^4}\left[\frac{1}{2}P_2'\left(\frac{x}{\rho}\right)-\frac{1\times3}{2\times4}\frac{y^2}{\rho^2}P_4'\left(\frac{x}{\rho}\right)+\frac{1\times3\times5}{2\times4\times6}\frac{y^4}{\rho^4}P_6'\left(\frac{x}{\rho}\right)-\ldots\right] \tag{23}$$

where

$$\rho^2=x^2+a^2 \tag{24}$$

Equations 22 and 23 are equivalent to equations 9 and 10, reference 1, page 248, volume 2, edition of 1893 and page 212, edition of 1921, changing 1,680x^4 to 1,680a^2x^4.

Formulas for Short Coils

All these formulas for circles can be integrated, though not always by one direct step, to give expressions for the flux density at a point in the end plane of a solenoid, each applicable to a certain range, as approximately indicated in Figure 5. Each formula may be used in an area in Figure 5 in which its number, such as equation 25, occurs, and up to the boundary marked by an arrow leading from that number. The areas are seen to overlap. Satisfactory convergence may be found beyond the boundaries marked, and, on the other hand, the rapidity of convergence may be very poor for the thickness correction formulas near the boundaries. Figure 5 is a preliminary guide, and the criterion for use of a certain formula in any given case is the rapidity of convergence of the series. If the convergence is not suitably rapid, the formula should not be used in that particular case.

By integrating equations 17 and 19 from 0 to x, for values of $r=\sqrt{x^2+y^2}$ less than approximately 0.7a (see Figures 3 and 5),

$$H_{x(\text{coil})}=\frac{2\pi nI}{10}\left[\frac{r}{a}P_1\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{12a^2}+\frac{t^4}{80a^4}+\frac{t^6}{448a^6}+\ldots\right\}-\frac{1}{2}\frac{r^3}{a^3}P_3\left(\frac{x}{r}\right)\times\left\{1+\frac{t^2}{2a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{1}{16}\frac{t^6}{a^6}+\ldots\right\}+\frac{1\times3}{2\times4}\frac{r^5}{a^5}P_5\left(\frac{x}{r}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{15}{32}\frac{t^6}{a^6}+\ldots\right\}-\frac{1\times3\times5}{2\times4\times6}\frac{r^7}{a^7}P_7\left(\frac{x}{r}\right)\times\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{21}{8}\frac{t^4}{a^4}+\frac{33}{16}\frac{t^6}{a^6}+\ldots\right\}\ldots\right]$$

lines per square centimeter (25)

where n = turns per centimeter of axial length of the coil. For dimensions see Figure 3.

$$H_{r(\text{coil})}=\frac{3\pi nIy}{20a}\left[\frac{x^2}{a^2}\left\{1+\frac{t^2}{2a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{1}{16}\frac{t^6}{a^6}+\ldots\right\}-\frac{5}{4}\left(\frac{x^4}{a^4}-\frac{3x^2y^2}{2a^4}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{15}{32}\frac{t^6}{a^6}+\ldots\right\}+\frac{35}{24}\left(\frac{x^6}{a^6}-\frac{15}{4}\frac{x^4y^2}{a^6}+\frac{15}{8}\frac{x^2y^4}{a^6}\right)\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{21}{4}\frac{t^4}{a^4}+\frac{33}{16}\frac{t^6}{a^6}+\ldots\right\}\right] \tag{26}$$

Formulas for $H_{x(a)}$ and $H_{r(a)}$ are obtained by putting $t=0$. For thick coils, the brackets containing power series in t/a may be replaced by the complete expression

$$\frac{a}{mt}\left\{\left(1-\frac{t}{2a}\right)^{-m}-\left(1+\frac{t}{2a}\right)^{-m}\right\} \tag{27}$$

where m is 0 or an even number. The values of m are 0, 2, 4, 6, in equation 25 and 2, 4, 6, in equation 26. For $m=0$, the binomials may be expanded and m cancelled out before m is put $=0$, thus giving a/t logn a_2/a_1 which may be obtained also by integration of $1/a$. The complete expressions may be used also for extending the formulas.

Formulas for Infinitely Long Coils

It is not permissible to integrate expressions 20 or 21 from the limit $x=0$, or past that point, for small values of y, because a/r would be greater than 1 and the series would be divergent. But the series and their integrals become 0 when x becomes infinitely great, and so it is possible to integrate from x to ∞ and obtain the field density at P due to a coil extending from x to ∞. By subtracting this from the field at P due to a coil extending from 0 to ∞, which may be called $H_{x\infty}$ and $H_{r\infty}$ respectively, one obtains the field due to a coil from 0 to x, at a point in the end plane, the same as for all the other formulas for solenoids listed in this paper.

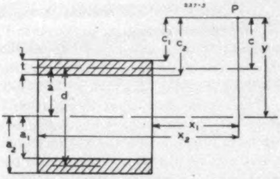

Figure 3. Magnetic field at point *P* near a solenoid

The axial component of field at a point P outside a solenoid which extends to an infinite distance in both directions from P is 0, as is well known. Such a coil is the limiting condition of a toroidal coil of very large coil diameter compared to the section diameter. The parts of the solenoid to the right and left of the radial plane through P give equal axial fields, which are therefore 0. Thus

$$H_{x\infty}=0 \tag{28}$$

for points outside the solenoid.

The axial field inside a coil extending to infinity in both directions is also well known to be $4\pi nI/10$, a constant. By taking a radial plane through P, the axial field due to the half coil to the left of P is by symmetry equal to

$$H_{x\infty}=\frac{2\pi nI}{10} \tag{29}$$

For values of y which lie between a_1 and a_2, Figure 3, the value of $H_{x\infty}$ is that due to the turns lying outside of y. In such a case

$$H_{x\infty}=\frac{a_2-y}{a_2-a_1}\times\frac{2\pi nI}{10} \tag{30}$$

The value of $H_{r\infty}$ requires more computation, for various values of y. First, for large values of y, the integration of equation 21 from $x=0$ to ∞ is permissible, and gives

$$H_{r\infty}=\frac{\pi nI}{10}\left[\frac{a^2}{y^2}\left(1+\frac{t^2}{12a^2}\right)+\frac{1\times3}{4\times2}\frac{a^4}{y^4}\left(1+\frac{t^2}{2a^2}+\frac{t^4}{80a^4}\right)+\frac{1\times3}{4\times6}\times\frac{3\times5}{2\times4}\frac{a^6}{y^6}\left(1+\frac{5}{4}\frac{t^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{t^6}{448a^6}\right)+\frac{1\times3\times5}{4\times6\times8}\times\frac{3\times5\times7}{2\times4\times6}\frac{a^8}{y^8}\left(1+\frac{7}{3}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{t^6}{16a^6}+\frac{t^8}{2{,}304a^8}\right)+\ldots\right] \tag{31}$$

See equation 40. The series in t/a are not infinite series, but are complete. The general expression is given in equation 41.

For convenience in computation, the numerical coefficients of powers of a/y in equation 31 are 1, $^3/_8$, $^{15}/_{64}$ and $^{175}/_{1024}$.

For small values of y, integration of equation 23 from $x=0$ to ∞ gives

$$H_{r\infty}=\frac{\pi nIy}{10a}\left[1+\frac{3}{8}\frac{y^2}{a^2}+\frac{15}{64}\frac{y^4}{a^4}+\frac{175}{1{,}024}\frac{y^6}{a^6}+\ldots+\frac{t^2}{a^2}\left(\frac{1}{12}+\frac{3}{16}\frac{y^2}{a^2}+\frac{75}{256}\frac{y^4}{a^4}+\frac{1{,}225}{3{,}072}\frac{y^6}{a^6}+\ldots\right)+\frac{t^4}{a^4}\left(\frac{1}{80}+\frac{9}{128}\frac{y^2}{a^2}+\frac{105}{512}\frac{y^4}{a^4}+\frac{3{,}675}{8{,}192}\frac{y^6}{a^6}+\ldots\right)+\text{terms in } t^6/a^6 \text{ and so on}\right] \tag{32}$$

For thicker coils

$$H_{r\infty}=\frac{\pi nIy}{10t}\left[\operatorname{logn}\frac{a_2}{a_1}+\frac{3}{16}y^2\left(\frac{1}{a_1^2}-\frac{1}{a_2^2}\right)+\frac{15}{256}y^4\left(\frac{1}{a_1^4}-\frac{1}{a_2^4}\right)+\frac{175}{6{,}144}y^6\times\left(\frac{1}{a_1^6}-\frac{1}{a_2^6}\right)+\ldots\right] \tag{33}$$

For values of y not very different from a, consider the mutual inductance of two infinitely thin coaxial solenoids of lengths b_1 and b_2 and with a distance between their adjacent end planes equal to w, as in Figure 4. The mutual inductance is given by equation 1, reference 5, as follows:

$$\frac{M}{nn'}=F(x1)-F(x2)-F(x3)+F(x4) \tag{34}$$

where, in the notation used in Figure 4

$x_1=b_1+b_2+w$
$x_2=b_1+w$
$x_3=b_2+w$
$x_4=w$

and where n and n' are the turns per centimeter of the two coils.

Assume that w is a small quantity, and that b_2 is smaller still, so that the right-hand coil is equivalent to a turn of very fine wire. Then $F_{(x1)}$ and $F_{(x2)}$ are to be computed by equation 7 and $F_{(x3)}$ and $F_{(x4)}$ by equation 6 of reference 5. Allow w to increase a small amount. Then, as in equation 7 of this paper, the radial flux density at the circumference of the right-hand coil is

$$-\frac{1}{\pi Dn'b_2}\frac{\partial M}{\partial w} \tag{35}$$

Differentiating $F_{(x1)}-F_{(x2)}$ with respect to w or x, and expanding the first few terms in powers of $1/b_1$, it is found that the result is 0 when b_1 becomes infinite. Differentiating

$$\frac{F_{(x3)}-F_{(x4)}}{\pi Dn'b_2}$$

and discarding higher powers of b_2 and w, the result, excepting the terms in c^8, is

$$H_{r(s)\infty}=\frac{nI}{10}\frac{\sqrt{d}}{\sqrt{D}}\left[\left(\operatorname{logn}\frac{16Dd}{c^2}\right)\left(1+\frac{3}{4}\frac{c^2}{Dd}-\frac{15}{64}\frac{c^4}{D^2d^2}+\frac{35}{256}\frac{c^6}{D^3d^3}-\frac{1{,}575}{128^2}\frac{c^8}{D^4d^4}\cdots\right)-4-\frac{1}{2}\frac{c^2}{Dd}+\frac{31}{64}\frac{c^4}{D^2d^2}-\frac{247}{768}\frac{c^6}{D^3d^3}+\frac{7{,}795}{128\times256}\frac{c^8}{D^4d^4}\cdots\right] \tag{36}$$

The process of taking the same function of w and $w+b_2$, subtracting and dividing by the small quantity b_2 is equivalent to differentiating with respect to w. Expression 36 is therefore the result of differentiating $F_{(x)}$ twice. But $F_{(x)}$ was the result of integrating equation 16, reference 4, twice. Equation 36 should therefore correspond to equation 16, reference 4, which it does, and so the two terms in c^8 can be added from the earlier publication.

The following correction for thickness may be added to equation 36:

$$\Delta H_{r\infty}=\frac{nI}{10}\left[\left(\operatorname{logn}\frac{16D^2}{c^2}\right)\frac{t^2}{D^2}\left(\frac{1}{12}+\frac{1}{4}\frac{c}{D}+\frac{17}{32}\frac{c^2}{D^2}+\frac{95}{96}\frac{c^3}{D^3}\cdots\right)+\frac{t^2}{c^2}\left(\frac{1}{3}+\frac{1}{3}\frac{c}{D}+\frac{1}{4}\frac{c^2}{D^2}-\frac{1}{12}\frac{c^3}{D^3}-\frac{157}{192}\frac{c^4}{D^4}-\frac{1{,}271}{576}\frac{c^5}{D^5}\cdots\right)\right] \tag{37}$$

Since terms in t^4 and higher powers of t are omitted in equation 37, the following formula for thick coils may be used instead of 36 and 37:

$$H_{r\infty}=\frac{nIc}{10t}\left[\left(\operatorname{logn}\frac{16D^2}{c^2}\right)\left(1-\frac{1}{2}\frac{c}{D}+\frac{1}{12}\frac{c^2}{D^2}+\frac{1}{16}\frac{c^3}{D^3}+\frac{17}{320}\frac{c^4}{D^4}+\frac{19}{384}\frac{c^5}{D^5}\cdots\right)-2+\frac{1}{2}\frac{c}{D}+\frac{5}{9}\frac{c^2}{D^2}+\frac{11}{96}\frac{c^3}{D^3}+\frac{7}{4{,}800}\frac{c^4}{D^4}-\frac{71}{1{,}440}\frac{c^5}{D^5}\cdots\right]_{c=c_1}^{c_2} \tag{38}$$

If y is less than a_1, Figure 3, then c_1 and c_2 are negative. If y has a value between that of a_1 and a_2, then c_1 is negative and c_2 is positive.

Formulas for Long Coils

By an integration of equation 20 from $x=x$ to ∞, there is obtained the following formula for the axial flux density at a point P in the end plane of a solenoid of length x, for cases in which $r=\sqrt{(x^2+y^2)}$ is greater than about $1.25a$ (see Figures 3 and 5):

$$H_{x(\text{coil})}=H_{x\infty}-\frac{\pi nI}{10}\left[\frac{a^2}{r^2}P_1\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{12a^2}\right\}-\frac{3}{4}\frac{a^4}{r^4}P_3\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{2a^2}+\frac{t^4}{80a^4}\right\}+\frac{3\times5}{4\times6}\frac{a^6}{r^6}P_5\left(\frac{x}{r}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{t^6}{448a^6}\right\}-\frac{3\times5\times7}{4\times6\times8}\frac{a^8}{r^8}P_7\left(\frac{x}{r}\right)\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{t^6}{16a^6}+\frac{t^8}{2{,}304a^8}\right\}\cdots\right] \tag{39}$$

See reference 12. The value of $H_{x\infty}$ is given by equations 28, 29, or 30, depending on the value of y.

By integrating equation 21 from $x=x$ to ∞, the corresponding formula for radial flux density is obtained:

$$H_{r(\text{coil})}=H_{r\infty}-\frac{\pi nIy}{10a}\left[\frac{a^3}{r^3}P_1'\left(\frac{x}{r}\right)\times\left\{1+\frac{t^2}{12a^2}\right\}-\frac{1}{4}\frac{a^5}{r^5}P_3'\left(\frac{x}{r}\right)\left\{1+\frac{t^2}{2a^2}+\frac{t^4}{80a^4}\right\}+\frac{1\times3}{4\times6}\frac{a^7}{r^7}P_5'\left(\frac{x}{r}\right)\left\{1+\frac{5}{4}\frac{t^2}{a^2}+\frac{3}{16}\frac{t^4}{a^4}+\frac{t^6}{448a^6}\right\}-\frac{1\times3\times5}{4\times6\times8}\frac{a^9}{r^9}P_7'\left(\frac{x}{r}\right)\left\{1+\frac{7}{3}\frac{t^2}{a^2}+\frac{7}{8}\frac{t^4}{a^4}+\frac{t^6}{16a^6}+\frac{t^8}{2{,}304a^8}\right\}\cdots\right] \tag{40}$$

where $r^2=x^2+y^2$. See reference 11, equation 5.

The value of $H_{r\infty}$ is given by equations 31–38 and sometimes two of these formulas can be used to check each other. If in Figure 3, $x_1^2+y^2$ and $x_2^2+y^2$ are so large that equation 40 is used for both, then it is evident that in the subtraction of the two results $H_{r\infty}$ cancels out and so does not need to be computed.

The general expression for the brackets in t is

$$\frac{a}{mt}\left\{\left(1+\frac{t}{2a}\right)^m-\left(1-\frac{t}{2a}\right)^m\right\} \qquad (41)$$

where $m=3$, 5, 7, and 9 for equations 39 and 40 as far as shown. They are not infinite series, but are complete.

From equations 22 and 23

$$H_{x(\text{coil})}=H_{x\infty}-\frac{2\pi nI}{10}\left[1-\frac{x}{\rho}-\frac{a^2}{\rho^2}\times\left\{\frac{1}{2\times2}\frac{y^2}{\rho^2}P_2'\left(\frac{x}{\rho}\right)-\frac{1\times3}{2\times4\times4}\frac{y^4}{\rho^4}P_4'\left(\frac{x}{\rho}\right)+\frac{1\times3\times5}{2\times4\times6\times6}\frac{y^6}{\rho^6}P_6'\left(\frac{x}{\rho}\right)\ldots\right\}+\frac{t^2x}{\rho^3}\times\left\{\frac{1}{24}\frac{x^2}{\rho^2}-\frac{1}{12}\frac{a^2}{\rho^2}-\frac{y^2}{\rho^2}\left(\frac{1}{16}\frac{x^4}{\rho_4}-\frac{21}{32}\frac{x^2a^2}{\rho^4}+\frac{3}{8}\frac{a^4}{\rho^4}\right)\ldots\right\}\right] \qquad (42)$$

$$H_{r\,(\text{coil})}=H_{r\infty}-\frac{\pi nIa^2}{10\rho^2}\left[\frac{y}{\rho}P_1'\left(\frac{x}{\rho}\right)-\frac{1}{4}\frac{y^3}{\rho^3}P_3'\left(\frac{x}{\rho}\right)+\frac{1\times3}{4\times6}\frac{y^5}{\rho^5}P_5'\left(\frac{x}{\rho}\right)-\frac{1\times3\times5}{4\times6\times8}\frac{y^7}{\rho^7}P_7'\left(\frac{x}{\rho}\right)+\ldots+\frac{t^2y}{a^2\rho}\left(\frac{1}{12}\frac{x^4}{\rho^4}-\frac{11}{24}\frac{x^2a^2}{\rho^4}+\frac{1}{12}\frac{a^4}{\rho^4}\right)-\frac{t^2y^3}{a^2\rho^3}\left(\frac{1}{8}\frac{x^6}{\rho^6}-\frac{17}{8}\frac{x^4a^2}{\rho^6}+\frac{159}{64}\frac{x^2a^4}{\rho^6}-\frac{3}{16}\frac{a^6}{\rho^6}\right)\ldots\right] \qquad (43)$$

where $\rho^2=x^2+a^2$

Since terms in t^4 and higher powers of t have been omitted from equations 42 and 43, the following equations may be used for thick coils:

$$H_{x(\text{coil})}=H_{x\infty}-\frac{2\pi nI}{10}\left[1-\frac{x}{t}\left\{\operatorname{logn}\frac{a_2+\rho_2}{a_1+\rho_1}+\left(\frac{1}{4}\frac{y^2}{x^2}-\frac{5}{16}\frac{y^4}{x^4}\right)\left(\frac{a_2^3}{\rho_2^3}-\frac{a_1^3}{\rho_1^3}\right)+\frac{33}{64}\frac{y^4}{x^4}\left(\frac{a_2^5}{\rho_2^5}-\frac{a_1^5}{\rho_1^5}\right)-\frac{15}{64}\frac{y^4}{x^4}\left(\frac{a_2^7}{\rho_2^7}-\frac{a_1^7}{\rho_1^7}\right)+\text{terms in higher powers of } y\right\}\right] \qquad (44)$$

$$H_{r(\text{coil})}=H_{r\infty}-\frac{\pi nIy}{10t}\left[\operatorname{logn}\frac{a_2+\rho_2}{a_1+\rho_1}-\frac{a_2}{\rho_2}+\frac{a_1}{\rho_1}-\left(\frac{1}{2}\frac{y^2}{x^2}-\frac{5}{8}\frac{y^4}{x^4}\right)\left(\frac{a_2^3}{\rho_2^3}-\frac{a_1^3}{\rho_1^3}\right)+\left(\frac{3}{8}\frac{y^2}{x^2}-\frac{27}{16}\frac{y^4}{x^4}\right)\left(\frac{a_2^5}{\rho_2^5}-\frac{a_1^5}{\rho_1^5}\right)+\frac{105}{64}\frac{y^4}{x^4}\times\left(\frac{a_2^7}{\rho_2^7}-\frac{a_1^7}{\rho_1^7}\right)-\frac{35}{64}\frac{y^4}{x^4}\left(\frac{a_2^9}{\rho_2^9}-\frac{a_1^9}{\rho_1}\right)+\text{terms in higher powers of } y\right] \qquad (45)$$

where $\rho_1^2=x^2+a_1^2$ and $\rho_2^2=x^2+a_2^2$.

See also reference 1, edition of 1921, equation 28, page 222 (change $5a$ to $5a^2$) and equation 31, page 225. Note that $r_1^2=a^2+(x+b/2)^2$ and $r_2^2=a^2+(x-b/2)^2$ where b is the length of the solenoid.

The following are useful formulas for zonal harmonics:

$$P_0(\mu)=1;\qquad P_1(\mu)=\mu;$$
$$P_2(\mu)=\frac{1}{2}(3\mu^2-1);$$
$$P_3(\mu)=\frac{1}{2}(5\mu^3-3\mu);$$
$$P_4(\mu)=\frac{1}{2\times4}(5\times7\mu^4-2\times3\times5\mu^2+1\times3);$$
$$P_5(\mu)=\frac{1}{2\times4}(7\times9\mu^5-2\times5\times7\mu^3+3\times5\mu);$$
$$P_0'(\mu)=0;\qquad P_1'(\mu)=1;$$
$$P_2'(\mu)=3\mu;\qquad P_3'(\mu)={}^1/_2(3\times5\mu^2-1\times3);$$
$$P_4'(\mu)=\frac{1}{2}(5\times7\mu^3-3\times5\mu) \qquad (46)$$

It is to be noticed that in the formulas, ratios of dimensions occur almost entirely and in such ratios, dimensions in inches may be used throughout instead of dimensions in centimeters, since powers of 2.54, the conversion factor, would occur equally in the numerator and denominator of a ratio and would cancel out. However, the letter n means turns per centimeter and not turns per inch. The letter N means turns per coil.

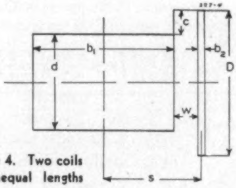

Figure 4. Two coils of unequal lengths

The laboratory measurements which were made agreed with calculated values within a very few per cent.

Example I. The following case, near to the meeting-point of three boundaries, enables one formula to be checked by the others: Find the radial component of field in the end plane of a solenoid where

$x/a=0.9$, $y/a=0.95$, $t/a=0$
$c/a=-0.05$

By equation 9, $H_{r(s)}=5.36\dfrac{nI}{10}$

By equation 36, $H_{r(s)\infty}=6.26\dfrac{nI}{10}$

By equation 40, $H_{r(s)}=6.26-\pi\times0.95(0.446-0.133-0.033+0.020+0.007\ldots)\dfrac{nI}{10}=5.35\dfrac{nI}{10}$

By equation 43, $H_{r(s)}=6.26-\dfrac{\pi}{1.81}\times(0.706-0.164-0.044+0.020+0.007\ldots)\times\dfrac{nI}{10}=5.35\dfrac{nI}{10}$

Example II. The following problem also can be computed by three different formulas. Find the horizontal component of the field in the end plane of the following solenoid:

$x/a=0.4$, $y/a=0.6$, $t/a=0$
$c/a=-0.4$

By equation 3, $H_{x(s)}=2.90\dfrac{nI}{10}$

By equation 25, $H_{x(s)}=2\pi\dfrac{nI}{10}0.721\,(0.555+0.105-0.004-0.013-0.004)=2.90\dfrac{nI}{10}$

By equation 29, $H_{x(s)\infty}=2\pi\dfrac{nI}{10}$

By equation 42, $H_{x(s)}=\left[2\pi-2\pi\times\left\{1-0.3712-\dfrac{1}{1.16}(0.0864+0.0171+0.0023-0.0001)\right\}\right]\dfrac{nI}{10}=2.90\dfrac{nI}{10}$

Example III. A solenoid consists of a single layer of fine wire, $a=0.971$ inch; $x=18.25$ inches; $y=0$. The search coil is in the calibrating position, at the center of the solenoid. By equations 29 and 42

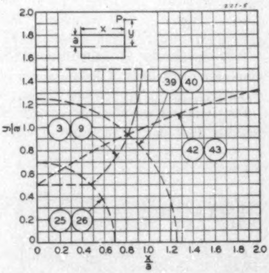

Figure 5. Approximate ranges of application of formulas for magnetic field in end plane of a solenoid

$$H_{x(s)}=\frac{2\pi nI}{10}\left[1-1+\left(1+\frac{a^2}{x^2}\right)^{-1/2}\right]=\frac{2\pi nI}{10}\left[1-\frac{1}{2}\frac{a^2}{x^2}\ldots\right]=\frac{2\pi nI}{10}[1-0.0014]$$

due to the half coil, which is 0.14 per cent less than the nominal value for a very long solenoid.

It is evident that, for accurate work, the calibration of search coils, or other measurements depending on the magnetic field in the middle of a long solenoid, should include a correction according to the formulas in this paper of the nominal field $4\pi nI/10$ in the middle of the solenoid.

Example IV. Find the radial component of the field in the end plane of the solenoid of example III, at eight inches from the axis.

In this problem, x is equal to the full length of the solenoid, 36.5 inches.

By equation 31, $H_{r(s)\infty}=\frac{\pi nI}{10}\times 0.01482$

By equation 43, $H_{r(s)_0}=$

$$\frac{\pi nI}{10}\,(0.01482-0.00014)$$

Only a very few terms in each series are needed. The turns per centimeter are 7.57 and the current in the test was 1.70 amperes, giving $H_{r(s)}$ by equation 43 $=0.0593$ lines per square centimeter. Test value $=0.0589$ (measurement by M. F. Miller).

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Aircraft Voltage Regulator and Cutout

R. C. JONES
NONMEMBER AIEE

D. W. EXNER
ASSOCIATE AIEE

SHERWIN H. WRIGHT
MEMBER AIEE

Synopsis: In the electrical system on aircraft, the voltage regulator and reverse-current cutout are key pieces of equipment which, in addition to having requirements of utmost dependability, must be small, light in weight, and of a design adaptable to manufacture on a quantity production basis. The conception and simplification of the prime requirements and their attainment by careful analytical designing is described in this paper in such a way as to show not only the steps in design but also the pertinent application features.

REQUIRED: A voltage regulator which weighs less than one-fifth its nearest relative in the industrial field, and a reverse-current cutout weighing less than one-sixth an industrial contactor of equivalent current rating. Such were some general requirements of the generator voltage regulator and cutout whose design and development are described in this paper. This development involved a careful combination of electrical and mechanical engineering principles and necessitated continuous attention being paid to the application requirements. The magnitude of the design problem involved is emphasized by stating that in addition to all those problems normally encountered there are the problems created by the prime requirements of low weight and small size, as well as those created by atmospheric conditions. In this paper the voltage-regulator is taken up first, and this is followed by the reverse-current cutout.

Mr. T. B. Holliday in the paper "Application of Electric Power in Aircraft" (ELECTRICAL ENGINEERING, volume 60, May 1941, pages 218–25) gave a very comprehensive picture of the problems and requirements of electric equipment for aircraft, as seen by a member of the materiel division, Wright Field, United States Army Air Corps. A review of that paper provides an excellent foundation for appreciation of specific design problems such as treated in this present paper.

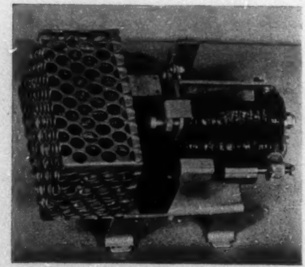

Figure 1. View of voltage regulator showing resistors on left and operating magnet on right

Voltage Regulator

The aircraft voltage regulator described in this paper is shown in Figure 1 and is suitable for operating with any approved 28.5-volt d-c self-excited generator whose maximum field current (at full load and minimum speed) is not more than eight amperes, and whose minimum field current (at no load and maximum speed) is 0.5 ampere. A schematic diagram is shown in Figure 2. Additional specific requirements are:

1. Hold voltage within ±2 per cent of 28.5 volts. This must be maintained over the range from no-load to full-load generator current, while the generator speed may vary from 2,500 to 4,500 rpm.
2. Radio interference must be held to a minimum.
3. Satisfactory operation with air conditions varying over a range of: −40 degrees centigrade to +60 degrees centigrade in temperature, sea level to 35,000 feet in altitude 10 to 90 per cent in relative humidity.
4. Must fit on a standard quick-mounting base which includes all electrical connections.
5. Dimensions including base must not exceed $6^{1}/_{2}$-inch length, 4-inch width, and $3^{7}/_{8}$-inch depth, and of this a total space of $2^{1}/_{2}$ inches by 4 inches by 1 inch must be kept free for mounting base terminal studs and cables.
6. Weight must not exceed 2.5 pounds.
7. Current-carrying capacity of contacts = 15 amperes continuous.
8. Regulator to vary the field resistor between 0.25 ohm and 75 ohms in such a way that generator-voltage surges do not exceed three volts.
9. Regulator must operate satisfactorily with as many as five generators and regulators connected in parallel and provide equal division of load. Maximum deviation from the average value of load must not exceed 10 per cent of generator full-load rating.

The above requirements call for a rather unusual piece of equipment involving major development. It might well be added that neither the attaining of such requirements nor their prior recognition as key characteristics are matters that could be or were settled in a moment or by superficial analysis or study. Rather, the setting down of the requirements and their practical attainment were the result of the engineers of the materiel division keeping a step ahead of the best efforts of the manufacturer's design engineers, thus leading to the final result in a minimum of time.

The voltage-regulator is conveniently divided into four parts in this discussion:

1. Contact device.
2. Operating magnet.
3. Resistor.
4. Base.

These four parts will be treated separately and in that order.

CONTACT DEVICE

A multicontact type of regulator was chosen as being best fitted to meet the requirements. The Silverstat unit illustrated in Figure 3 is an industrial type of multicontact device which has proven itself by years of service on innumerable applications. It consists essentially of a number of spring bronze leaves with built-in silver contacts at the movable end of each leaf. At the stationary or clamp end of the assembly short strips are used between each leaf to both insulate and space them from each other. The movable ends of the leaves lie against an insulation block whose face is cut at an angle such that, in the free position, all contacts are open and equally spaced. The opening force on each contact is dependent upon the

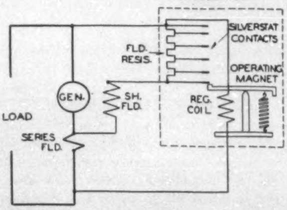

Figure 2. Schematic diagram of voltage regulator

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R. C. JONES is transportation control engineer, D. W. EXNER small motor division engineer, and SHERWIN H. WRIGHT new products division engineer, all with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

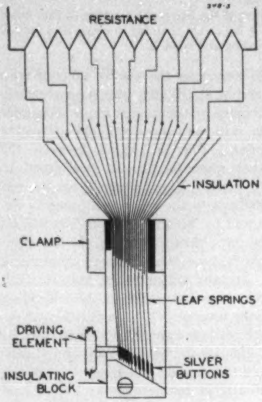

Figure 3. Industrial-type Silverstat unit

tension in its own leaf. Since the leaves are spaced approximately $^{1}/_{64}$ inch apart, and since accurate response to small changes in voltage is necessary, the leaf material must be straight and must have very consistent spring characteristics. On the standard Silverstat assembly, which has been in production for industrial applications for some years, it has been found necessary to maintain a very careful control of the leaf material in order to insure proper leaf characteristics. The standard leaf material, however, was impractical for the aircraft regulator, as markedly higher conductivity and shorter lengths of leaves operating in a wider range of ambient temperatures were required. In view of this, the successful quest for a new material was a metallurgical achievement of no small moment to the designer.

Other factors and problems entering into the design of the contact unit were those connected with the generator itself. In the first place the generators to be regulated were themselves in the development stage. Calculated generator characteristics had to be checked, and the designs changed to be suitable for operation in a regulated voltage system. Changes were being made in the generator specifications giving new field-current limitations. Initially, consideration was given to limiting the total field current to 12.5 amperes and allowing the use of two field circuits both on the generator and the regulator. Final specifications limited the field current to eight amperes and required that the regulator use only one circuit which would vary the field resistor from 75 ohms to 0.25 ohm in steps that would not produce surges higher than three volts at the generator terminals.

A double-deck construction of the contact assembly is used to obtain the necessary number of contact leaves in minimum space. This also reduces the movement required between the all-open and all-closed position of the contacts to one-half of the standard construction which has all leaves in line. The full number of steps is obtained by alternating the operation of the contact leaves in the upper and lower stacks. The mechanical assembly of this double-decked Silverstat is accomplished by the preassembly of the leaves and insulation in a fixture which establishes the correct relation of parts and cements them together. After being baked to fix the cement, these stacks are placed in the double-deck frame and adjusted to proper relation and spring tension. Each Silverstat assembly is checked in a pressure-measuring device and adjusted to have the same corresponding leaf in each stack operate at the same pressure. This is necessary to obtain consistent results required in quantity production.

OPERATING MAGNET

The operating magnet supplies the activating force for closing or releasing as many leaves and steps as is required to establish a voltage within the chosen limits. It must respond to small changes in voltage, moving only enough to readjust the field circuit resistance to the proper value, and it must be small and of such form as to fit in compactly with the other units. Temperature effects must be compensated for, and residual magnetism kept to a minimum. The moving member must be shockproof.

A clapper or moving-iron type is the form of magnet chosen for this application. See Figure 1 which shows the armature at the left of the coils. Two long slender coils are employed in order to obtain the lowest winding weight. The stationary part of the magnetic circuit is arranged in the form of a *U* with a coil on each leg. The armature which completes the circuit is pivoted parallel to the line of the two coils. To reduce the number of parts and obtain a unit assembly, the pivot support is made by extending the flat section of iron connecting the two cores, parallel to the coils to a point approximately even with the open end of the magnetic circuit, then bending this extension at right angles. To minimize weight, the extended section of frame is cut out, leaving only a skeleton form having sufficient mechanical strength. This is possible since the material is not in the magnetic circuit.

To obtain dependable as well as frictionless pivot action, a leaf spring is used. The armature itself is made of a single flat piece of iron cut out around the pivot for free action and extending beyond the pivot to provide for attachment of the calibrating spring and to act as a counterbalance. An insulation angle is attached to the coil side of the armature to act as the contact operating member. The stationary end of the calibration spring is supported by a strip of bimetal attached to the frame.

Adjustment of the calibration spring is obtained by a novel scheme using a minimum number of parts, and is best described with the aid of Figure 4. A hole is punched in the stationary support to fit a threaded stud with one side machined flat; a small point is lanced out of the support in proper relation to the hole to engage with notches on the circumference of the adjusting nut. With the regulator magnet completely assembled, the tension of the calibrating spring holds the adjusting nut in engagement with the point, locking it securely. To change the adjustment it is necessary to tip the adjusting nut and stud just enough to disengage them from the locking projection, then rotate the adjusting nut. The nut is made from a standard pinion stock rod and the fineness of adjustment is determined by the number of teeth. The final choice of:

1. Thread for the adjusting stud of the calibrating spring.
2. Teeth on the adjusting nut.

resulted in a design giving 0.05 ounce change in adjustment per notch or tooth.

The design of the calibrating spring is complicated by the building up of the contact assembly load when the spring loading decreases, also by the fact that the contact assembly load is high in proportion to the magnet pull.

With the lightweight clapper-type magnet it is impractical to obtain stable regulator operation without some form of damping. To obtain damping, trial was

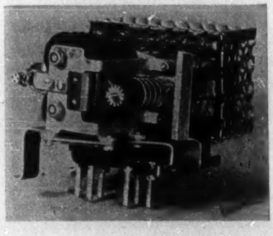

Figure 4. End view of voltage regulator

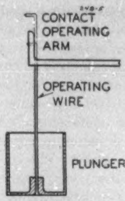

Figure 5(left). Dashpot attachment

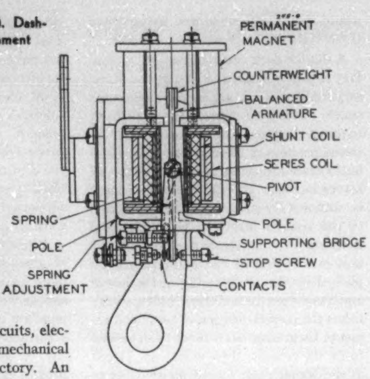

Figure 8 (right). Simplified view of reverse-current relay structure showing balanced armature and location of polarizing magnet

made of electrical damping circuits, electrodynamic dampers, and mechanical dampers, but none was satisfactory. An air dashpot was tried and found to have very desirable characteristics, and it gave excellent results. Both the piston and plunger of this dashpot are made of stainless steel. This minimizes the effects of differential expansion due to temperature, and of corrosion.

Because the end of the armature moves in an arc, and the dashpot plunger in a straight line, it was necessary to provide a connection between them which would not set up a side pressure on the stationary cylinder. Common practice is to include a universal joint in an operating-rod assembly of this type, but such construction is subject to friction and play, both of which are detrimental to accurate regulation. To avoid these faults a long piece of spring wire is employed as a column, one end being soldered in the plunger and the other end slipped through a close-fitting hole in the contact-operating arm. See Figure 5. This same end is bent at a right angle and is sprung to go into another close-fitting hole in the angle of the arm. This results in a tight connection with minimum weight and number of parts.

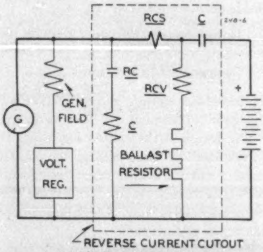

Figure 6. Schematic diagram of reverse-current cutout

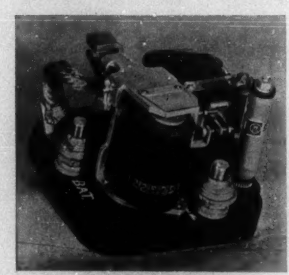

Figure 7. Reverse-current cutout

To allow maximum tolerance on the plunger and cylinder diameters, and thus facilitate manufacture, a needle valve is provided for adjusting the restraining effect of the dashpot. The cylinder is mounted on a stud extension from the main body, and the needle valve located inside this stud, thus adding only one part and no weight. Friction is minimized by putting a mirror-smooth polish on the outside of the plunger and the inside of the cylinder, and by use of a carefully chosen lubricant.

Because of the wide variation in operating temperature encountered by aircraft, considerable thought was given to the best method of compensating for the temperature range produced by both ambient change and coil heating. The temperature effect in this regulator is limited by winding the coils with an alloy wire having a temperature coefficient of approximately one-eighth that of copper and a specific resistance of six times that of copper. This gives heavier coils but eliminates the use of an external resistor. It also eliminates the problems of:

1. Finding a negative temperature coefficient resistor to suit this application.
2. A construction for keeping the temperatures of the two parts together so that there is a minimum time lag in correcting for coil heating and cooling.

To correct for the smaller effect of temperature on the alloy wire, a bimetal strip mounted on the magnet frame is used for the stationary support of the calibrating spring. This is located between the points where the two coil cores attach to the frame. Heat transfer from the coil windings to the cores and frame is improved by winding the coils directly on the cores, thus eliminating the heavy insulating tube and air space required where a separate self-contained coil is used. The bimetal strip reduces the calibrating spring tension with a rise in either coil or ambient temperature. It is necessary to co-ordinate the bimetal support and calibrating-spring design closely to gain the ultimate in compensation. The scheme used provides regulation within the required limits under all operating conditions.

RESISTOR DESIGN

The field resistor presents two separate problems. The first is to obtain a form that will be light enough, work into a compact unit and have taps which are accessible for wiring. The problem was solved only by the wholehearted co-operation of the resistor manufacturer. The original estimate of the resistor weight for the 200-ampere generator was one pound, leaving $1^1/_2$ pounds for the regulator mechanism and mounting base. The final resistor design weighing one-half pound was reached only after intensive study of operating conditions, and by many tests.

The second problem is that of designing the resistor with the right resistance steps to give even voltage response from the generator without overloading the contacts. This requires the co-ordination of generator characteristics with the regulator response and balancing these against practical voltage and volt-ampere limitations of the contacts. The work on this problem was complicated by the changes in generator design and by the requirement that the regulator be satisfactory for regulating any Air Corps approved generators of the 50-, 100- and 200-ampere capacities, without reconnection of the resistor. The characteristics of the various sizes of generators had to be studied, and the resistor steps changed to the best over-all compromise. The resistor steps were of such low value in the high current zone that a ribbon type of resistor material had to be used. This is corrugated after the fashion of the standard Ribflex round resistor tubes, but wound on a flat earthenware form and held in place by ceramic glaze.

MOUNTING BASE

The regulator base presents no special problems, since the other parts are designed as individual units, all mounting from the same flat surface. The base is, therefore, flat except for the special lugs for mounting on the standard subbase. It is cut out for weight reduction and for maintenance purposes and carries a special contact assembly needed to complete electrical circuits to the mounting panel. A perforated metal cover is furnished to protect the resistor.

The complete regulator in its final form as shown in Figures 1 and 4, weighs less than 2.0 pounds, is $5^{15}/_{16}$ inches long, 4 inches high, and $3^{3}/_{4}$ inches deep. It has passed all laboratory tests successfully, operated satisfactorily on test flights and is now in quantity production.

Reverse-Current Cutout

The demand for a reverse-current cutout to handle the 100- and 200-ampere 28.5-volt generators arose coincident with the need for a voltage regulator. In June 1940 the materiel division issued a tentative specification for a 100-ampere device weighing less than two pounds and a 200-ampere device with a 2.5-pound limit. For reasons explained later, the 100-ampere rating has since been abandoned.

While the aircraft cutout performs the same function as the well-known automobile cutout, namely to connect the generator to the battery and load when the generator voltage is sufficient to charge the battery, and to disconnect it when the voltage drops below the charging point, conditions peculiar to the aircraft electrical system demand a higher standard of performance. In particular, the storage battery has a much lower ampere-hour capacity in proportion to the generating capacity, making a low reverse-current dropout setting desirable. Because operation of two or more generators in parallel is usual, a pickup voltage as close as possible to the normal regulated voltage of the generators is necessary to prevent excessive chatter in closing. This requirement results in close specification of pickup voltage tolerances.

The tentative specification called for opening at a reverse-current of not more than five per cent of the continuous rating, and set the pickup voltage between 26.8 and 27.2 volts, or an accuracy of plus or minus three quarters of one per cent. It was further specified that the device should be capable of opening a current equal to 150 per cent of its rating. Maximum dimensions were set at four by four by four inches. These cutouts may frequently be mounted close to the main engines where severe vibration is encountered. To ensure sound mechanical construction a ten-hour vibration test is specified, with a total excursion of 0.07 inch and a frequency varying periodically between 10 and 55 cycles per second.

During the course of the development, modifications of the requirements narrowed down the original problems and introduced new ones. The first addition was the requirement that the cutout should not close on application of reversed voltage. The arc-rupturing capacity of the 200-ampere unit was increased to 500 amperes. An over-all millivolt drop was included and finally whittled down to 85 millivolts at rated current, with the further provision that this must not exceed 95 millivolts after 100 operations of the arc-rupturing test. On the basis of preliminary models submitted by manufacturers, the materiel division reduced the weight limit of the 200-ampere cutout to 2.25 pounds.

Past experience made it clear from the beginning of the development that the measuring and circuit-operating functions should not be combined in one structure, but rather that the design should comprise a relay in addition to a contactor. It is impractical to provide high calibration accuracy in a device which also must carry and efficiently rupture high current. In addition to the greater friction and contact wear effects, the calibration change with temperature cannot readily be controlled without an objectionable increase in weight. The necessity for polarization also influenced the decision to use a separate measuring relay. Figure 6 gives schematically the circuit of such a device consisting of a relay having a shunt voltage-measuring coil *RCV*, a series current coil *RCS*, and contacts *RC* energizing the coil of contactor *C*. Figure 7 shows the cutout in final form.

RELAY

The term "polarization" presupposes the existence of a reference factor of constant direction with which to compare the generator voltage. This factor may be voltage from a battery, the unidirectional conduction of a rectifier, or the fixed polarity of a permanent magnet. For the purpose of a reverse-current cutout, the use of battery voltage as a reference is the most direct line of attack and has the definite advantage of preventing circuit closure when either the battery or generator is reversed. It has the disadvantage of draining current from the battery when the generator is idle. Variations in battery voltage have an adverse effect on the accuracy of calibration.

Use of a rectifier as a reference, while satisfactory for many applications, in general involves a weight handicap. Dry-plate rectifiers, in a case of this kind, must be applied with caution because of the wide range of temperatures which may be expected. High ambient temperature, coupled with normal internal losses, may result in failure. The change in rectifier characteristics with temperature may be reflected in calibration errors, especially at sub-zero temperatures where all dry-plate rectifiers show a large increase in forward resistance. The fact that such rectifiers have a negative temperature coefficient can be used to advantage to balance the positive coefficient of copper windings over a moderate temperature range. Over a wide range this is less practical because the rectifier resistance does not change linearly with change in temperature.

Polarization by a permanent magnet overcomes the disadvantages of the other two methods. Proper application of the better magnetic materials which have been developed in recent years provides a reference polarity of stability adequate to meet the requirements. The proportions of the permanent magnet may be selected so that, with a given permeance of the associated magnetic circuit, the desired flux is obtained with minimum weight. It should be so located as to minimize the demagnetizing effect of external fields, particularly the field of the relay series coil.

The high sensitivity of a polarized relay recommends it for this service. The required sensitivity is determined by the specified reverse-current and the turns in the series coil. Minimum weight demands that the relay operate with a single-turn series coil for the highest current rating for which the frame is intended. Multiple-turn coils may then be used for lower ratings. The relay used in this cutout was designed to have a single-turn coil in the 200-ampere rating, so that with the reverse-current specified as 10 amperes, it must drop out on a 10-ampere-turn reversal as a maximum limit. Reasonable manufacturing and testing tolerances require a design value of six or seven ampere-turns.

For positive contact operation a snap action is necessary in opening and closing. This requires that the armature, in closing, move into a denser magnetic field where the force is greater than that required to move it out of its initial position. The reduction in ampere-turns required to decrease the pull to the drop-out value is produced by the bucking action of the series coil. Experience has shown that a drop-out-to-pickup ratio of 0.8 is near the maximum which will provide satisfactory snap action. If we use this value and a drop-out-to-pickup differential of seven ampere-turns, the correct pickup value will be approximately 35 ampere-turns.

Because of the vibration and shock conditions encountered on aircraft, as well as the high acceleration forces produced by maneuvering and flight in rough air, full static balance is essential on the moving parts of all accurate relays. If the relay structure is not inherently balanced, counterweights must be added even though they mean an objectionable increase in weight. The magnetically polarized relay is easily modified into an inherently balanced mechanical structure by the use of a center-pivoted armature having a working air gap at each end. This requires the addition of sufficient counterweight to balance the weight of the light moving contact only.

Figure 8 shows the details of the relay in its final form. The center-pivoted armature operates between the two *U*-shaped poles which are polarized by the cobalt steel magnet. The armature and poles are made of hydrogen-annealed Hipernik magnetic alloy to minimize hysteresis which would otherwise be objectionable in a low energy relay. To avoid the critical adjustment required in assembling cone pivots without damage or looseness, small diameter polished pin bearings are used. The shaft is 18-8 stainless steel running in bronze bearing screws. A stainless-steel torsion spring with screw adjustment contributes to the shock resistance of the relay and gives latitude of correction for manufacturing variations. A stamped brass bridge supports the armature, spring, and stop screw, so that the whole assembly may be removed after taking out the screws which fasten it to the poles. The combined shunt and series coil spool surrounds the armature and is contained within the poles, the whole assembly being supported from the base by an aluminum mounting frame.

CONTACTOR

The contactor magnetic circuit is of conventional clapper design, but the contact structure is turned around from the usual position in order to place the pivot near the center of gravity. This reduces the armature spring strength required to provide stability under shock, and so somewhat reduces the weight of the magnetic circuit.

When large currents are to be interrupted it is important to remove the arcing from the main contact surfaces to prevent a rise in millivolt drop after repeated operations. Auxiliary arcing tips, opening after the main contacts have separated, are sometimes used for this purpose, but they have several disadvantages. In lightweight devices there is a tendency to make them too weak so as to add as little as possible to the magnet load. They are, therefore, vulnerable to mechanical damage in handling and may not always be effective. Being electrically in parallel to the main contacts they carry a portion of the main current. Gritty dust deposited on the main contacts may cause all of the main current to pass through the arcing tips; a load which they seldom can be designed to carry for more than a short time.

A more satisfactory solution is the conventional rolling contact wherein the final opening always occurs at the tip, leaving the contact heel clean. By varying the kinematic layout, any reasonable value of wipe or scrubbing action may be obtained to keep the contacts clean and break through dust deposits. This is accompanied by a strong toggle action capable of prying apart contacts partially welded by heavy overload. Almost universal use of this construction on industrial contactors indicates its superiority.

The moving contact is an extruded copper shape with a silver-alloy contact face. This part is sufficiently balanced with respect to its pivot so that the spring is able to maintain adequate contact pressure against vibration accelerations in excess of those obtained in the specification test. The flexible copper shunt is brazed directly to the extruded contact by an electrically-controlled spotwelding process using a water spray to prevent annealing or oxidation of the shunt strands. To minimize the bending action as the contactor operates, the point of shunt attachment is located close to the contact pivot.

The silver-alloy-faced stationary contact is carried by a steel extension of the magnet frame, giving a rigid support and making the whole contactor a self-supported unit. The stationary contact structure is so shaped with respect to the moving contact that the current path forms a sharp loop as the contacts separate. This produces a noticeable magnetic blowout action which is particularly useful at currents of 100 amperes and higher.

Instead of using the conventional arrangement with separate armature and contact springs, both functions are performed by one stainless-steel tension spring. Elimination of the extra parts reduces the size and weight and simplifies the assembly. The characteristics of any usual double-spring combination may be essentially duplicated with care in the selection of the factors of spring stiffness, lever arm lengths, and pivot locations. Contact force of one pound is obtained in the sealed position, measured at the contact tip.

Previous to the design of the magnetic circuit, complete magnetic torque data were taken on a somewhat smaller frame having similar proportions. These data were extrapolated to obtain the proportions of a magnet having a torque curve fitting the desired torque curve of the contact and spring system. Tests on a model showed that the first approximation was fairly close, requiring only determination of the best core head and body diameters to obtain the optimum design. The critical portion of the torque-versus-distance curve is the point at which the contacts initially touch. As it is unreliable to depend on kinetic energy to carry the armature over this point, sufficient magnetic torque must be obtained at the minimum design voltage and maximum coil temperature. Best results are obtained with a three-quarter-inch head diameter and a seven-sixteenths-inch core-body diameter. The core end has an integral threaded stud which passes through the frame and is secured by a nut, a construction which eliminates the sharp reduction in core area caused by tapping a fastening screw into the core.

Since it is most economical in weight to limit saturation to the core at the touch point of the torque curve, the frame has a greater cross-sectional area than the core near the junction of the two. The frame area is reduced near the hinge because of the lower leakage flux passing into this portion.

Low reluctance of the unavoidable magnetic gap at the armature hinge is important, since this joint consumes ampere-turns which would otherwise be more usefully employed at the core-head air gap. In addition to minimizing mechanical clearance at this point, an ear is bent down from each side of the armature to utilize the side area of the frame. The magnetic parts are made from low carbon steel and given 925 degrees centigrade annealing in a hydrogen atmosphere to improve the permeability and decrease the residual. The results permit the use of a 0.005-inch-thick residual shim.

With the core diameter and length determined by magnetic considerations, the mean diameter of the coil is determined primarily by the heat dissipation rate. Usual construction methods, involving a coil wound on an insulating tube and slipped loosely over the core, give a high

Analysis of the Application of High-Speed Reclosing Breakers to Transmission Systems

S. B. CRARY
MEMBER AIEE

L. F. KENNEDY
MEMBER AIEE

C. A. WOODROW
MEMBER AIEE

IN recent years many new or improved kinds of apparatus have been made available for increasing the reliability of transmission circuits. These include high-speed relays, high interrupting speed breakers, ground-fault neutralizers, protector tubes, and high-speed reclosing breakers. Though they may be used in combination, ground-fault neutralizers, protector tubes, and high-speed reclosing breakers offer alternate methods for reducing the outages on the transmission circuit. The reliable loading of a transmission circuit may also be increased by introducing intermediate switching stations and by increasing the interrupting speeds of existing breakers.

Considerable technical-application information has been presented to aid in the general understanding and use of most of this equipment. Although many successful applications of high-speed breaker reclosing have been made in recent years, there still appears to be a real need for an analysis of the general possibilities and limitations of high-speed reclosing of both the three-phase and the single-phase types. This paper attempts to present such an analysis.

General Considerations for High-Speed Reclosing

High-speed reclosing circuit breakers have been shown by experience and analysis to be capable of improving the reliability of transmission systems.[1,2,3] Where they may be used to best advantage can be fairly well predetermined by stability studies and analysis of test and experience information. Fundamental to the problem are:

1. Maximum permissible time—after the fault has been cleared—for de-energization of the faulted circuit without loss of synchronism.
2. Minimum permissible time for deionization of the fault arc.

The first depends upon the following factors:

a. System arrangement and design.

b. Amount and distribution of generating capacity.

c. Load being carried on faulted circuit and remainder of system.

d. Type, duration, and location of fault.

The second depends upon many factors, among which are:

a. Fault current and its duration.

b. Length of arc.

c. Number of conductors involved.

d. Tower and circuit configuration.

e. Insulator dielectric strength.

f. Weather conditions.

g. Multiple lightning stroke phenomena.

The first fundamental consideration, the maximum time the faulted circuit may be deenergized without loss of synchronism, can be determined by analysis. It is generally recognized that systems sometimes pull back into synchronism even though they may be reclosed together out of synchronism or at

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S. B. Crary, L. F. Kennedy, and C. A. Woodrow are all of the central station engineering department, General Electric Company, Schenectady, N. Y.

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thermal drop between the two, and depend, therefore, mainly on heat dissipation from the barrel area. By winding the coil directly on the mica-insulated core and impregnating the whole assembly, an excellent thermal joint is obtained, allowing a large portion of the heat to be conducted into the frame and armature for dissipation. The reduction in coil weight by this construction may be as high as 50 per cent, based on designs having equal values of hot ampere-turns and temperature rise as measured by resistance.

The complete contactor, with a rating of 200 amperes and 29 volts continuously, weighs under 14 ounces and is capable of rupturing in excess of the 500 amperes called for in the specification. With its stainless-steel pivot pins operating in bronze bearings it has undergone a mechanical life test of 3,000,000 operations without measurable wear or broken shunt strands.

The contactor and relay designs were co-ordinated to permit combining them on a four-by-four-inch square base with a direct line of connection between the main generator and battery terminal studs so as to minimize the weight of connectors and the over-all millivolt drop, which is less than 75 millivolts at 200 amperes. The base is molded plastic, ribbed for stiffness and minimum weight, and is recessed to keep the main and control terminal posts from turning under wrench forces. Over-all weight of the complete 200-ampere cutout is less than 1.90 pounds. This is under the weight originally specified for the 100-ampere rating. Furthermore, the weights of models submitted by the several manufacturers showed a uniform difference of only a few ounces between the 100- and 200-ampere ratings. Since the weight advantage was so slight the materiel division wisely decided to eliminate the 100-ampere rating and use the 200-ampere size for both the 100- and 200-ampere generators.

Conclusions

Aircraft electrical equipment, such as voltage regulators and cutouts, must meet most of the requirements found in other industrial applications, and in addition must have certain special characteristics. These special characteristics have to do with weight, size, and atmospheric conditions.

While aircraft-application requirements of electrical equipment differ in certain respects from those of other related industrial applications, it is to be expected that the use of equipment specially developed for aircraft service may well be extended to other fields.

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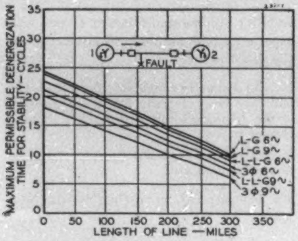


Figure 1. System tie—single-circuit line. Three-phase clearing and reclosing

Sending-end power $= 1.1 \times 2.5(\text{kv})^2$. $H_1 = H_2 = 20$ on kilovolt-ampere base of $2.5(\text{kv})^2$. Refer to appendix I and Figure 13

so large an angular displacement that synchronism is temporarily lost. The severity of the consequent disturbance depends upon many factors, but the phenomenon further supports the advisability of quick reclosing. However, operating experience, in general, indicates that it is not desirable even under emergency conditions, to allow for anything but a short interval of nonsynchronous operation.

The time for deionization of an arc is variable and it becomes good practice to allow as much time as possible for the deionization without encroaching too closely upon the stability limits of the system. Information as regards the deionization time is rather meager. The best data are given in a series of papers showing results obtained on the American Gas and Electric Company systems.[1,2,3] These indicate that a time of six to nine cycles for three-phase clearing and 12 cycles for re-energizing results in about 90 per cent successful operation. This gives a datum point of 12 cycles for deionization time at 138 kv, with three-phase clearing and reclosing. Under some conditions the actual time necessary for deionization will be less than 12 cycles, while under severe conditions it may be more. With single-phase switching the arc on the faulted conductors, after the line breakers have opened, tends to be maintained by the capacitive coupling with the sound phase or phases. Longer times are probably required for deionization of the arc path with single-pole switching.[4] See appendix III. It seems evident that the optimum time for de-energization is not necessarily a fixed value but is determined by system conditions. It is hoped and expected that further tests and experience information will be made available by the operating companies.

If the stability margins are small, it may become justifiable to decrease the allowed time for deionization; while if the stability margins are large, it would appear that a longer time could be allowed. An extreme case of a large stability margin (sufficient parallel circuit strength) would require no reclosing at all. Between the two extremes of stability conditions, represented by a single circuit tie and a circuit with many parallel ties, there exist many conditions and cases where high-speed reclosing of some form may be used to advantage. The aim is to obtain improved reliability over transmission circuits for the loads which those circuits are required to carry. This necessitates a proper evaluation of many factors and is an interesting and practical engineering problem for consideration by those responsible for system design and operation.

With the use of generally available a-c network analyzers, calculations are quite readily made to determine the maximum permissible time for de-energization of the faulted circuit. Such a stability study is an important part of the engineering analysis of the feasibility and desired characteristics for reclosing equipments for the particular system under consideration. Fortunately, however, a certain amount of helpful information can also be obtained by analysis of typical system arrangements and conditions. This was done with the use of a network analyzer. The conditions for the cases studied are given in appendixes I and II. Following is a discussion of the results which have been obtained.

Three-Phase Reclosing—Tie Lines and System Interconnections

A series of cases was taken as representative of interconnections or ties between system areas. The systems on either end of the interconnection were assumed to be of equal capacity having fairly low system impedances (five per cent on $2.5(\text{kv})^2$) corresponding to about 1,000,000 short-circuit kva when the transmission line voltage is 138 kv. The line length was varied from 0 to 300 miles in order to show the effect of change in line length. Both systems at either end of the line were assumed to be solidly grounded. The inertia constants were

Figure 2. System tie—100-mile single-circuit line

Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13

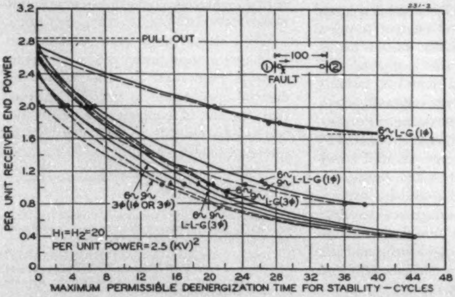
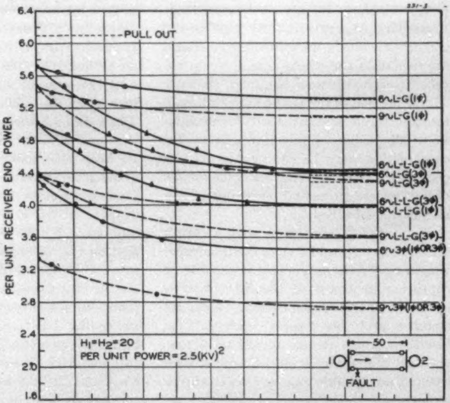


Figure 3. System tie—50-mile double-circuit line

No intermediate switching station. Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13



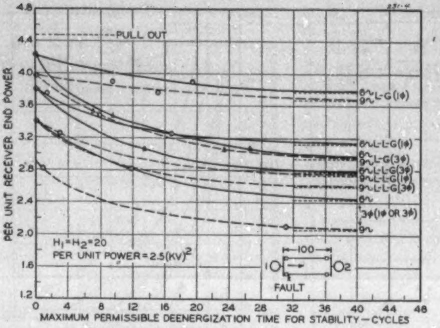


Figure 4. System tie—100-mile double-circuit line

No intermediate switching station. Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13

Table I. Units of Power for Various Line Voltages

Circuit Voltage—Kv	Unit Power in Kw* = 2.5 (Kv)²
287.5	208,000
230	132,000
161	65,000
138	47,500
115	33,000
69	11,900
34.5	3,000

*Corresponds to about the surge impedance or unity power-factor loading of the line when $\sqrt{L_1/C_1} \cong$ 400 ohms.

taken to correspond to systems having a connected capacity equal to about four times the unity power-factor loading (equal to 2.5(kv)²) of a single circuit. These represent relatively small systems compared to those which may be found in practice as ties between major system groups. However, the results may be interpreted to correspond to either larger or smaller systems by a corresponding change in the clearing and de-energization times, as explained in appendix I.

Figure 1 shows the maximum permissible de-energization time for stability versus line length for line-to-ground, double-line-to-ground, and three-phase faults at the sending-end terminal for a power transfer at the sending end corresponding to ten per cent above the unity power-factor or surge-impedance loading of the line. At 138 kv this corresponds to 1.1×2.5(138)² = 53,000 kw. Fault clearing times were taken as six and nine cycles with simultaneous operation at each end of the line. Under these conditions successful operation can be expected for even three-phase faults for line lengths up to 150 miles and 210 miles for a de-energization time of 12 cycles with nine cycles and six cycles clearing, respectively. Successful operation could be expected up to 250 miles for line-to-ground faults with either six or nine cycles clearing. A decrease in the clearing time from nine to six cycles for three-phase faults allows for an increase in the line length of 60 miles for the same de-energization time. It is of interest to note that the type of fault makes an appreciable difference in the allowable de-energization time. These results indicate the importance of keeping the clearing time to a minimum, as this makes possible a further increase in the stability margin of the circuit or an increase in the permissible de-energization times. This has been pointed out previously.[3]

The curves of Figure 1 were drawn for a loading equal to ten per cent above the unity power-factor loading of the line. This is probably the loading to which lines exceeding 150 miles may be loaded. However, for the shorter lengths of line higher loadings may be desired. Fortunately stability characteristics are such as to allow for an increase in loading for shorter lengths with the same de-energization time.

If the sizes of the terminal systems are increased, the resulting increase in the inertia constants produces more stable performance. Such changes can be estimated from Figure 1. For instance if the inertia constants are about double (2.25 times), the six-cycle clearing curve represents the performance with nine-cycle clearing on the new system if the ordinate scale, representing the maximum permissible de-energized time is multiplied by 1.5. The permissible de-energized time for a 150-mile line following the clearing of a three-phase fault in nine cycles is increased from 12 to 22 cycles. Conversely the same load may be transmitted 300 miles, rather than 150 miles, with the same 12 cycles de-energized time.

To illustrate the effect of change of load, the case of 100 miles was analyzed more in detail, results of which are shown on Figure 2. The power in this case is in terms of receiver-end power. The values of per-unit power equal to 1.04 correspond to the case of sending-end power of 1.1 per unit. Table I gives values of per-unit power for various transmission line voltages to be used in the interpretation of the curves.[5] Figure 2 also shows results for single-phase clearing and reclosing. Of particular interest is the very substantial increase in power limit with decrease in the de-energization time with three-phase switching. For three-phase or double-line-to-ground faults, the curves are such

Figure 5. System tie—200-mile double-circuit line

No intermediate switching station. Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13

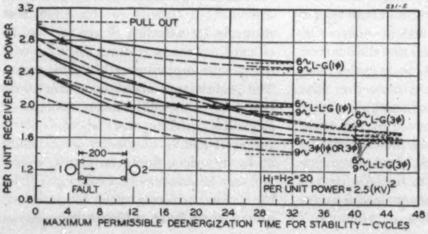
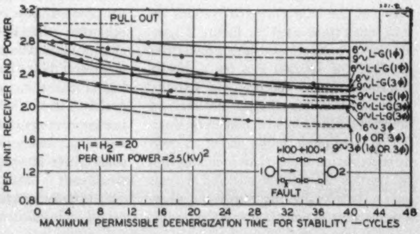


Figure 6. System tie—200-mile double-circuit line with intermediate switching station

Three-phase and single-phase switching. Type of switching indicated for each curve by (1φ) or (3φ). Refer to appendix I and Figure 13



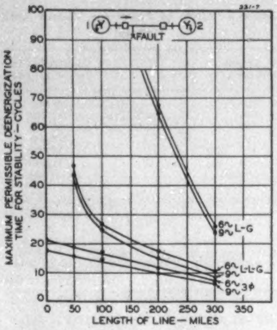


Figure 7. System tie—single-circuit line

Single-phase clearing and reclosing. Sending end power = 1.1 × 2.5(kv)^2. $H_1 = H_2 = 20$ on kva base of 2.5(kv)^2. Refer to Figure 13, Appendix I

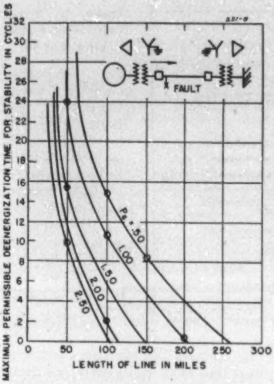


Figure 8. Hydroelectric system—single-circuit line

Single-phase switching of line-to-ground faults. Nine cycles clearing. P_G = rated generator output. Curves are for a loading ten per cent above generator rated output. Refer to Figure 14, appendix II

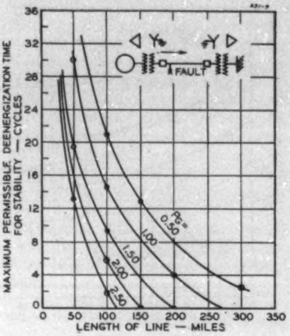


Figure 9. Hydroelectric system—single-circuit line

Single-phase switching of line-to-ground faults. Six cycles clearing. P_G = rated generator output. Curves are for a loading ten per cent above generator rated output. Refer to Figure 14, appendix II

that, in the region of 12 cycles de-energization time, a decrease of three cycles in the de-energization time is equivalent to about a three-cycle reduction in fault-clearing time. This indicates that a cycle reduction in fault-clearing time is equivalent to a cycle of reduction in de-energization time. There is possibly a further improvement with the quick clearing in that the ionization in the arc path does not involve as great a volume of air, and, therefore, a further reduction in deionization time may be allowed. This is of considerable interest because studies of transient stability limits of systems have indicated that there was not much to be gained by reduction in fault clearing times *without* reclosing below eight or possibly five cycles. Figure 2 indicates that with three-phase reclosing, three cycles reduction in time, either clearing or de-energization is worth about 15 per cent increase in power limit.

If the interconnected systems have greater inertias than those used in determining the curves on Figure 2, a correspondingly greater amount of power can be transferred for the same de-energization time. For example, if the inertia constants of the interconnected systems are 2.25 times those used for Figure 2 (generating capacity equal to about nine times 2.5 (kv)^2 or 400,000 kva for a 138-kv transmission line) the transient stability limit is increased from a per-unit receiver power of 1.16 to 1.62 with nine-cycle clearing of three-phase faults and a 12-cycle de-energization time. This indicates that the inertias of the interconnected systems have a considerable effect upon the allowable de-energization time and that, when applying reclosing mechanisms, this effect should be considered. It is evident that when the interconnected areas are of a large capacity compared with the tie-line capacity, three-phase reclosing may be entirely adequate, even for single-circuit ties for loads up to the maximum which the circuit will be required to carry.

Figure 4 gives the results for 100 miles of double-circuit line with no intermediate switching station. It will be noted from the bottom curve on this figure that a load corresponding to two per unit (this corresponds to unit power on each circuit) may be carried with no reclosing, following a three-phase terminal fault which is cleared in nine cycles. A power of 2.4 per unit may be carried with no reclosing if six-cycle clearing is used. As shown by Figure 4, the increase in power limit by the use of reclosing is much less than that for a single line, as shown in Figure 2. With 12 cycles de-energization time and nine cycles clearing for a three-phase fault, the power limit is increased about ten per cent over what it would be with no reclosing. It will be noted that the improvement in stability limit for six cycles clearing of the three-phase fault over nine cycles clearing is 40 per cent greater than the improvement obtained by the use of reclosing. These results indicate that for some types of system, depending upon the load it is desired to transfer, there is very little benefit to be obtained from the use of high-speed reclosing. However, if the systems have relatively large inertias, the increase in power limit can be further improved. This indicates that an appreciably greater gain may be obtained with three-phase reclosing on parallel lines when the inertia constants are large; whereas, if the inertia constants are small, it may be impossible to carry the rated load of the circuit. Additional curves are given in Figures 3 and 5 for 50 and 200 miles of double-circuit line, respectively, with no intermediate switching station. Figure 6 corresponds to a 200-mile double-circuit line bussed together at the center. It will be noted that by bussing, the 200-mile line performance is improved, and the gain due to reclosing is decreased, and may be such that it may not be necessary to use any reclosing. For the 50-mile double-circuit line, Figure 3, the curves are relatively flat indicating very little benefit to be derived from reclosing.

For circuits which are on the same tower, more than one circuit may be involved in the fault, and in such a case successful reclosure of even one of the two circuits may prevent loss of synchronism. Therefore, a benefit greater than that shown in the attached curves for double-circuit lines can be realized when the conductors are supported on the same tower. The performance under these conditions would be somewhat similar to that of single-line performance as shown on Figures 1 and 2. Single-line performance or simultaneous faults on double-circuit line can be adequately taken care of by three-phase reclosing when the interconnected areas have sufficient capacity or inertia effect. This has been supported by experience as given in reference 3.

Single-Phase Reclosing—Tie Lines and System Interconnections

Single-phase clearing and reclosing of transmission-line circuits has been considered a possibility for a long time but has been put into operation only to a limited extent. With a line-to-ground fault only one-phase wire need be interrupted, the remaining tie over the two sound phases and ground being sufficiently strong to prevent loss of synchronism for a considerable range of load and system conditions obtained in practice. When two-phase wires are involved, however, the remaining tie is so reduced in strength as to result in but little benefit over three-phase reclosing for the same de-energization times. There is, of course, no benefit if all three-phase wires are involved.

Figure 7 shows the maximum permissible de-energization time in cycles for stability of a single-circuit tie using single-phase clearing and reclosing. These curves are for a loading corresponding to a sending-end power ten per cent above the unity power-factor loading of the line and with the connected systems having a capacity about four times the transmitted power. As shown by these curves, this power can be transmitted up to 150 miles with three-phase switching, nine-cycle clearing, and a 12-cycle de-energization time, for a three-phase fault. It is also of interest to note that the permissible de-energization time for line-to-ground faults is considerably higher than for three-phase or double-line-to-ground faults. This indicates that for line-to-ground faults single-phase reclosing is a very effective way of preventing loss of stability. However, its greatest advantage occurs when the loading is such that three-phase reclosing is no longer adequate. This condition is more likely to exist when the interconnected systems are relatively small.

Figure 2 shows the results for a single-circuit line 100 miles long. This compares the results using single-phase reclosing with three-phase reclosing. If the loading of the line is increased above unity power-factor loading, and the inertias correspond to those used in the figure, the line-to-ground faults may be switched off by the use of single-phase switching without loss of synchronism up to a loading which is 50 per cent greater than that of a line-to-ground fault with three-phase reclosing based on the same de-energization time of 12 cycles. However, if the deionization time for single-phase reclosing must be 50 per cent longer, which may be necessary for a 100-mile line, the improvement is from a per-unit power of 1.5 to 2.05 or about 35 per cent increase in power limit. Similar comparisons can be made by examining the results shown on Figures 3, 4, 5, and 6.

High-Speed Reclosing—Hydroelectric System

Representative of a type of system where reclosing might be applied is a hydroelectric station located at some distance from a large system to which it delivers power. This case of a typical hydroelectric station was studied for various lengths of line and line loadings. The transmission-line constants were assumed to be the same as for the study of interconnections previously discussed. Typical hydroelectric generator constants were used ($x_d'=0.30$, $H=3.0$). The sending- and receiving-end transformer reactances were assumed as ten per cent on the generator rating. The receiving-end system was assumed to have infinite inertia and an equivalent reactance corresponding to ten per cent of the hydroelectric generator rating. See appendix II. Calculations were then made to determine the transient-stability limits with single-line operation and high-speed reclosing. The hydroelectric generators were assumed to have a rated power factor of 0.9 and to be operating at ten per cent above their kilowatt rating, a loading which about corresponds to their kilovolt-ampere rating. The results are therefore on the basis of the total hydroelectric generating capacity being operated at slightly above rated kilowatt load. The results indicate the maximum permissible de-energization times under these conditions.

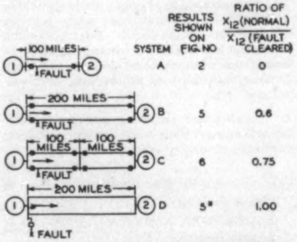


Figure 10. Systems used for interpretation of results in terms of present-day standard breakers shown on Figures 11 and 12

*De-energization times for this case taken to correspond to zero cycles

For this type of system, which has a relatively low inertia constant and which has a transmitted load equal to the total generated capacity at the sending end of the line, three-phase reclosing is impractical unless de-energization times of one to five cycles were possible. Furthermore, single-phase reclosing for anything but line-to-ground faults is impractical, because of the small available time for deionization before stability is lost. However, for line-to-ground faults single-phase reclosing may prove practical. Figures 8 and 9 show the results of calculations for this case with nine- and six-cycle fault clearing, respectively. It will be noted from Figure 8 that a line loading of unity corresponding to 50,000 kw at 138 kv may be transmitted 50 miles, with stability, when nine-cycle clearing and 27-cycle de-energization time are used. From Figure 9, if six-cycle clearing is used, a line loading of unity may be transmitted 55 miles with stability. For heavier line loadings a corresponding reduction in miles to which the power can be successfully transmitted through a line-to-ground fault is obtained.

Calculations were also made to determine the effectiveness of a high-resistance amortisseur winding for increasing the permissible de-energization time when single-phase switching is used. As is well known,[12,13] such a winding will increase the braking torque during a circuit unbalance and may be of advantage under such conditions in reducing the effective accelerating torque. For a 100-mile single-circuit line with a unit loading ($P_0=1.0$), the maximum permissible de-energization time was found to be increased about 35 per cent for line-to-ground faults. Such a winding may, therefore, be effective for a hydroelectric generator connected to its load area by single-circuit line, when it is desired to protect against line-to-ground faults by single-phase switching rather than by a ground-fault neutralizer. If it is necessary to protect against faults involving two or more phases, single-phase switching is not effective, and there is but little benefit to be derived by the use of a high-resistance amortisseur winding.

Summary From Preceding Studies

(*a*). THREE-PHASE RECLOSING ON SYSTEM TIES

1. The benefits to be obtained from reclosing are greatest for single-circuit lines, or simultaneous faults on double-circuit lines.

2. Three-phase reclosing is a very practical means for maintaining the transmission-line loading between interconnected areas when the interconnected areas have generating

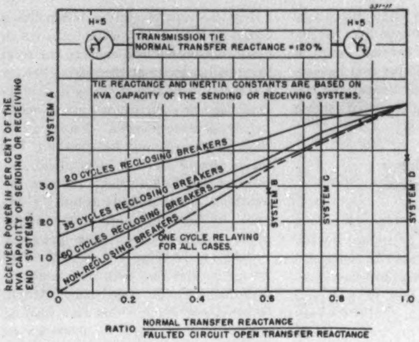


Figure 11. Stability limits for three-phase faults at sending-end line terminal, nine cycles clearing, as a function of reclosing time and ratio of transfer reactances

Refer to Figure 10. The two points marked "x" on the figure show the power limit for unsuccessful reclosure against a three-phase fault on one circuit using 20-cycle reclosing breakers

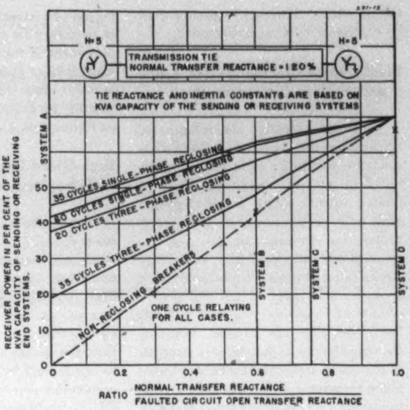


Figure 12. Stability limits for line-to-ground faults at sending-end terminal, nine cycles clearing, as a function of reclosing time and ratio of transfer reactances

Refer to Figure 10. The two points marked "x" on the figure show the power limit for unsuccessful reclosure against a line-to-ground fault on one circuit using 20-cycle three-phase reclosing breakers

capacities of the order of at least four times the transmission-line loading.

3. With three-phase reclosing of single-circuit lines a decrease in the clearing time for three-phase faults is as important as a decrease in de-energization time. Therefore, further reductions in the fault-clearing time directly improve the stability.

4. With parallel circuits, the benefits to be realized from reclosing decrease with decrease in line length and with increase in the number of intermediate switching stations.

(*b*). SINGLE-PHASE RECLOSING ON SYSTEM TIES

1. Single-phase switching provides increases in transient power limits on single-circuit lines for line-to-ground faults which are not possible with three-phase switching; the difference however, may be small if the interconnected areas have high generating capacity relative to the power to be transmitted.

2. The power limits of a single-circuit line are greater with a ground-fault neutralizer for extinction of line-to-ground faults than those which can be obtained with single-phase switching.

3. If the inertia constants of the interconnected systems or the connected capacities are sufficiently great, the practical advantages of single-phase switching may be negligible.

4. For line-to-ground faults, if single-phase switching is used, the time for de-energization is not particularly critical as regards its effect on the stability limits, and, therefore, it is not necessary to strive for low reclosing time as is the case with three-phase reclosing.

5. The advantage of single-phase switching appears to be important only when reclosing is used on single-circuit interconnecting lines and when the generating capacity of one of the interconnected areas is not much greater than the load which must be transmitted.

6. Single-phase switching appears to offer no advantage over three-phase switching for faults involving more than one phase.

(*c*). SINGLE-PHASE SWITCHING FOR A HYDROELECTRIC STATION DELIVERING POWER OVER A SINGLE-CIRCUIT TO A LARGE SYSTEM

1. Single-phase reclosing makes possible the maintenance of stability through self-clearing line-to-ground faults for normal line loadings and for short distances.

2. For longer lines and more heavily loaded circuits, other means such as the ground-fault neutralizer may be required in order that the system may ride through line-to-ground faults.

3. This type of system is not stable with either single-phase or three-phase reclosing of faults which involve more than one phase.

Ground-Fault Neutralizers

Ground-fault neutralizers should be compared with the use of single-phase switching, since the advantages of using the latter means are chiefly limited to line-to-ground faults. The ground-fault neutralizer has an advantage over single-phase switching in that there is no reduction in stability limits for line-to-ground faults. For the shorter lines this may be of no particular advantage over single-phase switching, as the circuit loadings may be well below the transient-stability limits with single-phase switching. However, it may be necessary to retain as much stability margin as possible in order to carry rated circuit loadings on the longer lines. This, therefore, indicates that for long single-circuit lines, ground-fault neutralizers have a distinct advantage over single-phase switching, whereas for the shorter lines and on systems which are necessarily solidly grounded, single-phase switching may have important advantages. When using ground-fault neutralizers on single-circuit lines, one should give attention to the switching and circuit arrangements in order to avoid the possibility of high transient switch voltages. This problem, however, is now well understood,[11] so that the conditions which give rise to such abnormal voltages can be avoided.

Relaying

In general, the use of reclosing on tie circuits requires that the terminal breakers be opened simultaneously in order to obtain the benefit of maximum deionization time to clear the arc path. This essentially requires the use of pilot-wire relays of some form to give the simultaneous tripping at both terminals. For the lines generally considered in this analysis, the line lengths are such that the carrier-current form of pilot relaying would be used. Successful operation of high-speed reclosing with carrier relaying is already a matter of record.[3]

For single-phase operation, it is only necessary to add to the basic carrier pilot-relay equipment phase-selecting relays to select the proper phase or pole to be opened in case of single-phase faults. Since single-phase operation is feasible only on a solidly grounded neutral system, these phase selecting relays can be simple impedance devices operating from line-to-neutral voltage and line residual current. In some cases of single-phase reclosing it may not be necessary to obtain simultaneous tripping at both terminals, because the permissible de-energization time is so great that sequential tripping could be tolerated.

Interpretation in Terms of Present-Day Standard Breakers

Although the previously presented results cover a wide range of possible system conditions and breaker speeds, they may require some interpretation in terms of present-day standard-breaker clearing and reclosing times. It was found that this could be conveniently done by cross-plotting the results for the clearing and de-energization times corresponding to standard breakers for the series of systems shown in Figure 10. All of these four system arrangements have essentially the same transfer reactance for normal conditions, neglecting resistance and capacitive susceptance. If it is assumed that the inertia constants of the two interconnected systems correspond to $H=5$ per unit on the kilovolt-ampere rating of the generator capacity of each system (four times the per-unit power of the preceding studies or $10(\text{kv})^2$), the transfer reactance is 120 per cent on the kilovolt-ampere capacity of each interconnected system. The ratio of the normal transfer reactance to the transfer reactance with the faulted section switched out can be used as an indication of the shock to the system resulting from switching out the faulted section. In the right-hand column of Figure 10 are tabulated these ratios.

(*a*). Three-Phase Faults

Figure 11 shows the per cent power (based on the generating capacity of the individual interconnected systems) which can be transferred with stability through a three-phase fault at the sending-end line terminal, plotted against the ratio of transfer reactance for the different standard-breaker times. Zero ratio of transfer reactance represents a single-circuit tie, whereas increasing ratios indicate an increasing number of intermediate stations on parallel lines.

In Figures 11 and 12 it is assumed that

(*a*) Relay delay is one cycle at each end of the circuit.

(*b*) Breakers with eight cycles interrupting time rating are used.

The circuit de-energized time is eight cycles less than the reclosing time rating. From Figure 11 several important effects are apparent:

1. The greatest possible gain with reclosing may be realized for systems which approach the single-circuit tie (system *A*, Figure 10).

2. The smallest possible gain is realized for systems which approach a multiple-circuit tie sectionalized by a large number of intermediate switching stations (system *D*, Figure 10). Except for the possibility of simultaneous double-circuit outages, there is little reason for using reclosing breakers in a system of this type. If double-circuit faults can occur, the performance is almost the same as that of the shorter single-circuit line (system *A*, Figure 10).

3. A point of diminishing returns is soon reached for further sectionalizing beyond that represented by the double-circuit tie with one intermediate switching station (system *C*, Figure 10).

4. The advantages of 20-cycle over the slower 35- or 60-cycle reclosing breakers are apparent.

5. If single-phase reclosing is slower than three-phase reclosing, the allowable power transfer during three-phase faults is correspondingly less.

6. The successful power transfer is very appreciably increased even when one strong tie is paralleled by another of relatively low capacity. If two such circuits have equivalent lengths of 150 and 300 miles, the transfer reactance ratio is approximately 0.4 and the allowable power transfer is at least 25 per cent greater than for a single 100-mile circuit.

(*b*). Line-to-Ground Faults

Figure 12 shows the performance of the same four systems analyzed in the same manner for line-to-ground faults, using both single-phase and three-phase reclosing breakers. Because a longer deionization time is required for single-phase reclosing than for three-phase reclosing, 35 and 60 cycles were taken as the reclosing times for the former, and 20 and 35 cycles for the latter. The general form of the curves is about the same as for Figure 11. From Figure 12 several important effects are apparent:

1. Appreciably greater power can be transmitted for the single-circuit system with three-phase reclosing, if 20-cycle rather than 35-cycle reclosing is used. A further but much smaller gain is realized by the use of 35-cycle single-phase reclosing.

2. With single-phase reclosing, the difference between 60 and 35 cycles reclosing is quite small. The speed of reclosing for single-phase switching on line-to-ground faults is less important than that for three-phase switching.

(*c*). Other Considerations

Figures 11 and 12, though limited to one value of normal transfer reactance (120 per cent) indicate the relative importance of several of the essential factors during line-to-ground and three-phase faults. A similar interpretation for double-line-to-ground faults shows that, where high-speed reclosing is applicable, 20-cycle three-phase reclosing gives appreciably better results than 35-cycle single-phase reclosing. Further interpretations show that, with increase in system inertia, the stability limits with high-speed reclosing are materially increased. This indicates that the use of high-speed reclosing will naturally tend to increase with the trend toward interconnection of large systems by ties of relatively small capacity.

High-speed reclosing involves the risk that occasionally the fault will not be self-clearing, and any attempt to reclose may do harm rather than good. This risk may be evaluated in two ways. First, the decrease in power limit due to an unsuccessful reclosure below that obtained with no reclosing. Second, the decrease in power limit due to an unsuccessful reclosure below that obtained with successful reclosing.

Calculations for both systems *B* and *D* of Figure 10 indicate that quick single-phase switching and reclosing of line-to-ground faults can involve negligible risk of either the first or second kind. The advantage of the possible small risk of the second kind may be largely only of theoretical rather than of practical value because to obtain it would require an unusually long period of unbalanced operation with one phase open. As shown by the points (marked x) on Figure 12, automatic three-phase unsuccessful reclosure against line-to-ground faults involves negligible risk of the first kind but appreciable risk of the second kind. For either single-phase or three-phase unsuccessful reclosure against the more severe faults, double-line-to-ground or three-phase (for three-phase faults points marked x on Figure 11), the risk of both the first and second kinds is appreciable. Calculations made for the case of delayed or manual reclosure showed no risk of the first kind. Calculations were also made for the 200-mile tie with an intermediate bussing station (system *C*) for the special case of a double-line-to-ground fault due to a single-line-to-ground fault on different phases of each circuit. Both circuits were assumed to be cleared in nine cycles, closed after 12 cycles de-energization time, unsuccessful reclosure on one circuit, and with subsequent nine cycles clearing on this faulted circuit. The critical loading for this case with three-phase reclosing was found to be about the same as for a self-clearing three-phase fault on one circuit of system-*A* type. With single-phase reclosing for this case, the critical loading is nearly double the value obtained with three-phase reclosing.

From both Figures 11 and 12, it is apparent that the risks are least where the possible gain from reclosing is greatest.

General Conclusions

1. Under favorable conditions for its use, high-speed reclosing provides a simple means for substantially increasing the system reliability.
2. The choice of the type and available time ratings for reclosing breakers requires an analysis to weigh properly their relative advantages.
3. With three-phase reclosing, de-energization time is important for all types of faults, and clearing time is important for double-line-to-ground and three-phase faults but not for single-line-to-ground faults.
4. For single-phase switching, both clearing and de-energization time are relatively unimportant for single-line-to-ground faults but are important for faults involving more than one phase.
5. Until more is known about the deionization time requirements with single-phase switching, it appears logical to limit single-phase reclosing to line-to-ground faults and to applications where moderate reclosing delay carries no particular stability penalty.
6. High-speed reclosing will show the greatest advantage and will tend to be more generally used as the number of system interconnections and the size of systems increase, relative to the strength of their individual interconnecting ties.

Appendix I. Study of System Interconnections

A series of cases was set up for study of three-phase and single-phase reclosing, having constants which were considered representative for illustrating the factors involved in system interconnections. These cases were studied on an a-c network analyzer. The following representative line constants of a 60-cycle overhead transmission line were used:

$x_1=0.8$ ohm per mile, positive sequence reactance.

$r_1=0.16$ ohm per mile, positive sequence resistance.

$Y_1=5.2\times10^{-6}$ mhos per mile, positive sequence susceptance.

$x_0=2.4$ ohms per mile, zero sequence reactance.

$r_0=0.43$ ohm per mile, zero sequence resistance.

$Y_0=3.12\times10^{-6}$ mhos per mile, zero sequence susceptance.

For study of double-circuit lines the zero-sequence mutual capacitance and reactance effects were neglected because

(*a*). These assumptions would apply for lines not on the same right of way.

(*b*). These are secondary effects only and would not modify appreciably the results or conclusions of this study even for lines on the same tower.

The base kilovolt-amperes and kilowatts were taken as 2.5 $(\text{kv})^2$ corresponding to the surge-impedance or unity power-factor loading of a line having the constants selected for study.[5] The sending-end and receiving-end systems were assumed to have equal impedances. The positive-, negative-, and zero-sequence impedances of sending- and receiving-end systems were assumed to be the same and equal to $0.002+j0.05$ per unit on a base kilovolt-ampere equal to 2.5 $(\text{kv})^2$. See Figure 13. The system was set up three-phase on the network analyzer so that the simultaneous dissymmetries produced by opening single-phase switches at either end of the line, with or without a fault on one or more phases, could be more easily represented. Also in this way the capacitive ground current and the fundamental frequency voltage on the cleared conductor would be determined directly for any condition.

The voltages back of system impedances were held fixed for all loadings corresponding to values which would give normal or unit voltage at each line terminal for a power transfer per circuit at the sending end of 1.10 per unit. For higher initial loadings the results give somewhat lower limits than may be actually obtained; whereas for lower initial loadings, the limits are somewhat higher. However, these differences are small because of the comparatively small terminal or system impedances.

The faults were all taken at the sending-end terminal and assumed to be cleared in six or nine cycles. The maximum permissible time for de-energization of the faulted conductor or conductors was determined by calculation.[6] The line length was varied from 0 to 300 miles. Both single- and double-circuit lines with and without intermediate bussing were studied. Some of the results obtained are shown on Figures 1–7. These results are given for equal sending- and receiving-end system inertias of $H=20$ per unit based on 2.5 $(\text{kv})^2$. A typical system may have an inertia constant (H) of 5.0 per unit on its connected kilovolt-amperes of generator capacity. Accordingly, with an $H=20$ on 2.5 $(\text{kv})^2$ and a transmission-line loading of unity, corresponding to unity power-factor transmission, the connected generator capacity of the sending- or receiving-end systems corresponds to about four times the power being transferred.

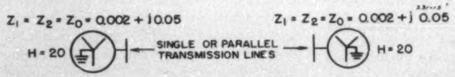

Z1 = Z2 = Z0 = 0.002 + j0.05
H = 20
SINGLE OR PARALLEL TRANSMISSION LINES
Z1 = Z2 = Z0 = 0.002 + j 0.05
H = 20

Figure 13. Typical system interconnection

Refer to appendix I

Since results have been obtained for both six-cycle and nine-cycle clearing, the curves for six-cycle clearing and $H_1=H_2=20$ can be readily interpreted for nine-cycle clearing with $H_1=H_2=45$ by increasing de-energization times given on the curves by a factor of 1.5.

Similarly the nine-cycle clearing results for $H_1=H_2=20$ may be interpreted as applicable for six-cycle clearing with $H_1=H_2=20/2.25=8.86$ if the de-energization times are decreased by dividing by 1.5.

The concept of equivalent or effective system inertia constant may be applied approximately (could be correctly applied if resistance were negligible) so that the above results may be interpreted to apply to systems having effective inertia constants corresponding to $H_e=H_1H_2/(H_1+H_2)$.[7,8]

The pull-out power shown in the curves corresponds to the receiving pull-out power.[9] This may also be used as the power limit for a line-to-ground fault for ground-fault neutralizer operation as compared with the other methods of fault clearing.

Appendix II. Conditions for Study of Hydroelectric Systems

A series of cases was studied corresponding to a hydroelectric station delivering power over a single-circuit line to a large system. The line constants were taken to be the same as for the interconnection study outlined in appendix I. The remaining system constants are, see Figure 14,

$x_d'=0.30$ on generator kva

$x_{T1}=x_{T2}=0.10$ on generator kva

$x_s=0.10$ on generator kva

$P_r=$ kw rating of generator in per unit of $2.5(\text{kv})^2$

Rated generator power factor $=0.9$

Generator kva $=P_r/0.9$

Normal voltage was assumed at the sending- and receiving-end high-voltage terminals under initial conditions. The faults were taken at the sending end.

The maximum permissible de-energization times were determined for a power transfer of $1.1\times P_r$ corresponding to a ten per cent stability margin or a power practically equal to the kilovolt-ampere rating of the generator. The results of the calculations made for these cases are shown on Figures 8 and 9 for nine- and six-cycle clearing. The maximum permissible deenergization times for faults involving more than one phase with either single-phase or three-phase switching were all less than five cycles.

The results for the six-cycle clearing can

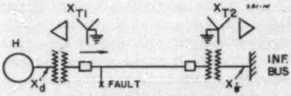


Figure 14. Typical hydroelectric system interconnection

Refer to appendix II

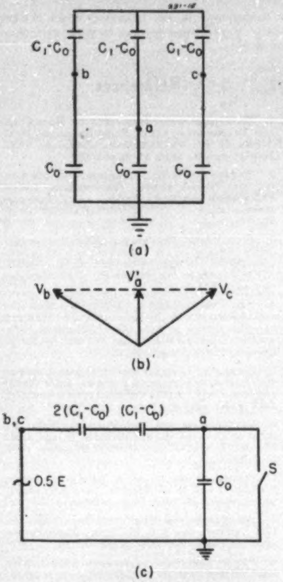


Figure 15. Circuits used in derivation of equations of appendix III

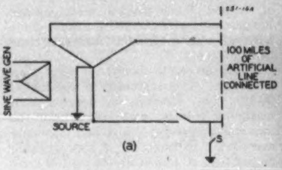
be interpreted for nine-cycle clearing when the inertia constant is increased from 3.0 to $(1.5)^2 \times 3.0 = 6.75$ by increasing the de-energization times by a factor of 1.5.

Appendix III. Self-Clearing Characteristics of Arcs to Ground With Single-Phase Switching

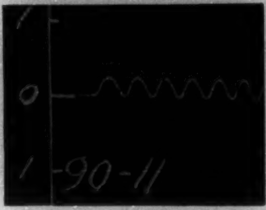
H. A. PETERSON*
MEMBER AIEE

In the case of single-phase switching, the capacity coupling from the sound phase or phases to the stricken phase or phases tends to maintain an arc to ground once it is established. This is illustrated in Figure 15 of this appendix. Figure 15a shows the capacitance circuit involved. If it is assumed that phase a has been de-energized, the effective voltages in the circuit are V_b and V_c of Figure 15b. These can be resolved into components approximately as shown. The components $V_b - V_a'$ and $V_c - V_a'$ will have no effect on the voltage or current of conductor "a", since they are equal and opposite. The resultant effective component is $V_a' = 0.5V_b = 0.5V_c$. This is effective in the single-phase equivalent circuit shown in Figure 15c where conductor "a" retains its identity while conductors "b" and "c" are shown as a common point.

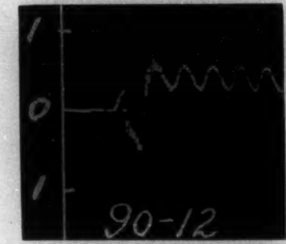
With switch S closed, a current flows through it to ground corresponding to the arc current which must be extinguished before conductor "a" can be normally energized for carrying power. The current flowing through S is



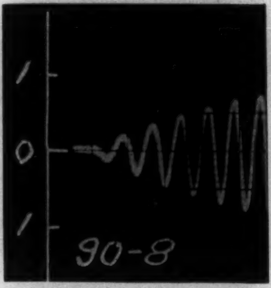
(a) Source reactance: $x_0 = x_1 = x_2 = 25$ ohms
Line:
$r_1 = 0.15$ ohm per mile
$r_0 = 0.36$ ohm per mile
$x_1 = 0.80$ ohm per mile
$x_0 = 3.2$ ohms per mile
$C_1 = 1.6C_0$
$C_0 = 0.01$ µf per mile



(b) 90-11—Recovery voltage for single-phase switching of line-to-ground fault with no restriking. Interruption at normal current zero



90-12—Recovery voltage for single-phase switching of line-to-ground fault with restriking. (Two restrikes)



90-8—Recovery voltage for line-to-ground fault with ground-fault neutralizer

Figure 16. Transient analyzer circuit and oscillograms of line-to-ground fault clearing phenomena

Refer to appendix III

$$I_s = \frac{0.5V_b}{\dfrac{1}{\omega(C_1-C_0)} + \dfrac{1}{2\omega(C_1-C_0)}} = \frac{0.5V_b}{\dfrac{3}{2\omega(C_1-C_0)}} = \frac{\omega(C_1-C_0)V_b}{3}$$

In terms of normal charging current for the section of line being switched out dividing by ωC, this becomes

$$\frac{I_s}{I_{nc}} = \frac{\omega(C_1-C_0)V_b}{3\omega C_1} = \frac{(C_1-C_0)V_b}{3C_1}$$

where I_{nc} is normal positive phase sequence line charging current. In a typical overhead line, $C_0 = 0.6C_1$ is representative. Therefore,

$$\frac{I_s}{I_{nc}} = \frac{(C_1-0.6C_1)V_b}{3C_1} = \frac{0.4V_b}{3} = 0.133\,V_b$$

or the current to be extinguished is 13.3 per cent of the normal line-charging current of the line section being switched with normal line-to-ground voltage on the other phases. Thus the arc current is proportional to line length switched out and system voltage.

The recovery voltage V_a appearing across the switch S following the extinction of the arc current is

$$V_a = \frac{0.5/\omega C_0 V_b}{\dfrac{3}{2\omega(C_1-C_0)} + \dfrac{1}{\omega C_0}} = \frac{0.5/C_0 V_b}{\dfrac{3C_0+2(C_1-C_0)}{2C_0(C_1-C_0)}} = \frac{(C_1-C_0)V_b}{C_0+2C_1}$$

Again, assuming $C_0 = 0.6C_1$

$$V_a = \frac{(C_1-0.6C_1)V_b}{0.6C_1+2C_1} = \frac{0.4V_b}{2.6} = 0.154\,V_b$$

or the voltage on "a" due to capacitance coupling following the extinction of the arc is 15.4 per cent of normal line-to-neutral voltage.

Similarly, from Figure 15a, it can be shown that with two phases "b" and "c" switched out, each having an arc to ground, the current magnitude in each arc is the same as for the single-phase case just considered. Furthermore, the voltage on the conductor first to clear is also the same in magnitude as the voltage for the single-phase case. However, the arc current for the last phase to clear of the two stricken phases will be increased when the first arc goes out. If conductor "*b*" clears first

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*H. A. Peterson is with central station engineering department, General Electric Company, Schenectady, New York.

$$\frac{I_e}{I_{nc}}=\frac{C_1-C_0}{2C_1+C_0}V_a$$

which, if $C_0=0.6C_1$, becomes

$$\frac{I_e}{I_{nc}}=\frac{(C_1-0.6C_1)V_a}{2C_1+0.6C_1}=\frac{0.4V_a}{2.6}=0.154V_a$$

or the arc current has increased from 13.3 per cent to 15.4 per cent of normal line-charging current in the section being switched out. The voltage on this phase when it has cleared will become

$$V_c=\frac{(C_1-C_0)V_a}{C_1+2C_0}=V_b$$

as the voltage magnitudes are equal on both conductors after both are cleared. If $C_0=0.6C_1$

$$V_c=V_b=\frac{(1-0.6)V_a}{1+1.2}=\frac{0.4V_a}{2.2}=0.182V_a$$

or the voltage on the conductors when both are cleared is 18.2 per cent of normal line-to-neutral voltage as compared to 15.4 per cent for either the single-phase case or the first conductor to clear for the two-phase case. Thus, it might be repeated that deionization times will be longer for double-line-to-ground than for single-line-to-ground faults.

In the extinction of these arcs, the *transient* recovery characteristic is also of importance. In order to study this phase of the problem, the transient analyzer was used. The system shown in Figure 16 was set up in miniature with the line open at the receiving end and only phase "*a*" open at both ends. By means of synchronous switching devices,[10] the arc current through switch *S* was interrupted at current zero. Oscillogram 90-11 shows the nature of this recovery voltage. It is important to note that the a-c component is completely offset, so that the instantaneous voltage to be cleared in each case is actually twice the sustained fundamental frequency component, or approximately from 30 to 35 per cent of normal line-to-neutral crest voltage. Furthermore, this voltage is reached one-half cycle after the assumed instant of clearing.

It is of interest to observe the effect of arc restriking. This is shown in oscillogram 90-12. Two restrikes, at approximately maximum voltage, were imposed for this case which gave rise to a voltage on the de-energized conductor "*a*" of approximately 65 per cent of normal line-to-neutral crest. These restrikes were controlled so as to give the maximum possible voltage per restrike. In the actual case, two restrikes would be extremely unlikely to give voltages this high. However, it is very likely that a good many more half cycles of arc current will flow before final interruption takes place, thus making possible a far greater number of restrikes.

A comparison of this voltage recovery characteristic and that of a ground-fault neutralizer system following the self-extinction of an arc to ground is enlightening. For the same system shown in Figure 16, but grounded through a neutralizer, the recovery characteristic is as shown in oscillogram 90-8. This illustrates the relatively slow "drift" of recovery back to normal restored voltage, taking three or four cycles to get up to 50 per cent of normal line-to-neutral voltage. Furthermore, arc restriking cannot increase this rate of voltage recovery, because each restrike simply causes the entire process to be started over again.

The preceding discussion has dealt with the dissimilarities of these two recovery characteristics. If only the first cycle following arc extinction without restriking is considered, there are also important similarities. In this interval, the recovered voltage versus time is of the same order of magnitude for both cases. Since this is likely to be the most significant interval of time during the extinction period, it is likely that arc extinction times may be expected to be somewhat similar for both cases. This time varies over wide limits, depending upon several factors, such as tower-footing resistance, atmospheric conditions, system voltage, system losses, and so on. Even with all of these factors fixed, there is an inherent randomness in arc behavior which makes arc clearing time subject to wide variation.

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Impulse and 60-Cycle Characteristics of Driven Grounds—II

P. L. BELLASCHI
FELLOW AIEE

R. E. ARMINGTON
Membership Application Pending

A. E. SNOWDEN
ENROLLED STUDENT AIEE

THIS paper extends the contribution on grounds of the first paper[1] in the following directions: experimentally, theoretically, and from the application standpoint.

The experimental data are from tests on twelve additional grounds driven in the field adjacent to the Sharon high-voltage laboratory. Seven of the grounds are in clay soil in a location which is naturally moist; these Meggered from 10 to 40 ohms. Two of the grounds are in naturally dry and gravelly soil, and one is in sand; the Megger resistance for these is from 60 to 220 ohms. The remaining two are in soil which consists of a mixture of clay and stones and Meggered from 25 to 190 ohms. The depth of the grounds ranges from 8 to 30 feet. Impulse and 60-cycle data are presented both for the single grounds and for parallel combinations. The impulse data extend up to 15,000 amperes and cover both positive and negative waves as well as other conditions. Tables, curves, and typical cathode-ray oscillograms present the more pertinent findings.

Analysis of the data shows that the decrease in resistance of driven grounds with increasing impulse current can be considered as the result of an increase in the effective radius and length of the rod. The agreement between experimental curves and curves calculated in this manner establishes the fact that the soil surrounding the rod breaks down at a critical voltage gradient. The two important factors associated with the soil are the resistivity and the breakdown gradient. In addition to providing a basic explanation of the physical processes involved, the analysis indicates that the impulse characteristic curve of a ground may be extrapolated with reasonable assurance to higher currents than the experimental data alone would permit. Furthermore, the methods established may be extended to study the relative characteristics of other types of grounds and arrangements, such as parallel grounds, the footing of tower structures, and so on.

This paper considers briefly the effect of lead inductance in combination with the ground proper, and it points out the practical importance of the lead or tower structure drop particularly for the rapid current discharge associated with direct strokes. Other factors affecting the impulse discharge are the conditions in the earth related to the soil and geological structure.

Physical Characteristics of Grounds

The 12 additional grounds, designated as *E*, *F*, *G*, *H*, *I*, *J*, *K*, *L*, *M*, *N*, *O*, and *P*, were driven in the spring of 1940 with the assistance of the Pennsylvania Power Company. The physical characteristics of the grounds are summarized in Table I. A view of the location and arrangement is shown in Figures 1 and 2. The one-inch-diameter rods and the rod in sand were driven with a sledge hammer; all other grounds were driven with a gasoline-operated hammer. The deeper grounds, *L* and *P*, are made up of eight-foot rods which are joined with suitable coupling.

All of the grounds except *M* are in natural soil and are located within 80 feet of the high-voltage laboratory. The grounds are well removed from fences and adjacent conducting objects with the possible exception of grounds *N* and *O*. Other details of adjacent objects and the terrain are apparent from the figures. Ground *M* consists of a six-foot diameter, ten-foot-deep hole packed with ordinary sand. The rod is driven down in the center of the sand. The sand was left to settle fully six months before the first serie sof impulse tests was made. At the time of the second series of tests a year later, the soil had gained considerable consistency and hardness.

The physical nature of the soil for grounds *E*, *F*, *G*, *H*, *I*, *J*, and *P* became apparent from the examination of a six-foot-diameter, ten-foot-deep hole (Figure 3) which was dug at a location some 30 feet from the grounds. While details of the strata and formation of the soil can vary considerably over this distance, nevertheless, Figure 3 indicates what to expect at the grounds. About six inches or more at the top is vegetation soil, then follows to a depth of four feet a combination of clay and gravel with a sprinkling of sand. Below this is a strata of about a foot of compact gravel and rocks. From five feet down to ten feet, the bottom of the hole, the soil is a thick blue clay. Water seeped in the hole at a depth of three feet. While this may not be the exact depth of the ground-water level, it indicates evidently that the soil for these grounds is naturally moist. It is difficult to state with certainty how far down bedrock lies. Rod *P* was driven with the gasoline hammer, straight down as far as it could be driven. Apparently it struck bedrock at 30 feet. According to the Geological Survey of Pennsylvania, bedrock in this district consists of fine-grained sandstone with alternate layers of shale.

Grounds *K* and *L* are located on the flat part of the embankment which rises

Table I. Physical Characteristics of Grounds

Ground	Date Driven	Diameter of Rod and Material	Depth Driven (Feet)	Nature of Soil	Spacing Apart and Location*
E *F* *G* *H* *I* *J*	4-12-40	1-inch steel	10	Largely clay. Soil naturally moist. See Figure 3	Grounds *E*, *F*, *G*, *H*, *I*, and *J* spaced 10 feet 2 inches apart. Ground *P* offset west from group above. Grounds driven in field
P	4-11-40	$^{5}/_{8}$-inch steel	29	Rod *P* struck bedrock 29 feet 3 inches below surface	
K	4-11-40	$^{5}/_{8}$-inch steel	8	Gravelly with clay mixture. Soil dry	Grounds *K* and *L* 17 feet apart. Driven in embankment
L	4-11-40	$^{5}/_{8}$-inch steel	16		
N *O*	4-12-40	$^{5}/_{8}$-inch Copperweld	8	Partly stony. Soil fairly moist	Ground *N* located adjacent to wood pole and *O* 10 feet apart
M	5-1-40	$^{5}/_{8}$-inch Copperweld	8	Ordinary sand used in construction work	6-foot diameter 10-foot deep hole filled and packed with sand

*For details on location, spacing, and terrain, see Figures 1 and 2.

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P. L. Bellaschi is development engineer and R. E. Armington is an engineer, both with Westinghouse Electric and Manufacturing Company, Sharon, Pa. A. E. Snowden is a student at Carnegie Institute of Technology, Pittsburgh, Pa.

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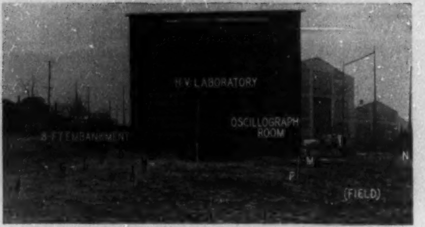


Figure 1. General view of grounds

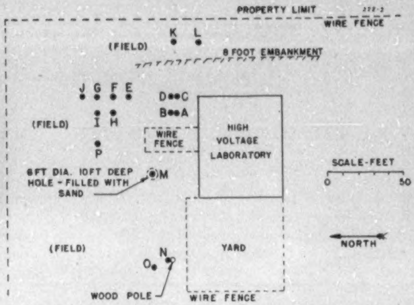


Figure 2. Arrangement of grounds

above the field where all of the other grounds are located. From *K* and *L* the land extends to the highway nearby, then up a hill rising beyond. A recent excavation of soil (October 1941) from the embankment near the grounds reveals that the top six feet are largely gravel with some clay, sand, and stones. Deeper down the soil becomes mostly clay with a mixture of sand. Due to the natural drain and the composition of the soil, grounds *K* and *L* are in naturally dry soil. This is particularly noticeable for ground *K* which in fact meggered the highest of all the grounds.

Grounds *N* and *O* are located in the flat terrain extending westward. Ground *N* is six inches from a distribution-line wood pole that was erected in 1934. Digging the hole for the pole at the time evidenced the presence of many large stones with clay, sand, and gravel. Ground *N* in particular and ground *O* to a lesser extent are embedded in soil of this nature. The terrain in which the twelve grounds are driven has a natural drain toward the Shenango River which lies a quarter mile westward at a level some 30 feet below.

The class and nature of the soil in which the twelve additional grounds are driven, as well as the physical characteristics of the grounds proper, represent a variety of typical conditions that should give rather wide scope to the investigation presented here.

Tests

The first series of impulse tests was completed late in the fall of 1940 before the ground frost started. Grounds *F*, *G*, *P*, *M*, and the parallel combination *F–G* were tested at this time. The results are summarized in Tables III and IV. For the grounds in clay (*F*, *G*, *P*) the wave is close to 20x50 microseconds, while for the ground in sand (*M*), it varies with increase in current from 8x125 to 25x65 microseconds. The currents for these tests cover a range of 400 to 15,500 amperes. Typical oscillograms are shown in Figure 5 (*AA* and *AV*) and Figure 6 (*AR*). Both positive and negative wave impulses were applied in the first series of tests.

The second series of impulse tests covers a period from August to September 1941. The results of these tests appear in Tables V, VI, VII, and VIII. The grounds tested are *F*, *H*, *I* and the parallel combination *F–G–H–I* all in clay soil; *M* in sand; and *K*, *L*, *N*, *O* in gravel and stones with a mixture of clay and sand. Some of these grounds were tested under the conditions of both fair weather and rain. In the preceding and other tests it was found that the polarity of the wave has little effect, and for this reason the second series of tests was made with negative waves, since this polarity appears to predominate in the lightning-stroke discharges to earth. In addition to the waves used in the first series, tests were

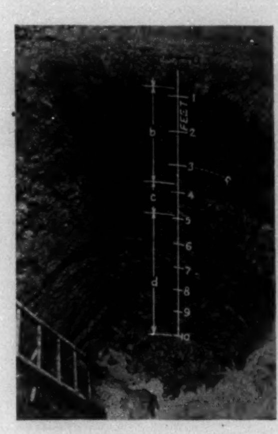


Figure 3. Section showing nature of soil for clay grounds

- (*a*) Vegetation soil
- (*b*) Combination of clay, sand, and gravel
- (*c*) Strata of gravel and rock
- (*d*) Thick blue clay
- (*f*) Ground-water level

Table II. 60-Cycle Test and Calculated Values Compared

Date of Test	Experimental Values (Ohms) Single Grounds E	F	G	H	I	J	K	L	N	O
7-22-40	28.0	23.0	21.6	21.8	22.3	26.9	133	49	54	24.4
12- 6-40	35.0	27.5	24.3	26.3	27.0	28.8				
3-20-41	40.0	27.5	23.0	27.0	30.0	32.0	218	65		
4-30-41	38.0	29.0	25.0	30.0	30.0	35.0	197	65		
6- 5-41	35.0	27.0	24.2	27.0	26.7	30.5	153	55		

Experimental and Calculated Values (Ohms) Grounds in Parallel

Date of Test	F & G		H & I		F–G–H–I		E–F–G–J		K & L		N & O	
	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.	Exp.	Calc.
7-22-40	13.0	12.7	12.9	12.6	8.36	7.7	9.58	8.2	39	35	17.5	22
12- 6-40	14.5	14.8	15.0	15.2	8.75	9.1	8.75	9.5				
3-20-41	13.8	14.5	16.8	16.3	11.3	9.3	10.5	10.1	46	46		
4-30-41	15.5	15.4	16.0	17.1	11.0	9.9	12.0	10.5	50	46		
6- 5-41	15.0	14.6	15.0	15.3	9.3	9.1	9.3	9.6	42	39		
Average per cent error	2.5%		3.1%		8.5%		8.6%		8.6%		25%	

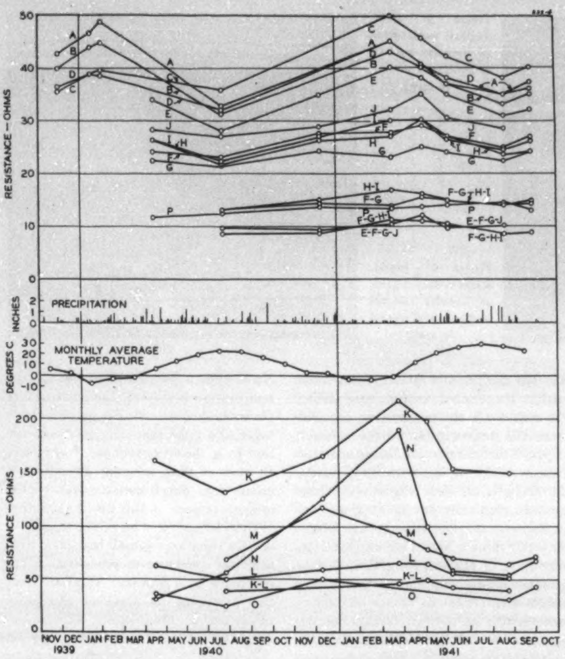


Figure 4. Seasonal variation of 60-cycle (Megger) resistance of grounds

also made with an 8x25-microsecond and a longer wave. The wave form and duration of the impulse for the tests on each ground are given in the tables. Typical oscillograms for these tests are shown in Figure 5 (*AL* and *AZ*) and Figure 6 (*CI* and *BE*).

Sixty-cycle measurements of the grounds have been recorded from time to time. Most of the readings were recorded with a Megger. These were taken through the assistance of the Pennsylvania Power Company in the conventional manner. In a few of the tests, the voltmeter-ammeter method was applied. This and previous investigations show that the Megger and the voltmeter-ammeter methods give practically the same resistance value. The two methods are, therefore, considered one and the same, and the data obtained from them are designated as 60-cycle resistance. In addition to the 60-cycle resistance measurements of single grounds and parallel combinations, the voltages developed from one ground to adjacent grounds were studied. The results of these tests are reported in Table IX. Similar tests made with impulse currents will be discussed in another section.

The method of test has been described in the first paper. Briefly, the impulse generator and the instruments are grounded to the common, low-resistance ground of the laboratory, which meggered for this investigation from 0.3 ohm to 0.9 ohm, and averaged 0.5 ohm to earth. The impulse voltage is recorded at the cathode-ray oscillograph through a voltage divider, and the current is recorded by means of a suitable shunt inserted in the grounded end of the impulse generator. Three values of impulse generator capacitance have been used, as stated in the tables. The impulses corresponding to each are nominally 20x50-, 8x25-, and 20x120-microsecond waves, the specific form depending on the resistance of the ground. The superimposed oscillations appearing on the front of the wave are due primarily to the combined effect of the stray capacitance and series inductance in the test circuit. For instance, *AR* of Figure 6 is an oscillogram with more than the usual amount of superimposed oscillations. These have no major effect on the ground proper but render difficult the analysis of the oscillograms below about three to five microseconds. It is an established principle that superimposed oscillations resulting from stray capacitance C_1 and series inductance L_s in the generator circuit may be damped out by a series resistance of $2\sqrt{L_s/C_1}$ or a load resistance of $^1/_2\sqrt{L_s/C_1}$. The series resistance is impractical for it materially limits the current delivered to the ground. A calculated resistance of 150 ohms was shunted across the surge generator with the improvement in the wave generated and applied as shown in oscillogram *CI* of Figure 6. Oscillograms *AR* and *CI* are directly comparable.

60-Cycle Characteristics

Sixty-cycle measurements for the 16 single grounds and five parallel combinations, taken over a period of two years, are plotted in Figure 4. The curves for the grounds in clay soil are grouped in the upper half of the chart; those for the grounds in sand, gravel, and stone are in the lower half. It is apparent that the seasonal variation of the resistance of grounds follows a cycle essentially inverse to the temperature cycle. While temperature appears to account largely for the seasonal change, rain precipitation, long dry spells, natural moisture content, composition of soil, and other factors can affect the resistance. For the grounds in clay, the variation from the annual mean resistance ranges from 10 to 20 per cent. The grounds in sand, gravel, and stone are subject to variations of 25 per cent and even more from the annual mean resistance. Since the annual variation of the average temperature of the top ten feet of soil would be in the order of 10 to 15 degrees centigrade, the temperature coefficient of the resistance of the soil is about two to three per cent per degree centigrade. These characteristics check the results reported by other investigators.[2,3,4] The deeper the ground is, the less will be the seasonal variation. For instance, *L* is twice as deep as *K* and has half the per cent variation in resistance. The seasonal change in the resistance of ground *P* which is 30 feet deep in clay soil is about eight per cent compared to 20 per cent for the nine-foot ground *A* also in clay. Ground *N*, embedded largely in stones, suffers very wide seasonal fluctuations in the 60-cycle resistance. The instability of this ground was further evidenced by the fact that while the 60-cycle measurements of all

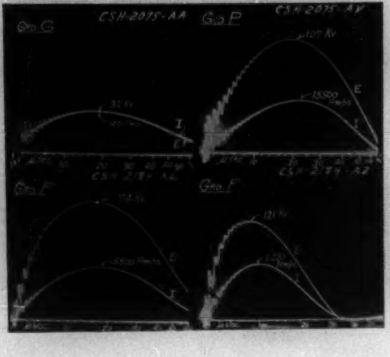


Figure 5 (left). Typical oscillograms of impulse currents and voltages (grounds in clay)

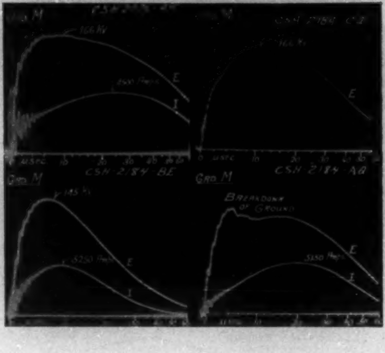


Figure 6 (right). Typical oscillograms of impulse currents and voltages (ground in sand)

the other grounds could be duplicated at a given time within the usual small experimental error, the check measurements of ground *N* would show wide variation.

The physical dimensions of the rod and the resistivity of the soil determine the 60-cycle resistance of a driven ground. The relationship is given in equation 1 of appendix I. While the physical dimensions are well defined, the resistivity of the soil may only be approximated, since it varies with the temperature and depends on the moisture content of the soil, the physical and chemical composition, as well as on other factors. Moreover, the soil may not be homogeneous even within the limited volume surrounding the rod, nor is the temperature constant from the surface down to the effective depth of the ground. All this is apparent from a study of the data for grounds, *E*, *F*, *G*, *H*, *I*, *J* in Figure 4. These grounds physically are identical and are in clay soil, driven within an area 10 by 30 feet. Over a period of one year, the resistivity of the soil for the individual grounds as determined from the equation ranges from 7,000 to 13,000 ohm-centimeters with an over-all average for the six grounds of 9,000-ohm-centimeters. Even when a suitable correction for the temperature is applied, the resistivity of the individual grounds for measurements taken at a given time may vary ±25 per cent from the average of the six grounds. In view of these inherent difficulties of calculation, direct measurement of the grounds remains to this day the practice.

Until more is known of the resistivity of soils, equation 1 cannot be applied with sufficient assurance to predetermine the resistance of a ground. However, certain principles are apparent and practically useful. For instance, according to equation 1 ground *P* (29 feet deep) should

Table III. Impulse Measurements of Grounds *F*, *G*, *F–G*, *P* (Clay)

Period of Tests 11-30-40 to 12-2-40

Driven Grounds	Wave Form (Microseconds)	Cathode-Ray Oscillogram 2075	Polarity	Measured Ground Crest Values Kv	Measured Ground Crest Values Amperes	Impulse Resistance (Ohms)	Ratio of Impulse to 60-Cycle Resistance	Comments*
F alone	Current wave 20x49 Voltage wave 18x50	*AB*	Pos.	31.5	1,545	20.4	0.74	Average Megger resistance of ground *F*, 27.5 ohms. Slight breakdown in ground at higher currents
		AC	Pos.	55.5	3,450	16.1	0.59	
		AD	Pos.	84.9	6,240	13.6	0.50	
		AE	Pos.	110.5	8,880	12.5	0.46	
		AF	Neg.	32.2	1,550	20.8	0.76	
		AG	Neg.	59.0	3,360	17.6	0.64	
		AH	Neg.	93.5	6,130	15.3	0.56	
		AI	Neg.	110.5	9,075	12.2	0.45	
G alone	Current wave 20x50 Voltage wave 19x51	*S*	Neg.	32.2	1,500	21.5	0.89	Average Megger resistance of ground *G*, 24.25 ohms. Major breakdown in ground at 6,000-ampere current and above
		T	Neg.	63.5	3,200	19.8	0.82	
		U	Neg.	91.8	5,550	16.5	0.68	
		V	Neg.	123.5	10,850	11.4	0.47	
		AK	Neg.	111.0	9,400	11.8	0.49	
		Y	Pos.	86.7	6,000	14.5	0.60	
		Z	Pos.	115.5	8,400	13.8	0.57	
		AA	Pos.	31.8	1,490	21.4	0.88	
F and *G* in parallel	Current wave 20x46 Voltage wave 19x47	*AL*	Pos.	115.0	8,130	14.2	0.59	Average Megger resistance of ground *F–G*, 14.5 ohms. Slight breakdown in ground at higher currents
		BC	Neg.	54.1	4,320	12.5	0.86	
		BD	Neg.	83.0	7,250	11.5	0.79	
		BE	Neg.	116	11,350	10.2	0.70	
P alone	Current wave 20x45 Voltage wave 20x45	*AS*	Neg.	26.5	2,480	10.7	0.78	Average Megger resistance of ground *P*, 13.75 ohms
		AT	Neg.	50	4,770	10.5	0.76	
		AU	Neg.	78.3	8,170	9.6	0.70	
		AV	Neg.	106.5	15,500	6.9	0.50	
		AW	Pos.	27.4	2,170	12.6	0.92	
		AX	Pos.	46.1	4,310	10.7	0.78	
		AY	Pos.	73.5	8,250	8.9	0.65	
		AZ	Pos.	94.5	11,700	8.1	0.59	
		BA	Pos.	95.8	11,700	8.2	0.60	

* In these tests the impulse generator consisted of three 100-kv, five-microfarad capacitor banks in series. Capacitance and inductance of test circuit were respectively 1.667 microfarads and approximately 250 microhenrys. No series resistance inserted in generator.

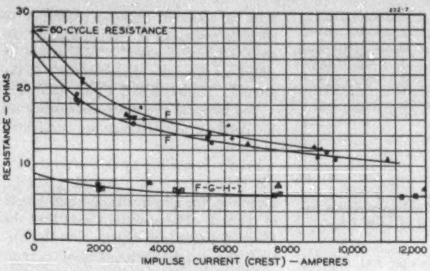


Figure 7. Variation of impulse resistance of clay grounds with polarity, wave shape, season and rain

Grounds in Clay

Test	Date	Polarity	Wave*	Weather
✚	Dec. '40	Pos.	20x50	Fair-cold
●	Dec. '40	Neg.	20x50	Fair-cold
⊙	Aug. '41	Neg.	18x45	Fair-warm
▽	Aug. '41	Neg.	18x45	Rain-warm
△	Aug. '41	Neg.	10x20	Rain-warm
⊡	Aug. '41	Neg.	18x40	Fair-warm

* Nominal value.

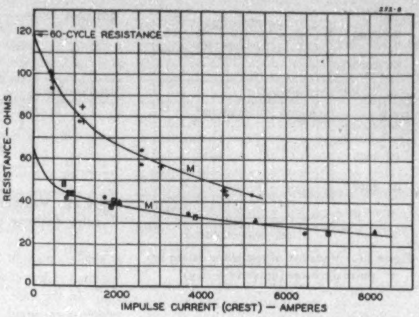


Figure 8. Variation of impulse resistance of sand ground with polarity, wave shape, season, and rain

Ground in Sand

Test	Date	Polarity	Wave*	Weather
✚	Dec. '40	Pos.	15x90	Fair-cold
●	Dec. '40	Neg.	15x90	Fair-cold
⊙	Aug. '41	Neg.	15x65	Fair-warm
△	Aug. '41	Neg.	8x25	Rain-warm
⊡	Sep. '41	Neg.	15x65	Fair-warm

* Nominal value.

measure 43 per cent of the resistance of a ten-foot ground driven in the same clay soil as *E, F, G, H, I, J*. Experimentally, ground *P* is 48 per cent of the average resistance of the six grounds. Equation 1 is also useful to determine the resistivity of different soils. Let us consider ground *K* in gravel, *M* in sand, and the grounds in clay. From the data we find that the average resistivities of the clay, sand, and gravel soils in this investigation are respectively 9,000, 20,800, and 41,800 ohm-centimeters or the resistivities are in the ratio of 1.0, 2.31, and 4.65.

For grounds in parallel, the 60-cycle resistance depends on the physical dimensions of the individual rods, the spacing and geometrical configuration of the rods, and the resistivity of the soil. The formulas and relationship for the parallel-ground arrangements in this investigation are discussed in appendix I. When the soil is fairly homogeneous, it is possible to determine the resistance of grounds combined in parallel, provided either the resistances of the single grounds are known experimentally, or the average resistivity of the soil has been established. Calculated and experimental data for the parallel grounds are compared in Table II. Grounds *E, F, G, H, I, J* in clay are in fairly homogeneous soil. The calculated resistance for the parallel pairs, *F–G* and *H–I*, is 57 per cent of the average single-ground resistance, for the four grounds *F–G–H–I* in a square arrangement and *E–F–G–J* in a row, it is respectively 35 and 33 per cent. The agreement between the calculated and experimental values for the two pairs, *F–G* and *H–I*, is within three per cent. For the two parallel groups, *F–G–H–I* and *E–F–G–J*, the agreement is within ten per cent. Part of the difference is accounted for by error in measurement, part is due to the assumptions in the calculation. Methods for determining the parallel resistance of two rods driven at different depths, as grounds *K* and *L*, have been developed. The calculated and experimental values for these two grounds which are in gravel lie within ten per cent of each other. A difference of 25 per cent is found for ground *N–O* in parallel. These two rods physically are identical. The large discrepancy is due likely to the instability of ground *N*, which is driven close to a wood pole, adjacent to a fence and possibly to other conducting objects in the soil, and is embedded in rocky soil. In summary, the resistance of parallel grounds can be calculated with good engineering approximation as a per cent of the resistance of the individual grounds, provided the conditions of the soil are fairly homogeneous, and no major extraneous effects are present.

The 60-cycle data in Table IX show that 85 per cent of the voltage applied to

Figure 9. Impulse characteristic for various soils and grounds

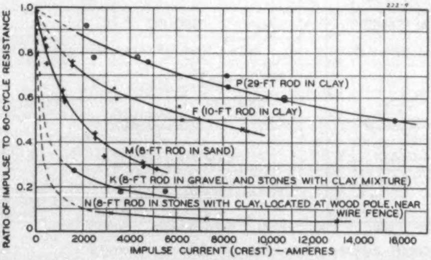
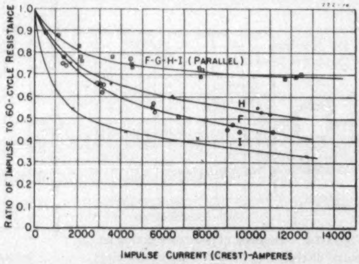


Figure 10. Impulse characteristic for single and parallel grounds



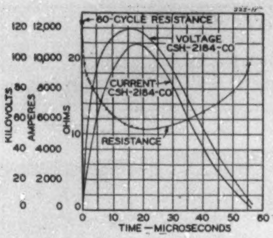


Figure 11. Variation of resistance during impulse discharge (ground *F*—clay)

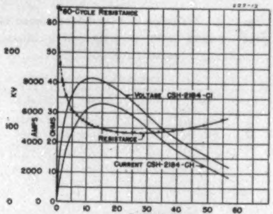


Figure 12. Variation of resistance during impulse discharge (ground *M*—sand)

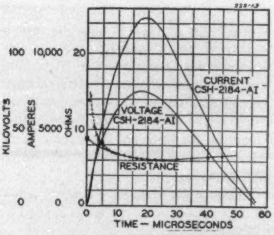


Figure 13. Variation of resistance during impulse discharge (parallel ground *F–G–H–I*—clay)

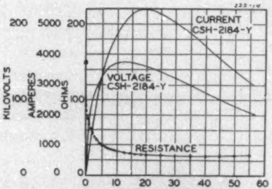


Figure 14. Variation of resistance during impulse discharge (ground *K*—gravel and clay)

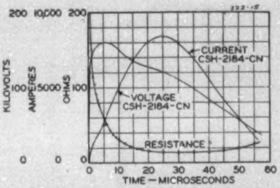


Figure 15. Variation of resistance during impulse discharge direct to earth (clay)

a single one-inch diameter rod appears within a radius of about ten feet from the rod. In the case of four rods in parallel which correspond to the four legs of a tower structure in the earth, 75 per cent of the voltage drop is absorbed within the first ten feet from the footings. Conditions of the soil can change this distribution of voltage and gradients, but on the whole, these data are in good agreement with the findings of other investigators.[4] In fairly homogeneous soil, it is apparent that the resistance of grounds is determined largely by the mass of earth within a few feet of the rods. In this confined region relatively high gradients may be developed, particularly adjacent to the rod. Analysis of the voltage distribution has been made and the results agree with the experimental data.

Impulse Characteristics

A. Variation of Resistance With Current

This and previous investigations indicate that when impulse currents are applied to driven grounds, the resistance decreases considerably with increasing current. The nature of this decrease can be shown by plotting the apparent resistance (crest voltage divided by crest current) against the crest current, as illustrated in Figures 7 and 8. To compare kinds of soil and types of grounds, the ratio of apparent impulse resistance to 60-cycle resistance is plotted against the crest impulse current. This curve is called the "impulse characteristic" of the ground: Figures 9 and 10 are examples.

The physical processes accounting for the decrease in resistance are considered in the next section.

Briefly, according to this analysis, the soil breaks down at a critical gradient, increasing the effective radius and length of the rod. The analysis shows that the effective radius is proportional to the current above critical breakdown current which corresponds to critical gradient at the rod surface.

B. Factors Affecting Impulse Characteristic

The impulse resistance data of Tables III and V for ground *F* in clay and of Table VII for parallel ground *F–G–H–I* all in clay are plotted in Figure 7. Similarly the data of Tables IV and VI for ground *M* in sand are plotted in Figure 8. These figures show the effect on impulse resistance of the polarity of the wave, the wave form, the seasonal changes, and other influencing factors due to the weather such as rain.

Present data confirm that polarity has little effect on impulse resistance. Within the range of waves applied in this and the previous investigation (6x13 to 20x120), the wave of the impulse does not affect in an appreciable amount the impulse resistance characteristic.

On the other hand, a seasonal variation is clearly in evidence and is an important factor. For instance, the impulse resistances at 1,000 amperes for grounds *F* and *M* are subject to about the same per cent variation as the corresponding 60-cycle resistances. The seasonal effect is particularly marked at the

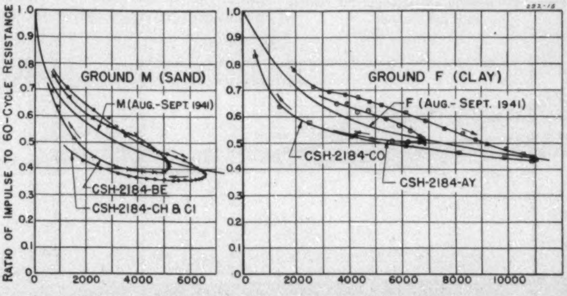


Figure 16. Variation of ratio during impulse current discharge compared with characteristic

low and medium currents but decreases at the higher currents. Tests on these grounds made within a few days of each other, both in fair weather and under or after sustained rain, indicate that rain does not affect the impulse resistance appreciably.

According to the analysis referred to previously, there are two properties of soil which determine in a large measure the impulse characteristic of a ground driven in it. These are the resistivity which determines the gradient for a given current density and the critical gradient at which the soil breaks down. The influence of soil on the impulse characteristic of grounds is illustrated in Figure 9. For comparison of results, curves for the similar grounds, *F*, *M*, *K*, and *N* are taken respectively from the data of Tables III, IV, and VIII. Considering critical gradient the same for two soils of different resistivity, the impulse characteristic of a rod driven in the high-resistivity soil will be moved to the left of the impulse characteristic of a similar rod in the low-resistivity soil by an amount approximately equal to the ratio of the lower resistivity to the higher resistivity. This relation is apparent from the curves in Figure 9 for grounds *F* in clay, *M* in sand, *K* in gravel and stones with clay, and *N* in stones with clay. As stated previously the 60-cycle soil resistivity for clay (*F*), sand (*M*), and gravel (*K*) are in the ratios of 1.0, 2.3, and 4.7. The characteristic curves for the three grounds are displaced to the left by an amount bearing an inverse relation to the corresponding resistivities of the soils. In short, grounds in very low-resistance soils approach the flat characteristic curve through unity which corresponds to a metallic conductor, while grounds in high-resistivity soil and of high resistance have a characteristic falling abruptly to low ratios. Grounds driven in high-resistivity soils such as *N*, *K*, and to a lesser extent *M*, may suffer in addition major breakdowns in the body of the soil, beginning at medium or even rather low currents, thus accentuating the characteristic still more. The voltage oscillogram of Figure 6-*AQ* illustrates a major breakdown in the body of the soil for ground *M* which occurred at a rather high current. This tendency for high-resistance grounds to break down at low currents has the effect of bringing closer together the resistance of low- and high-resistance grounds for high currents. For instance, ground *K* in gravel has a Megger value about six times as high as *F*,

Table IV. Impulse Measurements of Ground M (Sand)

Period of Tests 11-30-40 to 12-2-40

Cathode-Ray Oscillogram 2075	Wave Form: Voltage (Microseconds)	Wave Form: Current (Microseconds)	Measured Ground Crest Values: Kv	Measured Ground Crest Values: Amperes	Impulse Resistance (Ohms): Resistance at Crest Voltage (1)	Impulse Resistance (Ohms): Resistance at Crest Current (2)	Impulse Resistance (Ohms): Apparent Resistance Crest V/I (3)	Ratio of Impulse to 60-Cycle Resistance (1)	Ratio of Impulse to 60-Cycle Resistance (2)	Ratio of Impulse to 60-Cycle Resistance (3)	Comments*
Negative Polarity											
F	10x90	20x90	133	2,550	57.2	44.3	52.2	0.49	0.38	0.44	Average Megger resistance of ground *M*, 118 ohms. Slight breakdown at 4,500-ampere current and above
K	8x115	8x115	42.5	457	93	93	93	0.79	0.79	0.79	
L	8x85	25x90	125.5	2,550	64	45.8	49.2	0.54	0.39	0.42	
M	10x70	25x70	170	5,150	43.5	30.5	33.0	0.37	0.26	0.28	
R	9x100	16x100	82	1,100	77.4	73.1	74.6	0.66	0.62	0.63	
AM	8x125	8x125	41.9	425	98.5	98.5	98.5	0.83	0.83	0.83	
AR	10x60	25x65	165.5	4,500	45.4	28.6	36.9	0.38	0.24	0.31	
Positive polarity											
O	8x95	20x95	80.8	1,180	77.6	64.7	68.5	0.66	0.55	0.58	
P	8x75	25x85	122	3,000	56.5	35.3	40.7	0.48	0.30	0.34	
Q	8x65	25x70	155	4,570	43.2	30.2	34.0	0.37	0.26	0.29	
AN	8x125	8x125	40.4	400	101	101	101	0.86	0.86	0.86	
AO	8x125	8x125	41.7	433	96.7	96.7	96.7	0.82	0.82	0.82	
AP	8x100	25x100	81	1,150	84.5	64.4	70.5	0.72	0.54	0.60	
AQ	8x60	25x65	165.5	4,500	45.4	28.6	36.9	0.38	0.24	0.31	

*Same arrangement of impulse generator as in Table III. Inductance of test circuit approximately 200 microhenrys.

Table V. Impulse Measurements of Ground F (Clay)—Influence of Season, Rain, and Other Atmospheric Conditions; Effect of Wave

Cathode-Ray Oscillogram 2184	Wave Form*: Voltage (Microseconds)	Wave Form*: Current (Microseconds)	Measured Ground Crest Values: Kv	Measured Ground Crest Values: Amperes	Impulse Resistance (Ohms)	Ratio of Impulse to 60-Cycle Resistance**	Date of Tests	Atmospheric Conditions	Comments***
A & *B*	14x50	17x50	24.4	1,310	18.6	0.75	8-22-41	Fair	Impulse generator capacitance $C_s = 1.67$ microfarads
C	16x50	18x50	47.7	3,120	15.3	0.62			
D	15x46	18x45	73.2	5,610	13.0	0.53			
E	16x43	19x42	99.4	9,000	11.0	0.45			
K	16x50	18x50	26.1	1,360	19.2	0.78	8-23-41	Fair	$C_s = 1.67$ microfarads
M	15x47	17x50	48.6	3,000	16.2	0.66			
N	16x46	18x45	78.0	5,540	14.1	0.57			
Q	16x44	20x42	103	9,570	10.8	0.44			
AJ	15x50	16x50	25.0	1,375	18.2	0.74	8-25-41	Rain night before and during the tests	$C_s = 1.67$ microfarads
AK	15x47	17x50	49.0	3,020	16.2	0.66			
AL	16x45	18x45	74.2	5,500	13.5	0.55			
AN	15x44	19x43	108	9,270	11.7	0.47			
AV	20x80	23x90	24.6	1,130	21.8	0.88	8-25-41	Rain	$C_s = 5$ microfarads
AW	20x80	23x90	12.2	553	22	0.89			
AX	8x22	9x22	48.2	2,950	16.3	0.66	8-26-41	Heavy rain night before tests	$C_s = 0.42$ microfarad
AY	8x22	10x22	86	6,750	12.7	0.51			
AZ	8x21	11x22	121	11,220	10.8	0.44			

*Negative polarity.

**Average Megger resistance of ground *F*, 24.7 ohms.

***Capacitance (C_s) of test circuit as indicated; inductance of test circuit approximately 250 microhenrys. No series resistance inserted in generator.

yet on a 5,000-ampere impulse discharge, it has an impulse resistance only twice that of *F*. All things being equal, a ground inherently low in 60-cycle resistance is desirable and preferred.

The influence of the effective length of rod in the earth on the impulse characteristic of grounds is illustrated in Figures 9 and 10. For comparison of results, the curves for grounds *P* and *F* in Figure 9 are from the data in Table III, and the curves for grounds *F*, *H*, and *I*, and *F–G–H–I* in Figure 10 are from Tables V and VII. Following the analysis previously mentioned, the impulse characteristic for a long rod, compared to that for a short rod of the same diameter and in the same soil, lies displaced along the current axis in proportion to the ratio of the lengths. As an example in Figure 9 the characteristic is shown for rod *P* which has 2.9 times the length of *F*. For the same ratios of impulse to 60-cycle resistance, the curve corresponds to currents from 2.2 to 3 times the values for *F*. The curve for the parallel ground *F–G–H–I*, Figure 10, is also compared with a single ground. Ground *F* which is representative of the four single grounds is used for this comparison. The total length of the four rods is four times the length of a single rod, and the characteristic of *F–G–H–I* would, therefore, be displaced to the right of *F* by a factor of four except for the mutual effect. For a ten-foot spacing the mutual effect is not great. For the same ratios the characteristic of parallel ground *F–G–H–I* lies displaced to currents that are two to four times the

Table VI. Impulse Measurements of Ground *M* (Sand)—Influence of Season, Rain, and Other Atmospheric Conditions; Effect of Wave

Cathode-Ray Oscillogram 2184	Wave Form*		Measured Ground Crest Values		Impulse Resistance (Ohms)			Ratio of Impulse to 60-Cycle Resistance** (3)	Date of Test	Atmospheric Conditions	Comments***
					Resistance at		Apparent Resistance				
	Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes	Crest Voltage (1)	Crest Current (2)	Crest V/I (3)				
R	8x80	12x80	32.2	795	41.7	41.4	41.7	0.64	8-23-41	Fair	Impulse generator capacitance, C_s = 1.67 microfarads. Slight breakdown in ground at higher currents
S	8x70	13x70	65.0	1,730	42.0	36.7	37.6	0.58			
T	10x60	15x70	116.5	3,650	34.3	30.6	31.9	0.49			
U	12x50	17x50	161.0	6,420	25.8	23.6	25.1	0.39			
AO	10x70	12x80	33.1	795	41.6	40.9	41.6	0.64	8-25-41	Rain night before and during the tests	C_s = 1.67 microfarads. Major breakdown in ground at higher currents
AP	6x50	20x60	68.5	2,340	41.8	25.9	29.2	0.45			
AQ	15x45	20x45	80.5	5,150	15.8	15.1	15.6	0.24			
AS	15x42	18x40	113	9,220	12.8	10.9	12.3	0.19			
AT	15x150	15x150	14.1	285	49.5	49.5	49.5	0.76	8-25-41	Rain	C_s = 5.0 microfarads
AU	12x150	12x150	28.3	570	49.6	49.6	49.6	0.77			
BD	5x25	7x25	76.4	2,010	38.9	37.5	38.0	0.59	8-26-41	Heavy rain night before tests	C_s = 0.42 microfarad
BE	6x23	9x23	145	5,250	30.8	26.2	27.6	0.43			
BF	6x20	9x21	200	8,100	26.7	23.2	24.7	0.38			
BY	10x70	12x80	36.2	840	43.1	42.4	43.1	0.67	9-10-41	Fair	C_s = 1.67 microfarads. Slight breakdown in ground at higher currents
BZ	10x70	10x60	35.4	740	47.8	47.8	47.8	0.74			
CA	10x60	12x60	72.7	1,915	37.9	37.6	37.9	0.58			
CB	10x70	12x80	41.0	935	43.8	41.5	43.8	0.68			
CC	12x65	12x70	73.6	1,960	39.5	34.9	37.6	0.58			
CD	10x55	15x55	122	3,840	32.6	28.2	31.7	0.49			
CE	12x45	18x47	172	7,000	26.1	23.9	24.6	0.38			

*Negative polarity.

**Average Megger resistance of ground *M*, 65 ohms.

***Capacitance (C_s) of test circuit as indicated; inductance of test circuit approximately 200 microhenrys. No series resistance inserted in generator.

Table VII. Impulse Measurements of Grounds *H, I, F–G–H–I* (Clay)

Period of Tests 8-22-41 to 9-9-41

Driven Grounds	Cathode-Ray Oscillogram 2184	Wave Form*		Measured Ground Crest Values		Impulse Resistance Ohms	Ratio of Impulse to 60-Cycle Resistance	Comments**
		Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes			
F–G–H–I in parallel	*F–G*	18x40	20x43	14.5	2,160	6.70	0.76	Impulse generator capacitance C_s = 1.67 microfarads. Average Megger resistance of ground *F–G–H–I* in parallel, 8.8 ohms
	H	18x42	20x42	29.2	4,540	6.44	0.73	
	I	18x42	20x42	49.4	7,820	6.31	0.72	
	J	18x40	20x40	70.0	11,700	5.98	0.68	
F–G–H–I in parallel	*AD*	18x40	20x42	14.7	2,150	6.84	0.78	Impulse generator capacitance C_s = 1.67 microfarads. Average Megger resistance of ground *F–G–H–I* in parallel, 8.8 ohms
	AE	18x41	19x42	14.7	2,015	7.30	0.83	
	AF	18x41	20x42	30.0	4,450	6.74	0.77	
	AG	17x38	18x39	29.7	4,500	6.60	0.75	
	AH	18x41	20x41	46.9	7,730	6.07	0.69	
	AI	18x39	20x41	74.5	12,200	6.10	0.69	
F–G–H–I in parallel	*BA*	9x20	10x21	24.6	3,600	6.83	0.78	C_s = 0.42 microfarad
	BB	9x19	11x21	49.5	7,720	6.40	0.73	
	BC	9x19	10x20	76.4	12,450	6.13	0.70	
I Alone	*BO*	17x44	18x42	25.4	1,770	14.34	0.54	C_s = 1.67 microfarads. Average Megger resistance of ground *I*, 24.5 ohms
	BP	17x41	19x40	50.3	4,220	11.90	0.44	
	BQ	18x44	20x40	75.8	7,600	9.97	0.41	
	BR	20x42	21x39	102.5	12,700	8.07	0.33	
H alone	*BS*	16x43	18x43	55.5	3,500	15.6	0.66	C_s = 1.67 microfarads. Average Megger resistance of ground *H*, 23.5 ohms
	BT	16x42	17x38	90.5	6,480	14.0	0.60	
	BV–BU	18x43	19x41	133	11,000	12.1	0.52	
	BV	16x42	18x38	133	10,400	12.8	0.55	
	BW	16x43	19x40	132	10,600	12.5	0.53	
	BX	16x42	17x44	29.8	1,680	17.7	0.75	

*Negative polarity.

**Capacitance (C_s) of test circuit as indicated; inductance of test circuit approximately 250 microhenrys. No series resistance inserted in generator.

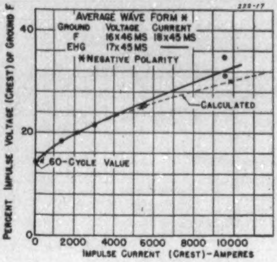


Figure 17. Voltage developed on *E–H–G* from impulse to ground *F*

currents corresponding to the characteristic of *F*. There are other factors in regard to parallel grounds which will be discussed later.

The presence of metal fences, pipes, and other extraneous objects located adjacent to a driven ground can materially affect the impulse characteristics of the ground. This is the case for grounds *N* and *O*. The particular point to note here is that for the same soil, the ratio of impulse to 60-cycle resistance when referred to unit length of rod or to current density at the rod surface is a fairly typical form of curve which is a characteristic of the soil; although a departure from this characteristic may be expected even for grounds driven in presumably identical soil. This variation is due to the fact that the soil may not be altogether homogeneous and entirely the same throughout the mass of earth in which the grounds are driven, but can vary considerably from location to location as the curves for *H*, *F*, and *I* in Figure 10 clearly demonstrate. It is apparent from Figure 3 that a soil which for convenience may be classified as a kind can vary even for a single ground.

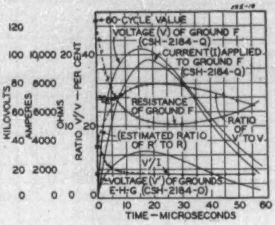


Figure 18. Variation of voltage on grounds *E–H–G* during impulse discharge to ground *F*

C. Analysis of Impulses

Valuable information concerning impulse characteristics can also be obtained by a direct analysis of the individual oscillograms. The method used is to plot the voltage and current from a given oscillogram to a uniform time scale and plot the resistance throughout. Typical oscillograms for grounds in clay, sand, and gravel are plotted and examined in Figures 11, 12, 13, 14, 15, and 18. Some of these oscillograms are practically free of superimposed oscillations on the front, as discussed in a preceding section with reference to oscillogram *CI* of Figure 6. In others these extraneous effects are averaged out.

In Figures 11 and 18, the resistance of ground *F* in clay is analyzed during the discharge for impulses close to 10,000 amperes crest. These results are in substantial agreement with the previous studies[1] on single grounds in clay soil. At the start of the impulse, the resistance appears to be the same as the 60-cycle value, dropping with the rising current on the front and reaching a minimum near and just beyond the crest current. As the current recedes beyond the crest, the resistance rises, apparently returning or directed toward the initial value. With this high current, the resistance does not vary more than 15 per cent from the minimum value within ten microseconds of the crest current. For lower currents the variation is less. As illustrated in *AA* of figure 5, for moderate currents the resistance varies only a small per cent over a relatively wide range of the discharge before and after crest current. For currents in the order of 5,000 amperes for a 10-foot ground (*F*) and 15,000 amperes for a 29-foot ground (*P*), as shown in *AL* and *AV* of Figure 5, the variation over some ten microseconds before and after crest is still within ten per cent of the minimum

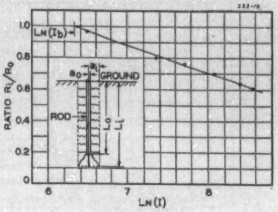


Figure 19. Method of determining the impulse characteristic of driven grounds

Table VIII. Impulse Measurements of Grounds *K*, *L*, *N*, and *O*

Gravel and Stones With Clay Mixture
Period of Tests 8-22-41 to 9-9-41

Driven Grounds	Cathode-Ray Oscillogram 2184	Wave Form*		Measured Ground Crest Values		Impulse Resistance Ohms	Ratio of Impulse to 60-Cycle Resistance	Comments**
		Voltage (Microseconds)	Current (Microseconds)	Kv	Amperes			
K alone	*V*	9x100		12.2				Average Megger resistance of ground *K*, 149 ohms. Breakdown in ground at 1,500-ampere current and above
	W	12x70	22x80	63.2	1,590	39.7	0.27	
	X	15x65	20x65	98.0	3,600	27.2	0.18	
	Y	15x53	20x60	146	5,530	26.4	0.18	
	Y	14x58	20x60	150	5,530	27.1	0.18	
L alone	*Z*		18x70		915			Average Megger resistance of ground *L*, 52 ohms.
	AA	14x60	16x58	56.6	2,110	26.8	0.52	
	AB	15x55	17x52	97.2	4,070	23.9	0.46	
	AC	14x52	18x50	137	5,830	23.5	0.45	
O alone	*BG*	14x40	19x40	26.4	2,150	12.3	0.40	Average Megger resistance of ground *O*, 31 ohms. Breakdown in ground at 2,000-ampere current and above
	BH	15x39	19x40	42.5	5,050	8.42	0.27	
	BI	15x37	20x38	64.2	9,680	6.63	0.21	
	BJ	18x40	21x38	87.0	17,200	5.05	0.16	
N alone	*BK*	16x37	20x36	14.8	3,180	4.65	0.08	Average Megger resistance of ground *N*, 55 ohms, Breakdown in ground early in discharge
	BL	15x37	20x36	25.0	7,400	3.38	0.06	
	BM	16x35	20x35	35.4	13,000	2.72	0.05	
	BN	16x37	21x36	49.2	20,300	2.42	0.04	

*Negative polarity.

**Capacitance of test circuit, $C_s = 1.67$ microfarads. Inductance of test circuit approximately 250 microhenrys for grounds *K* and *L*, and approximately 150 microhenrys for grounds *O* and *N*. No series resistance inserted in generator.

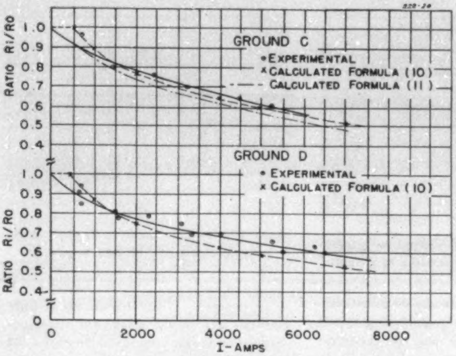


Figure 20. Impulse characteristic for grounds *C* and *D*

Calculated and experimental curves compared

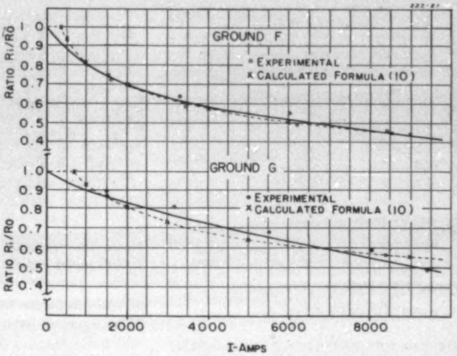


Figure 21. Impulse characteristic for grounds *F* and *G*

Calculated and experimental curves compared

value. In view of this small variation, the impulse resistance can be defined adequately as the ratio of crest voltage to crest current.

For the ground in sand (*M*) due to the higher resistivity of the soil, a greater variation is naturally encountered at the currents mentioned above. Figure 12 strikingly shows that the initial drop in the resistance from 65 ohms to 31 ohms at five microseconds follows rapidly the corresponding rise of the current from zero to two-third crest. As these oscillograms and the data in Tables IV and VI show, the resistance values of ground *M* corresponding to crest voltage and to crest current for these higher currents may differ as much as 30 per cent. Practical considerations have suggested to the authors that the ratio of crest voltage to crest current be accepted as the resistance, but to avoid possible misinterpretation, it is designated as the apparent resistance. As shown in Figure 14, even greater variation in the resistance is noted for ground *K* in gravel. An abrupt change from the initial value of 150 ohms to 60 ohms occurs in the first two microseconds. This rapid drop possibly is partly due to a major breakdown in the soil.

In Figures 11 and 18 the resistance curve of the clay ground *F* is indicated as a dotted line, dropping rapidly from the 60-cycle value. Even with good smooth oscillograms, a difficulty, in determining the true resistance initially and on the steep rising front during the first few microseconds, arises from the fact that the voltage (*E*) in addition to the resistance drop (*Ri*) includes an inductive drop (*Ldi/dt*) due to the rapid rising current and the presence of some inductance in the ground proper and the connections. For this reason, the curves determined from the ratio of voltage to current (*E/I*), which closely represent resistance down to three or four microseconds, are dotted in below these values. Estimating the inductance and correcting for the inductive drop, the authors find that the curve of the true resistance still points essentially toward the 60-cycle value initially at time zero. In Figure 13 is presented a similar analysis for parallel grounds *F–G–H–I* for an impulse in the order of 12,000 amperes crest. Up to this current the variation of the resistance for this ground, as is expected, naturally is relatively small. The true resistance during the discharge is shown by the full line. The high ratio of inductance to resistance inherent in this low-resistance ground thus shows emphatically the presence of the inductive effect (dotted line is *E/I*) in the early part of the discharge. For the higher-resistance grounds as *M*, the initial inductive drop is less prominent than for *F*, so that the ratio *E/I* in Figure 12, even below three or four microseconds, practically corresponds to the true resistance of ground *M*.

From the initial abrupt rise or "kick" of the voltage and the initial rate of rise of the current, scaled from a number of oscillograms, we estimate that the inductance as measured is about 15 microhenrys. Actual calculations of the inductance based on the dimensions of the driven rods and the lead connections of

Figure 22 (left). Impulse characteristic for ground *M*

Calculated and experimental curves compared

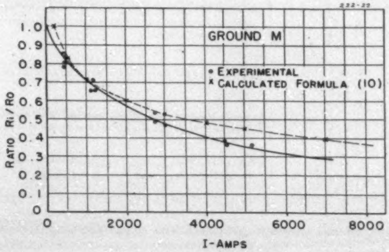
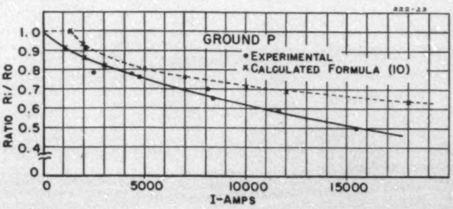


Figure 23. Impulse characteristic for ground *P*

Calculated and experimental curves compared



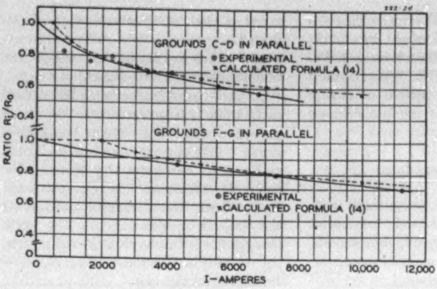


Figure 24. Impulse characteristic for parallel grounds C–D and F–G

Calculated and experimental curves compared

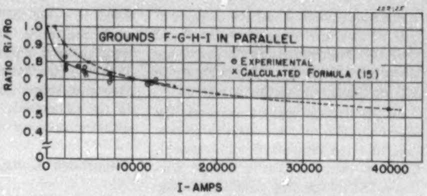


Figure 25. Impulse characteristic for parallel ground F–G–H–I

Calculated and experimental curves compared

the test confirm this value. About a third of the inductance as measured is in the rod proper, and even less in the large mass of the earth path of the discharge. The remainder, about ten microhenrys, is picked up in the leads, partly in the short segment of the discharge circuit proper from the top of the rod to the potential lead, and a greater part induced from the discharge circuit in the potential lead. The potential lead extends some 30 feet to the voltage divider, which is located adjacent to the oscillograph room.

The variation of the ratio of impulse to 60-cycle resistance during discharge for grounds *F* and *M* and currents from 5,000 to 11,000 amperes (crest) is shown in Figure 16. The ratio is plotted against current and is compared to the impulse characteristic (ratio for crest current) for each of the two grounds. The variation during discharge follows fairly close to the characteristic, intersecting it near crest current, and then returns below the characteristic, describing a considerable loop much in the same manner as found in the first investigation.[1] Since an inductive drop is present for the rapidly increasing part of the current, the true resistance curves would be even closer for the downward part than indicated. The analysis of these data and the previous curves establish that the variation of the true resistance rapidly follows the current with no appreciable time lag for wave fronts as fast as some two or three microseconds, the limit that the data can be interpreted. On the basis that the lowering of the resistance is a breakdown or sparking phenomenon consisting of small arcs, theoretically it stands to reason that little or no time lag in the change of the resistance would be expected for wave fronts longer than one or two microseconds. The exact fundamental mechanism which determines the lowering of the resistance is not well defined and established, and it is quite possible that the true resistance for a very rapid rise of current to crest in one microsecond would result in a time lag in the decrease of the resistance from high to low value.

It is apparent from the oscillograms and the figures that the effect of the high resistance of the ground in the initial stage of the discharge is to displace the voltage wave from the current and give it a more abrupt and steeper front. This is clearly shown by a comparison of Figures 11, 12, 14, and 15 in their sequence order. The higher the resistance of the ground the more accentuated is the effect. The inductance of the ground and lead connection also adds to displace the front of the voltage even further forward. Therefore, the rate of rise of the voltage for the same current is proportionately greater for grounds in high-resistivity soils and having high inductance associated with them. Due to the sustained ionization process, the low value of the resistance at and beyond crest current is sustained for a good part of the duration. The subsequent recovery of the resistance does not seem to have particular practical significance.

The question may be raised as to the resistance of a lightning-stroke discharge direct to earth. Figure 15 records a typical discharge of 8,300 amperes crest to the open field (clay soil) adjacent to the laboratory. All driven grounds and conducting objects are well removed from the spot hit. A small part of the grass at the earth's surface was scooped out and thrown up by the discharge. The resistance initially is very high, but as the discharge penetrates or pierces the earth, it drops to 60 ohms in five microseconds and still further to a minimum of 18 ohms at crest current. The stroke discharge, therefore, "drives" its own ground, for the resistance attains a value not much greater than that of a ten-foot rod driven in soil similar to that struck. On the other hand, the initial high resistance develops a very steep-front voltage wave which reaches 90 per cent of its crest in about a microsecond: long before the current reaches its crest. The resistance continues to fall rapidly so that at crest current the voltage has dropped to 75 per cent of its crest value. There are a number of ramifications to this problem. These tests indicate that the resistance characteristic of the location hit by lightning may influence the rate of current discharge and, to some extent, limit the maximum current attained. The investigation of the influence of the earth on lightning should be continued.

D. Voltage Developed in Soil

The impulse characteristics of driven grounds are further revealed from a study of the voltages induced or developed on adjacent objects. Data on impulse tests of this kind are presented in Figures 17 and 18. In these tests impulse discharges from 1,000 to 10,000 amperes crest were

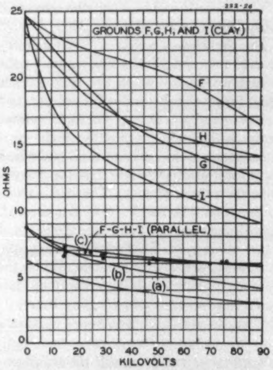


Figure 26. Method of determining impulse-resistance characteristic of parallel grounds from characteristics of component individual grounds

applied to ground *F* and the voltages *V* and *V′* measured respectively at *F* and *E–H–G*. The grounds *E*, *H*, and *G* are all spaced ten feet from *F* (see Figure 2) and are connected together in a group.

As the impulse current increases, the effective radius of *F* increases in proportion, and the ratio of *V′* to *V* should, therefore, increase; that is, a larger portion of the voltage drop should be outside the ten-foot radius. In Figure 17 the voltage developed on *E–H–G* is expressed as a per cent of the voltage at *F* so that the curve shows the variation of V'/V with the discharge current (crest). This ratio or coupling factor from a low current up to 10,000 amperes rises continuously, practically doubling in amount. These data further confirm that the effective size of the rod increases with the current.

In Figure 18 are reproduced oscillograms of the voltages and current and the variation of the coupling factor with the discharge. The voltage *V′* developed on *E–H–G* is of the same general form as the voltage *V* at *F*. The coupling factor V'/V naturally varies during the discharge. At crest current or slightly beyond, the effective radius and length of rod *F* reach a maximum; the coupling factor likewise rises to a maximum, and the resistance attains a minimum. As the current drops beyond crest, the sparking in the earth recedes, and ionization of the arcs subsides, so that the coupling factor falls off, and the resistance rises, both directed toward their 60-cycle values. From 20 microseconds (crest current) to 5 microseconds the coupling factor drops off. Still further down, two curves are shown, one dotted which represents the true coupling factor, and a heavy line through the circled dots which is merely numerically the ratio V'/V. The reason for the high ratio V'/V below five microseconds is found in the inductive effects and measurement. Due to the rapid current rise in the initial stage of the discharge, a voltage is picked up by the potential lead which runs some 30 feet from the grounds to the voltage divider. The lead is much the same whether *V* or *V′* is measured, with the result that an induced voltage of the same order is superimposed on the true voltage of the grounds. Thus the ratio V'/V below five microseconds does not represent the true coupling factor.

A curve has also been drawn on Figure 18 by dividing *V′* by *I*. This curve indicates the resistance outside the equipotential surface including *E–H–G* except for the first few microseconds which indicate inductive "kick." The resistance is practically constant, and, moreover, it is equal in value to the corresponding 60-cycle resistance, determined by taking 14.33 per cent of the 60-cycle resistance of ground *F* (see Table IX). This constant resistance shows that there is no change in the soil outside the ten-foot distance even for high currents.

Theoretical Considerations

This section deals theoretically with the physical processes that take place when ground rods carry impulse currents. The lowering of the resistance with increase of current is explained on the basis that at a critical current, a voltage gradient is attained at the rod surface causing the soil surrounding the rod to break down, thus increasing both the effective radius and length of the rod. The sketch insert in Figure 19 illustrates schematically the physical conditions which accompany a current above breakdown value. The irregular lines represent breakdown in the soil, and the dotted lines the enlarged effective dimensions of the rod. On this basis formulas and methods for determining the characteristic curves of grounds are developed. That breakdown of the soil may account for the lowering of the resistance had been proposed previously,[1,5,6] but the methods reported here enlarge on a quantitative approach which was set forth soon after the completion of the first paper.[7]

Table IX. 60-Cycle Voltage Distribution Tests

Date of Tests 7-22-40

Voltage Applied*		Voltage Recorded**	
Ground	Per Cent	Ground	Per Cent
J	100	*E*	3.3
		F	6.5
		G	11.9
		H	5.1
		I	8.1
		P	2.3
G	100	*E*	8.0
		F	15.5
		H	12.2
		I	15.8
		J	15.5
		P	3.0
F	100	*E*	13.5
		G	15.0
		H	14.5
		I	11.8
		J	7.5
		P	3.7
F–G–H–I in parallel	100	*J*	24.8
		E	26.6
		P	13.5
P	100	*F–G–H–I* in parallel	9.0
D–C in parallel	100	*A–B* in parallel	9.1
		Fence	0
A–B in parallel	100	Fence	0

*Voltage applied ranged from 80 to 200 volts. Current ranged from 5.5 to 18 amperes.

**Due to the voltmeter resistance (283 ohms) the actual voltage is a small amount greater than the per cent value indicated. No correction made.

On the basis of the foregoing theoretical explanation, there should be for each ground a current at which breakdown starts. This current we shall designate as I_b. It is determined by plotting the ratio of impulse to 60-cycle resistance R_t/R_o versus the natural logarithm of the impulse current lnI. The best straight line is drawn through the points, and where it crosses the ordinate $R_t/R_o = 1$, lnI_b is established. This procedure is illustrated for ground *C* in Figure 19. The value of I_b for grounds *C* and *D*, determined from the data in Table IV of the first paper, and for grounds *F*, *G*, *P*, and *M*, determined from the data in Tables III and IV of this paper, are tabulated in Table X. For all the grounds but *M*, R_t is obtained from the ratio of crest voltage to crest current. In the case of ground *M* (sand), R_t is obtained from the ratio of crest voltage to the current corresponding to crest voltage. In Table X are given also other essential data on these grounds; namely, the dimensions of each rod, the 60-cycle resistance, the soil resistivity ρ_o, and the critical gradient g_c at which the soil breaks down. The latter two quantities are determined respectively from equations 5 and 7 in appendix II.

Let us consider first the lowering of the impulse resistance as caused by the increase in the effective radius of the rod, disregarding the increase in length. On this basis we have equation 10 which relates the ratio R_t/R_o to the current *I*. From the data in Table X substituted in the formula, curves are calculated for the grounds *C*, *D*, *F*, *G*, *M*, and *P*. These are shown dotted in Figures 20, 21, 22, and 23, and may be compared directly with the experimental values. It is of interest to note that the effective radius of the ten-foot rods in clay increases to about ten inches at 10,000 amperes.

A refinement is introduced considering both the increase in radius and the increase in length as indicated by equations 11, 12, and 13 of appendix II. A curve for ground *C* has been calculated from these equations, substituting established values of a_t and estimated values of L_t. This curve is plotted in Figure 20. A rapid increase in the length at low currents, reaching an increase of about ten per cent of the length (L_o) at the higher currents, accounts for the lowering of the curve as shown.

The difference between the calculated curves and the experimental results is due,

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in part, to the various simplifying assumptions. We have assumed that the soil is homogeneous, that soil resistivity stays constant, and that breakdown in the soil is not accompanied by a voltage drop. Each of these assumptions is not fully valid, yet is sufficiently justified for an engineering approach. Allowing for the increase in rod length as discussed for ground *C*, and accounting for the voltage drop which accompanies breakdown, appear to bring the calculated curves for *C*, *D*, *F*, and *G* closer to the experimental data. The divergence for grounds *M* and *P* is apparently due to other factors which lower the characteristic at the higher currents a greater amount than the calculations indicate.

The validity of the breakdown process set forth above is further established by an analysis of the tests from which Figure 17 is obtained. Considering that the radius of *F* increases with current in the manner previously stated (equation 9, appendix II) and that the equipotential surfaces in the region included by *E–H–G* approach cylindrical form, the potential V' of *E–H–G* can be calculated as a function of current and is plotted in Figure 17 as the ratio V'/V. Other methods for calculating V' give essentially the same results. The agreement between experimental and calculated curves is apparent from the figure.

The procedure followed for a single rod can also be applied to parallel grounds. The methods are presented in appendix II. For two rods in parallel, the characteristic is given in equation 14, and for four rods arranged at the corners of a square, in equation 15. Calculated curves based on increase in radius only and experimental curves for parallel grounds *C–D* and *F–G* are compared in Figure 24. In these parallel grounds the two individual grounds in each arrangement are quite alike, and this similarity accounts for the close agreement between the calculated and experimental results. Allowance for the increase in the effective length of the rods with current would move the calculated curves in Figure 24 closer to the experimental curves in much the same manner as found for the single rods. The curves for the parallel ground *F–G–H–I* are plotted in Figure 25. The effect of the increase in length of the rods again is to move the calculated curve bodily down as described previously for other grounds.

The question arises whether the characteristic of a parallel ground may be predetermined from the characteristic curves of the individual grounds which comprise the parallel ground. In this connection ground *F–G–H–I* is examined in Figure 26, which shows resistance plotted against voltage. When an impulse is applied to grounds in parallel, the same voltage drop is present in each, and voltage is, therefore, used as the proper basis for comparison. In the figure the experimental characteristics are plotted and designated as *F*, *G*, *H*, *I*, and *F–G–H–I*. It is again to be noted that the single grounds differ. The characteristics of the four single grounds are first combined in parallel directly by the reciprocal relation as four completely independent grounds should be combined and are plotted in curve *a*. It is apparent that the mutual effect between the rods raises the resistance by an amount dependent on the mutual factor. For low current, the actual resistance for the four grounds in parallel is raised by a factor $0.35/0.25 = 1.4$ as found by equation 3, appendix I. Curve *b* is obtained by applying the factor 1.4 to curve *a*. Curve *b* follows the experimental curve at low currents but deviates at the higher currents, for the reason that the effective radius and length of the rods increase with current and the mutual factor becomes greater. The correct factor, which increases from 1.4 at low current to 1.65 at 10,000 amperes (62 kv), applied to curve *a*, thus gives curve *c*, which is the correct characteristic derived from the experimental curves of the four individual rods. Curve *c* compares favorably with the actual experimental characteristic of parallel ground *F–G–H–I*, and this comparison, therefore, shows that it is possible to predetermine the characteristic of a parallel ground from the experimental curves of the single grounds provided they are not too dissimilar.

Table X. Impulse Resistance Data for Ground Rods

Rod	Length Centimeters	Radius Centimeters	R_o Ohms	ρ_o Ohm-Centimeters	I_b Amperes	g_c Volts Per Centimeter
C	228	1.27	39.5	10,150	550	3,070
D	228	1.27	38.5	9,950	495	2,710
F	295	1.27	27.5	8,720	345	1,270
G	295	1.27	24.25	7,680	680	2,210
P	885	0.80	13.75	10,250	1,180	2,740
M	236	0.80	118	28,900	173	4,240

The foregoing analysis establishes that the decrease in resistance of driven grounds with increasing impulse current is due to the increase of effective radius and length. The agreement between calculated and experimental curves is close enough to establish that the soil surrounding a rod does have a critical voltage gradient at which it breaks down. This gradient is in the order of one to four kilovolts per centimeter. It follows from the equations, provided ρ_o and g_c of the soil are known, that the breakdown current I_b can be determined. Then from the dimensions of the rod or driven-ground system and the equations, the impulse characteristics of the grounds can be calculated. The need for further experimental study of the factors ρ_o and g_c is apparent.

In addition to providing a basic explanation of the physical processes involved, the results of this paper show that the calculated characteristic curve of a ground may be extrapolated with reasonable assurance to higher currents than the experimental data permit. Furthermore, the methods may be extended to determine the relative characteristics of grounds for various conductors and arrangements.

Effect of Discharge-Circuit Inductance and Conditions of Earth

Other important factors connected with the problem of grounds are the effect of the discharge-circuit or lead inductance and the conditions in the earth. A full discussion of these factors lies within the scope of the third paper proposed in the introduction. However, in the interest of a more complete survey of the problem they are dealt with here briefly.

A ground installation physically consists of a lead or tower structure which connects the grounded part of the electrical apparatus (protective device, apparatus tank, overhead ground wires, and so on) to the ground proper in the earth. The downlead or tower essentially can be considered an inductance, while the ground proper acts as a resistance in the manner discussed previously. The lead or tower inductance becomes particularly significant when steep-front currents of high magnitude, rising to crest in the order of a microsecond, are discharged to earth. Analysis shows that for high rates of rise of current, the voltage developed in a 25- to 50-foot lead or a 50- to 75-foot tower may well attain and exceed the ground-resistance drop. This lead or tower drop is of short duration and is practically over in one or two microseconds. Besides the lead or tower inductance, appreciable inductance may be present also in the ground proper, as is the case for grounds driven deep into the earth, or for a counterpoise spread over a wide area. Operating experience in the field [8] and the results of tests and co-ordination of station apparatus on steep-front impulses[9] corroborate the importance of inductance effects in protection.

For grounds of very high resistance, the capacitance of the rod or ground conductor to the mass of the earth is not entirely a negligible factor. The distribution of the electric charges in the earth preceding the current discharge through the ground is still another factor that requires study. Other factors related to the problem of grounds which require particular attention arise from the variation of the soil resistivity, the soil structure, the geology, and related questions.[3,10–14]

Summary

The 16 grounds tested are typical of those found in practice and represent a variety of soils and conditions. The resistance varies with soils, moisture, and soil conditions. A ten-foot ground Meggers 20 to 40 ohms in clay, 70 to 120 ohms in moist sand, and 100 to 200 ohms in a mixture of gravel and clay. The annual variation of this ground from the mean is 10 to 25 per cent, depending on the soil, and is less for deeper grounds.

The impulse resistance decreases with increasing current. It varies with season in much the same manner as 60-cycle resistance. Rain and weather conditions have little immediate effect. The impulse resistance is practically independent of polarity and of wave form within the range applied. The per cent decrease in resistance becomes greater with high-resistivity soils. During the discharge the resistance drops rapidly attaining minimum or impulse value near crest current and rises as the current decreases.

A method for determining the impulse resistance has been developed. At a critical gradient, the soil breaks down increasing the effective radius and length of the rod. Then from the equations in the appendix the impulse characteristic can be calculated. Curves calculated are in substantial agreement with the experimental results.

Various parallel combinations of single grounds were investigated. The resistance of parallel grounds, both 60-cycle and impulse, can be derived from the resistance of the component grounds.

The lead and ground inductance becomes particularly significant for rapid current discharges when the inductance drop may exceed the ground resistance drop.

Appendix I. Method of Calculating 60-Cycle Resistance of Single and Multiple Grounds

The formula used to calculate the resistance of single grounds is that given by H. B. Dwight.[15]

$$R_1=\frac{\rho}{2\pi L}\left[\!\!\left[\ln\left(\frac{4L}{a}\right)-1\right]\!\!\right] \tag{1}$$

where

R_1 = resistance of single rod in ohms

ρ = soil resistivity in ohms per cubic centimeter

L = rod depth in centimeters

a = rod radius in centimeters

This formula may also be used to determine the soil resistivity from the dimensions and resistance of a ground. The following formula for two grounds in parallel is also given by Dwight.

$$R_2=\frac{\rho}{4\pi L}\left[\!\!\left[\ln\left(\frac{4L}{a}\right)-1+\ln\left(\frac{2L+\sqrt{S^2+4L^2}}{S}\right)+\frac{S}{2L}-\frac{\sqrt{S^2+4L^2}}{2L}\right]\!\!\right] \tag{2}$$

where

R_2 = parallel resistance of two rods in ohms

S = distance between rods in centimeters

The following formula was derived by Dwight's method for four grounds of equal length arranged in a square

$$R_4=\frac{\rho}{8\pi L}\left[\!\!\left[\ln\left(\frac{4L}{a}\right)-1+2\ln\left(\frac{2L+\sqrt{S^2+4L^2}}{S}\right)+\frac{S}{L}-\frac{\sqrt{S^2+4L^2}}{L}+\ln\left(\frac{\sqrt{2}L+\sqrt{S^2+2L^2}}{S}\right)+\frac{S-\sqrt{S^2+2L^2}}{\sqrt{2}L}\right]\!\!\right] \tag{3}$$

where

R_4 = parallel resistance of four rods in ohms

S = length of any side of the square in centimeters.

Formulas were also derived for four equally spaced rods in a straight line and for two rods of unequal length. The lack of complete symmetry of these arrangements makes the formulas rather complex, and for this reason they are omitted.

There are two methods commonly used for the calculation of grounds. Both methods employ the artifice of images by projecting an image upward a distance equal to the depth of the ground. The earth's surface thus bisects the rod, and all currents close to the surface flow parallel to it, satisfying the actual conditions. The earlier method, developed by O. S. Peters,[16] replaces the ground by an ellipsoid of revolution, whose minor axis is the diameter of the rod, and whose major axis is twice the depth of the rod. The capacitance of an isolated ellipsoid may be calculated directly, and from this, the resistance by the formula

$$R=\frac{\rho}{2\pi C} \tag{4}$$

where C is the electrostatic capacitance of the ellipsoid. The more recent method, developed by H. B. Dwight, uses the rod in its exact shape and is based on the simplifying assumption that the charge distribution along the surface of the rod is uniform. Calculating for various points on the rod, the potential induced by this uniform charge, and dividing the total charge by the average induced voltage, Dwight finds the approximate capacitance of cylinder. This is converted to resistance by formula 4.

The two methods have their advantages: the former method lends itself readily to the calculation of equipotential surfaces and field distribution, while it is easier to calculate resistance for parallel grounds and noncylindrical rods by latter method. With typical values of L, a, and ρ, Peters' formula gives a resistance from five to ten per cent higher than Dwight's. This is expected since the surface area of the cylinder is greater than for the ellipsoid. It must be remembered, however, that both formulas are approximations, and that their accuracy decreases with decreasing ratios of L/a.

Appendix II. Method of Calculating Impulse Resistance of Single and Multiple Grounds

The basic formula used to calculate impulse resistance has the same form as that for 60-cycle resistance. Equation 1 of appendix I is adapted to the symbols shown in the insert of Figure 19, as follows

$$R_0=\frac{\rho_0}{2\pi L_0}\left[\!\!\left[\ln\left(\frac{4L_0}{a_0}\right)-1\right]\!\!\right] \tag{5}$$

Considering the increase in the effective radius (a_i) of the rod resulting from breakdown of the soil at high current as discussed under "Theoretical Considerations" equation 5 can be rewritten

$$R_i=\frac{\rho_0}{2\pi \mathrm{L}_0}\left[\!\!\left[\ln\left(\frac{4L_0}{a_i}\right)-1\right]\!\!\right] \tag{6}$$

The effective radius of the rod is determined as follows. Since the voltage gradient along the surface of the rod is the product of current density and soil resistivity (ρ_0), the critical value of this gradient (g_c) at which the radius (a_0) of the rod starts to increase, is determined from the critical current (I_b)

$$g_c=\frac{I_b\rho_0}{2\pi a_0 L_0} \tag{7}$$

For current I above I_b, the gradient at the effective radius assumes critical value and is expressed by

$$g_c=\frac{I\rho_0}{2\pi a_i L_0} \tag{8}$$

From 7 and 8

$$a_i=\frac{a_0 I}{I_b} \tag{9}$$

This value of a_t is substituted in equation 6 and the ratio of R_t to R_0 can be written

$$\frac{R_t}{R_0}=\frac{\ln\left(\frac{4L_0I_b}{a_0I}\right)-1}{\ln\left(\frac{4L_0}{a_0}\right)-1} \tag{10}$$

Next let us consider the increase in length with current as shown in the sketch of Figure 19. Referring to equations 5 and 6 and allowing for the increase in length, we can express the ratio of R_t to R_0 as

$$\frac{R_t}{R_0}=\frac{L_0\left[\left[\ln\left(\frac{4L_t}{a_t}\right)-1\right]\right]}{L_t\left[\left[\ln\left(\frac{4L_0}{a_0}\right)-1\right]\right]}. \tag{11}$$

The relation of a_t to the current can be approximated by equation 9 or better determined from

$$\frac{I}{I_b}=\frac{a_tL_t}{a_0L_0} \tag{12}$$

The effective length L_t may be written as a function of the current

$$L_t=L_0+kI \tag{13}$$

where the factor k is constant only for limited ranges of current.

The impulse resistance for two rods in parallel can be obtained by substituting the proper values of a_t and L_t in equation 2 of appendix I. The ratio of R_t to R_0 can then be obtained, and for reference purposes it is designated as equation 14. The impulse resistance for four arranged in a square can be obtained by a similar substitution in equation 3, and the ratio of R_t to R_0 for this combination is designated as equation 15.

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The Application of Voltage Regulators to Aircraft Generators

L. W. THOMPSON
ASSOCIATE AIEE

F. E. CREVER
ASSOCIATE AIEE

Synopsis: The use of high-capacity d-c generators on aircraft has required the development of regulators with rapid response, high accuracy, and with features for parallel operation of variable speed machines. The fundamental requirements of light weight, ability to withstand shock and vibration, and to give low radio interference were vital factors considered in the development. A device incorporating these features is described and analyzed. Results of tests on these equipments have shown that regulators with these features are vital to the operation of the electrical loads of modern planes, both the military and commercial types. The tests show that the requirements have been met with devices suited to high production methods.

THERE has been, and it seems safe to predict, will continue to be an increasing use of electrical devices in modern aircraft, both military and commercial. In order to supply the increased load, larger capacity generators than ever before utilized are being used. The generators are driven at variable speed from the main engines of the aircraft over a speed range of approximately two to one. Depending upon the size and use of the aircraft, an auxiliary power plant driving a constant-speed generator may be used in addition. A d-c system employing a 24-volt battery is in common use requiring a 28.5-volt bus voltage in the plane. The main generators must be controlled so as to maintain proper voltage for the battery and the various loads and, at the same time, insure close load division between the generators when operating in parallel. Close load division is important to allow utilization of the complete installed generating capacity.

The Importance of Generator Voltage Regulators

Voltage regulators play an extremely important part in making a system of this type practical. In fact, the system cannot be operated without suitable voltage regulators, since the variable-speed generators operating at low-flux densities at high speed are unstable to manual control. If the problem of parallel operation with close load division is accomplished by the same device, the importance of the voltage regulators in the system is immediately apparent.

Previous to the rapid increase in electrical loads on aircraft 12-volt d-c generators of small capacity had been used under the control of vibrating contact voltage regulators. Principal sources of trouble with the vibrating regulators were contact deterioration, radio interference, and failure to provide close load division of generators operating in parallel. Regulators of this type are inadequate for the larger capacity machines, on account of the increased power that must be handled in the generator field circuit which aggravates the troubles experienced on the small capacity machines. In this respect regulators for use in aircraft are closely following the history of regulators employed in stationary installations where the vibrating types have been largely displaced by other types. Direct-acting regulators have been successfully employed in stationary installation for sev-

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L. W. Thompson is engineer and F. E. Crever is administrative assistant and engineer, both in the generator voltage regulator engineering department, General Electric Company, Schenectady, N. Y.

The authors acknowledge the suggestions of T. B. Holliday and E. G. Haven which were helpful in the development of regulators for high-power aircraft generators.

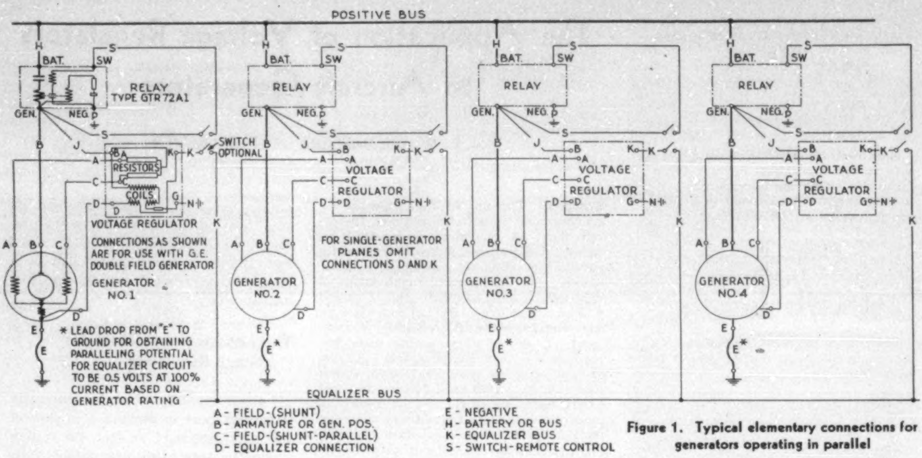
POSITIVE BUS
H
BAT.
SW
S
RELAY
TYPE GTR 72A1
GEN.
NEG.
J
SWITCH OPTIONAL
RESISTORS
COILS
VOLTAGE REGULATOR
CONNECTIONS AS SHOWN ARE FOR USE WITH G.E. DOUBLE FIELD GENERATOR
GENERATOR NO.1
* LEAD DROP FROM "E" TO GROUND FOR OBTAINING PARALLELING POTENTIAL FOR EQUALIZER CIRCUIT TO BE 0.5 VOLTS AT 100% CURRENT BASED ON GENERATOR RATING
RELAY
VOLTAGE REGULATOR
FOR SINGLE-GENERATOR PLANES OMIT CONNECTIONS D AND K
GENERATOR NO.2
GENERATOR NO.3
GENERATOR NO.4
EQUALIZER BUS
A - FIELD-(SHUNT)
B - ARMATURE OR GEN. POS.
C - FIELD-(SHUNT-PARALLEL)
D - EQUALIZER CONNECTION
E - NEGATIVE
H - BATTERY OR BUS
K - EQUALIZER BUS
S - SWITCH-REMOTE CONTROL

Figure 1. Typical elementary connections for generators operating in parallel

eral years both on a-c and d-c generators. Their record has been very good from a standpoint of reliability, low maintenance, and freedom from excessive radio interference. Principles and means of providing for parallel operation have been worked out, and a great deal of experience gained on stationary installations. It therefore seemed that such regulators could be successfully used in aircraft installation, provided the special problems incident to their installation in aircraft could be solved.

Primary requirements for aircraft installation

1. A minimum of weight.
2. Ability to withstand shock and vibration.
3. Operation over a wide temperature range (from −55 to +65 degrees centigrade).
4. Ability to operate over an atmospheric pressure range from sea level to high altitudes.

Secondary requirements to meet present day requirements

1. Readily adaptable to generators having different field characteristics.
2. Suitable to quantity manufacture.
3. Simplicity of adjustment.

A regulator based upon principles found satisfactory in stationary installation has been designed to meet both the primary and secondary requirements listed above.

The Type of Regulator Applied

This regulator is of the direct-acting type and has a magnetic circuit energized by windings through a diluting resistor from the voltage of the generator being regulated. The force developed by the magnetic circuit by virtue of the applied voltage is balanced against a spring. At the required voltage setting the force of the armature overcomes the calibrating spring and operates in succession a multiplicity of contacts to insert small steps of resistance into the field circuit of the generator to maintain the voltage at which the regulator is balanced. It is necessary to so design the magnetic circuit that the mechanical forces are just balanced by the electromagnetic forces for all positions of the regulator armature at the same applied voltage.

By using a sufficiently large number of contacts the operation is made nonvibrating in nature, and the power handled per contact is kept below the critical arcing limit.

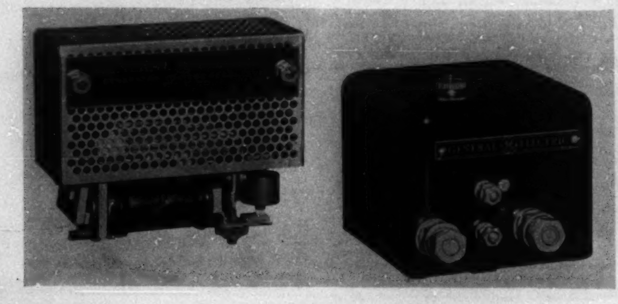

Figure 2. Plug-in-type regulator and relay for aircraft installation

Figure 3. Regulator in separate case with *AN* corrector

Means for Accomplishing Parallel Operation

A second winding on the regulator is used to accomplish load equalization under parallel operation. The equalizing coils are connected as in Figure 1 and have been found very effective in obtaining equal load division of generators.

This method of equalization requires a small voltage drop (0.5 volt at rated load) in series with each generator. This drop does not cause a drooping voltage characteristic with load, as the voltage is regulated at a point beyond this drop. It does, however, cause a flow of current in the equalizing coils of the regulators when necessary to insure proper load equalization. In some instances the generators have an internal compensating winding which can be used to obtain the voltage drop for equalizing purposes. In order to obtain uniform drops and in order to insure interchangeability of generators of different manufacture, it has been the authors' recommendation that the potential drop required for equalizing purposes be made external to the generators, and in many instances the drop in the negative lead itself is utilized. In applications where the generator design is known, and an internal compensating winding is present, it is desirable to utilize this drop for equalizing purposes. In such instances potential dividers can be connected across these compensating fields to allow adjustment to compensate for differences in voltage drop.

Tests have been made with this scheme of equalization of load, and it has been demonstrated that close load division of generators in parallel can easily be obtained whether or not the speeds of the generators are equal.

When one or more generators in a group operating in parallel are disconnected, a drooping characteristic with load is obtained on the remaining units, unless the equalizing coil of the generator that is off the lines is opened. In some cases it is desirable to open the equalizer coil, and in other instances the drooping characteristic is desirable under these conditions, in order to allow the battery to share in supplying the higher load peaks.

In order to illustrate the practicability and desirability of the equalizing scheme shown in Figure 1, let us consider an example.

Suppose two regulators each having an accuracy of +1 per cent are to be operated in parallel and at a total load of twice the rating of one generator, the loads are to be divided within ten per cent of the rating of one generator.

Under the worst condition consider one regulator to be one per cent high and the other one per cent low on the basis of its potential winding alone. With the scheme using equalizing windings and 0.5-volt series drop at rated load, there will be at ten per cent difference in load 0.05 volt to cause current flow through the equalizing windings. The equalizing winding is designed so that this difference in voltage will cause a current to flow in the equalizing windings to change the ampere turns of the regulator one per cent of the total ampere turns, on one regulator in a direction to raise, and on the other in a direction to lower its voltage. These proportions have been found to be practical. There is a loss in the series resistors at rated load of approximately 1.75 per cent of the generator rating.

Now consider the same regulators connected without equalizing coils but with enough series resistance beyond the point of regulation to equalize the loads to the same degree. Assume that there is a difference in regulated voltage of two per cent with one regulator one per cent high and the other one per cent low. The series resistance between the regulated points must be such as to allow not over five per cent circulating current with two per cent voltage difference or 20 per cent resistance per generator circuit to obtain the same current equalization as with the scheme using equalizing coils. This is of course entirely impractical.

Other Considerations for Regulator Application in Aircraft

While regulators can be made to cover a range of field current requirements, it must be realized that the design of the generator field determines the power to be dissipated in the regulator. With fields of normal design the power dissipated for 200- and 275-ampere generators is about 65 watts. The aircraft manufacturer must provide sufficient ventilation for the dissipation of the heat.

While a regulator can stabilize generators that would be unstable on manual control, there are limits beyond which the regulator cannot stabilize the system. Generators with rising voltage characteristics as the load is increased are invariably unstable to a degree that the regulators cannot cope with on battery loads. This practically rules out the use of compound-wound variable-speed generators for aircraft application.

Regulators of the type described have been successfully applied on 120-volt systems. Also they have been applied to special purpose separately excited a-c generators by exciting the regulator coil through a dry-disc-type rectifier.

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The Magnetic-Drag Tachometer

R. G. BALLARD
ASSOCIATE AIEE

Synopsis: A discussion of a magnetic-drag-tachometer equipment designed principally for aircraft use. The principle of operation makes possible an indicator which may be read very accurately under extreme conditions of aircraft use and at the same time may be easily adjusted and maintained. Because of the extreme reading accuracy obtainable, low inherent temperature errors and good compensation are essential. A method of obtaining these features is presented.

MECHANICALLY operated aircraft tachometers include the centrifugal and chronometric types; prior to the development of the magnetic-drag tachometer, remote indicating electrical types included the a-c generator and voltmeter combination and the d-c generator and voltmeter combination.

The disadvantage of the mechanically operated tachometer rests in the excess weight caused by the necessity for a flexible cable between the engine and the indicator.

Generator and voltmeter tachometers lie in two general classifications, namely:

1. Voltage-sensitive devices.
2. Frequency-sensitive devices.

The voltage sensitive instruments may consist of a voltmeter, connected directly to a d-c generator or connected through a copper-oxide rectifier to an alternating generator. It is necessary to maintain a fixed lead resistance between the indicator and generator, and any change in generator output is immediately reflected in the accuracy of indication.

The problem of overcoming some of the objections of the voltage sensitive instruments was solved by the development of a frequency-sensitive tachometer whose indication is proportional only to the frequency of the generator output. This tachometer consists of a voltmeter connected to an a-c generator with a copper-oxide rectifier and a saturated core transformer. The transformer core is saturated throughout the operating range of the aircraft engine, and the output of the transformer is consequently directly proportional to engine speed. This development permitted the exchange of generators or indicators without calibration adjustment, eliminated changes in calibration due to changes in generator output produced by vibration or temperature changes, and permitted the installation of tachometer equipment without making it necessary to maintain a fixed lead resistance.

As the size and speed of aircraft increased, due to the availability of more powerful engines, it became necessary to control the engine speed more accurately in order to obtain the most economical engine operation. High altitude operation presented the problem of increased accuracy of engine speed indication throughout the temperature range from approximately −40 degrees centigrade to +50 degrees centigrade, requiring marked improvement in temperature compensation. Due to the more accurate indication required, it became necessary to obtain tachometer equipment with a greatly increased scale length to provide more accurate readability.

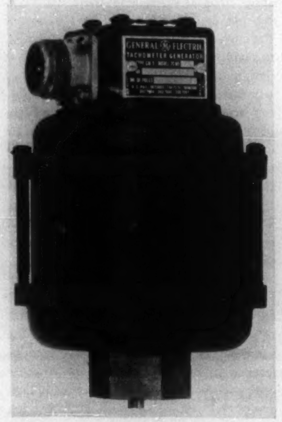

Figure 1. Tachometer generator

It is the object of this paper to indicate how the problems outlined in the preceding paragraph were solved by the development of a tachometer operating on the magnetic-drag principle and to present pertinent details regarding the operation of representative tachometers.

Consideration of the application of tachometer equipment to aircraft indicated that the wide variation between the temperatures of the indicator and the generator, in combination with the very severe vibration of the generator, made it absolutely essential that the indicator be entirely frequency responsive. This is necessary, due to the changes in generator-output voltage caused by vibration and to temperature changes which vary the resistance of the generator circuit.

The generator–voltmeter tachometer with saturated core transformer overcame problems due to the effect of vibration and temperature changes on the generator; however, as was true with other types of remote electrical indication tachometers, the maximum scale length obtainable was 270 degrees. In order to increase this scale length materially, a tachometer operating on the magnetic-drag principle was developed.

The Magnetic-Drag Tachometer

The magnetic-drag tachometer consists essentially of a three-phase a-c generator, mounted on the aircraft engine, and an indicator, consisting of a synchronous motor operating a magnetic-drag assembly.

The problem of changes in generator output voltage was eliminated by the adoption of the generator and synchronous motor combination, which constitutes a frequency-sensitive system for transmitting an indication of engine speed to the indicator with absolute accuracy.

For many installations it is desirable to obtain engine speed indication at two different stations in the aircraft. This system is ideal for this application, since there is no change in indication, due to the addition of a second indicator in parallel with the first; synchronous motor operation in each indicator is only dependent upon the availability of sufficient power to operate both motors.

Generator (Figure 1)

Primary factors of light weight and small size, plus trouble-free operation, were problems which had to be met in the generator design.

Light weight was accomplished by the use of aluminum alloy for the end shields.

Light weight and small size were obtained by making use of a permanent magnet rotor with high magnetic energy content. The physical dimensions of the rotor required a magnetic material having

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R. G. BALLARD is with the aircraft instrument engineering section, General Electric Company, West Lynn, Mass.

high coercive force, due to the short magnet length and in order to prevent demagnetization. Because of the extreme vibration encountered, it is necessary to have a material which is not readily affected by vibration. Alnico offered the answer to those requirements and is cast directly onto the rotor shaft.

In order to obtain high resistance to wear produced by vibration, stainless steel inserts were cast into the generator end shields.

As the aircraft-engine accessory drives often transmit to a considerable extent torsional oscillations originating at the crankshaft, the tachometer generator includes a long slender drive shaft carried within the hollow rotor shaft, having sufficient flexibility to prevent failure of the drive key as well as to accommodate small misalignments between generator and engine pad.

Two limiting conditions are necessary with regard to output of the generator:

1. With a single indicator connected to the generator, the voltage output at normal cruising speed must not exceed a certain limit in order to prevent excessive temperature rise in the indicator.
2. At the lowest operating speed, with two indicators connected to a single generator, the output voltage must exceed a certain minimum value, so that there will be sufficient power output available to start and operate the indicators at any temperature.

These requirements were met by taking advantage of the properties of the Alnico rotor which permitted shorter—hence lower resistance—turns, resulting in improved voltage regulation. Adjustment of voltage within the limits specified is obtained by means of an a-c knockdown coil after complete assembly of the generator; this adjustment also stabilizes the magnet to minimize effects of vibration.

Indicator

The indicator (Figure 2) as briefly described in a previous paragraph, includes a synchronous motor and a magnetic-drag assembly.

The requirements for a satisfactory motor include the ability to start and to run synchronously at any speed throughout the operating range, when two indicators are connected to a single generator, and yet not to overheat when connected singly to a generator. Since the generator output decreases with a decrease in engine speed, it is necessary that the indicator start with very low input at the lowest speeds. It is essential that the motor should meet these requirements with minimum weight and size and yet be simple and rugged for long life and ease of maintenance.

A unique motor assembly in conjunction with a conventional three-phase wound stator offered a solution to these requirements. The rotor assembly as shown in Figure 3 consists of a polarized rotor, a spring, and a hysteresis disk. The starting torque is supplied by the polarized rotor at low engine speeds, where the frequency is too low to produce appreciable torque in the hysteresis disk. As the generator frequency is increased by increasing engine speeds, a greater share of the torque is produced by the hysteresis disk, and the polarized rotor assists by bringing the motor into synchronism.

In order for the polarized rotor to perform the above functions, it was necessary to design a rotor with an extremely low moment of inertia and with high magnetic strength; a sintered oxide material was used for this application, resulting in a rotor having approximately four-tenths the weight of any other available magnetic material.

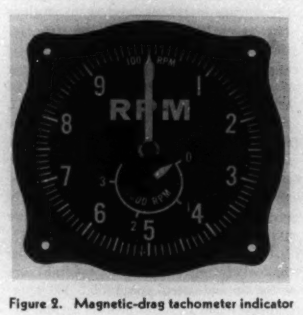

Figure 2. Magnetic-drag tachometer indicator

The hysteresis disk is produced from an alloy of copper, nickel, and iron, heat-treated to produce the desired magnetic characteristics.

It was found that there was very definitely an optimum spacing between the polarized rotor and the hysteresis disk to obtain the best starting characteristics throughout the entire speed range.

Bearings for the indicator motor presented a major problem for the following reasons:

Generator output decreases as the generator speed is lowered, resulting in a very low indicator input at engine idling speeds, thus requiring low friction in the bearings to permit proper starting.

Bearing lubricant gradually becomes more viscous as the temperature is decreased, resulting in increased bearing drag.

Since bearing life is dependent upon amount of lubrication it is desirable to supply the maximum quantity of lubricant possible.

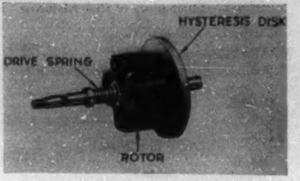

Figure 3. Indicator-rotor assembly

In order to properly protect the ball bearings until ready for installation in indicators, they are stored completely filled with lubricant; when needed for assembly, each bearing is thoroughly washed and cleaned to remove all traces of the lubricant, the bearing is then dried, and a definite quantity of inhibited black fish jaw oil is applied to the bearing. Repeated laboratory tests have indicated the superiority of this oil with regard to rate of evaporation, acidity, and change with continued evaporation. The quantity of lubrication applied is dependent upon minimum operating temperature to which the particular instrument is to be exposed.

The magnetic-drag assembly may properly be divided into two sections, the rotating magnet assembly and the armature assembly.

Since the rotating magnet assembly (Figure 4) is a part of the rotor assembly of the synchronous motor, it is necessary to obtain a magnetic assembly with as low a moment of inertia as possible, which will produce the required torque on the magnetic-drag disk. To meet these requirements five pairs of Alnico magnets are arranged as shown in Figure 4; this arrangement concentrates the flux near the outside edge of the magnetic-drag disk with minimum weight of magnetic material.

The armature assembly includes the shaft, springs, and pinion gear illustrated in Figure 5, in combination with a drag disk and pointer. The magnet assembly of Figure 4 and the flat drag disk permit ready inspection of the air gap, and by

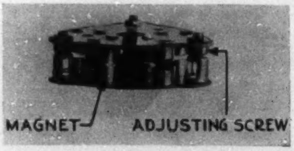

Figure 4. Rotating-magnet assembly

Table I. Comparison of Disk Materials

Material	Temperature Coefficient (Per Cent Per Degree Centigrade)	Conductivity	Density (Grams Per Cubic Centimeter)
Aluminum	0.34	60	2.7
Copper	0.385	100	8.9
Aluminum-manganese	0.040	12	2.8
Copper-manganese	0.036	15	8.8

means of adjusting screws, the element may be raised or lowered to centralize the disk in the magnet gap.

The spring assembly must permit $3^1/_2$ revolutions of the pointer, must be rigid enough to withstand the effects of vibration, and must be designed to operate well below the elastic limit in order to prevent appreciable changes in indication when deflected for long periods of time.

These spring problems were solved by the unique spring arrangement illustrated in Figure 5. Here two springs are effectively connected in series by a spring arm joining the outer turns of the two springs, the inner turn of one spring is fastened to the shaft, and the inner turn of the second spring is fastened to an adjusting arm mounted on the element plate. By this method of construction $3^1/_2$ revolutions of the pointer result in only $1^3/_4$ revolutions of each spring, permitting shorter and consequently more rigid springs. Increased stiffness of the spring is also allowed, since the torque per degree deflection of each is double the torque of an equivalent single spring. With this arrangement no restraining plates are required, and the spring arm prevents any possible entanglement of the two springs.

The pinion actuates the short pointer illustrated in Figure 2 to indicate the number of revolutions of the long pointer.

The magnetic-drag disk is made from an aluminum-manganese alloy in order to obtain minimum weight with the requisite conductivity and temperature coefficient. It is essential to obtain a temperature coefficient within certain definite limits in order to obtain optimum accuracy of indication for rapidly changing temperatures. Data for several disk materials are indicated in Table I, where the advantage of the aluminum-manganese alloy is evident due to the combination of light weight and low temperature coefficient.

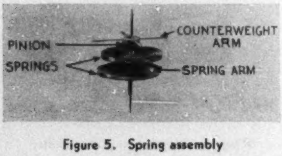

Figure 5. Spring assembly

Temperature Compensation

In order to obtain the required accuracy throughout a temperature range from −35 to +45 degrees centigrade, it is necessary to provide compensation for the changes in indication due to these temperature changes. Changes are produced by

(*a*) Magnets.

(*b*) Disk.

(*c*) Springs.

(*d*) Other very small effects such as those caused by expansion and contraction.

As the temperature decreases, the magnet strength increases, tending to produce an increase in indication; the disk conductivity increases, also tending to increase the indication, and the torque of the springs increases, tending to produce a decrease in indication. The temperature-torque coefficient of the springs is approximately 0.04 per cent per degree centigrade; by proper heat treatment of the disk alloy, it is possible to obtain a similar coefficient, thus cancelling the errors produced by the disk and springs.

Both the thin disk and the springs are similarly exposed to the air inside the instrument case, so that they will quickly respond to rapid changes in temperature in order to eliminate transient errors.

The temperature error produced by the magnets is indicated by curve *A* in Figure 6 for an indicated speed of 3,000 rpm; compensation is accomplished by the addition of temperature-sensitive magnetic shunts placed in the magnetic assembly of Figure 4.

As the magnetic flux increases with decrease in temperature, it is necessary to shunt the extra flux directly across the poles of the magnet to maintain constant flux in the disk gap. These shunts are placed underneath the clips which fasten the magnets to the plates in Figure 4.

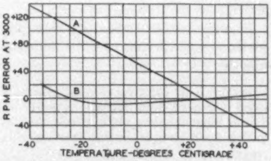

Figure 6. Temperature-compensation curves

A—Uncompensated indicator
B—Compensated indicator

The addition of a single type of temperature-sensitive material corrects the greater share of the fundamental change; this material is a nickel-iron alloy with a release point of approximately +90 degrees centigrade. This release point is the temperature at which the alloy becomes practically nonmagnetic.

Greater compensation is produced by this type of material above room temperature than below room temperature; to correct for this difference a copper-nickel alloy, with a release point at or slightly above room temperature, is added to produce the compensated curve *B* of Figure 6 for a representative instrument. Close contact between the temperature sensitive material and the magnets tends to maintain both materials at the same temperature regardless of the rate of change of temperature, thus eliminating transient temperature errors.

Calibration

A good tachometer should include:

1. Full-scale adjustment.
2. Zero adjustment.
3. Method of balancing element assembly.

Preliminary full-scale adjustment is obtained by the use of an a-c knockdown coil to reduce the air-gap flux produced by the rotating magnet assembly; by this method the magnet strength is reduced at least five per cent in order to stabilize the magnets and to bring the calibration within 0.1 per cent of the correct indication at full scale. After a calibration check has been completed throughout the scale range, final adjustment may be made by further application of the knockdown coil, or the indication may be increased by movement of any one or all of the three adjusting screws illustrated in Figure 4. These adjusting screws vary the air gap to increase or decrease the flux and permit ready and positive adjustment at any time.

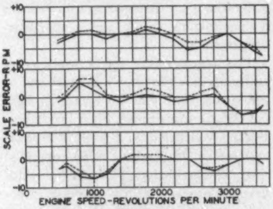

Figure 7. Calibration Curves

——— Engine speed increasing
– – – Engine speed decreasing

Synthetic or Equivalent Load Curves

R. F. HAMILTON
MEMBER AIEE

Synopsis: This paper describes short approximate methods for the solution of several types of intangible problems which require load-curve analyses. Laborious effort may be expended in compiling results from a group of curves without greater precision than is believed possible to obtain from the use of one or a few synthetic curves which combine the characteristics under investigation, and permit the determination of such items as peak loads, losses, and required capacities of equipment.

LOAD curves are familiar to engineers. The variety of precision instruments which have been developed to draw them automatically, and the ever increasing number of uses to which they are applied would require, for adequate description, an historical record dating from the days of the rocker-beam steam engine. Today they are so commonplace that no attempt will be made herein to classify them, or to review methods generally applied.

There are particular types of problems in which the characteristics of a number of similar curves, taken in the aggregate, are more helpful than those of any single curve. For example, in a study made recently, including the load curves of many large industrial and utility companies, it became evident that laborious effort may be expended in compiling results from a group of curves, such as daily curves for a yearly period, with scarcely greater precision in the end than is believed possible to obtain from the use of one, or a few synthetic curves which combine the characteristics under investigation.

One problem occurs frequently in electrical calculations; that involving losses, and consequently a squared function of a current or load curve. This can determine principally the capacity of many kinds of equipment. Consider too the losses in transmission-line and cable circuits. The economical value of these conductors is a function of the annual energy loss, which in turn may depend upon several variable factors. Kelvin's law, in its application, presumes "an equivalent mean-annual current."

Or consider traction motors, rolling-mill motors, hoist and crane-equipment, generators and transformers in the service of loads which fluctuate rapidly over wide values; about which it may well be said that no two load curves are likely to be duplicates. It would seem that methods are needed for giving quick and reliable solutions for the capacities and losses involved.

The purpose of this paper is to examine some features of these problems and to present a few methods which have been evolved for their solution, with the thought that they may be of interest, and perhaps stimulate further investigation in the field of short solutions to these important problems.

Basic Elements

Load curves, as commonly understood, involve the rate of change of some physical quantity plotted as ordinates versus units of consecutive time as abscissae. Examples of ordinates are kilowatts, horsepower, amperes, gallons per minute, passengers per hour, car loadings per day. In utility work the ordinates customarily represent a rate of change of energy, which results in the area under the curve becoming a measure of the energy involved. Such curves may be reduced to a common denominator for comparison of shapes by making the maximum value and total time in each case equal to unity, and plotting the lesser values as fractions of these maxima. This procedure permits direct reading of those factors which are also expressed in fractions of the maxima, and, being without physical dimensions, the scales on the axes of co-ordinates need be numerical only.

The essential elements in the building of a synthetic load curve are the following:

1. *T*, the time or duration of the load; herein primarily the time of some definite cycle, or the transition period between extreme values; which period could be some fraction of a cycle.

2. *N*, the minimum value of the load.

3. *M*, the maximum value of the load.

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R. F. Hamilton is a consulting engineer and is chief of the plant engineering unit of the Corps of Engineers, Washington, D. C.

After initial calibration it is often desirable to raise or lower the complete calibration curve in order to equalize the positive and negative errors. This is taken care of by the application of an adjusting arm which rotates the armature assembly; this adjustment is made with the instrument operating at any desired indication, thus permitting very accurate correction.

Addition of balance weights to the counterweight arms illustrated in Figure 5 permits adjustment to mechanically balance the armature assembly so that the instrument may be operated in any position.

Representative Data

Calibration curves for three representative tachometer indicators are shown in Figure 7; the solid curves are for increasing engine speeds, and the dotted curves are for decreasing engine speeds.

As explained in a previous paragraph, curve *B* of Figure 6 indicates the change in calibration for a typical indicator throughout the temperature range from −35 to +50 degrees centigrade.

Table II

Operating range	500–3,500 rpm (250–1,750 rpm generator speed)
Accuracy (+25 C)	±10 rpm
Generator weight	2.5 pounds
Indicator weight (each)	1.0 pounds
Indicator input (2,400 rpm)	5 watts
Position error	6 rpm
Magnetic effect (5 inches from compass needle)	0.5 degree
Indicator temperature rise (2,400 rpm)	35 C
Generator temperature rise (2,400 rpm)	20 C

Table II lists pertinent details for a representative tachometer installation consisting of two indicators and one generator.

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1. ALNICO—ITS PROPERTIES AND POSSIBILITIES, **J. Q. Adams.** *General Electric Review*, volume 41, December 1938, pages 518–22.

2. WATT-HOUR-METER BEARINGS, **I. F. Kinnard, J. H. Goss.** AIEE TRANSACTIONS, volume 56, 1937, January section, pages 129–37.

3. A SELF-COMPENSATING TEMPERATURE INDICATOR, **I. F. Kinnard, H. T. Faus.** AIEE TRANSACTIONS, volume 49, 1930, July section, pages 949–51.

4. TEMPERATURE ERRORS IN INDUCTION WATT-HOUR METERS, **I. F. Kinnard, H. T. Faus.** AIEE TRANSACTIONS, volume 44, 1925, pages 275–85.

4. W, the energy involved in the period considered.

5. A, the average value of the load.

6. ϕ, the load factor; the ratio of the average load to the maximum load.

7. θ, the loss factor; the ratio of the average loss to the maximum loss, or loss at the maximum load.

8. K, the variation factor; the ratio of the minimum load to the maximum load.

9. r, the root-mean-squared value of the loads.

10. u, the capacity factor; the ratio of the root-mean-squared value of the loads to the average value.

11. ρ, the capacity factor squared, or the ratio of the average loss to the loss at the average load.

By definition, these elements are connected by the simple relations:

$$N=KM,\ W=AT,\ A=M\phi,\ r=uA,\ r^2=\rho A^2,\ r^2=M^2\theta,\ \theta=\rho\phi^2$$

Just as the load factor (ϕ) is a fraction denoting the ratio of the average load to the maximum load, so the loss factor (θ) is a fraction expressing the ratio of the average loss to the maximum loss, and the variation factor (K) is a fraction giving the ratio of the minimum load to the maximum load.

Capacity factor is a useful ratio, always greater than unity, for expressing r in terms of A. For example, a capacity factor of 1.20, as applied to a piece of equipment, would mean that its rating for the period which may be considered must exceed the average load carried by 20 per cent, to care for the I^2R losses under variable load.

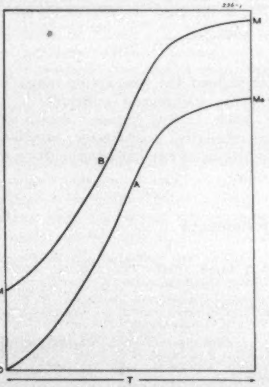


Figure 1. Curves of $f(t)$ and $f(t)+KM$

Fundamental Equations

Referring to Figure 1, assume that m is a continuous function of t for curves A and B. These curves are identical in shape, so that for any value of t the corresponding values of m for the respective curves differ by KM.

Let curve A be defined by $m=f(t)$, then curve B is defined by $m=KM+f(t)$. Then, as shown in appendix A, the load factor and loss factor corresponding to curve B may be expressed respectively as

$$\phi=K+(1-K)\phi_0 \tag{1}$$

and

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2 \tag{2}$$

In the above equations the symbols without subscripts refer to curve B, while those with the zero subscripts refer to curve A. These equations are fundamental and apply to all types of continuous functions. Equation 2 expresses the loss factor (θ) as the load factor squared (ϕ^2) plus some fraction (ρ_0-1) of the square of the difference between the load factor and the variation factor $(\phi-K)^2$.

The value of the fraction (ρ_0-1) depends on the shape of the curve so that the factor (ρ_0) may be said to identify the curve form. By definition

$$\rho_0=\frac{\theta_0}{\phi_0{}^2}$$

and from equation 2

$$\rho_0-1=\frac{\theta_0}{\phi_0{}^2}-1=\frac{\theta-\phi^2}{(\phi-K)^2}$$

or

$$\rho_0=1+\frac{\theta-\phi^2}{(\phi-K)^2}$$

A more suitable way of expressing the load as a function of time is in the form: $m/M=f(t/T)$. The maximum values of m/M and t/T will then be unity or 1.0, occurring when $m=M$ and $t=T$.

The function of t/T may take a variety of forms. One which has proved most useful is given by $(t/T)^n$ which can be made to cover the parabolic types of power functions by using real positive values for n, and the hyperbolic types by rearranging the configuration and using negative values.[5]

The exponent (n) of $(t/T)^n$ is a function of the load factor (ϕ) and the variation factor (K), the relation being

$$n=\frac{1-\phi}{\phi-K} \tag{3}$$

Using $(t/T)^n$, the curve B of Figure 1 is expressed by

$$m=M\left[K+(1-K)\left(\frac{t}{T}\right)^{\frac{1-\phi}{\phi-k}}\right] \tag{4}$$

from which

$$\theta=\phi^2+\frac{(\phi-K)^2(1-\phi)^2}{(1-K)^2-(1-\phi)^2} \tag{5}$$

Thus is given a value for the loss factor (θ) in terms of the load factor (ϕ) and the variation factor (K). The path of the curve is determined by the values of K and ϕ, and the area under the curve is equal to the energy W ($W=\phi MT$).

For the special case in which K is equal to zero, the lower curve of Figure 1 applies and

$$\theta=\frac{\phi}{2-\phi} \tag{6}$$

$$\rho=\frac{1}{\phi(2-\phi)} \tag{7}$$

Derivations of equations 3 to 7 inclusive, are shown in appendix B. The curves of Figure 2 give values of $(t/T)^n$ for values of n varying from 0.05 to 20.0. In Figure 3 are given curves of ϕ, θ and ρ as functions of n for two values of K, namely $K=0$ and $K=0.2$. In Figure 4 are given curves of ρ and θ as functions of ϕ for five values of K.

There is an endless variety of functions to which equation 1 may be applied. A

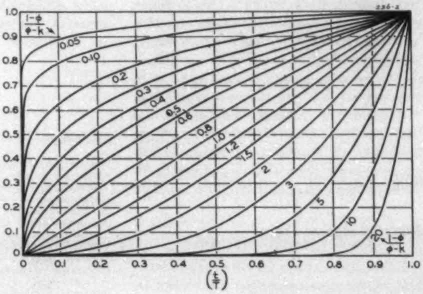


Figure 2. Curves showing $(t/T)^n$ where $n=\dfrac{1-\phi}{\phi-k}$

Table I

No.	Description	Equation of Curve (m/M)	Load Factor ϕ	$\rho_0 = \theta_0/\phi^2$	Loss Factor (θ)	Minimum Value of ϕ
1	Straight Line	$K + (1-K)\,t/T$	$\frac{1+K}{2}$	$4/3$	$1/3(1+K+K^2)$ or $\phi^2 + 1/3(1-\phi)^2$	0.5
2	Parabolic Power	$K + (1-K)\left(\frac{t}{T}\right)^n \quad n = \frac{1-\phi}{\phi-K}$	$\frac{1+Kn}{n+1}$	$\frac{1}{\phi_0(2-\phi_0)}$	$\phi^2 + (\phi-K)^2\left[\frac{1}{\left(\frac{1-K}{1-\phi}\right)^2 - 1}\right]$	0
3	Exponential	$K + (1-K)\left(\frac{e^{t/T}-1}{e-1}\right)$	$0.418 + 0.582\,K$	1.46912	$\phi^2 + 0.4691(\phi-K)^2$ or $\phi^2 + 0.2397(1-\phi)^2$	0 418
4	Sine	$K + (1-K)\sin\frac{\pi}{2}(t/T)$	$K + 2/\pi(1-K)$	$\pi^2/8$	$\phi^2 + 0.2337(\phi-K)^2$ or $\phi^2 + 0.7173(1-\phi)^2$	$2/\pi = 0.6366$
5	Versine	$K + 1/2(1-K)\operatorname{vers}\pi(t/T)$	$\frac{1+K}{2}$	$3/2$	$\phi^2 + 1/2(\phi-K)^2$ or $\phi^2 + 1/2(1-\phi)^2$	0.5
6	Ellipse or Circle	$K + (1-K)\sqrt{1-(t/T)^2}$	$K + \frac{\pi}{4}(1-K)$	$3^2/3\pi^2$	$\phi^2 + 0.0808(\phi-K)^2$ or $\phi^2 + 1.082(1-\phi)^2$	$\pi/4 = 0.7854$
7	Witch	$(K/1-K)\left[\frac{1}{(t/T)^2 + \frac{K}{1-K}}\right]$	$\sqrt{\frac{K}{1-K}}\cos^{-1}\sqrt{K}$	$K \neq 0$	$\frac{\phi+K}{2}$	0.4163 $K = 0.1$
8	Hyperbolic Secant.	$\operatorname{Sech}\left[(t/T)\operatorname{Sech}^{-1}K\right]$	$\frac{\cos^{-1}K}{\operatorname{Sech}^{-1}K}$	$K \neq 0$	$\phi\left(\frac{\sqrt{1-K^2}}{\cos^{-1}K}\right)$	0.4913 $K = 0.1$
9	Power – 3rd Degree (Slope = 0 for $t = 0$ & $t = T$)	$K + 3(1-K)(t/T)^2 + 2(1-K)(t/T)^3$	$\frac{1+K}{2}$	$52/35$	$\phi^2 + 0.4857(\phi-K)^2$ or $\phi^2 + 17/35(1-\phi)^2$	0.5
10	Inverted Parabola	$1 - (1-K)(t/T)^2$	$1/3(K+2)$	$6/5$	$\phi^2 + 0.2(\phi-K)^2$ or $\phi^2 + 0.8(1-\phi)^2$	$2/3$
11	Power – 2nd Degree	$K + 2(3\phi-2K-1)(t/T) + 3(1+K-2\phi)(t/T)^2$			$\phi^2 + 1/15\left[2(\phi-K)^2 + (1-\phi)^2 + (1-\phi)(1-K)\right]$ or $\phi^2 + 1/15\left[3(\phi-K)^2 - 3(1-\phi)(\phi-K) + 2(1-K)^2\right]$	
12	Power Series – 3rd Degree (Slope = 0 for $t = 0$)	$K + \left[12(\phi-K) - 3(1-K)\right](t/T)^2 + \left[-12(\phi-K) + 4(1-K)\right](t/T)^3$			$\phi^2\ 1/35\left[12(\phi-K)^2 + (1-\phi)^2 + 2(1-\phi)(1-K)\right]$ or $\phi^2 + 1/35\left[13(\phi-K)^2 - 4(1-K)(\phi-K) + 3(1-K)^2\right]$	
13	Power Series – 4th Degree (Slope = 0 for $t = 0$ & $t = T$)	$K + 6\left[5(\phi-K) - 2(1-K)\right](t/T)^2 + 4\left[-15(\phi-K) + 7(1-K)\right](t/T)^3 + 15\left[2(\phi-K) - (1-K)\right](t/T)^4$			$\phi^2 + 1/35\left[8(\phi-K)^2 + 7(1-\phi)^2 + (1-\phi)(1-K)\right]$ or $\phi^2 + 1/35\left[15(\phi-K)^2 - 15(1-K)(\phi-K) + 8(1-K)^2\right]$	
14	Power Series – 3rd Degree Slope = 0 for $t = T$	$K + \left[12(\phi-K) - 6(1-K)\right](t/T) + \left[15(\phi-K) - 24(1-K)\right](t/T)^2 + \left[12(\phi-K) - 8(1-K)\right](t/T)^3$			$\phi^2 + 1/35\left[3(\phi-K)^2 + 10(1-\phi)^2 + 2(1-\phi)(1-K)\right]$ or $\phi^2 + 1/35\left[13(\phi-K)^2 - 22(1-\phi)(1-K) + 12(1-K)^2\right]$	
15	Based on Frequency Curve	$K + (2/\sqrt{\pi})(\phi-K)(\log_e T/t)^{1/2}$ or $t/T = e^{-\frac{(m/M-K)^2\pi}{4(\phi-K)^2}}$		$4/\pi$	$\phi^2 + 0.27324(\phi-K)^2$	
16	Power Sine (ϕ; K & θ Given)	$K + 2(3\phi-2K-1)(t/T) + 3(1-K-2\phi)(t/T)^2 + C_1\sin 2\pi\,t/T$ where $C_1 = \frac{1-K}{\pi}\sqrt{\left(\frac{1-K}{\pi}\right)^2 - 2\phi^2 + 2\theta - 2/15\left[2(\phi-K)^2 + (1-\phi)^2 + (1-\phi)(1-K)\right]}$				
17	Horizontal Parabola (See No. 2)	$K + (1-K)(t/T)^{1/2}$	$1/3(K+2)$	$9/8$	$\phi^2 + 0.125(\phi-K)^2$ or $\phi^2 + 0.5(1-\phi)^2$	$2/3$
18	Hypocycloid	$K + (1-K)\left[1-(t/T)^{2/3}\right]^{1/3}$	$K + 3\pi/32(1-K)$	$\frac{16384}{945\pi^2} = 1.75667$	$\phi^2 + 0.75667(\phi-K)^2$ or $\phi^2 + 0.13188(1-\phi)^2$	$3\pi/32 = 0.2945$

few have been tabulated in Table I. The accuracy desired may require that thought be given to apply the function best suited to the problem in hand, and in this connection it should be kept in mind that the lower limit which may be used for the load factor (ϕ) is determined by the load factor (ϕ_0) of the basic function. For example, for the parabolic type of curve represented by equation 4, the load factor (ϕ) may have any value from zero to unity. When the load factor exceeds the value of 0.5, the versine and hyperbolic-secant functions often apply. A little practice will lead to the proper selection upon inspection of the conditions.

Referring to Table I it will be noted that for some of the functions, two expressions are indicated for the loss factor (θ), one being a function of ($\phi - K$) and the other a function of ($1-\phi$), which permits the calculation of two values for the loss factor. By taking an average of the two, a closer value of the loss factor can be obtained. If the curve fits the conditions exactly, the two will be equal in value.

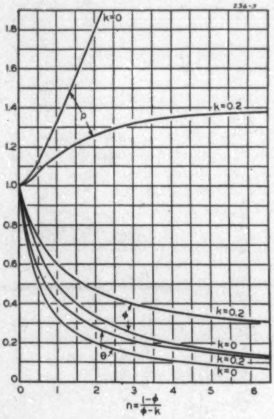


Figure 3. Curves showing variations of ϕ, θ and ρ in terms of n, where $n=\dfrac{1-\phi}{\phi-k}$

Application to Power-Station Curves

Referring to Figure 5, an industrial load which covers one day's output from a power station is represented by curve A. By rearranging the load values in an increasing series, curve B is obtained. The two curves are equivalent for present purposes, for it is immaterial in what order the values occur so long as the magnitudes and corresponding durations remain unchanged. For both curves, the maximum load, the minimum load, the load factor, and the loss factor are identical.

It is entirely possible to construct synthetic curves having the same values of W, T, ϕ, ρ, and θ as have curves A and B, such as represented by curve C which is defined by formula 16 of Table I. However, this is not ordinarily the problem. What is desired are values for θ and ρ corresponding to the given values of ϕ and K for curves A and B. The accuracy with which the former can be determined will be governed by the formula selected from Table I to represent curve B of Figure 5. For example, using equation 5 above, which corresponds to formula 2 of Table I, a value of 0.344 is obtained for θ which is 5.5 per cent lower than the actual value of 0.364. It is to be noted that the slope of curve B is approximately zero at the lower and upper limits. For this condition, formula 13 of Table I would apply, from which a value of 0.366 is obtained for θ which is only 0.55 per cent above the actual value and therefore within acceptable engineering accuracy.

Along the same line consider curve A of Figure 6 which depicts a utility load curve of different shape. Again, curve B is curve A redrawn. The actual factors for these curves are $K=0.150$; $\phi=0.639$; and $\theta=0.478$. Using equation 5 above, a value of 0.461 is calculated for θ which is 3.6 per cent low. Since the value of ϕ exceeds 0.5, and considering the shape of curve B, formulas 14 and 15 of Table I are suggested. With the former, the value obtained for θ is 0.484, which is 1.2 per cent high; with the latter a value of 0.474 is obtained which is 0.8 per cent low. The graph for formula 14 is represented by curve D.

In both Figures 5 and 6, the curves B have been obtained by tabulating the average values of m for small intervals of t, and then arranging these values consecutively in ascending order. Since the area under each curve is $\int(m)dt$, or equally, $\int(t)dm$, the same result may be obtained by taking average values of t for small intervals of m. In either case, the resulting curve is the well-known load-duration curve, with the scale of abscissae reversed in direction.

Load-Curve Combinations

When daily load curves for a given system follow the same general pattern, one method of approach for the consideration of a longer period such as a month or year, is illustrated in Figure 7. The solid shown is obtained by arranging the days, not chronologically, but in the order of increasing energy values, so that the area of the front face measures the energy on the minimum day and that of the rear face, the energy of the maximum day. The volume then measures the energy for the total period. The hours in a single day are represented by T_d and the days in the period by T_y.

By making use of geometrical ratios many relations can be established between the daily and yearly factors involved, depending upon the nature of the problem under consideration. If the left-hand curves of Figure 8 are considered as representing two of the 365 daily load curves for a period of one year, the others lying between these two, then the daily energy values can be represented as shown by the right-hand curve. The area under the latter measures the annual energy. The various formulas which have heretofore been considered can be applied, requiring only the substitution of the energy value W for the maximum load value M in the appropriate formulas.

An able exposition of the weekly and seasonal variations in load curves and their components, classified as to time of day and character of load, has been given by Farley C. Ralston. To those readers interested in problems having to do with predictions of future demand and energy, his paper[3] is particularly recommended.

The Use of Probable Values

Another method for handling longer periods, which is perhaps more satisfactory, may be developed from the characteristics of a single curve, such as the one shown in Figure 9. This indicates the total hours during which the load reaches the value indicated.

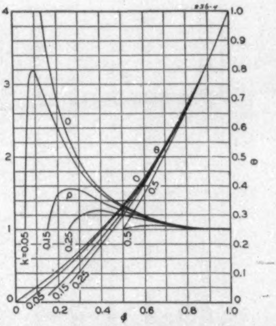


Figure 4. Curves showing values of θ and ρ in terms of ϕ for various values of k

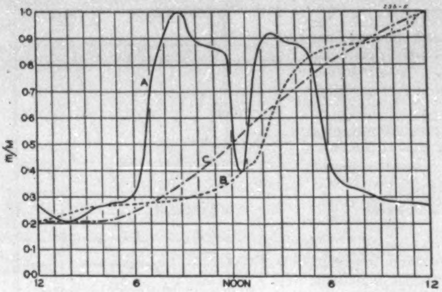


Figure 5. Industrial load curve

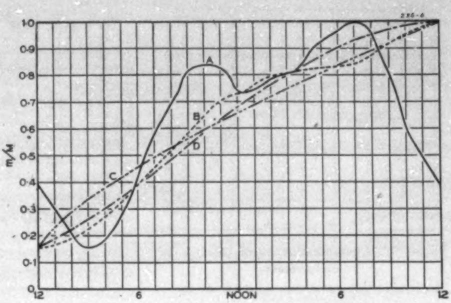


Figure 6. Utility load curve

Experience has demonstrated that, when a considerable number of days are combined in this manner, the curve tends to assume the general shape shown. This is due to the fact that extremely low and high values of load do occur, but less frequently than those nearer to the average value. This circumstance leads naturally to a comparison with the well-known frequency-curve as given in Figure 10. The mode of the latter corresponds to the time (T). Referring to Figure 9, the probability, or duration, of a load not less than C in value is measured by T_1, corresponding to P_1 in Figure 10, and therefore C corresponds to Q in value. In other words the probability of the load's being at least KM in value is unity; of its being M or greater, say $^1/_{48}$ or one-half hour each day, and between these two, as shown for intermediate values.[6,7]

The curve of Figure 9 is, in reality, a load-duration curve reduced to daily values. If a yearly period be covered, then by dividing each value of abscissae, given in hours, by 365, the curve represents a daily period, which, however, includes all values of the load encountered throughout the year; and p represents the time by which the daily peak is measured. Otherwise stated, if a one-hour peak be considered, $p=1.0$ for $T=24$ hours or $p=^1/_{24}$ for $T=1.0$.

Should a maximum be so rare that its value is reached for one hour only during the year, then the value of p would be 1/8,760 for $T=1.0$. So p may be considered as a measure of the probability that the load will not be less than the value corresponding to D.

The equation for the load-duration curve of Figure 9 is

$$m=M\left[K+\frac{2}{\sqrt{\pi}}(\phi-K)\left(\log_e\frac{T}{t}\right)^{1/2}\right] \tag{8}$$

and the loss factor corresponding thereto is defined by

$$\theta=\phi^2+\left(\frac{4}{\pi}-1\right)(\phi-K)^2$$
$$=\phi^2+0.27324(\phi-K)^2 \tag{9}$$

where

$$\rho_0=\frac{4}{\pi}$$

This is a most useful form for application to utility loads and appears to increase in accuracy of results as the time under consideration increases.

Maximum-demand instruments will usually read the peak load as an average during the time considered, in which case the relations hold as given. Referring to Figure 9, the curve is sometimes considered as having the value (D) for the given period (p). For this condition the equations for the curve and corresponding loss factor are expressed by

$$m=M\left[K+(1-K)\left(\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right)^{1/2}\right] \tag{10}$$

$$\theta=\phi^2+\left(\frac{4}{\pi}-1-\frac{cp}{T}\right)(\phi-K)^2 \tag{11}$$

With reference to Figure 9, equation 10 applies between the limits for $t=p$ and $t=T$. Between the limits for $t=0$ and $t=p$ it is obvious that $m=M$ or $m/M=1.0$.

The value of c in equation 11 is a complicated function of p/T. Values of c can be had from curve B of Figure 13 for values of p/T up to 0.1, which is equivalent to 144 minutes in a 24-hour period. The term cp/T is empirical to make equation 11 similar in character to equation 9, the change in the value of θ being small for small values of p/T.

The derivations of these equations are included as appendixes C and D.

Comparison of Maximum Demands

The curve of Figure 9 may also be used as a basis for converting a peak demand, averaged for a given time interval, to some other time interval. For example, suppose a peak of M_2 is metered on a demand of t_2 minutes. Its value metered on a demand of t_1 minutes could be expressed by

$$M_1=N+(M_2-N)\frac{g_1}{g_2} \tag{12}$$

The fraction g_1/g_2 is also a complicated function of p/T. Values of g_1/g_2 which are commonly used and can be substituted in equation 12 are given in Table II. It is to be noted that when $N=0$, the peaks

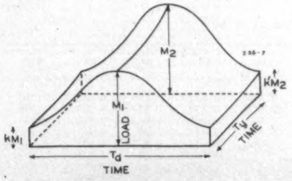


Figure 7. Grouping of daily load curves represented by a solid

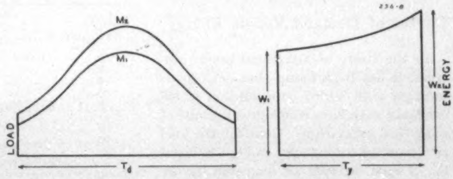


Figure 8. Grouping of daily load curves represented by an area

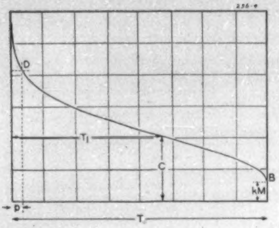


Figure 9. Load-duration curve reduced to daily values

are in direct ratio to the value of g_1/g_2. Relations other than those of Table II may be obtained by reading the values of g_1 and g_2 from curve *A* of Figure 13. The derivation of equation 12 is shown in appendix E.

Utility Distribution Losses

The losses attending a diversified distribution of energy can, as is well-known, be classified into two principal groups: those remaining practically constant, such as transformer-core losses, leakage, dielectric, and corona losses; and those which vary as the square of the current at each point and are known from experience to vary, practically, as the square of the total load.

Over a determined period, the sum of these two groups is known from meter readings at the points of generation and reception. If it happens in the two selected periods that the load factors and the average values of the losses differ sufficiently, then the loss factors determined in one of the manners outlined, furnish a method for segregating these variable and fixed losses.

Let L_1 = average loss corresponding to θ_1 and M_1
L_2 = average loss corresponding to θ_2 and M_2
L_0 = constant loss

Then as derived in appendix F

$$L_0 = L_2 - \frac{L_2 - L_1}{1 - \frac{\theta_1}{\theta_2}\left(\frac{M_1}{M_2}\right)^2} \qquad (13)$$

Curves of Demand Versus Energy

In the study of integrated power systems, it has been found that peak loads increase with added consumption in accordance with laws which give promise of analytical expression. Consider the load curves of the form shown in Figures 5 and 6. If each ordinate be increased by the same percentage, the total area, or energy, will be increased by an equal percentage, and the load factor will remain unchanged. Also, if the lower values are increased in general a greater percentage than is the percentage increase in the peak value, the resulting increase in energy will be accompanied by an increase in load factor. This latter type of growth is the desideratum of all utility men, but unfortunately the opposite seems more often to happen, namely, the peak increases at a more rapid rate than the energy, with a resulting decrease in load factor.

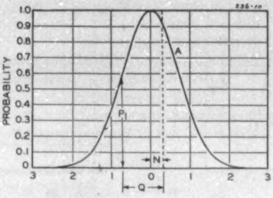


Figure 10. Probability curve

These relations can be analyzed with reference to the well-known load-energy curve illustrated in Figure 11. Referring to the latter, ϕ_0 is the load factor for a fixed period (T). The variation-factor is K_0. With the maximum value of M_0 and T as unity, the distance between *C* and *B* will be equal to $1-\phi_0$. Some line joining *A* and *B* will be the locus of the peak loads with variation of consumption.

Experience with growth in California lead engineers in that state to the belief that the curve from *A* to *B* is a parabola, tangent to *AC* at *A* and tangent to *CB* at *B*. Also, they and others feel that all load-energy curves can be expressed in this parabolic form.[1,2]

A general equation of the curve of Figure 11 derived in appendix G, may be given by:

$$\phi = 1 - (1-\phi_0)\left[\frac{(W/M_0T) - K_0}{\phi_0 - K_0}\right]^{\beta} \qquad (14)$$

where the exponent β can be determined for the specific case by obtaining values of *M* and the associated values of *W*, other than those of the terminal points.

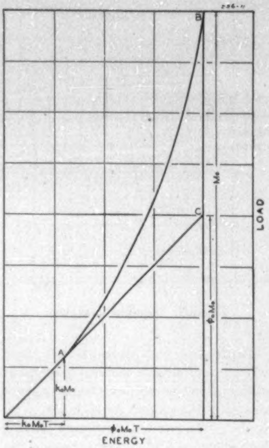


Figure 11. Peak-load versus energy curve

Some specific applications are illustrated in Figure 12. The load-energy curves are shown as full lines and the corresponding load-factor curves as dotted lines. Curve *D* is a parabola with points of tangency at *A* and *B*. Curve *E* indicates the relations for a linear increase of peak load with energy. Curve *F* is obtained for $\beta = 1.0$. It is to be noted that the line *O-C* in Figures 11 and 12 represents the diagonal of a square of which ϕ_0 is one side.

In the Philadelphia district, it was determined a few years ago, that the growth in energy output and maximum yearly peak could be expressed closely by the same constant percentage increase. This condition would mean a constant yearly load factor. With reference to Figure 12, this condition, which represents a stabilization of the change in load factor, means that the curve *E'* will have become horizontal at some point beyond *C*, and the curve *E* continue beyond the

Table II. Values of g_1/g_2

Minutes	5	10	15	20	30	45	60	90	120
5	1.000	0.945	0.911	0.889	0.852	0.817	0.788	0.747	0.717
10		1.000	0.964	0.940	0.902	0.864	0.834	0.791	0.759
15			1.000	0.975	0.935	0.896	0.865	0.820	0.787
20				1.000	0.959	0.919	0.886	0.841	0.807
30					1.000	0.958	0.924	0.877	0.842
45						1.000	0.965	0.915	0.878
60							1.000	0.948	0.910
90								1.000	0.960
120									1.000

Multiply when converting from minutes at left to minutes at top. Divide for the reverse operation.

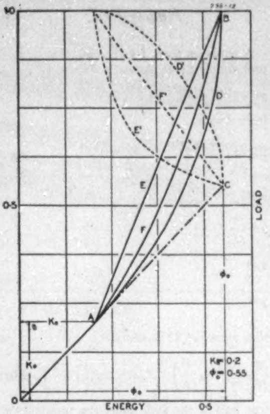


Figure 12. Peak-load versus energy and load-factor versus energy curves

point *B* as a straight line with less slope than the curve *E* has at *B*. In fact, this result would make curve *E* slightly concave downward at *B*.[3]

Combination of Equal Factors

In this discussion of load factors, it may not be amiss to rewrite two formulas which have proved most useful in determining the combined load factors or loss factors of a number of loads (*Q* in number) which occur at random and are about equal as to load factors, magnitudes, and durations.[4]

$$\phi_Q = \frac{1}{Q^{-1/\alpha}\left(\frac{1}{\phi_1}-1\right)+1} \tag{15}$$

$$\theta_Q = \frac{1}{Q^{-1/\psi}\left(\frac{1}{\phi_1}-1\right)+1} \tag{16}$$

For a uniform distribution of loads throughout the period considered, it would seem that the values of α and ψ should have a theoretical value of 2, to harmonize with the theory of least squares as applied to deviations. The author has found from experience that chiefly due to a nonuniform load distribution, over the time considered—such as a day—the values of α and ψ will be greater than 2. The actual values, once found for specific conditions, can be re-employed for others, in which the magnitudes involved may be quite different.

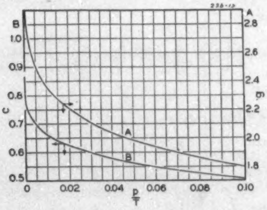


Figure 13. Values of c and g in terms of p/T

These formulas, the derivation of which is shown in appendix H, may be applied to many problems involving demand factors and diversity factors and have been found particularly helpful in the design of utility distribution systems, whether for electricity, gas, water, or sewage.

Conclusions

The formulas presented in this paper may be applied in many ways, and their effectiveness enhanced by the addition of constants derived from experience in actual calculations. Here, they demonstrate methods only.

Often comparison between the behavior of small loads and large loads is lost sight of through the differences in magnitudes and scales used. It is felt that a reduction to unity values is a first step in arriving at a greater simplicity in the visualization of essential factors.

In passing from the specific case to the more general, in which the elements of probability enter, it is believed that more useful and accurate solutions are obtained, especially in that field in which the elements have an economic significance which depends on yearly values.

There are many routes by which one may arrive at answers for the intangible problems to which these formulas are best suited. It is possible even to obtain rms values of loads drawn on circular charts by weighing the aggregate amount of paper included within the graphs; or the same results often may be gotten by instruments and meters. If this presentation assists some engineers in reducing the detailed labor of plotting, planimeter work, and summations, one of its purposes will have been accomplished.

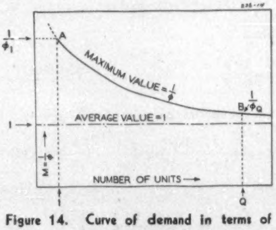


Figure 14. Curve of demand in terms of number of units contributing

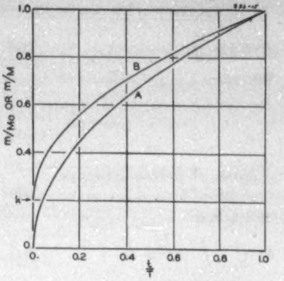


Figure 15. Curves of f (t/T) and f (t/T)+k

To explain more fully direct methods of application, the solutions to some simple examples are included as appendix I.

Appendix A

Referring to Figure 1 of the paper, the equation for curve *A* can be expressed as

$$m = f(t)$$

then

$$W_0 = \int_0^T f(t)dt$$

and

$$\phi_0 = \frac{W_0}{M_0T} = \frac{1}{M_0T}\int_0^T f(t)dt$$

or

$$\int_0^T f(t)dt = \phi_0 M_0 T \tag{17}$$

for curve *B*

$$m = KM + f(t)$$

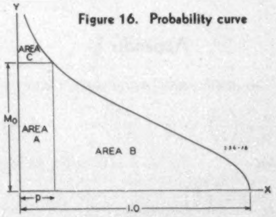
and

$$W = \int_0^T [KM + f(t)]dt$$

$$\phi = \frac{W}{MT} = \frac{1}{MT}\int_0^T [KM + f(t)]dt$$

$$= K + \frac{1}{MT}\int_0^T f(t)dt \tag{18}$$

Figure 16. Probability curve



substituting equation 17 in equation 18

$$\phi=K+\phi_0\frac{M_0}{M} \tag{19}$$

but

$M=M_0+KM$ or $M_0=(1-K)M$ and

$$\frac{M_0}{M}=(1-K)$$

therefore

$$\phi=K+\phi_0(1-K) \tag{20}$$

By definition

$$\theta_0=\frac{1}{M_0^2T}\int_0^T[f(t)]^2dt \tag{21}$$

and

$$\theta=\frac{1}{M^2T}\int_0^T[KM+f(t)]^2dt \tag{22}$$

from which

$$\theta=\frac{1}{M^2T}\left[K^2M^2T+2KM\int_0^T f(t)dt+\int_0^T[f(t)]^2dt\right] \tag{23}$$

from equation 21

$$\int_0^T[f(t)]^2dt=\theta_0M_0^2T \tag{24}$$

substituting equations 17 and 24 in equation 23 and since

$$\frac{M_0}{M}=(1-K)$$

$$\theta=K^2+2K(1-K)\phi_0+(1-K)^2\theta_0 \tag{25}$$

from equation 20

$$\phi_0=\frac{\phi-K}{1-K}$$

and by definition

$$\rho_0=\frac{\theta_0}{\phi_0^2}$$

Therefore

$$\theta_0=\rho_0\phi_0^2=\rho_0\left(\frac{\phi-K}{1-K}\right)^2$$

substituting in equation 25

$$\theta=K^2+2K(\phi-K)+\rho_0(\phi-K)^2$$

from which

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2 \tag{26}$$

Appendix B

By plotting the curves of Figure 1 with

$$\frac{m}{M_0}=f\left(\frac{t}{T}\right)$$

and

$$\frac{m}{M}=K+f\left(\frac{t}{T}\right)$$

as shown in Figure 15, then if

$$f\left(\frac{t}{T}\right)=\left(\frac{t}{T}\right)^n$$

equations for curves A and B become respectively,

$$\frac{m}{M_0}=\left(\frac{t}{T}\right)^n \text{ and } \frac{m}{M}=K+(1-K)\left(\frac{t}{T}\right)^n$$

$$W_0=\int_0^1\left(\frac{t}{T}\right)^n d\left(\frac{t}{T}\right)=\frac{1}{n+1} \tag{27}$$

when

$$m=M_0=1.0,\quad t=T_0=1.0$$

$$\phi_0=\frac{W_0}{M_0T_0}=W_0=\frac{1}{n+1} \tag{28}$$

Since

$\phi=K+(1-K)\phi_0$ (from appendix A)

$$\phi=K+\frac{(1-K)}{(n+1)} \text{ from which } n=\frac{1-\phi}{\phi-K} \tag{29}$$

Then

$$\frac{m}{M}=K+(1-K)\left(\frac{t}{T}\right)^{\frac{1-\phi}{\phi-K}}$$

or

$$m=M\left[K+(1-K)\left(\frac{t}{T}\right)^{\frac{1-\phi}{\phi-K}}\right] \tag{30}$$

by definition

$$\theta_0=\int_0^1\left(\frac{t}{T}\right)^{2n}d\left(\frac{t}{T}\right)=\frac{1}{2n+1} \tag{31}$$

and from appendix A

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2$$

also by definition

$$\rho_0=\frac{\theta_0}{\phi_0^2}=\frac{1}{2n+1}\div\frac{1}{(n+1)^2}=\frac{n^2}{2n+1}+1$$

or

$$\rho_0-1=\frac{n^2}{2n+1}$$

so that

$$\theta=\phi^2+\left(\frac{n^2}{2n+1}\right)(\phi-K)^2$$

or

$$\theta=\phi^2+\frac{(1-\phi)^2(\phi-K)^2}{(1-K)^2-(1-\phi)^2} \tag{32}$$

Since

$$n=\frac{1-\phi}{\phi-K}$$

For the special case where $K=0$

$$\theta=\phi^2+\frac{(1-\phi)^2\phi^2}{1-(1-\phi)^2}=\frac{\phi}{2-\phi} \tag{33}$$

and

$$\rho=\frac{\theta}{\phi^2}=\frac{\phi}{(2-\phi)\phi^2}=\frac{1}{\phi(2-\phi)} \tag{34}$$

Appendix C

A common form for the equation of the probability curve shown in Figure 10 is

$$y=e^{-h^2x^2}$$

from which

$$x=\frac{1}{h}\left(\log_e\frac{1}{y}\right)^{1/2} \tag{35}$$

Let

$$x=\frac{m}{M_0}$$

and

$$y=\frac{t}{T}$$

then equation 35 becomes

$$\frac{m}{M_0}=\frac{1}{h}\left(\log_e\frac{T}{t}\right)^{1/2} \tag{36}$$

One half of the area under the probability curve is

$$\int ydx=\int_0^\infty e^{-h^2x^2}dx=\frac{\sqrt{\pi}}{2h} \tag{37}$$

which is also the area corresponding to W_0. From appendix B

$$W_0=\phi_0$$

and from appendix A

$$\phi_0=\frac{\phi-K}{1-K}$$

Therefore

$$W_0=\phi_0=\frac{\phi-K}{1-K}=\frac{\sqrt{\pi}}{2h}$$

from which

$$\frac{1}{h}=\frac{2}{\sqrt{\pi}}\left(\frac{\phi-K}{1-K}\right)$$

substituting in equation 36

$$\frac{m}{M_0}=\frac{2}{\sqrt{\pi}}\left(\frac{\phi-K}{1-K}\right)\left(\log_e\frac{T}{t}\right)^{1/2} \tag{38}$$

from appendix B

$$\frac{m}{M}=K+(1-K)\frac{m}{M_0}$$

so that

$$\frac{m}{M}=K+\frac{2}{\sqrt{\pi}}(\phi-K)\left(\log_e\frac{T}{t}\right)^{1/2}$$

or

$$m=M\left[K+\frac{2}{\sqrt{\pi}}(\phi-K)\left(\log_e\frac{T}{t}\right)^{1/2}\right] \tag{39}$$

for

$$m=M_0$$

and

$$t=T$$

$$\theta_0=\int_0\left(\frac{m}{M_0}\right)^2dt$$

or

$$\theta_0=\frac{1}{h^2}\int_0^1\left(\log_e\frac{T}{t}\right)d\left(\frac{t}{T}\right)$$

$$=-\frac{1}{h^2}\int_0^1\left(\log_e\frac{t}{T}\right)d\left(\frac{t}{T}\right)=\frac{1}{h^2}$$

then

$$\rho_0=\frac{\theta_0}{\phi_0^2}=\frac{1}{h^2}\div\left(\frac{\sqrt{\pi}}{2h}\right)^2=\frac{4}{\pi}$$

and

$$(\rho_0-1)=\left(\frac{4}{\pi}-1\right)$$

and

$$\theta=\phi^2+(\rho_0-1)(\phi-K)^2$$

or

$$\theta=\phi^2+\left(\frac{4}{\pi}-1\right)(\phi-K)^2 \tag{40}$$

With the curve of Figure 9 terminating at point D, when

$$t=p,\ m=D=M_0+KM$$

so that equation 36 becomes

$$\frac{m}{M_0}=\frac{M_0}{M_0}=1.0=\frac{1}{h}\left(\log_e\frac{T}{p}\right)^{1/2} \tag{41}$$

for the origin at $Pt\ O$, KM from which

$$h=\left(\log_e\frac{T}{p}\right)^{1/2}$$

substituting in equation 36

$$\frac{m}{M_0}=\frac{\left(\log_e\frac{T}{t}\right)^{1/2}}{\left(\log_e\frac{T}{p}\right)^{1/2}}=\left[\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right]^{1/2}$$

also

$$\left[\frac{\log_{10}\frac{T}{t}}{\log_{10}\frac{T}{p}}\right]^{1/2} \tag{42}$$

and

$$\frac{m}{M}=K+(1-K)\frac{m}{M_0}=K+(1-K)\left[\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right]^{1/2}$$

from which

$$m=M\left[K+(1-K)\left(\frac{\log_e\frac{T}{t}}{\log_e\frac{T}{p}}\right)^{1/2}\right]$$

$$=M\left[K+(1-K)\left(\frac{\log_{10}\frac{T}{t}}{\log_{10}\frac{T}{p}}\right)^{1/2}\right] \tag{43}$$

between the limits $t=p$ and $t=T$.

Appendix D

Derivation of values for c and g of equations 11 and 12 for a peak-load period of one hour in 24 hours ($p=1/24$). From appendix C the equation for the probability curve (Figure 16) may be expressed as

$$y=\frac{1}{h}\left(\log_e\frac{1}{x}\right)^{1/2} \tag{44}$$

where $y=M_0$ and $x=t/T$ then

$$\text{area } B=\frac{1}{h}\int_{x=p}^{x=1}\left(\log_e\frac{1}{x}\right)^{1/2}dx$$

let

$$z=\left(\log_e\frac{1}{x}\right)^{1/2}=\left(\log_e\frac{1}{p}\right)^{1/2}$$

when $x=p$. Therefore

$$\text{area } B=\frac{2}{h}\left[\left(\frac{z^3}{3}+\frac{z^7}{14}+\frac{z^{11}}{264}+\ldots\right)-\left(\frac{z^5}{5}+\frac{z^9}{54}+\frac{z^{13}}{1{,}560}+\ldots\right)\right]=\frac{0.801579}{h} \tag{45}$$

$$\text{area } (A+B+C)=\frac{\sqrt{\pi}}{2h}=\frac{0.886227}{h}$$

(from appendix C) (46)

then

area $(A+C)$ = equation 46 less equation 45

$$=\frac{0.084648}{h}$$

Defining g as the average load during the period p, the value of g is obtained from

$$g=\frac{\text{area }(A+C)}{p}=\frac{24\times0.084648}{h}=\frac{2.031552}{h} \tag{47}$$

Since the objective is to obtain a ratio for two values of g, there is no need to obtain a numerical value for h which cancels out, or one can use $h=1.0$. The numerator of equation 47 is therefore one point on curve A of Figure 13 which has been plotted for the value

$p=1/24$ or 0.04167

From equation 44

$$y^2=\frac{1}{h^2}\log_e\frac{1}{x}$$

and the area under this curve corresponding to section B =

$$\int_p^1\frac{1}{h^2}\left(\log_e\frac{1}{x}\right)dx=\frac{1}{h^2}\left[1-p\left(1+\log_e\frac{1}{p}\right)\right]$$

and for $p=1/24$

$$r_B^2=\frac{0.825914}{h^2} \tag{48}$$

From equation 44

$$M_0=\frac{1}{h}\left(\log_e\frac{1}{x}\right)^{1/2}=\frac{1}{h}\left(\log_e\frac{1}{p}\right)^{1/2}$$

and for $p=1/24$

$$M_0=\frac{1.782712}{h}$$

and

$$M_0^2=\frac{3.178062}{h^2}$$

so that the corresponding area under M_0^2 for section A becomes

$$r_A^2=M_0^2p=\frac{3.178062}{h^2}\times\frac{1}{24}=\frac{0.132419}{h^2} \tag{49}$$

$$r_A^2+r_B^2=\frac{0.825914}{h^2}+\frac{0.132419}{h^2}=\frac{0.958333}{h^2} \tag{50}$$

by definition

$$r^2=M^2\theta$$

so that

$$\theta_{A+B}=\theta_0=\frac{r_A^2+r_B^2}{M_0^2}=\frac{0.958333}{M_0^2h^2}$$

by definition

$$\phi=\frac{W}{MT}$$

and for $T=1.0$

$$\phi=\frac{W}{M}$$

then

$$\phi_{A+B}=\phi_0=\frac{W_A+W_B}{M_0}$$

$$W_A=M_0p=\frac{1.782712}{h}\times\frac{1}{24}=\frac{0.074280}{h}$$

$W_B=\dfrac{0.801579}{h}$ from equation 45

Therefore

$$\phi_{A+B}=\phi_0=\left(\frac{0.074280}{h}+\frac{0.801579}{h}\right)\frac{1}{M_0}$$

$$=\frac{0.875859}{M_0h}$$

$$\rho_0=\frac{\theta_0}{\phi_0^2}=\frac{0.958333}{M_0^2h^2}\times\frac{M_0^2h^2}{(0.875859)^2}$$

$$=1.249248 \tag{51}$$

and

$$\rho_0-1=1.249248-1.0=0.249248$$

From appendix C $(\rho_0-1)=(4/\pi-1)$ for curve without constant value for M_0 during period p. As a correction for effect of latter condition, assume

$$(\rho_0-1)=\left(\frac{4}{\pi}-1\right)-\frac{cp}{T}$$

where cp/T is correction factor. Then

$$(\rho_0-1)=0.249=\left(\frac{4}{\pi}-1\right)-\frac{cp}{T}$$

and for $p/T=1/24$

$c=0.5758$ which represents one point on curve B of Figure 13 which has been plotted for the value $p=1/24$ or 0.042.

Appendix E

Referring to Figure 9, let p_1 and p_2 be two intervals during which the corresponding peak loads M_1 and M_2 are measured as average values. Then from appendix D

$$g_1 = M_1 - KM = M_1 - N$$

and

$$g_2 = M_2 - KM = M_2 - N$$

since

$$KM = N$$

Therefore

$$\frac{M_1 - N}{M_2 - N} = \frac{g_1}{g_2}$$

or

$$M_1 = N + (M_2 - N)\frac{g_1}{g_2}$$

Appendix F

Total loss = constant loss + variable loss

or $L_1 = L_0 + CM_1{}^2\theta_1$
and $L_2 = L_0 + CM_2{}^2\theta_2$

then

$$L_2 - L_1 = C(M_2{}^2\theta_2 - M_1{}^2\theta_1)$$

where C = constant, from which

$$C = \frac{L_2 - L_1}{M_2{}^2\theta_2 - M_1{}^2\theta_1}$$

and

$$L_0 = L_2 - M_2{}^2\theta_2\left(\frac{L_2 - L_1}{M_2{}^2\theta_2 - M_1{}^2\theta_1}\right)$$

or

$$L_0 = L_2 - \frac{L_2 - L_1}{1 - \left(\frac{\theta_1}{\theta_2}\right)\left(\frac{M_1}{M_2}\right)^2}$$

Appendix G

Referring to Figures 11 and 12, it is seen that the load factor varies from a value of 1.0 at the base load to a value of ϕ_0 at the maximum peak and output for a given time, the line OC being the locus of the peaks for unity load factor. The curve defining this variation may be expressed by

$$(1 - \phi) = C(W - K_0M_0T)^\beta$$

where C = constant, when

$$\phi = \phi_0, \quad W = \phi_0M_0T$$

so that

$$(1 - \phi_0) = C(\phi_0M_0T - K_0M_0T)^\beta$$

from which

$$C = \frac{(1 - \phi_0)}{(\phi_0M_0T - K_0M_0T)^\beta}$$

Therefore

$$(1 - \phi) = (1 - \phi_0)\left[\frac{W - K_0M_0T}{\phi_0M_0T - K_0M_0T}\right]^\beta$$

$$= (1 - \phi_0)\left[\frac{\frac{W}{M_0T} - K_0}{\phi_0 - K_0}\right]^\beta$$

and

$$\phi = 1 - (1 - \phi_0)\left[\frac{\frac{W}{M_0T} - K_0}{\phi_0 - K_0}\right]^\beta$$

Appendix H

Referring to Figure 14, it is assumed that the average load for one of Q units is the same as the average value for a single unit. If the average load be taken as unity, the maximum load for a single unit becomes $1/\phi_1$ and for each of Q units, $1/\phi_Q$. Some curve connecting A and B represents the variation of $1/\phi$ with Q.
When $Q \rightleftarrows \infty$, ϕ and $1/\phi \rightleftarrows 1$, the average.
When $Q = 1$, $\phi = \phi_1$ and $1/\phi_1$ is the maximum for 1 unit.

Curve A-B may be expressed in the hyperbolic form

$$\left(\frac{1}{\phi} - 1\right)^\alpha \times Q = C \tag{52}$$

where C = constant. When

$$\phi = \phi_1 \quad Q = 1$$

and

$$C = \left(\frac{1}{\phi_1} - 1\right)^\alpha$$

When

$$\phi = \phi_Q \quad Q = Q$$

and

$$C = \left(\frac{1}{\phi_Q} - 1\right)^\alpha \times Q$$

then

$$\left(\frac{1}{\phi_1} - 1\right)^\alpha = \left(\frac{1}{\phi_Q} - 1\right)^\alpha Q \tag{53}$$

and

$$\left(\frac{1}{\phi_Q} - 1\right) = Q^{-1/\alpha}\left(\frac{1}{\phi_1} - 1\right) \tag{54}$$

or

$$\frac{1}{\phi_Q} = Q^{-1/\alpha}\left(\frac{1}{\phi_1} - 1\right) + 1 \tag{55}$$

so that

$$\phi_Q = \frac{1}{Q^{-1/\alpha}\left(\frac{1}{\phi_1} - 1\right) + 1} \tag{56}$$

For the special case when the minimum load is zero, so that $N = KM = O$, equation 56 becomes

$$\phi_Q = \frac{1}{Q^{-1/\psi}\left(\frac{1}{\phi_1} - 1\right) + 1} \tag{57}$$

also

$$\theta = \frac{\phi}{2 - \phi} \quad \text{or} \quad \phi = \frac{2\theta}{1 + \theta}$$

from appendix B
substituting in equation 57

$$\frac{2\theta_Q}{1 + \theta_Q} = \frac{1}{Q^{-1/\psi}\left(\frac{1 - \theta_1}{2\theta_1}\right) + 1} \tag{58}$$

from which

$$\theta_Q = \frac{1}{Q^{-1/\psi}\left(\frac{1}{\theta_1} - 1\right) + 1} \tag{59}$$

Appendix I

Equipment I²R Losses and Capacities

1. A 400-ton suburban train is operating on a schedule for which the average current in the traction motors is 20 per cent of the maximum. To find a figure for the increase in copper losses in the motors if trailers be added to make a total train-weight of 600 tons:

(*a*) The accelerating relays and starting-resistance grids are not altered, and the same schedule is kept by less coasting.

(*b*) The relays are altered to permit 25 per cent more accelerating current.

(*a*) Let

W_1 = energy consumption of motors for 400-ton train
W_2 = energy consumption of motors for 600-ton train

Then

$$W_1 = \phi_1M_1T_1; \quad W_2 = \phi_2M_2T_2$$

and

$$\frac{W_2}{W_1} = \frac{\phi_2}{\phi_1}$$

Since

$$M_1 = M_2 \text{ and } T_1 = T_2$$

Since

$$W_2 = 1.5W_1 \quad \phi_2 = 1.5\phi_1$$
$$\phi_1 = 0.2$$

Therefore

$$\phi_2 = 0.3$$

From equation 6

$$\theta_1 = \frac{\phi_1}{2 - \phi_1} = \frac{0.2}{1.8} = 0.111$$

$$\theta_2 = \frac{\phi_2}{2 - \phi_2} = \frac{0.3}{1.7} = 0.176$$

Per cent increase in copper losses

$$= \frac{\theta_2 - \theta_1}{\theta_1} \times 100 = \frac{0.176 - 0.111}{0.111} \times 100$$

= 58.6 per cent. Say 59 per cent.

(*b*) $W_1=\phi_1 M_1 T_1$ and $W_2=\phi_2 M_2 T_2$
But $W_2=1.5W_1$; $M_2=1.25M_1$ and since $T_1=T_2$
Therefore

$$W_2=\phi_2 M_2 T_2=1.5W_1=1.5\phi_1 M_1 T_1$$

or

$$\phi_2\times 1.25M_1T_2=1.5\phi_1 M_1 T_1$$

or

$$\phi_2=\frac{1.5\phi_1}{1.25}=\frac{1.5\times 0.2}{1.25}=0.24$$

Then

$$\theta_1=\frac{\phi_1}{2-\phi_1}=\frac{0.2}{1.8}=0.111$$

and

$$\theta_2=\frac{\phi_2}{2-\phi_2}=\frac{0.24}{1.76}=0.136$$

Let L_1 = present copper loss
L_2 = new copper loss

Then

$$\frac{L_2}{L_1}=\frac{C\times 0.136\times (M_2)^2}{C\times 0.111\times (M_1)^2}=\frac{0.136}{0.111}(1.25)^2=1.92$$

where C is a constant
or $L_2=1.918L_1$
Per cent increase in copper loss is 92 per cent.

2. An induction motor is to be applied to a load which is expected to fluctuate between no load and 500 horsepower momentarily, and it is known that 60 horsepower-hours must be delivered at the shaft during a 20-minute cycle. To determine the minimum necessary rating of this motor:
Given:

$$M=500 \text{ hp};\ W=60 \text{ hp-hr};\ T={}^1/_3 \text{ hr};\ K=0$$

Then

$$A=\frac{W}{T}=60\div {}^1/_3=180 \text{ hp}$$

and

$$\phi=\frac{A}{M}=\frac{180}{500}=0.36$$

From equation 7

$$\rho=\frac{1}{\phi(2-\phi)}=\frac{1}{0.36\times 1.64}=1.694$$

By definition $r=uA$ and $r^2=\rho A^2$ or

$$r=A\sqrt{\rho}=180\sqrt{1.694}=234 \text{ horsepower}$$

which is the required rating. The next higher standard rating is 250 horsepower, and a motor of this capacity will serve, provided it has sufficient torque to carry the 500-horsepower peak.

3. A motor-generator set supplying a reversing rolling-mill motor consumes 150,000 kilowatt-hours during a 24-hour run. Switchboard instruments show that the power to this set fluctuates between 400 and 12,000 kw with almost constant power factor. The cable system to the motor is known to have a loss of 50 kw on a 3,000-kw load. It is required to find the average loss in the cables, and the transformer capacity needed for this load only, if a separate bank be installed to carry this set and cable losses. Power factor 0.83. Given:

$$W=150{,}000 \text{ kwhr};\ M=12{,}000 \text{ kw};$$
$$KM=400 \text{ kw};\ T=24 \text{ hr}$$

$$A=\frac{150{,}000}{24}=6{,}250 \text{ kw}\quad \phi=\frac{6{,}250}{12{,}000}=0.5208$$

$$K=\frac{400}{12{,}000}=0.0333$$

Cable Losses

$$\text{At maximum load}=50\times\left(\frac{12{,}000}{3{,}000}\right)^2=800 \text{ kw}$$

$$\text{At minimum load}=50\times\left(\frac{400}{3{,}000}\right)^2=0.89 \text{ kw}$$

say 1.0 kw

Average cable losses $=\theta\times$loss at maximum load $=\theta\times 800$

From equation 5

$$\theta=\phi^2+\frac{(\phi-K)^2\times(1-\phi)^2}{(1-K)^2-(1-\phi)^2}$$
$$=(0.521)^2+\frac{(0.521-0.033)^2(1-0.521)^2}{(1-0.033)^2-(1-0.521)^2}$$
$$=0.350$$

therefore average cable losses $=0.350\times 800=280$ kw

For Transformer

Maximum load = 12,000 + 800 = 12,800 kw
Average load = 6,250 + 280 = 6,530 kw
Minimum load = 400 + 1 = 401 kw

Then

$$\phi=\frac{6{,}530}{12{,}800}=0.510$$

$$K=\frac{401}{12{,}800}=0.031$$

and

$$\theta=\phi^2+\frac{(\phi-K)^2\times(1-\phi)^2}{(1-K)^2-(1-\phi)^2}$$
$$=(0.510)^2+\frac{(0.510-0.031)^2(1-0.510)^2}{(1-0.031)^2-(1-0.510)^2}$$
$$=0.339$$

$$r^2=M^2\theta \text{ or } r=M\sqrt{\theta}=12{,}800\sqrt{0.339}$$
$$=7{,}450 \text{ kw}$$

and at 83 per cent power factor the transformer capacity required = 8,980 kva, say 9,000 kva.

Extrapolation of Demands

4. On one of the days of maximum load a utility company had a peak load of 245,000 kw and an output of 3,116,400 kilowatt-hours. On a day of less load the output was 2,508,000 kilowatt-hours with a peak of 190,000 kw. With this basis, to determine the peak which may be expected when the daily output reaches 3,500,000 kw, the base load is 35,000 kw.
Given:

$$M_0=245{,}000 \text{ kw}\quad W_0=3{,}116{,}400 \text{ kwhr}$$
$$M_1=190{,}000 \text{ kw}\quad W_1=2{,}508{,}000 \text{ kwhr}$$

$$K_0M_0=35{,}000 \text{ kw}$$

or

$$K_0=\frac{35{,}000}{245{,}000}=0.143$$

$$T=24 \text{ hr}\quad W_2=3{,}500{,}000 \text{ kwhr}$$

$$\phi_0=\frac{W_0}{M_0T}=\frac{3{,}116{,}400}{245{,}000\times 24}=0.53$$

$$\phi_1=\frac{W_1}{M_1T}=\frac{2{,}508{,}000}{190{,}000\times 24}=0.55$$

From equation 14

$$\phi_1=1-(1-\phi_0)\left[\frac{\dfrac{W_1}{M_0T}-K_0}{\phi_0-K_0}\right]^\beta$$

or

$$0.55=1-(1-0.53)\left[\frac{\dfrac{2{,}508{,}000}{245{,}000\times 24}-0.143}{0.53-0.143}\right]^\beta$$

from which $\beta=0.140$

Therefore

$$\phi_2=1-(1-0.53)\times\left[\frac{\dfrac{3{,}500{,}000}{245{,}000\times 24}-0.143}{0.53-0.143}\right]^{0.140}=0.52$$

$$M_2=\frac{W_2}{\phi_2T}=\frac{3{,}500{,}000}{0.52\times 24}=280{,}000 \text{ kw}$$

5. To find a value for the increase in demand in an apartment house which may be caused by the installation of 125 electric refrigerators and 125 electric ranges. Each refrigerator is reported to consume 240 watts and about 40 kilowatt-hours monthly. Each range has an average peak of 3,500 watts and consumes about 125 kilowatt-hours monthly.

For the Refrigerators

$M_1=240$ watts $\quad T=24\times 30=720$ hrs per month

$W_1=40{,}000$ watt-hours $\quad Q=125$

$$A_1=\frac{40{,}000}{720}=56 \text{ watts}\quad \phi_1=\frac{A_1}{M_1}=\frac{56}{240}=0.23$$

From equation 14

$$\phi_Q=\frac{1}{Q^{-1/\alpha}\left(\dfrac{1}{\phi_1}-1\right)+1}$$
$$=\frac{1}{\dfrac{\left(\dfrac{1}{0.231}-1\right)}{\sqrt[3]{125}}+1}=0.60$$

where α assumed to have a value of 3.0.

Therefore

maximum coincident demand (M_{Q1}) =

$$\frac{56\times125}{0.60}=11{,}700 \text{ watts, say } 12 \text{ kw}$$

and

$$\text{demand factor}=\frac{11{,}700}{125\times240}=0.39$$

For the Ranges

$M_2=3{,}500$ watts $\quad T=720$ hr

$W_2=125{,}000$ watt-hours $\quad Q=125$

$$A_2=\frac{125{,}000}{720}=174\text{watts}\qquad \phi_2=\frac{174}{3{,}500}=0.050$$

$$\phi_Q=\cfrac{1}{\cfrac{\left(\cfrac{1}{0.05}-1\right)}{\sqrt[3]{125}}+1}=0.21$$

therefore

maximum coincident demand (M_{Q2}) =

$$\frac{174\times125}{0.21}=103{,}600 \text{ watts, say } 104 \text{ kw}$$

and

$$\text{demand factor}=\frac{103{,}600}{125\times3{,}500}=0.24$$

Since the two peaks can be coincident the combined maximum demand is 104+12 = 116 kw, and the over-all demand factor =

$$\frac{116}{125(0.24+3.5)}=0.25$$

6. Measurements taken at a transformer bank which supplies a group of 300 residences show a peak of 130 kw and an average load of 24 kw. The maximum consumption rate in these homes is known to be about one kilowatt each. To determine the expected demand in a similar group of 450 houses; also for one of 1,000 houses.

For the Group of 450 Houses

Given for 300 houses

$$M_1=1\text{ kw}\qquad A_1=\frac{24}{300}=0.08\text{ kw}$$

$$\phi_1=\frac{0.08}{1}=0.08$$

$$M_Q=130\text{ kw}\qquad A_Q=24\text{ kw}\qquad \phi_Q=\frac{24}{130}=0.185$$

$$Q=300\qquad \text{Demand factor}=\frac{130}{300\times1}=0.433$$

From equation 14

$$\phi_Q=\frac{1}{Q^{-1/\alpha}\left(\frac{1}{\phi_1}-1\right)+1}$$

$$=\frac{1}{300^{-1/\alpha}\left(\frac{1}{0.08}-1\right)+1}=0.185$$

from which $\alpha=5.96$. For the 450 houses $Q=450$ and

$$\phi_{Q1}=\frac{1}{(450)^{-\frac{1}{5.96}}\left(\frac{1}{0.08}-1\right)+1}=0.195$$

and maximum demand for 450 houses =

$$\frac{24}{0.195}\times\left(\frac{450}{300}\right)=185\text{ kw}$$

$$\text{demand factor}=\frac{185}{450\times1}=0.41$$

For 1,000 houses

$$\phi_Q=\frac{1}{(1{,}000)^{-\frac{1}{5.96}}\left(\frac{1}{0.08}-1\right)+1}=0.217$$

maximum demand for 1,000 houses =

$$\frac{24}{0.217}\times\left(\frac{1{,}000}{300}\right)=369\text{ kw}$$

and

$$\text{demand factor}=\frac{369}{1{,}000\times1}=0.37$$

Distribution and Transmission Losses

7. During two periods within a year, for 31 days and 59 days respectively, the records of a utility company show the results given in Table A.

Required to find the constant losses of the distribution system:

Table A

Generated Kwhr	Sold Kwhr	1-Hr Peak, Kw	Minimum Load Kw
86,421,000	72,080,000	242,000	41,100
146,566,000	122,498,000	195,300	35,000

Given:

$M_1=242{,}000$ kw

$T_1=31$ days

$K_1M_1=41{,}000$ kw

$W_1=86{,}421{,}000$ kwhr

$M_2=195{,}300$ kw

$T_2=59$ days

$K_2M_2=35{,}000$ kw

$W_2=146{,}566{,}000$ kwhr

Then

$$A_1=\frac{86{,}421{,}000}{31\times24}=116{,}200\text{ kw}\qquad A_2=\frac{146{,}566{,}000}{59\times24}=103{,}500\text{ kw}$$

$$\phi_1=\frac{116{,}200}{242{,}000}=0.48\qquad \phi_2=\frac{103{,}500}{195{,}300}=0.53$$

$$L_1=\frac{86{,}421{,}000-72{,}080{,}000}{31\times24}=19{,}300\text{ kw}\qquad L_2=\frac{146{,}566{,}000-122{,}498{,}000}{59\times24}=17{,}000\text{ kw}$$

$$K_1=\frac{41{,}100}{242{,}000}=0.17\qquad K_2=\frac{35{,}000}{195{,}300}=0.18$$

From equation 9

$$\theta=\phi^2+0.273(\phi-K)^2$$

Then

$$\theta_1=(0.48)^2+0.273(0.31)^2=0.257$$

$$\theta_2=(0.53)^2+0.273(0.35)^2=0.314$$

From equation 13

$$L_0=L_2-\frac{L_2-L_1}{1-\frac{\theta_1}{\theta_2}\left(\frac{M_1}{M_2}\right)^2}=17{,}000-\frac{17{,}000-19{,}300}{1-\frac{0.257}{0.314}\left(\frac{242{,}000}{195{,}300}\right)^2}=8{,}100\text{ kw}$$

8. A projected transmission line is to deliver 100 million kilowatt-hours annually with a line loss not to exceed five million kilowatt-hours. There will be a base load of 5,000 kw and a peak of 25,000 kw. The permissible line loss at the maximum load is required.

$M=25{,}000$ kw $\quad KM=5{,}000$ kw

$W=100{,}000{,}000$ kwhr $\quad L=5{,}000{,}000$ kwhr

$T=8{,}760$ hr

$$A=\frac{100{,}000{,}000}{8{,}760}=11{,}420\text{ kw}$$

$$\phi=\frac{11{,}420}{250{,}000}=0.457$$

$$K=\frac{5{,}000}{25{,}000}=0.2$$

Table B

Company	Peak Load Kw (M_1)	Duration of Peak (Minutes) ($p/T\times1{,}440$)	Production Kwhr (Thousands) (W)
A	310,000	60	1,414.828
B	148,200	15	630,941
C	407,500	20	2,152,529
D	204,000	30	1,022.187
E	55,300	5	221,384

Company	ϕ	K	KM_1	M_1-KM_1	g_2/g_1	$M_r=KM_1+(M_1-KM_1)g_1/g_2$
A	0.521	0.208	64,600	245,400	1/0.924	330,200
B	0.486	0.194	28,800	119,400	1/0.935	140,400
C	0.603	0.241	98,300	309,200	1/0.959	394,800
D	0.572	0.229	46,700	157,300	1/1.000	204,000
E	0.457	0.183	10,100	45,200	1/0.852	48,600

K assumed as 0.4ϕ $\qquad \phi=W/8{,}760M_1$ $\qquad g_2/g_1$ from Table II

From equation 9

$$\theta=\phi^2+0.273(\phi-K)^2=(0.457)^2+0.273)\times(0.457-0.2)^2=0.227$$

$$\text{Average loss}=\frac{5{,}000{,}000}{8{,}760}=571\text{ kw}$$

$$\text{Loss at maximum load}=\frac{\text{average loss}}{\theta}=\frac{571}{0.227}=2{,}520\text{ kw}$$

9. An isolated feeder circuit supplies a manufacturing plant 40 million kilowatt-hours annually, metered at the plant. On the maximum day the peak was 12,000 kva at 0.85 power factor; consumption 123,000 kilowatt-hours; base load 1,800 kva. On the minimum day the peak was 9,000 kva at 0.90 power factor; consumption 105,000 kilowatt-hours with a base load of 1,500 kva.

At a load of 3,000 kva, the feeder loss is 120 kw. The annual feeder loss is required to determine if additional copper is warranted.

Given:

Annually	*Maximum Day*	*Minimum Day*
$W=40{,}000{,}000$ kwhr	$W_2=123{,}000$ kwhr	$W_1=105{,}000$ kwhr
	$M_2=12{,}000$ kva $=10{,}200$ kw	$M_1=9{,}000$ kva $=8{,}100$ kw
	$K_2M_2=1{,}800$ kva	$K_1M_1=1{,}500$ kva
	$K_2=\frac{1{,}800}{12{,}000}=0.15$	$K_1=\frac{1{,}500}{9{,}000}=0.167$
	$\phi_2=\frac{123{,}000}{24\times10{,}200}=0.502$	$\phi_1=\frac{105{,}000}{24\times8{,}100}=0.540$
	$(\phi_2-K_2)=(0.502-0.15)=0.352$	$(\phi_1-K_1)=(0.540-0.167)=0.373$
	$(1-K_2)=1-0.15=0.85$	$(1-K_1)=1-0.167=0.833$

Using curve of function 11 of Table I

$$\theta=\phi^2+{}^1/_{15}[3(\phi-K)^2-3(\phi-K)(1-K)+2(1-K)^2]$$

$$\theta_1=(0.540)^2+{}^1/_{15}[3(0.373)^2-3(0.373\times0.833)+2(0.833)^2]=0.349$$

$$\theta_2=(0.502)^2+{}^1/_{15}[3(0.352)^2-3(0.352\times0.85)+2(0.85)^2]=0.313$$

then

$$L_1=120\times\left(\frac{9{,}000}{3{,}000}\right)^2\times0.349=377\text{ kw}$$

$$L_2=120\times\left(\frac{12{,}000}{3{,}000}\right)\times0.313=602\text{ kw}$$

Using the conception of a solid as illustrated by Figure 7, the value of the energy (W_m) for the theoretical mid-section can be obtained by assuming that the daily energy consumption varies in accordance with formula 2 of Table I so that

$$\frac{w}{W_2}=K+(1-K)\left(\frac{t}{T}\right)^n$$

or

$$w=\left[K+(1-K)\left(\frac{t}{T}\right)^n\right]W_2$$

where $n=\frac{1-\phi}{\phi-K}$ and $w=W_m$ when $\frac{t}{T}=0.5$

For the above equation, K is obtained by the ratio of W_1 to W_2 or

$$K=\frac{105{,}000}{123{,}000}=0.8537$$

The average daily energy consumption is

$$W_a=\frac{40{,}000{,}000}{365}=109{,}600\text{ kwhr}$$

so that

$$\phi_1=\frac{109{,}600}{123{,}000}=0.8910$$

then

$$n=\frac{1-0.8910}{0.8910-0.8537}=2.922$$

and

$$W_m=[K+(1-K)(0.5)^{2.922}]W_2$$
$$=[0.8537+(0.1463\times0.1319)]123{,}000$$
$$=107{,}400\text{ kwhr}$$

By direct interpolation

$$M_m=8{,}100+[10{,}200-8{,}100]\times\frac{107{,}400-105{,}000}{123{,}000-105{,}000}=8{,}730\text{ kw}$$

and

at the average power factor of

$$\frac{0.90+0.85}{2}=0.875$$

$$M_m=\frac{8{,}730}{0.875}=10{,}000\text{ kva}$$

and

$$\phi_m=\frac{107{,}400}{24\times8{,}730}=0.513$$

Assume

$$\frac{K_m}{\phi_m}=\frac{\frac{K_1}{\phi_1}+\frac{K_2}{\phi_2}}{2}=\frac{\frac{0.167}{0.540}+\frac{0.150}{0.502}}{2}=0.304$$

so that

$$K_m=0.304\phi_m=0.304\times0.513=0.156$$

then

$$\theta_m=(0.513)^2+{}^1/_{15}[3(0.357)^2-3(0.357\times0.844)+2(0.844)^2]=0.323$$

and the loss corresponding to the mid-section load curve is

$$0.323\times120\times\left[\frac{10{,}000}{3{,}000}\right]^2=413\text{ kw}$$

The volume of the solid representing the losses, to correspond to Figure 7 may be considered as that of a prismatoid, the formula for which is

$$V=\frac{1}{6}h(A+B+4C)$$

where

V = volume
h = distance between end faces A and B
$A+B$ = area of end faces
C = area of mid-section

then

$h=365$ days
$A=602$ kw $\times24$ hours
$B=377$ kw $\times24$ hours
$C=413$ kw $\times24$ hours

So that

$$\text{annual kwhr loss}=\frac{365}{6}\times24[602+377+(4\times413)]=4{,}133{,}000\text{ kwhr}$$

and

$$\text{the average loss}=\frac{4{,}133{,}000}{24\times365}=472\text{ kw}$$

Comparison of Maximum Demands

10. Utilities A to F report the peak loads and energy generated during a year shown in Table B. For comparative purposes, it is necessary to express all peaks on a 30-minute basis. See formula 12.

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Acoustics and the Quiet Train Ride

WILLIAM A. JACK
NONMEMBER AIEE

Synopsis: This paper discusses those fundamentals of acoustics which are important in attacking practical problems in noise reduction. A description is given of the instruments suitable for measuring the physical quantities involved. The train problem is analyzed, and data taken by the author on a relatively quiet type of coach are presented. The various methods of noise control which may be applied to a coach are discussed, and data are presented indicating the possibilities of each method.

I. Some Fundamentals on Sound and Hearing

THE auditory sensation is subjectively simple, and so familiar that the average reaction to the acoustical engineer and his load of instruments is: "I know it's noisy, why measure it; I don't see why your meter readings jump around; you have measured two sounds which are certainly different to our ears, and your meter gives the same reading; the decibel is a silly unit; all I want is a few square feet of absorbing material which will soak up the sound." The critical gentleman may certainly be forgiven, because his viewpoint is understandable. Nevertheless, there are very definite reasons for the techniques and vocabulary of acoustical analysis and for the variety of methods which may be needed to bring about the desired quiet ride.

Sound in Air

Sound, considered physically in air at a point, is a fluctuation in the barometric pressure above and below normal atmospheric pressure. It may be simply a sinusoidal pressure variation with time, or it may be of the greatest complexity. Using a 1,000-cycle-per-second pure tone the ear is found to be

(*a*) Sensitive—it can hear as sound a pressure variation of 2.96×10^{-9} pounds per square inch.

(*b*) Rugged—it can give a hearing sensation (with some discomfort) for a pressure variation of 9.35×10^{-2} pounds per square inch.

These are rms values. This is a remarkable range, covering a pressure ratio of 3.16×10^7 to 1. The pressure variation is usually spoken of simply as the sound pressure. The threshold of hearing varies with frequency, the ear being less acute at each end of its range. Below 20 cycles and above 15,000 cycles, the hearing sensation is not stimulated no matter how large the sound pressure.

The ear can tell us something about pressure and frequency. It has remarkable powers in judging the quality of complex sounds. It cannot remember loudness accurately, however, for even a short time. A pressure gage is needed, with indicating or recording devices, so that a measure of the pressure variation with time, at a point, may be taken. We may wish to get the instantaneous variation over the cycle, or we may be satisfied with an rms reading. When we have these we have all the data the ear ever had, and furthermore we have everything the air can tell us at this point. What we are able to deduce from it depends on experience with a variety of sound sources, and on the relations between the physical disturbance in the air and the hearing reactions of an auditor.

Decibels

The pressure units given above are inconvenient numbers and cover so wide a range that logarithmic plots would have to be used. The concept of the decibel has been adopted, because it reduces the above pressure range to 150 steps. This is useful, because it is approximately true that 150 variations in loudness may be detected in passing through this range. The difference in decibels between two sound pressures, P_2 and P_1, P_2 being the larger, is defined as

$$\text{decibel difference} = 20 \log_{10} \frac{P_2}{P_1} \qquad (1)$$

The decibel involves a ratio always. When a train noise is said to be 80 decibels, it refers to 80 decibels above some reference pressure. This reference has now been standardized at 2.96×10^{-9} pounds per square inch rms.*

The pressure level of a sound pressure P, expressed in pounds per square inch rms is

$$20 \log_{10} \frac{P}{2.96 \times 10^{-9}} \text{ decibels} \qquad (2)$$

If desired, sound readings may be given in pressure units. Meters are usually calibrated to read directly in pressure level, which answers most purposes very well.

The practicing acoustical engineer finds the relation between pressure level reduction in physical units and human reaction about as follows.[2]

1 decibel—Barely detectable
5 decibels—Definite improvement, worth some expenditure
10 decibels—Striking improvement, worth considerable cost
20 decibels—Outstanding improvement
25 decibels—Almost like "off and on"

For practical purposes discussed later, decibel here can be replaced by the closely related loudness level unit, phon.

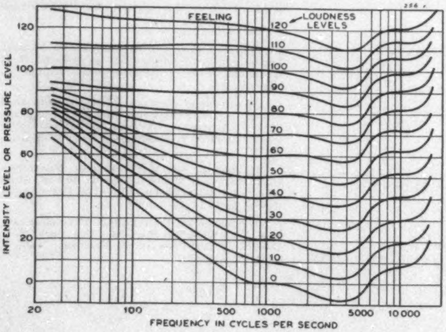

Figure 1. Loudness contours, showing how the sensation of loudness varies with frequency and pressure level

Pressure levels are in decibels; loudness levels are in phons

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WILLIAM A. JACK is research engineer, acoustical engineering section, Johns-Manville Research Laboratories, Manville, N. J.

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*Standard nomenclature employs centimeter-gram-second units. The rms pressure at the threshold of hearing at 1,000 cycles is 0.000204 bar. See reference 1.

This table is true for initial pressure levels of 60 to 90 decibels, the range in which most noise-quieting problems lie. It is difficult to converse at 100 decibels and virtually impossible at 120 decibels. In an extremely noisy environment such as a ship engine room, acoustically untreated, a reduction from 120 decibels to 110 decibels might not be judged as "worth considerable cost." The level would still be unpleasantly high and conversation would still require great effort.

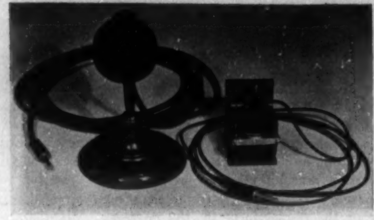


Figure 2. Typical transducers

Dynamic microphone (left) and dynamic vibration velocity unit

Vibration and Sound

In addition to the sound pressure affecting his ears, the coach passenger is subjected to direct vibration through the seat cushions, the floor, and those portions of the wall and window he may lean against. Except for outside noise entering through air leaks and inside sources such as fans, the sound pressures in the coach are built up by vibrating surfaces.

The air particles near a vibrating surface which acts as a sound source move with the same displacement and frequency as the surface and in phase with it. If the vibrating surface moves at 1,000 cycles per second with an rms velocity of 1.92×10^{-6} inches per second, the nearby sound pressure will be 2.96×10^{-9} pounds per square inch, which is the threshold of hearing. Accordingly we may write, when V is expressed as inches per second rms

Vibration velocity level =

$$20\log_{10}\frac{V}{1.92\times10^{-6}}\text{ decibels} \qquad (3)$$

A velocity gage is needed, with indicating or recording devices so that a measure of the velocity variation with time, at a point, may be taken. We may wish to get the instantaneous variation over the cycle, or we may be satisfied with an rms reading. When we have these, we have more than the ear in air can get, because we may explore various surfaces with a probe and determine their individual vibration characteristics. The ear in air can judge merely the pressure characteristics at that point, and the pressure at that point may be dictated by several sources plus the effect of room acoustics. If desired, vibration readings may be given in velocity units. It is convenient to express them in decibels of vibration velocity level. If a surface has a vibration velocity level of 80 decibels, it will generate near it a pressure level of 80 decibels.

Displacement, Velocity, Acceleration

Just because a surface has a large displacement, it must not be thought that a loud sound will result, nor is it necessarily true that a small displacement results in a weak sound. The pressure level near the surface depends on the *velocity* of the vibration, which is a function of both displacement and frequency. Displacement and acceleration may be of interest when stresses, unbalance, metal fatigue, and passenger bodily comfort are under consideration. They may be measured by special instruments, or they may be computed from readings of vibration velocity level and frequency.

For sinusoidal vibrations we have the following relations:

$$x=B_m\sin(2\pi ft) \qquad (4)$$

$$\frac{dx}{dt}=v=B_m2\pi f\cos(2\pi ft)$$
$$=B_m2\pi f\sin\left(2\pi ft+\frac{\pi}{2}\right) \qquad (5)$$

$$\frac{d^2x}{dt^2}=a=-B_m4\pi^2f^2\sin(2\pi ft)$$
$$=B_m4\pi^2f^2\sin(2\pi ft+\pi) \qquad (6)$$

where

x, v, a = instantaneous values of displacement, velocity, acceleration

B_m = maximum displacement of particle from neutral position

f = frequency of vibration in cycles per second

t = time in seconds

Equation 4 describes a particle whose displacement varies sinusoidally with time, f cycles per second, between the limits $+B_m$ and $-B_m$. Let us assume $t=0$ is now. The equation shows that the particle displacement is now zero, about to increase positively. Equation 5 describes the velocity of this particle. It is similar to the displacement but differs in amplitude and phase. The equation describes a particle whose velocity varies sinusoidally with time, f cycles per second, between the limits $+B_m2\pi f$ and $-B_m2\pi f$. At f cycles per second, one cycle is completed in $1/f$ seconds. When $t=0$, the particle velocity is the maximum, $+B_m2\pi f$, about to decrease. At a time $1/4f$ seconds earlier, ($t=-1/4f$), the velocity was zero about to increase positively. This is a sufficient value for t which makes $\sin(2\pi ft+\pi/2)=0$ and about to increase. This is the same state the displacement is in now. Thus, the velocity "leads" the displacement by $1/4f$ seconds, which is the time required to execute one-quarter cycle. With the convention of representing a vibration as the projection of a vector rotating in a circle, the velocity leads the displacement by 90 degrees. Equation 6 describes the acceleration of this particle. It is similar to the displacement but differs in amplitude and phase. The equation describes a particle whose acceleration varies sinusoidally with time, f cycles per second, between the limits $+B_m4\pi^2f^2$ and $-B_m4\pi^2f^2$.

Table I. RMS Displacements, Velocities, and Accelerations Over the Range of Sound Levels and Frequencies

Units Are Inches, Inches Per Second, Inches Per Second Squared

	0 Decibel			80 Decibels			150 Decibels		
Frequency	D	V	A	D	V	A	D	V	A
1	3.06×10^{-7}	1.92×10^{-6}	1.21×10^{-5}	3.06×10^{-3}	1.92×10^{-2}	1.21×10^{-1}	9.67	60.7	3.81×10^{2}
10	3.06×10^{-8}	1.92×10^{-6}	1.21×10^{-4}	3.06×10^{-4}	1.92×10^{-2}	1.21	9.67×10^{-1}	60.7	3.81×10^{3}
100	3.06×10^{-9}	1.92×10^{-6}	1.21×10^{-3}	3.06×10^{-5}	1.92×10^{-2}	1.21×10^{1}	9.67×10^{-2}	60.7	3.81×10^{4}
1,000	3.06×10^{-10}	1.92×10^{-6}	1.21×10^{-2}	3.06×10^{-6}	1.92×10^{-2}	1.21×10^{2}	9.67×10^{-3}	60.7	3.81×10^{5}
10,000	3.06×10^{-11}	1.92×10^{-6}	1.21×10^{-1}	3.06×10^{-7}	1.92×10^{-2}	1.21×10^{3}	9.67×10^{-4}	60.7	3.81×10^{6}

When $t=0$, the particle acceleration is zero about to increase negatively. At a time $1/2f$ seconds earlier ($t=-1/2f$), the acceleration was zero about to increase positively. This is a sufficient value for t which makes $\sin(2\pi ft+\pi)=0$ and about to increase. This is the same state the displacement is in now. Thus the acceleration leads the displacement by $1/2f$ seconds, which is the time required to execute one-half cycle, or the acceleration leads the displacement by 180 degrees. The phase differences are indicated directly in equations 5 and 6 as $\pi/2$ and π respectively. The explanation has been given to help visualize the actual time differences involved, and what the differences mean in terms of the complete cycle.

In Table I are given the rms displacements, velocities, and accelerations associated with 0, 80, and 150 decibels pressure levels. The units are respectively inches, inches per second, and inches per second squared. These tabulations represent physical conditions, and the decibel is a physical unit. For example, a vibration of an air particle at one cycle per second having an rms displacement of 3.06×10^{-3} inches develops an rms velocity of 1.92×10^{-2} inches per second and is thus 80 decibels pressure level. Due to the limitations of the ear, this would not evoke the sensation of hearing. However, it is perfectly possible for a vibrating surface or air particle to have this motion. This vibration in a surface could be detected by the fingers. Sound pressure levels of 150 decibels at 1,000 cycles have been measured in airplane motor test houses. Such levels are reachable by generating nonstreamline flow in air by propellor blades or causing high-speed jets to impinge on sharp edges. They are not reachable generally by flat surfaces vibrating normal to their plane. Note that the rms acceleration associated with this level is 3.81×10^{5} inches per second squared. This is approximately 1,000 times gravity ($a_g=386$ inches per second squared). A large force would be required to move even a lightweight panel at this acceleration, and the mass reactions throughout the panel would be so high the inherent stiffness would not be great enough to make the panel vibrate as a plane.

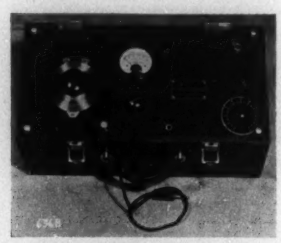


Figure 3. One type of electrical filter

Heterodyne type passing a narrow band of frequencies

So far we have discussed mainly a physical way of designating sounds and vibrations. Ear sensations have been referred to in terms of the threshold of hearing at 1,000 cycles, the region of pain at 150 decibels, and the reaction of an auditor to various decibel steps in noise reduction. We will now discuss loudness level and its relation to pressure level for pure tones.

Pressure Level and Loudness Level

The loudness level of a pure tone is defined as the pressure level of the equally loud 1,000-cycle tone, a matter decided by the vote of a sound jury.[1] The unit is the phon. Figure 1 shows this relationship in the form of equal loudness contours. The loudness level in phons of a 1,000-cycle tone is the same as the pressure level in decibels. For any other frequency the loudness level varies with the pressure level, varying both with frequency and with pressure level. In general, the ear lacks acuity at low frequencies and low pressure levels. Typical values are given in Table II. The tabulated values are decibels pressure level. Using the "70 phons" column for example, the table shows that 82 decibels are required at 50 cycles to evoke the hearing sensation of 70 phons loudness level, but only 67 decibels are required at 3,000 cycles to evoke the sensation of 70 phons. The "0 phons" column gives the minimum pressure levels in decibels necessary to evoke the sensation of hearing. For example, the ear threshold is 53 decibels higher at 50 cycles and 36 decibels higher at 100 cycles than it is at 1,000 cycles. At 3,500 cycles the ear is most acute. At high levels the ear has a substantially flat response, as shown by the "100 phons" column. The response at intermediate levels is indicated by the "40 phons" and "70 phons" columns.

Table II. Pressure Levels in Decibels Required Over the Frequency Range to Give Four Loudness Levels

	Loudness Levels—Phons			
Frequency	0	40	70	100
50	53	72	82	100
100	36	62	78	100
250	18	49	72	100
500	6	42	70	101
1,000	0	40	70	100
1,500	−2	40	70	98
2,000	−4	39	69	97
2,500	−6	38	68	95
3,000	−7	37	67	93
3,500	−8	37	67	92
4,000	−7	38	67	92
5,000	−5	39	69	95
7,500	6	51	81	105
10,000	8	52	82	105

Adding Decibels

If the measured sound pressure in air at a point is P_1, the energy E_1 flowing through a unit area in unit time is given by

$$E_1=kP_1^2 \tag{7}$$

where k is a constant of proportionality depending on the system of units selected. Accordingly, from equations 2 and 7, letting P_0 represent the threshold pressure, and E_0 the threshold energy

$$\text{Pressure level}=20\log_{10}\frac{P_1}{P_0}$$

$$=10\log_{10}\frac{P_1^2}{P_0^2}=10\log_{10}\frac{E_1}{E_0} \tag{8}$$

Suppose that a single source of sound causes a measured pressure level L_1. The energy associated with it is E_1. Suppose that we stop the first source and sound

Figure 4. Oscilloscope

Used in study of wave form of original sound, indicating instantaneous variations over the cycle a second source, which causes a measured pressure level L_2, with an associated energy E_2. If now we sound both sources together, the energy passing through the point of measurement will be E_1+E_2. For the random phase relationships which occur with multiple wall reflections and radiation from complex sources, the new pressure will be $\sqrt{P_1^2+P_2^2}$, where P_1 and P_2 are the pressures associated with L_1 and L_2. Using equation 8 and the fact that associated energies add arithmetically, it may be shown that if Δ is the difference in decibels between L_1 and L_2, L_1 being the larger, the new pressure level is obtained by adding β to L_1 as shown in Table III.

Handling the Analysis

Suppose that an analysis of pressure level under representative conditions has been made. The pressure levels measured are given in Table IV, column 1. In column 2 are the loudness levels from these, obtained from the loudness contours of Figure 1. Single frequencies near the mid-points of the bands were used to represent the bands: 30, 150, 300, 600, 1,200, 2,400, 3,600, 6,000 cycles.

The "total" figures for the decibel values of columns 1 and 4 could have been obtained by computing the energies in the bands, adding the individual energies to get the total energy, then converting back to pressure level. Actually the short-cut method of Table III was used, which is entirely satisfactory. Strictly speaking, individual band loudness levels in phons do not add as decibels do, but the author has found that a total figure obtained this way is a practical method of assigning a single number having some meaning in terms of the sensation of hearing. This is not the preferred method. Noise-reduction problems should be judged by the reduction in each band. This is discussed at greater length under "multiple sources."

Suppose that an outstanding improvement is desired in the total loudness level. By our standard, this calls for a 20-phon reduction from 88 to 68 phons. Most commercial band spectra give a total that is 2–5 decibels (or phons) higher than the highest single component. In this case, we might guess at a design objective of 64 phons for the loudest band after acoustical correction, with the other bands reduced to something under 64 phons, and write down column 3. This desired spectra has been made to drop off in the high frequency end, as experience has shown the high frequencies have a heavy "annoyance weighting." The total turns out to be 69 phons or a reduction of 19 phons. We will let this trial stand. In column 4 the phons loudness level of column 3 have been converted to decibels pressure level, using the loudness contours of Figure 1. Column 5 shows the decibel reductions necessary in each band to produce the desired loudness level projected in each band. It may prove impossible to effect an 11-decibel reduction for example, in the 50–100 band within the price, weight, and space limitations imposed upon train problems, but by this type of analysis a rational method is available for surveying and attacking a complicated situation.

Table III. Addition Factor for Combining Two Decibel Levels Knowing Their Difference

Difference in Two Level—Δ	Add to Larger Level—β
0	3.0
1	2.5
2	2.1
3	1.8
4	1.5
5	1.2
6	1.0
7	0.8
8	0.6
9	0.5
10	0.4

Multiple Sources

Table IV is a general procedure based on measuring the interior level. It is usually necessary to investigate further a complex structure such as a train to determine the contribution of each source to the interior pressure level. Sometimes controlled experiments can be run, such as driving the compressors with the train stationary, or checking the floor transmission by an artificial sound source. More often the interior must be probed for vibration velocity level and an estimate made as to why certain panels vibrate and what can be done to reduce their contributions to interior pressure level.

Table IV. Handling a Pressure Level Analysis (See Text)

Frequency Bands	Measured Db Pressure Level Col. 1.	Measured Phons Loudness Level Col. 2	Design Phons Loudness Level Col. 3	Design Db Pressure Level Col. 4	Decibel Reduction Necessary Col. 5
0–50	91	83	63	85	6
50–100	87	84	64	76	11
100–200	81	79	62	69	12
200–400	78	77	58	61	17
400–800	75	75	55	55	20
800–1,600	72	72	52	52	20
1,600–3,200	65	66	48	46	19
2,400–4,800	60	62	43	41	19
4,800 +	48	43	38	43	5
Total	93	88	69	86	

Suppose that near a certain passenger position a complete survey of likely sources is made. Hypothetical data for the 400–800-cycle band are presented in Table V. From this tabulation we can make certain informed guesses.

The noise from the air conditioner appears to come directly through the air to the passenger, as no panels have been discovered which can account for 50 decibels. The noise when "at speed" appears to be due to (*a*) sound pressures generated at the wheels striking the floor and coming through in part, or (*b*) broadcasting of sound energy from the floor surface, or both. The floor may be driven strongly from the frame, which is subjected to road shocks. Table V does not tell us whether the other panels vibrate at the levels shown, because they are part of the strongly vibrating floor system, or because external sound pressures cause them to vibrate at these levels.

The data suggest that measurement of external levels under the coach and at window height would throw further light on the mechanism of sound transfer. As the air-borne pressure level due to the compressor is 60 decibels, reducing the "at-speed" floor contribution much below 57 decibels is a waste of time and money, unless the compressor is also reduced. This is a direct consequence of the decibel additive relationship shown in Table III. With 60 decibels and 57 decibels, the total is 61.8 decibels. If the floor contribution were removed entirely, the pressure level could drop only 1.8 decibels which is not an important amount.

The noise from the compressor appears to come through the floor either by its airborne noise striking the underside of the floor or by structural vibration or both. With a tube-like structure such as a coach, an intense source in one portion, such as vestibule noise leaking through a

Table V. Investigation of Probable Sources (See Text)

	Train Stationary		Train at Speed
	Compressor Only	Air Conditioner Only	Compressor and Conditioner Operating
Air-borne pressure level	60	50	75
Floor vibration velocity level	63	35	79
Window vibration velocity level	57	32	70
Side panel vibration velocity level	55	30	65
Ceiling vibration velocity level	50	32	62

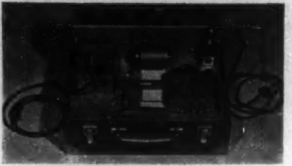


Figure 5. High-speed level recorder

Makes graphic record which can be read as pressure level against time; an rms type of instrument

partly open door, will be distributed as air-borne sound. This type of trouble is readily detected, however, by exploring likely panels and air spaces for intense local sources.

A most practical and important aspect of the decibel additive relationship of Table III occurs when the contributions of each frequency band to the total are considered. Usually the total level is dictated by the most intense band, which in transportation is in the low frequency end. If all the energy above 500 cycles were removed, the *total* level would not decrease by an amount sufficient to measure. The ear would notice a big difference, and it has been the author's experience[3] that reductions in the higher frequencies are useful out of all proportion to their contributions to total level. The ear will readily detect the difference in a coach when hisses, rattles, squeaks and high-pitched whines have been eliminated. It will judge this coach definitely quieter than before, even though the low-frequency components which dictate total level have not been reduced. A band analysis will show the higher frequencies have been reduced substantially, and this portion of the measurement will be in good agreement with the ear judgment.

Sound in a Closed Space

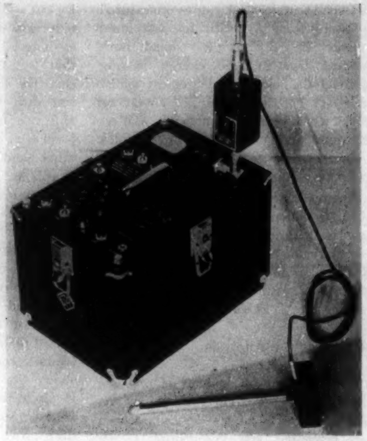
The pressure level built up inside a closed space by air-borne sound striking the outside may be computed closely enough for practical purposes by a formula derived from a simplified mathematical analysis.

$$PL_i = PL_o - TL - 10 \log_{10} \frac{a}{A} \qquad (9)$$

where

PL_i = average inside pressure level
PL_o = pressure level of the sound outside striking the transmitting area A
TL = transmission loss of the area A
a = total sabines absorption inside the closed space
A = transmitting area in square feet

Figure 6. Sound-level meter with crystal vibration pickup



Transmission loss is the decibel drop produced on air-borne sound by a sound barrier, measured close to each side, under conditions that discount the effect of reflected waves. The sabines absorption of a surface is the product of its area in square feet by its coefficient of sound absorption. The coefficient may be thought of as the percentage of energy absorbed at one incidence when a ray of sound strikes the surface at a random angle.

If a closed space has few sabines initially, PL_i can be reduced appreciably by adding an efficient acoustical treatment. Generally speaking, more improvement is possible in working with TL alone than in working with acoustical treatment alone for sources outside the space. Adding a treatment is discussed further in section III.

II. Electrical Instruments Suitable for Noise-Reduction Problems

We have seen that

(*a*) The sound pressures and vibration velocities of interest to the acoustical engineer are quantities covering an enormous range but, even at their largest, small by ordinary standards.

(*b*) The ear responds in a complex manner depending on both the frequency and intensity of the sound in question.

(*c*) It is important to measure the changes in each frequency band.

Rugged instruments of adequate analyzing power are required.

It is safe to say that the developments of the vacuum-tube amplifier and the high-quality microphone have allowed the engineering practice of acoustics to reach its present stage. Because proper electrical instruments are available, field problems can be analyzed in a rational manner, numbers can be assigned to physical measurements which have meaning in terms of human ear reactions, and corrective measures, developed by the experimental method in the laboratory, can be applied to the field problems.

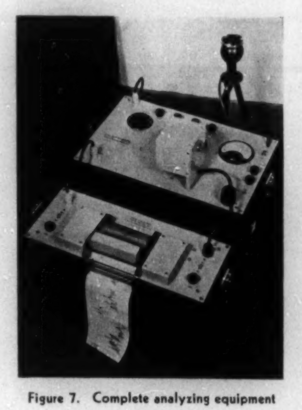


Figure 7. Complete analyzing equipment

Makes graphic record which can be read as pressure level against frequency. Analyzer passes a narrow band of frequencies, covering audible range automatically

The displacements associated with frequencies below 20 cycles are sometimes large. Because of this, the curve of displacement against time often can be traced by the seismic type of instrument with mechanical leverage to obtain desired amplification. Optical magnification types are also available. This region is of interest in investigations of riding comfort. See, for example, the work by Jacklin[4] and his associates. The problems of measuring and correcting unbalance in rotating machinery have been the subject of extensive investigations outside the scope of this paper. See, for example, publications by Thearle[5] and Rushing.[6]

The acoustical engineer in the wide regions of the audible spectrum finds indispensable the electrical method of measurement, because frequency analysis is desirable, and the displacements associated with his problems are very small indeed. In general, the instrument assembly consists of four parts:

Transducer.
Amplifier.
Filter.
Final indicating or recording means.

A transducer is a device which has an electrical output similar to the mechanical input to the device. The output energy can be amplified by the desired amount necessary to give the final reading. Filter networks which will give a frequency discrimination can be inserted ahead of the final reading, enabling the frequencies involved in the original vibration to be investigated.

What follows is not intended to be an exhaustive catalogue of sound measuring equipment. It does describe in general terms the types which the author has found useful in the field and laboratory.

(1) Transducers

(*a*) MICROPHONES

1. Dynamic. A diaphragm exposed to sound pressure carries a coil moving in a permanent magnetic field. A preferred type is resistance controlled, having a substantially flat response over the frequency range. The velocity of the diaphragm, and hence the voltage output of the coil, is proportional to the sound pressure.

2. Condenser. This microphone consists in its elements of two plates close together which form a condenser in the amplifying circuit. Sound pressure moves one of the plates, causing a voltage fluctuation. The natural frequency is high, and the damping and polarizing voltages are such that the response is substantially flat. This type usually requires a built-in stage of amplification which may be inconvenient in the field.

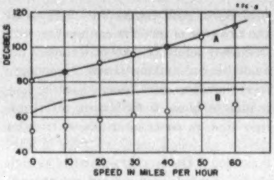


Figure 8. Train noise

Graph of total pressure level versus speed
Curve *A*—Noise underneath
Curve *B*—Noise inside at passenger head level
Circles—Associated loudness levels

3. Ribbon. A thin metallic ribbon exposed to sound pressure moves in a permanent magnetic field. With the back of the ribbon enclosed, this microphone responds to the sound pressure on the exposed face and is nondirectional. With both sides of the ribbon open to the sound field, the microphone responds to the pressure gradient and is insensitive in the plane of the ribbon. This is often called a velocity microphone. The two types are sometimes combined into one microphone having a substantially unidirectional response.

4. Crystal. A crystal of Rochelle salt exhibits the piezoelectric effect, generating an electromotive force when deformed. Sound pressures cause this deformation and the microphone using this principle has a substantially flat response.

(*b*) VIBRATION PICKUPS

These instruments are, in general, similar to microphones but are adapted to measure directly vibration in a mechanical part, rather than to respond to the sound pressure built up by moving air particles. The self-contained dynamic type carries a stylus fastened to a coil moving in a permanent magnetic field. The stylus is positioned against the vibrating member and partakes of its motion exactly. The case of the instrument, due to its inertia and weak coupling spring, remains at rest in space. Voltage output is proportional to coil velocity and, thus, to the velocity of the investigated part. For laboratory work a coil may be fastened to the vibrating part and made to move in a magnetic field, or a plate forming part of a condenser may be attached. A crystal pickup is available which is light (two ounces) and useful where the heavier dynamic type would interfere with the free motion of the part. The crystal may be mounted with a free corner, and the case shaken by the vibrating part. The output of this crystal type increases with frequency, for constant velocities of vibration. This may be useful where increased weighting should be given to higher frequencies. It is possible to use an equalizing circuit which will correct for the rising characteristic of this type.

Table VI. Improvement Due to Double Acoustical Floor in Motor Coach

Frequency Bands	Conventional Floor VVL	Acoustical Double Floor VVL	Reduction in Vibration Decibels
0–64	45	45	0
64–256	70	68	2
256–512	72	67	5
512–1,024	72	62	10
1,024–2,048	69	56	13
2,048–4,096	64	52	12
4,096–8,192	42	34	8

(2) Amplifiers

The design of amplifiers covers a wide range beyond the scope of this paper. In general, high gain of the order of 120 decibels with a flat frequency characteris-

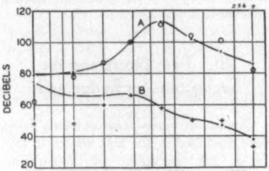


Figure 9. Train noise

Graph of a frequency analysis at 60 miles per hour. Pressure level versus frequency. Readings were taken with filter band widths of one octave centered at points shown
Curve *A*—Noise underneath
Curve *B*—Noise inside
Circles—Loudness level underneath
Crosses—Loudness level inside

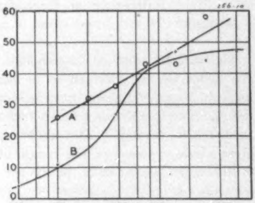


Figure 10. Train noise

Graph of difference in pressure level underneath and inside versus frequency
Curve *A*—Train stationary with noise source a loudspeaker on track
Curve *B*—Train running, producing its own noise
Data averaged from six speeds over the entire speed range

tic is needed. Commercial sound level meters are now provided with two frequency weighting networks. Amplifier circuits usually include an attenuator graduated in decibels. The complete reading consists of the attenuator setting plus the final meter reading.

(3) Filters

For obtaining a single figure representing pressure level, commercial meters, as mentioned in paragraph 2, have provision for a flat frequency characteristic in the 100 phons loudness level range, and two weighting networks, related to the equal loudness contours of Figure 1, one for the 70 phons and the other for the 40 phons range. The author prefers to make frequency analyses with a band pass filter, covering the audible range of octave steps. The filtering action is obtained with coil and condenser circuits controlled by a single tap-switch. Narrow pass filters are available with band widths as small as five cycles. There is another type with a band width of a fixed percentage of the frequency to which it is tuned. Both the heterodyne method with a fixed filter and the selective amplifier method operating on the degeneration principle are available. The types which permit aural monitoring with a telephone receiver are useful, as important components in the original noise may be identified by ear in a preliminary survey. Narrow band filters can be used to obtain accurately the frequency of a disturbing component. In certain cases this can be related to known occurrences such as tooth contact frequency in a particular pair of gears. Sources are sometimes identified this way.

(4) Final Indicating or Recording Means

The useful output of an amplifier is a-c. This can be delivered to an oscillograph or oscilloscope, or it can be rectified and indicated as rms volts on a meter graduated in decibels. The oscilloscope gives visual evidence of the complexity of the original sound wave, but its use is normally confined to the laboratory. For field work, good results are obtained by the rms type of meter in conjunction with frequency analysis. Level recorders are available for making charts of pressure level variation with time. For the particular problem of measuring reverberation time in room acoustics, a recorder which can follow quick changes in level is needed. Decay characteristics as high as 200 decibels per second are encountered in broadcasting studios, for example. The level recorder is useful in making frequency analyses in narrow band widths. The analyzer and recorder are driven by synchronous motors so that the abscissa can be read in terms of frequency, and the ordinate in pressure level of the passed band.

Figures 2–5 show some of the individual units described above. Figure 6 shows a sound-level meter ordinarily used with a crystal microphone, here used with a crystal type of vibration pickup. This particular design has a control box with integrating circuits which may be set for displacement, velocity, or acceleration. The photograph shows it set for velocity. It will then give a single reading proportional to the velocity of the stylus which would turn out, for the average field problem, to be due to the single most intense frequency component of velocity. For information regarding less intense components, a frequency analyzer can be used between the pickup and the sound-level meter. Without analyzer equipment, a rough check on frequency distribution can be obtained by displacement and acceleration readings, and by using the weighting networks on the sound-level meter. Figure 7 shows a set-up for making an automatic frequency analysis with a graphic recorder. With elaborate equipment of this type, data can be taken rapidly and in the detailed form necessary for the complete handling of acoustical problems.

III. Train Noise and the Techniques for Reducing It

Certain assumed measurements have been discussed in the first section of this paper for purposes of illustrating the principles involved. Measurements on actual train noise made by the author are presented in Figures 8–10. These data have been selected as representing the over-all aspects of the problem. No attempt is made in this paper to compare various designs. The data selected, however, are from extensive tests on a rather quiet type.

Noise and Vibration Paths

In Figure 11 are indicated schematically the various paths that sound energy from three representative sources may take on its way into the coach to disturb the passenger. The noise radiated from the wheels and trucks and the shocks delivered to the center plate depend upon spring design. A discussion of the reduction of these disturbances by design modifications is outside the scope of this paper. On the benefits of rubber springing see, for example, the paper by Hirshfeld and Piron.[7]

The simplest way to control a noisy source is to surround it with a barrier having an adequately high transmission loss (*TL*). The air-borne noise radiated from the compressor surfaces of Figure 11, for example, could be reduced in this way. There may be practical considerations such as space limitations or need for free air flow for cooling the machine. The direct structural path between the compressor and the floor may transmit vibrations which will reradiate from the floor to the air inside the coach. If necessary, the compressor may be isolated with resilient mountings. Proper isolation provides flexible connections to the machine. With due precautions for maintaining alignment between driving and driven units, mountings of high compliance and, consequently, high isolation may be used.

Direct leakage of air-borne sound through openings may nullify the results of careful work on other phases. Such

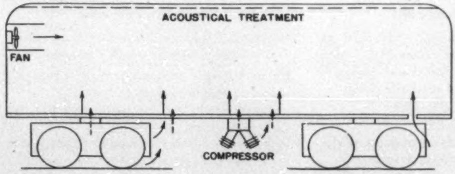


Figure 11. Schematic diagram of noise sources and energy paths in coach

Solid arrows—air-borne path

Dotted arrows—structural path

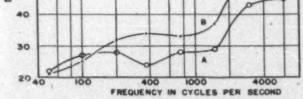


Figure 12. Transmission loss of coach floor

Graph of *TL* versus frequency

Curve *A*—Untreated floor

Curve *B*—Floor with deafening plate screwed to floor members and rock-wool blanket between floor and plate openings should be kept at a minimum. For example, a good window construction having a TL of 30 decibels, if opened two inches, is no better than a very ordinary construction with a TL of 20 decibels kept closed.

A fan of an air-conditioner system may deliver noise along with the air. Large reductions are possible by treating a run of duct with sound-absorbing material.

The floor is subjected to an intense air-borne noise as indicated in Figures 8 and 9. It also is vibrated by way of the structural path from the wheels. Figure 10 indicates that the inside pressure level at the middle frequencies is due to the transmission of air-borne noise. At the lower frequencies it appears that structural vibration contributes importantly to the inside pressure level. The TL of the floor can be increased by various means, and it can be isolated from the structure. If the floor must help in adding strength to the structure, isolation is not feasible. An extra floated floor may be a possibility.

Sound Barriers and Enclosures

Structures with substantially rigid connections throughout have a transmission loss linearly proportional to the logarithm of the weight per square foot. See for example the work by Knudsen[8] and many others. The average TL at the nine frequencies commonly used is expressed by

$$TL = 22.4 + 14.3 \log_{10} w \qquad (10)$$

The weight w for a typical coach floor is ten pounds per square foot. Substituted in equation 10, this gives an average TL of 36.7 decibels. For convenience, this equation may be thought of as giving the TL at 1,024 cycles. Measurements on a wide variety of homogeneous structures show that for a fixed weight the TL increases four decibels per octave, and for a fixed frequency increases four decibels on doubling the weight. These are averages for many tests. A single panel or wall may deviate five decibels or more from these predictions at any one frequency due to resonances. The average over the frequency range usually will be within two decibels of equation 10.

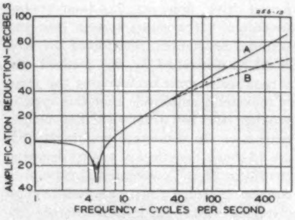


Figure 13. Theoretical vibration reduction due to isolation

Decibels change from rigid mounting versus frequency of the driving force. System has natural frequency of 4.95 cycles, showing amplification at all frequencies below seven cycles

Curve *A*—Zero damping

Curve *B*—Commercial amount of damping

Table VII. Improvement Due to Vibration Isolation

15-Hp Motor on Concrete Slab

Frequency Bands	Slab Vibration Velocity Levels: Motor Bolted Directly	Motor on Springs	Motor on Rubber	Reduction—Decibels: By Springs	By Rubber
0–64	105	72	85	33	20
64–128	83	90	62	33	21
128–256	81	51	49	30	32
256–512	76	53	47	23	29
512–1,024	65	45	44	20	21
1,024–2,048	54	29	29	25	25
2,048–4,096	43	10	23	33	20

If we wish to improve the TL of the above floor by ten decibels, a single floor weighing 50 pounds per square foot is indicated. This would be prohibitive in any practical transportation problem. Broadcasting-studio construction methods can be used in a suitable way. The TL of a three-pound-per-square-foot barrier by equation 10 is 29.2 decibels. Using our original figure of 36.7 decibels, we might hope that with a three-pound extra floor properly applied, the losses would add to a total of 65.9 decibels. Because the two must be used close together and joined compliantly to the same structure or to each other, the indicated high total is not reachable. However, by proper structural isolation, and using sound-absorbing material between the two floors, 48 decibels may be obtained. Thus, we have exceeded the estimated performance of a 50-pound homogeneous floor by using a special 13-pound composite floor.

Table VIII. Improvement in Decibels Due to Vibration Isolation

Train Floor on Shear Rubber Isolators Compared to Floor Mounted on Steel Blocks

Frequency	Design A 3/8-Inch Thick—0.2-Inch Deflection	Design B 3/8-Inch Thick—0.4-Inch Deflection	Design C 3/4-Inch Thick—0.3-Inch Deflection
100	8	10	21
225	8	15	20
375	7	21	12
512	12	22	21
750	10	11	14
1,024	9	13	10
1,500	9	17	14
2,048	9	12	15

Actual field figures on the vibration performance of a double acoustical floor in a motor coach in terms of vibration velocity level are presented in Table VI. The isolated element was only one pound per square foot with rock wool between the floors. If the base floor had been increased homogeneously by this amount, the decrease in VVL would have been negligible. By adding this light weight with proper attention to the acoustical principles involved, a substantial decrease in VVL of the important radiating surface was obtained.

The addition of sound-absorbing material and a deafening plate, screwed to the under side of the floor-stiffening members, provides a smaller but definite improvement. Figure 12 shows transmission loss versus frequency for a typical coach floor tested in the laboratory, using a large test section 86 inches by 78 inches. The walking surface was $^5/_8$ inch Chanarch poured with sufficient magnesite to fill in the slots of the Chanarch and extend above it one-half inch. Five two-inch Z bars provided stiffening. Are movable 22-gauge deafening plate was screwed to the Z bars.

Vibration Isolation

A vibrating piece of equipment, such as the compressor mentioned above, radiates air-borne noise directly from its metal surfaces and usually vibrates the structure to which it is rigidly bolted. The structural vibrations will travel long distances with negligible loss, and cause trouble by radiating sound inside enclosures which have sufficient TL to bar the air-borne sound striking them directly.

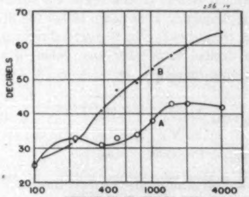


Figure 14. Transmission loss of coach floor

Graph of TL versus frequency

Curve *A*—Untreated floor

Curve *B*—Floor with deafening plate attached to floor members by shear rubber isolators and rock-wool blanket between floor and plate

The remedy consists in isolating the equipment by rubber or springs. The improvement may be striking, and, where this step may be taken, it is the most important single method available.

Table VII shows the large reductions brought about by a commercial type of isolator using the controlled spring principle in combination with rubber. This particular application was to lessen the vibration in the concrete slab under a 15-horsepower motor. Vibration velocity levels of the slab are given. Data on the conventional type of bonded rubber isolator used in shear are also given. It was less efficient than the controlled spring design, but the reduction in vibration by its use was large. For these tests, the set of isolators, both spring type and rubber type, were loaded to 0.35-inch deflection.

The same principle may be used in isolating the floor from the supporting structure. Table VIII gives actual reductions by laboratory tests on such a construction using rubber in shear. An electrical driving unit was attached to the structure so that it vibrated the subframe at the desired frequency over the entire frequency range. The vibration velocity level of the floor surface was measured at each frequency with a rigid structural connection and with three variations of shear rubber isolators.

Data such as these indicate by actual tests that best isolation results are obtained with the greatest deflection permissible, and, for a given deflection, it is advantageous to use the thickest rubber.

If a motor is to be isolated so that its vibrations are to be kept out of the structure, or a floor is to be mounted so that vibrations in the structure will not enter the floor, theory calls for suspending the motor or floor by compliant means so that the natural frequency of the suspended system is well below the disturbing frequency, commonly called the driving frequency. The theoretical reduction in decibel form,[9] for any value of the driving frequency, is

$$20\log_{10}\frac{\sqrt{(c\omega)^2+(k-m\omega^2)^2}}{\sqrt{k^2+(c\omega)^2}} \qquad (11)$$

where

m = mass of suspended weight (slugs)
k = pounds to produce one-foot deflection in the compliance
c = pounds force required to maintain unit velocity
$\omega = 2\pi f$ where f is the driving frequency in cycles per second

Table IX. Theoretical Reductions Due to Vibration Isolation for Various Static Deflections

Commercial Amount of Damping

Static Deflection Inches	Natural Frequency—Cycles Per Second	Reduction—Decibels Driving Frequency—Cycles Per Second				
		20	50	100	200	500
1/16	12.56	4	23	35	44	54
1/8	8.86	9	29	39	48	58
1/4	6.28	18	35	44	52	61
1/2	4.43	24	39	48	56	64
1	3.13	31	44	52	59	67

Experience has indicated that the actual decibel reductions obtained in commercial installations are less than half of the theoretical values.

Good isolation calls for mountings soft enough to deflect one-eighth inch or more under the static load of the suspended part. The natural frequency f_n in cycles per second is

$$f_n = 3.13\sqrt{\frac{1}{\delta}} \qquad (12)$$

where δ = static deflection in inches

Isolator *B*, for example, of Table VIII supported 40 pounds per inch of isolator with a deflection of 0.4 inch. This means a k value of 1,200 pounds. Using an isolator one inch long the damping factor c, evaluated by the methods of reference 9, was 0.23 per inch of isolator. Most commercial materials have damping factors similar to this, which are small in comparison to the masses and stiffnesses involved.

A plot of equation 11 for $c = 0.23$ and $c = 0$ is given in Figure 13, using $m = 40/386$ and $k = 1{,}200$. This is a plot from theoretical considerations. The natural frequency is 4.95 cycles per second, and for a driving frequency of this value there is an amplification of 23 decibels with the commercial damping measured, and an infinite amplification for no damping. The curves cross the 0-decibel line at $\sqrt{2}\times 4.95 = 7.0$ cycles and show increasing isolation for higher frequencies. Above seven cycles the damped curve lies below the non-damping curve, but the theoretical detrimental effect of a small amount of damping is of less practical importance than the departure of actual systems from the simple one-degree-of-freedom system here assumed.

Table X. Improvement Due to Ceiling Acoustical Treatment in Coach •

Frequency	Sabines Absorption			Reduction—Decibels
	Before	Added	After	
128	140	150	290	3.1
256	225	336	561	4.0
512	354	594	948	4.3
1,024	354	594	948	4.3
2,048	314	546	860	4.4
4,096	265	492	757	4.6

The theoretical value of using the softest mounting possible, from the isolation standpoint, is seen in Table IX. As noted above, actual reductions are of the order of one-quarter to one-half theoretical. Above resonance a useful simplified form of the isolation equation, corrected so as to give the range of actual reduction is

$$10\log_{10}\frac{f}{f_n} \text{ to } 20\log_{10}\frac{f}{f_n} \qquad (13)$$

where f_n = natural frequency of the suspended system
f = driving frequency

Instead of screwing a deafening plate directly to the floor members, as in Figure 12, isolation may be utilized. Figure 14 shows transmission loss versus frequency taken in the laboratory for a typical coach floor 86 inches by 41 inches. The deafening plate was attached by shear rubber isolators, and rock wool was used between the plate and the test floor The improvement was very marked.

Panel Damping

The addition of damping compounds to vibrating panels is useful in shortening the decay time of transient vibrations, and in lessening the response at resonant frequencies. Automobile doors, for example, when slammed, emit a clanging tinny sound, due to exciting segmental vibrations in the thin gauge metal by the sudden shock. With a damping compound added, the slam noise becomes a dull thump, and the improvement is remarkable. The usefulness of this method for controlling the vibrations of forced drive, however, has been greatly overrated. If a panel system has pronounced resonances from forced drive, it could be controlled by this method, but it is so easily done by touching the point of maximum amplitude that this type of problem does not usually come to the attention of the acoustical engineer. Figure 15 shows the effect of a good damping treatment of the conventional type on a circular 18-gauge panel mounted in rigid clamping rings 12 inches internal diameter. The panel was driven through resonance by an electromagnet actuated by an audio oscillator and the pressure level three inches away measured. A drop of 20 decibels at the resonant frequency was attained, but away from resonance the levels were changed very little. The resonant peak for the treated panel is at a lower frequency because the treatment increased the effective mass of the panel without adding sufficient compensating stiffness.

Ceiling Acoustical Treatment

The average energy density built up by a source of fixed acoustical power in a closed space is inversely proportional to the sabines absorption in the space. If the space has a_1 sabines initially, and the total number of sabines is increased to a_2 by adding acoustical treatment, the decibel reduction in pressure level will be

$$10 \log_{10} \frac{a_2}{a_1} \qquad (14)$$

Equation 14 shows that relatively large reductions will be obtained by treating a given ceiling area, for example, with a material having an 0.85 coefficient if a_1 is small. If a_1 is initially large, then smaller reductions will be obtained.

The sound-absorption coefficient varies with frequency. The most efficient commercial material having the largest sale today has the following coefficients:

128	256	512	1,024	2,048	4,096	*NRC*
0.25	0.56	0.99	0.99	0.91	0.82	0.85

The last value is the noise reduction coefficient, which by current practice is the linear average of the coefficients at 256, 512, 1,024, and 2,048. It is assigned in an effort to give a single figure rating of a material. The author, however, believes in judging noise reduction by the improvement over the frequency range.

Suppose the inside of a coach is 60 feet by 10 feet by 9 feet high. Assume 40 upholstered seats at seven sabines each (at 512 cycles) and 2,460 square feet of metal, glass, and floor surface at an average coefficient of 0.03 (at 512 cycles). The total sabines at 512 cycles are $40 \times 7 + 2{,}460 \times 0.03 = 354$. Suppose that the 600 square feet of the ceiling is to be treated with the above material. The original steel ceiling surface contributed 18 sabines at 512, which would then be covered. This is not enough to affect the final result, and this small correction will be neglected here. Making some justifiable assumptions on the sabines originally present at frequencies other than 512 cycles, we can prepare Table X, showing the sabines before and after acoustical treatment and the decibel reductions computed by equation 14. The reductions are of the order of 3–5 decibels. If we had started with a coach having less sabines initially, such as the suburban motor type with leather seats, the reduction by adding an acoustical treatment would be higher, ranging up to eight decibels in the middle and high frequencies.

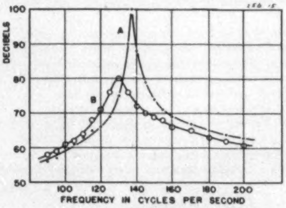


Figure 15. Vibration reduction due to damping

Graph of pressure level versus frequency.

Test panel excited over resonance range by electromagnet driven at various frequencies. Pressure level taken three inches away from panel center

Curve *A*—Bare 18-gauge panel

Curve *B*—Panel with $^5/_{32}$-inch commercial damping felt cemented to it

The contribution of ceiling acoustical treatment to human comfort, particularly in making conversation easier, seems to be out of all proportion to the decibel reductions figured on a sustained tone analysis.[3] Reverberation times are cut in half by treatments which account for a three-decibel reduction in average pressure level. It may be that it is this shortening effect on rail clicks and rattling dishes and silverware which accounts for the substantial improvement in ceiling-treated dining cars, for example. These die away quickly, and the syllables of conversation, maintained for a relatively long time, are heard. High frequencies which might reflect back on the passenger from a hard ceiling are partially absorbed with an acoustical ceiling.

Duct Acoustical Treatment

Fans and air conditioners sometimes cause trouble due to air rush and fan or equipment noise delivered with the air. If a run of duct is available in which acoustical treatment may be placed, the noise delivered to the passengers can be reduced greatly. Papers by Parkinson[10] and Sabine[11] deal with the practical aspects.

A rule of thumb for lining ducts is to treat a length equal to at least ten times the smallest duct dimension. The reduction obtained varies of course with the efficiency of the absorbing material and to some extent with the size of the duct. For a six-inch square duct lined for sixty inches with a material having a coefficient of 0.50, Parkinson's curves show 14 decibels, and Sabine's formula computes to 16 decibels. Fan and air-rush noise covers a wide frequency range. It has been found fairly satisfactory to correct for 256 cycles. The reductions at other frequencies which are obtained by the usual acoustical duct-lining materials are then acceptable for most applications.

Figure 16 gives results on silencing a difficult conditioner problem. The only available place for treatment was at the air-discharge grille in a space 48 inches by 76 inches cross section, and it was required that the treatment be 30 inches long. Lining only the sides of such a short run of this large section would be inadequate. The treatment adopted was one-inch rock-wool boards placed five inches on centers dividing the space into 13 flues each 30 inches long and 48 inches by 4 inches cross section. Data taken with an octave band pass analyzer have been plotted at frequencies near the midpoint of the bands, and a smooth curve drawn. The grille was in the ceiling, and the readings were taken at head level beneath it. Due to the predominance of the band centered at 375 cycles, the total noise was reduced by the order of four decibels, but reductions in the various bands ranged up to 20 decibels. By ear the improvement was very marked. Before treatment, the noise generated by air rush and water sprays could be heard the full length of the corridor beneath the grille—about 50 feet. After treatment, it was necessary to stand directly under the grille to hear the disturbance.

Conclusion

Acoustics is a reasonably complex science. Good instrumentation is available for attacking practical problems. The quiet train ride is obtainable by a rational application of known techniques,

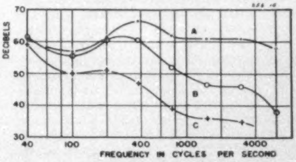


Figure 16. Noise reduction by treating air-conditioner duct

Graph of pressure level versus frequency

Readings were taken with filter band widths of one octave centered at points shown

Curve *A*—Original noise beneath grille

Curve *B*—Noise after treatment was installed

Curve *C*—Background noise with conditioner not running developed in the laboratory by careful measurement and used in the problem as indicated by the proper interpretation of the instrument readings.

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A Turbine-Governor Performance Analyzer

Discussion and author's closure of paper 41-158 by W. O. Osbon, presented at the AIEE South West District meeting, St. Louis, Mo., October 8–10, 1941, and at the AIEE winter convention, New York, N. Y., January 26–30, 1942, published in AIEE TRANSACTIONS, 1941, November section, pages 963–7. The following discussion was presented at the 1942 winter convention.

P. B. Juhnke (Commonwealth Edison Company, Chicago, Ill.): Concerning Mr. Osbon's paper many questions come to one's mind. We recently completed a series of measurements to determine the sensitiveness of the governors of the machines on our system, and, curious to say, we found a range considerably beyond anything we had anticipated, some of the larger and most modern units having a regulation ranging up to 20 per cent; whereas machines of older vintages had a regulation of 10 per cent or less. Perhaps the underlying reason for the poorer regulation of the latest machines is that they were intended for base load, and this poorer regulation was thought to be an asset in that field. These premises for future machines may well be reviewed as to their validity, as the subject of system regulation during the last five years has made tremendous strides, and a lower per cent of regulation in the future should no longer be the liability it was once thought to have been.

R. Sheppard and **C. Concordia** (General Electric Company, Schenectady, N. Y.): There has been considerable discussion recently of turbine speed-governing characteristics and of their effects on power-system operation. Of the many aspects of governor response, it has previously been indicated[1,2,3] that probably two of the most important are the dead or insensitive frequency band, within which the governing system as a whole will not respond, and the steady-state incremental speed regulation (the ratio of speed change to load change for a small change from normal speed). That is for the reasons that on most large power systems, with their accompanying large rotary inertias and relatively small load changes, the frequency changes are rather slow, and the principal contributing factor in delaying the governor response is not the dynamic lag, due to governor inertia, oil flow, steam, or water storage, and so forth, but the frequency dead band, due to backlash, friction, valve lap, and so forth. It is evident that the slower is the frequency change, the more apparent the effect of dead band becomes, and the less the dynamic lags appear. Dead band appears inherently as a frequency lag (that is, a more or less definite small frequency change is required before the control mechanism will respond), while the dynamic effects appear inherently as time lags. Thus with small frequency changes and long times involved, the relative importance of dead band becomes obvious.

Moreover, with the small frequency changes usually encountered on large power systems, the effective magnitude[3] of the machine response, that is, the effective incremental speed droop, is greatly affected by dead band. This does not imply that one can forget the dynamic lags. These lags, together with the incremental regulation, on the one hand, affect the degree of frequency stability when the machine is not connected to the system during synchronizing, and, otherwise, when the machine is connected to the rest of the system, affect the system frequency fringe, particularly the magnitude of self-excited frequency oscillations produced by dead band.

Because of these considerations, it has been concluded that dead band should be made as small as possible.[1,2,3] Once such a conclusion has been reached, it becomes desirable to know what can be done, and how far one should go to produce improvement in those systems requiring it. A first step is to determine the characteristics of governing systems presently in operation. The results of a few of such measurements of governor response which have been made on turbines operating on several power systems are reported in this discussion.

Description of Measuring Instrument

The measuring device which has been designed and used to determine the significant response characteristics of turbine speed-governing systems is similar in principle to that developed and recently described[4] by Mr. J. E. Allen of The Pennsylvania Water and Power Company. Some of its features are:

1. The instrument shows directly the principal characteristics of interest: dead band and incremental speed regulation. No replotting or working up of data or complicated interpretation of the results is necessary, except for multiplication of the valve travel record by a factor to take account of the lever ratio between valve motion and carriage motion, and of the ratio between turbine kilowatt input change and valve motion at the point in question.

2. The measurement does not interfere with normal operation of the machine and requires little time. Shutting down or even any special loading schedule is not required, and no restraint is imposed on the normal or emergency functioning of the governor.

3. The indications are accurately recorded.

4. The indications are easily understood and easily interpreted in terms of both the effect of governor performance on system operation and the effects of governor characteristics on governor performance.

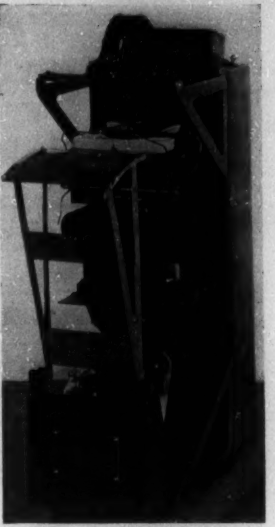

Figure 2. General Electric photoelectric frequency meter with rocking chart carriage for turbine-governor tests

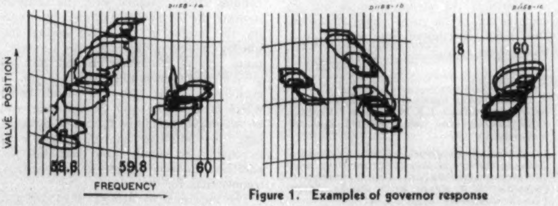

Figure 1. Examples of governor response

The instrument used in most of these measurements is essentially an adaptation of the engine-indicator card principle, in which system frequency is automatically plotted against steam valve motion. Thus, referring to Figure 1 of this discussion, as the frequency starts to change from, for example, an upward swing to a downward swing, the turbine valve may not respond until a definite change in frequency has been attained; then the valve will begin to move at a rate depending on the ratio of the rate of change of frequency to the incremental regulation. The two most important factors, dead band and incremental regulation, are thus shown very simply on a single record. Moreover, as there are always minor frequency swings occurring on the system, no special means for producing the motions are required. Experience has shown that the changes are usually sufficiently slow so that the dynamic lags do not unduly affect the pictures obtained. To obtain data on the more rapid transient response which may occur with large load changes or loss of load, oscillographic methods have been used.

In the tests reported here, frequency is recorded by means of a General Electric photoelectric frequency recorder on a card mounted on a carriage mechanically connected by a wire or cord to the steam valve, operating piston, governor beam, or to any point the motion of which is desired. The motion is usually amplified by a lever or crank in order to obtain a suitable plotting

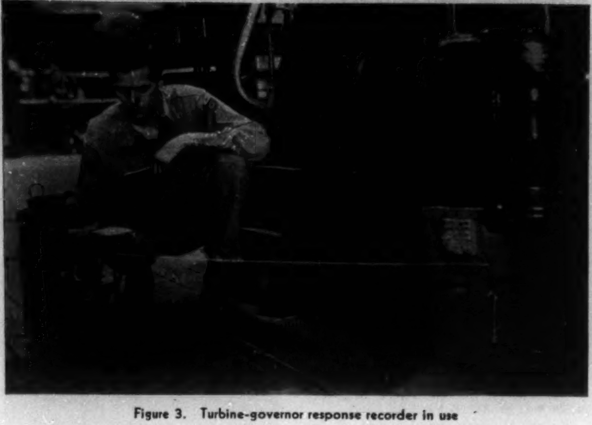

Figure 3. Turbine-governor response recorder in use

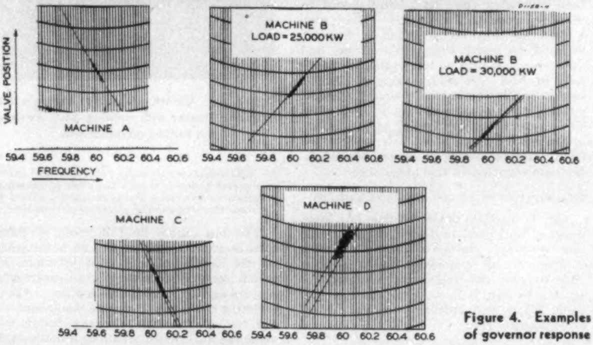

Figure 4. Examples of governor response

scale. It is thus possible to determine the amount of dead band contributed by each component of the mechanism. The frequency recorder, with the carriage for recording mechanical motions in place, is shown in Figure 2 and in use in Figure 3 of this discussion.

Further Examples of Test Results

Figure 4 of this discussion shows examples of the records obtained on a few of the machines tested. Machines *A* and *C* have two-stage hydraulic relays, while machines *B*, *D*, and *E* have only one-stage relays. The dead band is seen to vary from practically zero to about 0.1 per cent in these machines. However, because of the relative scarcity of data, it cannot be inferred that these values are representative of all machines, or even that they indicate probable values of dead band on other machines. Parallel lines have been drawn on some of these charts in order to illustrate further the procedure used to determine dead band, which is taken as the horizontal distance between the parallel lines and incremental regulation, which is proportional to the change in speed, or frequency, divided by the change in valve position.

Conclusion

In conclusion, it may be remarked that this method of governor testing to determine dead band and incremental regulation has shown itself to be very useful and informative. Further similar tests can probably be made by interested power companies, with mutual advantage to these companies and to the manufacturers of turbine-governing equipment. We take this opportunity to acknowledge the co-operation of our associates and of the many power companies that have contributed time and effort to make these measurements possible.

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W. O. Osbon (In collaboration with A. F. Schwendner, nonmenber; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Concerning Mr. Juhnke's remarks we are assuming that he means over-all steady-state regulation with the speed changer in the no-load position when he refers to a regulation of 20 per cent. Present practice for larger central-station units calls for four to six and not more than eight per cent over-all steady-state regulation. Six per cent regulation should satisfy most central-station generating systems. Load limiting devices which can be adjusted to relieve the governor of control are supplied with units intended for use as base load units. In any event the instruments described in the paper will aid greatly in measuring the speed regulation of existing machines and in determining the requirements for new ones.

The discussion by Messrs. Sheppard and Concordia brings out the fundamental differences between the governor analyzer and the instrument used by them. The governor analyzer was developed primarily for the purpose of giving information about the performance of each of the elements which make up the complete governing system. It shows not only the over-all response of the governor, but reveals also how much each individual element contributes to the final result. The instrument described by the discussers, on the other hand, gives only the values of frequency dead band and incremental governor regulation. Other instruments must be used to determine the elements of the governing system responsible for the result. It serves well to give a rough quick determination of dead band, provided that the rate of frequency change is smaller than the rates of response of the governor and of the recording instrument. If the rate of frequency change is higher, the width of the recorded dead band will be distorted by the lack of response. The photoelectric recorder used by the discussers is definitely limited in its rate of response. This effect is apparent in Figure 1 of the discussion where certain of the loops are definitely oval in shape. When such records are obtained, there is no way of knowing that the lack of response is not due to dynamic lags in the governor itself, rather than to the limitations of the instrument.

The governor analyzer, on the other hand, is capable of following the most rapid frequency changes likely ever to be encountered. Consequently not only does the running chart obtained with the analyzer indicate the dead band, but also, by checking the motion of the valve against frequency changes of different rates, the maximum rate of frequency change that the governing system is able to follow can be established. The value of dead band, which is a steady-state phenomenon, can be separated definitely from the dynamic lags. It is admitted that considerable work is required to obtain these data, but the additional information obtained adequately compensates for the trouble.

The statement by Messrs. Sheppard and

Concordia that incremental regulation can be obtained directly by the use of their instrument is somewhat misleading. They correctly define incremental regulation of a generating unit as "the ratio of speed change to load change for a small change from normal speed." Their instrument, however, gives only the ratio of speed change to valve movement. Because of curvature in the valve lift characteristic, the ratio of load change to valve motion must also be carefully measured. The determination of the correct value of this ratio for a particular load point is as important as the measurement of the governor regulation itself. By recording simultaneously all the quantities required for a determination of incremental regulation of a generating unit, the governor analyzer leaves nothing open to question.

Messrs. Sheppard and Concordia state that no special loading schedule is required when using their instrument. This statement also is misleading. It is true that dead band and regulation can be measured at one particular load, but a complete investigation of governor characteristics requires measurements at a number of different load points. That the discussers recognize this fact, in spite of their statement to the contrary, is indicated by their Figure 4 in which measurements on machine *B* at two different load points reveal different values of incremental governor regulation.

Control of Tie-Line Power Swings

Discussion and authors' closure of paper 42-72 by C. Concordia, H. S. Shott, and C. N. Weygandt, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 306–14.

Earle Wild (Commonwealth Edison Company, Chicago, Ill.): The paper by Messrs. Concordia, Shott, and Weygandt is a valuable contribution to the study of supplementary control. The conclusions, however, are applicable in but few cases today. The optimum regulation is seldom needed, and most tie-line controllers are probably not used at over ten per cent of this rate. It is possible that there are some cases where limited tie-line facilities and large highly variable loads could use the principles developed. The Chicago experience does not indicate any trend in this direction, but does indicate that a reasonable, orderly and fairly slow correction is satisfactory, permitting the large momentary power swings to be absorbed by the inertia of the large interconnection, but—all important—having a continuous correction of the tie-line load trends of longer duration.

J. E. McCormack (Consolidated Edison Company of New York, Inc., New York, N. Y.): The operating companies are interested in the characteristics of governors, frequency controllers, and other regulating devices to the extent that these equipments function with each other and with the other electric and steam equipments in the generating station. The rate of response of a governor or a frequency controller is only one of a series that includes the rate of response of the boiler control equipment, stokers, coal pulverizers, and the like. In general, the maximum available rate of response of the present electrical equipment in the frequency or load-control schemes is faster than the rate of response of the steam plant. If faster response of the supplementary control system is required, attention must be turned to the steam plant in order to obtain the faster rate.

Two types of controllers are discussed:

1. The proportional type which operates on accumulated time error.
2. The floating type which operates on instantaneous frequency.

Once again, the operating companies are not interested in their fundamental differences but are concerned in the results of these controllers operating on the system. Either type will do a good job as long as the system frequency, time error, or tie line, and station loads are not permitted to drift beyond the allowable limits of the operating company. If these limits are exceeded, the controller will have distinct disadvantages. The operation of a controller outside of the limits set up by the operating company is academic, because during this period the operator removes the automatic control from service and uses manual control to restore conditions to normal.

The installation of the correct type of automatic control, on an interconnected system consisting of generating stations and tie lines of varying capacities, will result in a narrower frequency variation, a smaller time error, a better utilization of the tie-feeder capacity, a reduction in the load swings on a station, and a lower maintenance cost in the boiler plant.

C. Concordia, H. S. Shott, and **C. N. Weygandt:** The object of our paper was to determine the possibilities and limitations of tie-line controllers when an attempt is made to get the most out of them, on the assumption that the tie-line controller is the limiting factor, that is, that boiler equipments and so on, are more than adequate. However, we are in entire agreement with Mr. Wild that the rates of response thus found to be optimum are in general much higher than would be necessary or desirable on the more usual large interconnected system, and with Mr. McCormack that, if such high rates of response are actually used, it may be found that the response of the boiler and its equipment will be the limiting factor. This paper represents a second step in the complete analysis of a controlled power system, the first step being the analysis of speed governors[1] presented a short time ago, and the third step possibly being a more detailed consideration of boiler performance. The boiler and turbine performance have already been discussed in the companion paper.[2]

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2. SUPPLEMENTARY CONTROL OF PRIME-MOVER SPEED GOVERNORS, S. B. Crary and J. B. McClure. AIEE TRANSACTIONS, volume 61, 1942, April section, pages 209–14.

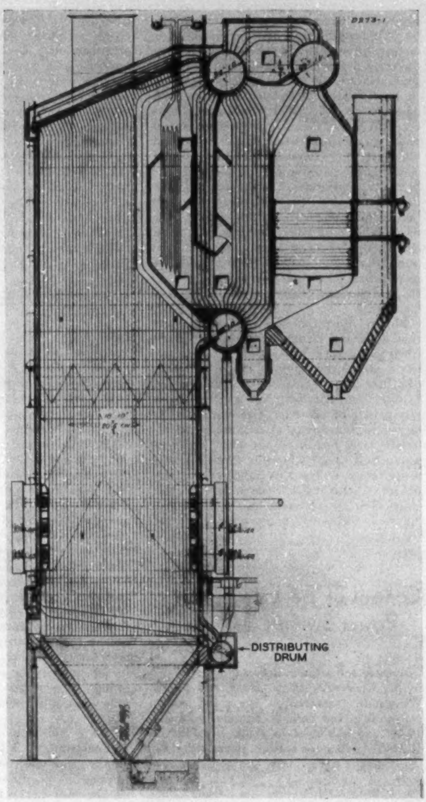
Supplementary Control of Prime-Mover Speed Governors

Discussion and authors' closure of paper 42-73 by S. B. Crary and J. B. McClure, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 209–14.

P. B. Juhnke (Commonwealth Edison Company, Chicago, Ill.): The paper presented at this session by Messrs. Crary and McClure is evidence of decided progress in a field in which, until comparatively recently, wrong notions and concepts were rampant, because thorough scientific analysis had not touched it before. I and many other operating men concerned with this subject were victims of conclusions only partly founded in the facts observed. We made observations and misinterpreted them. Observations were frequently made in connection with the loss of generating capacity, with effects expressed only in frequency variations in a downward direction, and contained in the formula: one per cent loss in generating capacity carries with it a drop in frequency of $^1/_{10}$ cycle, and vice versa. In reality, there was another effect, completely lost sight of for some time, presumably due to the all-absorbing interest which had attached itself to frequency and its maintenance as a principal factor in the operation of interconnected systems. Repeated inconsistencies in our observations and the results reported by New York in the preceding winter convention prevailed on us to make further investigations. And then we began a series of tests to definitely determine the merits of our previous conclusions. We isolated certain sections of our system to simplify the tests, the sections ranging between 60,000 and 100,000 kw. The load on them was totalized on one meter, the voltage was held constant, and then the effects of the variability of the load with frequency, through a range of four cycles, was observed. In this manner we obtained evidence that our previous conclusions were 500 per cent high, or only about 20 per cent right. The conclusions drawn from these tests may be laid down in the formula: $^1/_{10}$ per cent variation in frequency is attended by not more than 0.2 per cent variation in system load. How could we harmonize with this the observation, made over and over, that $^1/_{10}$-cycle variation in frequency was accompanied by one per cent variation in load? We searched and found that every such loss in generating capacity was attended by voltage reduction —not large, but enough so that 80 per cent of the change in generation, which took place whenever the entire interconnected system lost one of its large units, had to be allocated to this cause.

To more definitely corroborate this, we very recently conducted voltage tests to determine the precise dimension of the effect of

Figure 1



voltage variation on the system load. During these tests all automatic circuit regulators were taken out of service and the system voltage varied approximately five per cent. We then found that on a system load of 1,720,000 kw this variation in voltage produced a load change of approximately 70,000 kw, or four per cent, giving a ratio of 0.8 per cent load change for one per cent of voltage change.

Messrs. Crary and McClure's paper may evoke some difference of opinion as to methods and detail, whether the governor should be depended upon in conjunction with controllers equipped with time bias only for speed regulation, or with frequency and time bias. It is one of those subjects on which a difference of opinion may be warranted. We of the Chicago group lean to the belief that a controller based on tie line with frequency and time bias is the *ne plus ultra*, as a time bias alone is somewhat slow of response, whereas frequency bias is practically instantaneous in restorative efforts.

Henry Kreisinger (Combustion Engineering Company, New York, N. Y.): In the design of steam generating equipment for rapidly changing load the following factors should be given consideration:

The response of combustion control in supplying fuel and air to the furnace in proportion to the steam demand. This includes the response of the pressure fan supplying air to the furnace, and the induced draft fan moving the products of combustion through the steam generating unit.

The availability of fuel supply for immediate use with large and rapid increase in load. With gas and fuel oil response is obtained in 5 to 15 seconds, with pulverized coal 15 seconds to two minutes depending on the magnitude of the load increase.

The repeating mechanical stresses imposed on the fan structure with large and rapid changes in load.

The repeating temperature stresses to which the metal of the heat absorbing surfaces is subjected.

The combustion control might be made so responsive that the fuel and air supplied to the furnace could be doubled in five seconds. The fans would have to handle twice the weight of air and gases against much higher pressure, and severe mechanical stresses would be imposed on the fans and the motors. The speed of the rotating parts could not be stabilized in such a short period of time; the speed would slow down and then speed up, and these changes in speed would be reflected in the draft in the furnace and puffy fire. The burning of twice the amount of fuel would result in twice the rate of heat transfer by the heating surfaces, which would require twice the temperature difference between the water in the boiler and the furnace side layers of metal forming the heat-absorbing surfaces. For high steam pressures the metal of the heating surfaces must be made thick, and this increased thickness results in proportionately higher temperature of the furnace side layers of the metal. Doubling the temperature difference causes large changes in the temperature of the furnace side layers of the metal which imposes severe temperature stresses in the metal. Such stresses when periodically repeated result in leaky joints, cracking, and corrosion of the tubes. Thinner tube walls would reduce these temperature stresses, but thinner tube walls would require small diameter tubes which in turn would require forced circulation.

With pulverized coal firing small load increases of about 15 per cent can be taken within about 10 seconds by drawing on the pulverized coal already in suspension within the mill by increasing the air flow through the mill. Most mills are provided with coal classifier which classifies the pulverized coal, allowing the finer coal to pass through to the burners and returning the coarser coal to the mill. Normally the amount of coal circulating through the classifier is five to six times the amount that passes through to the burners. When air flow through the mill is increased, the proportion of coal passing through is increased, and less is returned to the mill. The fineness of the coal going to the burners is only slightly decreased. Increased air flow through the mill also tends to put more coal in suspension, thereby reducing the amount of fines in the coal between the milling surfaces. Inasmuch as the fines have cushioning effect on the process of pulverization, the reduction of the fines in the coal between the milling surfaces tends to increase the rate of pulverization.

At the same time, when the air flow through the mill is increased, the coal feed to the mill is also increased. The rapidity of the response of the coal flow out of the mill to the increase of the coal feed is delayed, because the coal fed to the mill must be dried and pulverized.

The drying of the coal fed to the mill is facilitated by the greater flow of heated air through the mill. The delay in the response of the coal flow out of the mill to the increased feed of coal to the mill varies with the percentage of moisture in the coal. When very wet coal is fed to the mill, the response is apt to be slow, because the wet coal blankets the coal already in the mill, so that the fines are not readily picked up by the air flow. In plants where the coal is apt to be very wet, provision should be made for supplying hotter air to the mill to speed up the drying process. It is the coal already in suspension in the mill air that is immediately available for picking up and holding the load until the increased coal feed raises the rate of pulverization.

With a small drop in load the procedure is reversed, the air flow through the mill and the coal feed is reduced, and this reduction in turn decreases the amount of coal delivered to the burners.

Small changes in load put moderate duty on steam generating and steam temperature regulation. However, large changes in load impose a severe duty on the equipment and present difficult problems in steam pressure and temperature control.

With large and quick variation of load, it is difficult to follow the demand for steam with fuel and air supply to the furnace, and to vary the draft to move the varying weight of products of combustion through the steam generating unit. Even if such variation in fuel and air supply to the furnace could be obtained, there would be too quick and large variation in furnace temperature, which would cause severe temperature stresses in the metal of the heat-absorbing surfaces. Such stresses would be particularly severe with high pressures, because of the thick metal walls of tubes required for high pressures.

With the conventional natural circulation boiler fired with pulverized coal, the load could be raised from half to full load in about three minutes by doubling the fuel and air supply to the furnace, but this is not usually quick enough for most steel plants. In such plants the large and quick variation in the demand for steam is satisfied partly by varying the fuel and air supply to the furnace, and partly by drawing on the heat storage in the water and the steam space of the boiler. This stored heat is made available for immediate use by a small steam-pressure drop. The steam storage space includes the drums, the superheater, and the pipe line. When more steam is taken out of this steam space than is made by the combustion of the fuel, the pressure drops, and this drop in pressure causes part of the boiler water to flash into steam. The steam formation occurs throughout the body of the boiler water, and inasmuch as the steam has a much greater specific volume than the water, the water level in the steam drum rises. The rise in water level reduces feed water supply, thereby causing the flashing of the water into steam to be accomplished with smaller pressure drop. By the time the water level in the drum is lowered by the evaporation to a point when the feed water regulator would increase the feed to the boiler, the increased combustion rate is catching up with the steam demand or the peak load interval may be over. When the load drops, the pressure starts to rise, and the rise of pressure reduces the ebullition, thus causing further drop of the water level and increase of feed water supply. This increased water supply requires more heat to bring the water to steaming temperature, and this heat requirement gives the combustion control time to reduce the fuel supply to the furnace.

The steam-pressure drop during a large and rapid increase in load depends on the water content in the boiler and the steam space in the steam drums, superheater, and the pipe line. The larger the water content and the steam space, the smaller the steam pressure drop. A bent tube boiler with multiple steam drums of large diameter is best suited for such large and rapid changes in load, because it has large water content and steam space. Each tube is directly connected to one of the steam drums, so that the tubes can be quickly relieved of the excess steam during the peak.

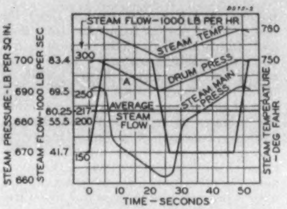


Figure 2. Steam pressure and temperature variation with rapidly changing load

A—Excess steam 432 pounds, six pounds from superheater, six pounds from steam drums, and 420 pounds from heat stored in water and metal

Water volume = 2,200 cubic feet = weight of water = 107,300 pounds

Steam volume in drums = 512 cubic feet

Steam volume in superheater = 230 cubic feet

Weight of metal in boiler = 442,000 pounds

Weight of superheater = 64,000 pounds

Water equivalent of boiler metal = 51,350 pounds

Total water equivalent = 107,300 + 51,350 = 158,650 pounds

In addition to the pressure drop in the steam drum, due to using stored heat in the boiler water for making steam, there is a pressure drop through the superheater and the pipe line. This pressure drop is necessary to make the steam flow and increases approximately as the square of the steam flow. That is, when the steam flow is doubled, the pressure drop is nearly quadrupled. The increase of this pressure drop may be two or three times the pressure drop, due to taking stored heat out of the boiler water.

The residual heat in the furnace is stored heat which works against good pressure and steam temperature control. The heat is stored during the peak load when it should be used for producing steam and is released during the low load period when no extra steam is needed. Therefore, the residual heat should be reduced to minimum by proper furnace design. Refractories and slag accumulations in the furnace should be avoided. The furnace should be completely water-cooled with dry bottom.

The steam generated at the peak loads with the heat stored in the boiler must be superheated with the heat stored in the metal of the superheater. Increased flow of steam through the superheater tubes increases the rate of heat transfer from the superheater metal to the steam, with the result that the temperature of the metal drops. The heat released by this drop of metal temperature is available for superheating the steam made with the heat stored in the boiler. When the load is dropped, heat is again stored in the boiler and the superheater. Superheaters of large weight of metal give more nearly constant temperature of steam with variable load.

When the high and low load periods are of comparatively long duration, the storage of heat in the boiler and superheater gives the combustion control time to catch up with the steam demands. When the periods are of short duration, the stored heat may take care of the load variation, and the fuel may be burned at constant rate. The latter case can be illustrated by the following specific example:

A steam-generating unit is to supply steam for rapidly varying load. The steam demand rises from 150,000 pounds in five seconds to 300,000 pounds per hour; it stays at this peak for 16 seconds and then drops in five seconds to 150,000 pounds where it stays for 21 seconds. This load cycle is repeated every 47 seconds. The unit is to be fired with blast-furnace gas, with pulverized coal as supplementary fuel.

It is obvious that combustion control could not follow such large and quick variation. Even if it were possible to supply fuel and air to the furnace at such varying rate, it would not be good engineering to operate the unit in such a manner, because of the severe temperature and mechanical stresses imposed on the furnace and boiler heating surfaces and the fan equipment.

The unit selected for this load is shown in Figure 1 of this discussion. It consists of a three-drum bent-tube boiler with an additional small drum for distribution of water to the furnace walls. The two top drums are 54 and 60 inches in diameter; the water drum is 48 inches, and the distributing drum 27 inches in diameter. The furnace is completely water-cooled. The following are the principal data bearing on the pressure and temperature regulation:

Operating drum pressure	700 psi
Steam temperature	760 Fahrenheit
Boiler water volume	2,200 cubic feet
Weight of boiler water at operating pressure	107,300 pounds
Steam volume in drums and steam tubes	512 cubic feet
Weight of metal in boiler	442,000 pounds
Water equivalent of metal in boiler	51,350 pounds
Total water equivalent	158,650 pounds
Weight of superheater	65,000 pounds

The operation of the unit is illustrated in Figure 2 of this discussion, which is a chart showing load, steam temperature, and pressure variation with time. The abscissae is time in seconds. The ordinates are steam flow (the zigzag line), the steam temperature (the top curve), drum pressure (second curve from top), and steam main pressure (the lowest curve).

Fuel is supplied to the furnace at constant rate to generate 217,000 pounds of steam per hour, or 60.25 pounds per second, as shown by the horizontal line passing through this flow point on the scale. The area above this horizontal line and below the zigzag line of the peak load represents the quantity of steam made with the stored heat in the boiler, which is 432 pounds. It is also equal to the area below the constant-heat supply line and above steam-flow line during the low load period which represents the heat put in storage. The variation in steam temperature is nine degrees and is likely to be two or three degrees less, because of the increased rate of heat transfer from the metal of the superheater to the steam which has not been included in its determination.

The drum pressure drop is 10 pounds and is likely to be smaller, because the feed water supply will be reduced during the peak load and increased during the low load period. The effect of this variation of feed water supply has not been included in the determination of the pressure drop.

The variation of the pressure drop through the superheater and the steam main is the largest and is difficult to reduce. If the resistance through the superheater is made too low, the distribution of steam over the superheater may be uneven at low loads.

Earle Wild (Commonwealth Edison Company, Chicago, Ill.): The authors' conclusions in the first paper, excepting conclusion 2, confirm the opinions now held by the majority of operators in the largest interconnection in the country. Governors have been found by test to be the essential device for controlling the speed. In most of the turbines they were found to be active and sensitive, although the per cent regulation left considerable to be desired, especially in some of the newer and larger turbines. The governors very definitely determine the width of the frequency band. Changes in load demand with frequency and changes with voltage were found to be real, but of so much smaller magnitude that the effect of governor action is the only major factor in instantaneous speed control. As pointed out in the first conclusion, with good governors the rate of response of the frequency controlling stations can be much slower, since the error will integrate at a slower rate and correction can also be made at the source of the frequency error.

After recognizing the merits of the governor and the need for improvement in governors, its limitations must also be recognized. The three things that a governor will not do are:

Restore frequency to the tolerance band.
Correct time errors.
Allocate loads in the most economical manner.

These functions must be left to the supplementary controllers. This leads to another aspect of the problem, which has hardly been touched upon in any of the governor or related papers written in the last two years. In the large interconnection there is a load-control problem, which is entirely separate from the speed-control problem, and the importance of which has not been fully appreciated. In any local region of this large interconnection there will be dozens and perhaps hundreds of load changes in which there are no corresponding frequency changes, as contrasted with an occasional load change which is accompanied by a corresponding frequency change. Theoretically, if tie lines are unlimited in capacity, these hundred load changes could be ignored, and only the one which is accompanied by a frequency change corrected, but the fact remains that tie lines are limited in capacity, and no economical justification exists in the majority of cases for increasing their capacity or transferring load to other stations. The load-control problem must, therefore, be met, and this is best done through tie line controllers supplementing the governor action and using the tie-line bias principle, as correctly recognized by Messrs. Crary and McClure.

The periods of abnormal frequency in the large interconnection are caused by a number of local regions neglecting their load control problems at the same time. They are caused by such factors as system operators incorrectly prognosticating the system load, turbines or boilers being brought in too late for the load, the human element ignoring obvious indications of incorrect generation, inadequate telemeters with which to guide the operators. When a series of these maloperations cascade together, a gradual frequency change results, several times the width of the normal frequency departures, and considerably beyond the range of the frequency controlling stations. These occurrences are most severe during periods of rapid load change, when probably 90 per cent of the governors are being reset every few minutes to meet local load changes. Ordinary concepts of governor regulation during these periods are meaningless, because of this frequent resetting of the governors. In other words, if a governor calls for a three per cent load increase due to low frequency, while the operator is making a six per cent increase to meet local load demands, it is hardly expected that the operator would know he should make a nine per cent load increase in order to satisfy the governor. When the frequency was corrected finally, the operator would have to make an additional three per cent increase, in the interim not satisfying the low frequency requirements and increasing after the frequency is normal. This example of inadequacy of governors could be ideally handled with a supplementary tie-line controller. The major problem, then, is not what is the per cent regulation and the sensitivity of the governors, but why were so many turbines and boilers incorrectly loaded at the same time in widely scattered regions of the interconnection? The conclusion which has been reached, after each study of this problem, is that a wider application of supplementary controllers in local regions is needed, and it is very gratifying that the summary of Messrs. Crary and McClure bears out this opinion.

The major contribution of the Chicago area to this problem the past year has been the utilization of the tie-line controller impulses to vary the load of the three large variable load stations simultaneously. The burden of regulation on each one has become almost insignificant, while the deviations of the tie line from schedule are extremely rare, except in response to governor action during frequency changes. This confirms an earlier conclusion that a large part of the difficulty in regulating load and frequency comes from cross regulation between stations within any one group, and that the meeting of one per cent load change per minute is relatively easy. This experience is being extended to two other groups in this interconnection.

G. B. Warren (General Electric Company, Schenectady, N. Y.): The general question of what rate of change in temperature or what rate of change in load a turbine can stand is one of great complexity depending upon the conditions for which the turbine was designed, its capacity, its revolutions per minute, and the initial pressure and temperature at which the machine is operating. The change in initial temperature and change in load are related, since, even at constant initial temperature, changes in load bring about changes in internal temperature in the turbine, which may be exaggerated by the changes in initial temperature occurring simultaneously as a result of boiler conditions.

In a machine such as a turbine, through which steam is passing, the cross section of all parts cannot of necessity be made the same in relation to their surfaces in contact with the steam, and, hence, some parts are heated or cooled faster than others. This gives rise to changes in clearances and sets up strains and stresses, the magnitudes of which are a function of both the amount and the rapidity of the temperature change. This, in turn, may be a function of the rate and magnitude of the load change as well as the initial temperature change.

While exact data on the detrimental effect of these factors on turbines are not available, we have made careful efforts to ascertain from the operating people whether turbines subjected to varying loads require more maintenance than turbines subjected to more constant loads. There does not seem to be any great evidence to indicate that this is so, but in a few isolated cases turbines subjected to great changes in temperature and many washings without careful control of the temperature have appeared to require increased maintenance.

As a result of a careful survey recently made by a group of turbine designers in the General Electric Company, the following conclusions were reached:

> A plus or minus 75-degree-Fahrenheit change in internal temperature, either sudden or cyclical around a mean temperature, will not cause serious strains or clearance changes. These variations in internal temperature may be caused by variation in load or initial temperature or both. However, the maximum temperature reached must not exceed the contract limits both as to duration and intensity.
>
> With constant initial temperature, a 75-degree-Fahrenheit change in internal temperature generally corresponds to a change of load of approximately plus or minus 25 per cent of the load at which the turbine is operating.

When Messrs. Crary and McClure asked us for a statement of this situation, our preliminary investigation indicated that the 75-degree-Fahrenheit change corresponded to a change of about 15 per cent of rated flow. A more careful study indicates that a more accurate statement would be 25 per cent of the load or flow at which the turbine is operating. For two-thirds load this is not far from the statement in the paper.

The question of whether greater rates of change in temperature or load can be taken without increased maintenance is not so easily determined. The answer depends upon the size of the machine, the pressure for which it is designed and, hence, the thickness of the parts, whether the machine is an 1,800-rpm machine with a large diameter or a 3,600-rpm machine with a smaller diameter, and whether it is of single- or double-shell construction, and also whether or not it is a fixed back-pressure machine or a condensing machine. In general, the sensitivity of a machine to such changes in internal temperature decreases in the following order:

1. 1,800-rpm single-shell machines.
2. 3,600-rpm single-shell machines.
3. 1,800-rpm double-shell machines.
4. 3,600-rpm double-shell machines.

Condensing machines have a greater internal temperature variation for a given load change than noncondensing machines operating at a fixed and fairly high back pressure, and smaller turbines can probably stand a greater rate of temperature change than larger turbines, other conditions being comparable.

An effort is made to design all turbines so that in cases of necessity they can drop all load instantaneously without damage, or pick up full load from no load as fast as dry steam can be supplied.

Except when special circumstances alter the situation, we know that the most sensitive of the above machines have safely withstood the temperature changes incident to a sudden increase from zero to full load or of a sudden loss of full load. We do not know how many times in the life of a machine it can be safely put through such drastic changes in temperature without increased maintenance being required. In the case of temperature changes in excess of 75 degrees Fahrenheit, it would appear desirable to allow some time for the change to take place. Except under emergency conditions internal temperature changes should probably not exceed 75 degrees Fahrenheit suddenly plus five degrees Fahrenheit per minute for class 1 machines above, and for all other classes should not exceed 75 degrees Fahrenheit suddenly plus 10 degrees Fahrenheit per minute. Thus, an internal temperature change of 200 degrees Fahrenheit (whether from a load change or initial temperature change or a combination of both) should not take place in class 1 machines above in less than $(200-75)/5$ degrees = 25 minutes. 200 degrees Fahrenheit internal temperature change corresponds to a change in load from about 50 per cent to full rated load, and twice this change will correspond to a swing throughout the full rated load. The conclusions which one would reach with respect to large changes of load from the above principles and simple calculations are not far from the 30 to 60 minutes suggested in the paper for a 100 per cent change in flow.

The above rules are intended to cover general cases. Some machines are particularly sensitive to temperature or load changes, in so far as temporary increases in vibration are concerned, and must, until such conditions are rectified, be accorded special consideration. Specific machines for especially high initial pressures or other peculiar operating conditions may also be exceptions.

These rules do not apply to changes in temperature, incidental to washing solids from the internal parts of turbines where water may be present in the steam with resulting increased heat content and conductivity of the steam, and resultant need for slower temperature changes and for which special instructions have been issued.

F. G. Sandstrom (Consolidated Edison Company of New York, Inc., New York, N. Y.): The morning rate of load growth of the entire Consolidated Edison Company load averages approximately 0.4 per cent per minute. On some stations this rate averages 0.5 per cent per minute. However, for short periods of about 10 minutes this rate may be as high as 2.0 per cent per minute at a station.

When our supplementary controllers were first installed, the rate of load change of the controller was high enough to take care of the short-time load changes. The steam group soon objected to this high rate of pickup because of the increased maintenance costs, objections to smoke and fly, has possibility of blowing a hole in the fuel bed, inability of maintaining steam pressure and so forth. The supplementary controllers are now adjusted for a rate of load change of approximately $1^1/_2$ per cent per minute which the steam group feel is allowable on stoker-fired boilers.

As a result of our experience, we feel that the present supplementary controllers are fast enough. If they are used with too fast a rate of load change, the frequency and load-swing problems are not solved, and an additional problem may be created for the steam men.

The over-all governor response on our system is from 11 to 17 per cent, or several times the optimum rate quoted in the paper by S. B. Crary and J. B. McClure. There is nothing wrong with their theory that the smaller the regulation rate, the smaller will be the frequency swings. However, we are not concerned with frequency swings but are concerned by the load swings accompanying the frequency swings. These load swings are usually caused by the governors on the generators making up a deficit or an excess of generation on the system. If the system suddenly becomes short 50 megawatts, the load swings on the tie feeders and generating stations will be about the same, whether all the generators have 12 per cent regulation or 6 per cent regulation.

We have found that the cause of the load swings is usually incorrect anticipation of the change in the actual consumed load, or manual adjustments on turbine governors that tend to force the frequency away from 60 cycles. On the Consolidated Edison system load swings have been reduced considerably by furnishing the station operators with a synchroscope connected between a standard frequency and the system frequency. The standard frequency at each station is the Bell Telephone standard. The station operators pick up load when the frequency is low and drop load when the frequency is high.

On our system the stations are connected by relatively low capacity ties. An automatic supplementary controller that works on instantaneous frequency or time error would have to be constantly supervised by the operators in order to prevent overloading the tie lines and maintain the economy load schedule. Consequently, on a system similar to ours we feel that the tie-line bias control appears to be most suitable, because it supervises the tie-line loads and allows all stations so controlled to contribute to any large load changes that might occur. Then the station where the load change occurred can slowly at a controlled rate pick up the total load change in the area.

J. E. McCormack (Consolidated Edison Company of New York, Inc., New York, N. Y.): The conclusion of Messrs. Crary and McClure that absolutely flat frequency, zero time error and flat tie-line load are unnecessarily severe limitations may lead to the belief that a close approach to these conditions is desirable. I would like to point out that to approximate these aims, the controlling station or stations would have to withstand rapid load changes of a very high percentage of the station capacity. It appears to me that it is more desirable to let the frequency, time, or tie load drift in order to spread the load change over the entire system, so that the change on any station would be a small percentage of its capacity.

Automatic controllers cannot be expected to do all of the regulating, as the operator must make many manual adjustments. Machines must be started and loaded quickly to their minimum load, the regulating range on the regulating machine must be maintained, and the load has to be distributed between machines for economy reasons. The phase angle between bus sections must be maintained within allowable limits in stations supplying network load. To make these manual changes intelligently, the operator should have a knowledge of the response of the system and be provided with suitable instruments.

We have found a common frequency reference in all major stations to be useful when making manual load changes. A standard frequency is wired to the major stations where a synchroscope is connected between the standard and the system frequency. The synchroscope, being a very sensitive indicator of system frequency, is used by the operator as a guide in making manual load changes only at a time when they tend to restore system frequency to 60 cycles. Load increases are made when the frequency is low, and load decreases are made when the frequency is high.

Experiments have been made using a frequency standard transmitted by radio for comparing the system frequency by means of a stroboscopic image at each station. This method will be an economical way of obtaining a common frequency standard at the generating stations on a widespread system.

The trend toward larger interconnections made it imperative to have a thorough understanding of the effect of changes in load, voltage, and frequency on our system, in order to obtain the most advantages from the interconnection. The first consideration should be to obtain data for each system, which would require making tests allowing one of these three variables to depart from normal, while the other two are maintained normal. The carefully controlled departures of the system conditions from normal to obtain this vital information will result in a better understanding of system operation. The information will aid in obtaining the maximum utilization of the generation and tie lines, as well as aid in restoring conditions to normal in times of an emergency.

When this information is available, the next and most important job is to place the information into the hands of the operating force in the individual plants. Any steps taken to give the operators a better understanding of the importance of performing their operations correctly will produce surprising results in the improvement of system operation.

As an example, I discussed with our system operators the possibilities of a stroboscope using a radio signal for a common frequency standard. Two weeks later one of these men produced a working model of the idea. More important than obtaining this working model, the system operating group have become very interested in obtaining a better understanding of the frequency control problem. The operating men want to do a good job but are often prevented, because the problem has not been presented to them properly.

S. B. Crary and **J. B. McClure:** The authors wish to express their appreciation for the discussion on this paper. This subject is an important one to both operators and manufacturers, and their combined viewpoints should broaden their understanding of it.

Mr. Juhnke has cited an interesting series of tests on sections of his system to determine the relative effects of voltage and frequency variations on system load. His results check those previously obtained by Mr. J. E. McCormack, namely, that the system load is much more sensitive to voltage variations than it is to frequency variations. It is recognized that these results are peculiar to their respective systems and cannot be generally applied to all systems. Mr. Juhnke's point regarding the use of supplementary time bias in addition to frequency bias is well taken. We did not mention time bias in our paper, since it is ordinarily applied at such a slow rate that it may be added without difficulty. However, it is a valuable addition to automatic frequency and tie-line bias control.

Mr. Kreisinger's discussion of the factors involved in the design and performance of steam-generating equipment will be of particular interest to many electrical engineers who are not so familiar with the mechanical engineers' problems associated with governor performance and supplementary control on a system. We also appreciate his description of a boiler which is so designed as to be able to handle rapidly fluctuating loads. However, from our discussions with the operating groups, the older steam-generating equipments, in many cases, have distinct limitations in this respect.

Mr. McCormack's emphasis on allowing some leeway in the control of frequency, time error, and tie-line load is very much to the point. We had hoped that our conclusions were clear in this respect, particularly for the benefit of groups just embarking on this area of control. Other groups have been through the experience of attempting to force the control of frequency, time, and tie-lines to the limit of perfection and have invariably concluded that the intelligent solution is necessarily a compromise among the several requirements which must be considered in the complete operation of an interconnected system. His reference to the possibility of using a radio-transmitted common master frequency over the entire system indicates that manually controlled stations soon will be able to make their adjustment so as to always help rather than aggravate the over-all picture.

Mr. Sandstrom has cited rates of transfer on their automatic stations which are interesting, and to some who have not had experience with automatic control, and to others whose systems are relatively rough, these rates will appear surprisingly low. Again, this cannot be taken as a general answer, and every system will have individual characteristics in this respect. We concur with his conclusion that relative generator and tie-line loadings will be practically the same for a system equipped with 12 per cent governors as for one with 6 per cent governors. As soon as the operating philosophy recognizes that frequency departure is not important, then it is not so important to have the composite governor regulation at the optimum value. We merely tried to point out that governor as well as supplementary controller characteristics are important factors in determining the regulated frequency characteristic. We emphasized the optimum governor regulation, hoping that that viewpoint would be helpful to some who are advocating operation of their systems with governors having appreciably narrower regulation than at present, in establishing a lower limit of regulation below which no net gain would be obtained.

Mr. Wild mentioned their recent practice of extending the tie-line controller impulses to several stations simultaneously. The benefits of distributed control are so apparent that we can expect the number of regulating units to increase steadily.

The comments of Messrs. McCormack, Sandstrom and Wild on the importance of proper co-ordination of all manual control and the benefits to be derived from educational effort will be of particular interest to other operating companies. Manual control will always be present to some degree, and thorough appreciation of the problem and the objectives is essential to those assisting on manual control. Proper instruments are also essential, and, as the needs are recognized, improved instruments will be made available.

Mr. Warren and his associates assisted us in arriving at the statements made in the paper regarding rates of load transfer on the newer higher-temperature high-pressure steam turbines. We are pleased to have Mr. Warren outline in more detail the factors which influence the performance of a turbine under fluctuating load conditions.

PCC Car Operating Results in Pittsburgh

Discussion of paper 42-52 by W. J. Clardy, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 214–17.

H. G. Moore (General Electric Company, Erie, Pa.): Mr. Clardy, in his paper, has shown in great detail the economic advantages of modernization of street railway service by the use of PCC cars in Pittsburgh. This confirms by data, and adds to the list of universally favorable reports on the successful operation of really modern street cars in other cities.

For example, the first PCC car installation was in Brooklyn. Four lines equipped increased their revenue by 15, 24, 26, and 33 per cent in a period when other lines had decreasing revenues of one to five per cent. Schedule speeds were also increased 13 and 14 per cent on two lines where PCC cars are used exclusively.

Other figures, at random, may also be cited. In Chicago, riding increased approximately 12 per cent over the balance of the system, and schedule speeds were increased eight to ten per cent. In Baltimore, revenue increased eight per cent, and maintenance was reduced. In Los Angeles it has been estimated that the increased patronage provides increased receipts of $3,000 per car annually on one line, and $4,000 per car annually on another line. Washington reported somewhat increased schedule speeds in spite of operating on lines with older cars, 22 to 37 per cent decrease in platform expenses; 15 to 30 per cent decrease in accidents and 30 to 49 per cent reduction in maintenance costs. Philadelphia increased revenue 16 per cent and schedule speed 15 per cent.

While all of these figures compare operation of modern PCC cars with older types of cars, it should also be kept in mind that PCC cars compare even more favorably with other types of vehicles sometimes proposed for handling heavy-density surface traffic. I would refer to a paper[1] presented at the winter convention four years ago by Mr. C. M. Davis outlining the particular fields of public transit served best by one or another type of vehicle.

The principal governing factor is the traffic density, or the number of passengers to be carried per unit time in the direction of maximum movement. For the heaviest type of service, the rapid-transit system operating over private right of way under ground or overhead has no competition in handling 100,000 to 180,000 passengers per hour on two tracks, one express and one local. On the surface, the street car cannot be excelled in the range of 6,000 to 12,000 passengers per hour.

In low-density traffic such as that permitting the use of 40 passenger vehicles with headways of 15 minutes or more (approximately 240 passengers per hour) an advantage can probably be shown for the use of gas busses. But even here, electric transmission with either a gas or Diesel engine may be economically desirable, particularly in frequent stop service.

Between the fields best served by gas busses on one hand and street cars on the other, there is a large field in the vicinity of 2,500 passengers per hour which usually can be best served by trolley coaches.

We may conclude, therefore, that where such facts as Mr. Clardy presents relative to the PCC car are obtained and fairly used in the selection of transit vehicles, the inevitable decision must be in favor of some form of electric drive for transporting more than three quarters of the millions of people who daily use public transportation services.

REFERENCE

1. APPLICATION OF MODERN ELECTRIC VEHICLES TO URBAN TRANSPORTATION, C. M. Davis. AIEE TRANSACTIONS, volume 57, 1938, January section, pages 57–60.

Modern Electrical Equipment for Industrial Diesel-Electric Locomotives

Discussion and author's closure of paper 42-50 by Lanier Greer, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April Section, pages 229–32.

Robert W. Barrell (General Electric Company, Erie, Pa.): Here are some facts illustrating exactly how the modern electrical equipment described by Mr. Greer's paper together with the modern engine very greatly affected the industrial Diesel-electric locomotive within a short period of time.

In 1935 a 65-ton locomotive power plant consisted of a single 300-horsepower 550-

rpm Diesel engine and generator with auxiliary generator. In 1940 there became available for such a locomotive a power-plant unit consisting of a 190-horsepower 1,800-rpm Diesel engine and a generator developed especially for the purpose. This new engine-generator set was exactly one such as described in this paper. Two of the new sets were used in place of the former single set.

The interesting facts in comparing the new locomotive with the old are shown in Table I.

Table I

	Per Cent
Increase in net horsepower at the wheels	19
Decrease in total engine cost	71
Decrease in total generator cost	38
Decrease in total cost of power plant alone	65
Net decrease in power plant cost when applied to locomotive	61

The net decrease when applied to locomotive is less than the decrease in the total cost of power plant alone, because it was necessary to maintain the locomotive weight at 65 tons. Hence, steel ballast was added to offset the decrease in power plant weight, and the ballast cost has been deducted from the saving.

Now the power plant cost in the old locomotive represented 43 per cent of the total locomotive cost. Hence, it is to be noted that modernization of just the power plant alone brought about a cost reduction of 26 per cent in the over-all locomotive cost. And this was accomplished while improving performance by an increase of 19 per cent in the net power.

The paper mentions several advantages of the modern traction motor with double reduction gearing. From a locomotive builder's viewpoint, one great advantage of such a motor is its flexibility in adapting it to several sizes of locomotives. This is well-illustrated by a motor now in use which is actually being applied to as many as six sizes of locomotives, ranging from 25 to 70 tons. One, two, or four motors are used on a locomotive, depending on the weight. Also, any one of several gear ratios may be selected.

It is very apparent, therefore, that such a motor makes possible a high degree of standardization resulting in many benefits. Not only is there a standardization of the motor and items of control equipment, but also many mechanical parts, such as wheels, axles, and motor suspensions, can be standardized to a large degree.

Apropos to the subject of this paper, some mention can be made of the generator and motor-control equipment for modern industrial Diesel-electric locomotives. Such equipment, while a much smaller portion of the locomotive, nevertheless has played its part in bringing about cost reduction, simplification, and standardization.

One control item, the reverser, can be cited specifically as an illustration. There has been developed a direct-operated type of motor reverser, radically different in design, much simpler and lower in cost than former conventional types of reversers, which has proved to be a complete success for single-motor and two-motor industrial locomotives. An appreciable saving, simplification, and standardization were thus realized over the conventional electropneumatically operated reverser with its master controller.

As stated in this paper concerning the propulsion equipment, it can likewise be said of the control equipment that new materials and methods have been developed and adopted, which have made it possible to reduce weight and cost and, at the same time improve the quality of the product.

An interesting improvement recently incorporated in this type of modern generator, briefly referred to in Mr. Greer's paper, is the engine cranking feature. It is still a common practice to use separate automotive-type geared starting motors with high-speed Diesel engines, but it now has been proved that a cranking winding can be added to the generator with the important three-fold result of reduction in over-all cost, increase in reliability, and simplification.

The idea of using the generator as a cranking motor is old and formerly commonplace. Its application to the modern high-speed equipment is a splendid example of the efforts to produce for industrial Diesel-electric locomotives machinery of the simplest and most rugged design.

Along this same line of thought concerning the development of simple and rugged Diesel-electric equipment for industrial locomotives, it here seems in order to amplify the description of and give facts about the generator excitation and engine control scheme.

A Diesel engine is inherently a constant-speed, constant-power machine. The combination of traction generators and traction motors on a locomotive is not. Thus, the fundamental problem concerning the electrical equipment on a Diesel-electric locomotive is to devise for this combination of dissimilar machines the most suitable scheme for utilizing the power of the engine.

On slow-speed industrial switching locomotives it is obvious that perfect utilization of the engine can be sacrified in favor of simplicity, ruggedness, and good accelerating characteristics. The excitation scheme described by Mr. Greer does exactly this. Without the use of complicated rotating devices, speed or torque-control schemes, or special windings or separate exciters, a practical method of engine utilization and good locomotive acceleration is accomplished.

This method employs the inherent speed-torque characteristics of a Diesel engine together with the inherent characteristics of a simple shunt generator with but the minor modification of a small amount of separate excitation from the standard cranking battery. The price paid for this extremely simple scheme is very small in terms of loss of power utilization.

Actually the figures on the two commonest industrial locomotive engine sizes are as follows:

> At normal operating generator temperature the maximum reduction in engine speed when operating on the full-throttle torque curve results in 10 per cent reduction in power on one engine and $7^1/_2$ per cent reduction on the other. However, this maximum power reduction occurs at but one point in the full power utilization range. The actual integrated power reduction realized over only the utilization range is less than 5 per cent for the one and 3 per cent for the other. Since it is not necessary for the utilization to cover the entire speed-tractive effort range of these locomotives, the true integrated power reduction values realized at the wheels are found to be less than 4 per cent and $2^1/_2$ per cent respectively.

Therefore, it can be said for these industrial Diesel-electric locomotives that the maximum power available at the wheels is but $2^1/_2$ to 4 per cent less than it would be were a perfect power utilization scheme employed. But it should be observed that this reduction applies only to the possible maximum power obtainable at full throttle. Now in the usual industrial switching service, full throttle is used for but a small fraction of the work, hence the net result of this power reduction when measured in terms of work done is negligible. Surely, this simple and rugged design of modern electrical equipment for industrial Diesel-electric locomotives is well justified.

Lanier Greer: Mr. Barrell's discussion has brought out the fact that the modern electrical equipment for industrial Diesel-electric locomotives has reduced greatly the cost of equipment for these locomotives. Also the mention of ballast being added implies that the weight of the equipment has been reduced. This is a fact and can best be illustrated by comparing the new high-speed equipment with the old low-speed equipment.

In the traction generator this weight-saving can be measured in total pounds of material used per engine horsepower. The modern high-speed generator weighs only 8 to 10 pounds per horsepower against 20 to 21 pounds per horsepower for the old low-speed generator.

For industrial switching, tractive effort is of prime importance. Therefore the continuous tractive effort rating per pound of material used is a measure of weight saving in the traction motor and gearing. The modern high-speed traction motor with double-reduction gearing has a continuous rating of 2.2 pounds tractive effort per pound of material used in the combined motor and gear unit. The older type of traction motor with single-reduction gearing had a continuous rating of only 0.9 pound tractive effort per pound of material used in the combined motor and gearing.

These figures show weight savings of more than two to one when measured by actual ability to do work. Savings of material are always important and are especially so at this time, as all materials used in this equipment are of strategic value to our national security.

Facilities for the Supply of Kilowatts and Kilovars

Discussion and author's closure of paper 42-53 by H. K. Sels and Theodore Seely, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 249–55.

J. W. Butler (General Electric Company, Schenectady, N. Y.): The authors have called particular attention, in conclusion 1, to the effect that the data, on which the study was made, should not form the basis of a generalization, but apply only to the Public Service System. In order that this be further emphasized, and in an attempt to speculate in the general case to allow a generalization, I offer this discussion.

In Figure 3 of the paper the authors give data indicating the annual kilovar load duration requirements of their system. A curve of this general shape can be taken as typical of most systems. With such a kilovar requirement, one may ask: "What is the most economical way of supplying this requirement consistent with good operating practice?"

For purposes of discussion this kilovar requirement will be divided into three sections, as given in Figure 1.

Unregulated Reactive

The major portion of the system base reactive requirement can be supplied by unswitched capacitors. The maximum amount of this type of reactive source that can be used depends primarily on:

1. The light-load voltage conditions in load areas.
2. The system-stability margins during light load.

The light-load voltage problem in the load areas can be sufficiently controlled by judiciously locating capacitors so that this limitation can be sufficiently far removed to the background to bring the other factor to the front. For instance, the closer to the generation the capacitors are placed, the lower will be the voltage rise, and if this problem presents itself, unswitched banks can even be placed on substation busses of four kilovolts or higher for supplying base kilovar requirements.

The next important consideration is, therefore, one of stability. One can readily visualize that if unswitched capacitors are installed in sufficient quantities to require generators to run underexcited during certain periods, instability may arise. Therefore, it is not generally considered good practice to so operate unless intelligent generator voltage regulators are employed. We can, therefore, tentatively set as an upper limit that value of unswitched capacitors which allows the generators to operate at 1.0 power factor during light-load periods. Whether this value can be practically obtained depends upon two factors: transient and steady-state stability.

Transient stability can be very critical to fault-clearing times, for a given kilowatt load and excitation on the generators, and so it follows that if transient stability is a limiting factor, faster switching times allow more unswitched capacitors to be used for the same stability margins. On the other hand, if steady-state stability is the limiting factor, the use of generator voltage regulators will materially increase this type of stability, allowing again more unswitched capacitors to be used for the same stability margins.

Thus, it is seen that with modern relaying and breaker times, and with intelligent generator voltage regulators, this base, or firm system reactive requirement can for all practical puposes be completely supplied by unswitched capacitors. In fact, it may under certain conditions be economical to more than supply this requirement, but this discussion will not go into that refined phase of the problem.

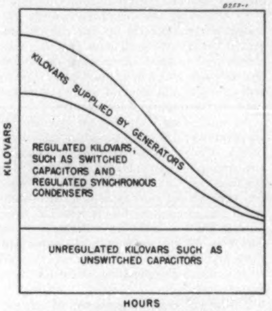


Figure 1. Representative annual kilovar-load duration curve

Kilovars Supplied by Generators

At high-operating power factors it is generally accepted that generators can carry reactive economically. The actual amount that can be carried by the generators in a particular case depends upon the particular generator design and the value and amount of reserve kilowatt capacity, and so, for the sake of discussion, the line of demarcation between the reactive supplied by generators and the regulated reactive will be only approximately located.

Regulated Kilovar Supply

It is in this center region—the regulated reactive requirement—that all engineers are particularly interested, and this is the region in which they will be more so in the future.

I quote from the last paragraph of the section of the paper labeled "Kilovar Supply."

> ". . . . After the limit of static capacitors is reached, if additional reactive capacity cannot be provided economically in new generators or maintained in existing generators because of the value of their kilowatt output, synchronous condensers offer a quick and economic way in which kilowatt generation can be obtained to meet unexpected demands. The higher over-all cost of large synchronous condensers may be partly offset by the convenience of their operation and their stabilizing effects . . . ."

I am concerned as to how the so-called "limit of static capacitors"—as apparently defined in this paper—was arrived at. It is appreciated that the authors' reasoning—and hence their conclusion—in respect to the static capacitor, was made on the basis of the *unswitched* capacitor. But it is not known what factors were responsible in this instance for ruling out the switched capacitor, and so my general findings in this respect will be outlined with the thought that possibly I have overlooked pertinent factors obtaining in the actual operating system, which oversight may be responsible for my arriving at a conclusion somewhat different from the authors.

Let us first consider the economic picture. I have made an economic analysis of the switched capacitor versus the synchronous condenser, based on known factors that can be reasonably evaluated in dollars and cents. Other intangible operating factors will also be discussed. In this analysis, the annual charge for each equipment was determined, and the type of equipment "proving in" was obviously the one having the lower annual charge.

As the equipment losses are the most important factor in such a comparison—where either the condenser or capacitor can fulfill the system requirements—they will be given careful consideration. The fixed, or no-load losses of a synchronous condenser are about 50 per cent of its full-load losses, the remaining loss varying approximately as the square of the load, with full-load losses being from two to three per cent. The losses in a capacitor are a constant, being one third of one per cent of the connected capacity. From this one can see that the total kilowatt-hour loss consumed by the condenser for a given kilovolt-ampere-hour requirement, is a function of the load pattern, while in a capacitor it is independent of the load pattern. Therefore, to make a fair comparison, this must be taken into consideration. So the data in Figure 2 were prepared from standard listed information.

In calculating which equipment would

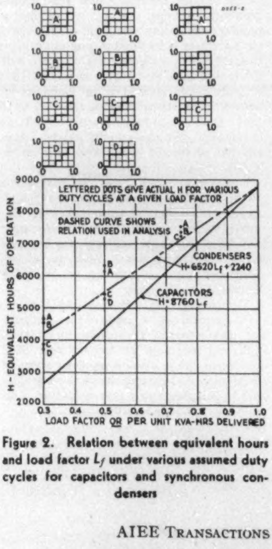


Figure 2. Relation between equivalent hours and load factor L_f under various assumed duty cycles for capacitors and synchronous condensers

have the lowest annual charge, the following basic assumptions were made:

1. Per-unit annual carrying charge on equipment investment taken as 0.125.

2. Annual energy charges on losses figured for data given in Figure 2, for various load factors.

3. Demand charge—per-unit annual carrying charge for use of system investment taken as 0.125.

4. Maintenance and operating charges are taken as equal for each.

On this basis were calculated the data plotted in Figure 3, which show the economic dividing line between 5,000-kva synchronous condensers and capacitors as a function of energy charge in mils per kilowatt-hour, kilovar load factor, and installed cost difference, for two conditions, namely:

1. No evaluation on demand.

2. Capitalized demand charge of $100 per kilowatt.

In equipments of 5,000-kw capacity, a liberal cost differential can be taken as $2 per kilovolt-ampere. On this basis, these calculations show that capacitors prove in, if no demand charge is levied against the peak losses, and only 2.5 mils per kilowatt-hour is made for energy losses, providing the kilovar load factor is not less than 0.3. Since 2.5 mils for energy—and a 0.30 load factor—are conservative figures, it indicates that capacitors in large banks switched for providing regulated reactive are relatively low-priced reactive generators. This, of course, is in substantial agreement with the authors' first conclusion listed under the section "Kilovar Supply," except I believe they were speaking of the unswitched capacitor, close to the load.

Now to consider the differences in system operation obtaining when using switched capacitors or condensers for the regulated reactive supply.

1. *System Stability.* If a system is designed and operated in accordance with well-established principles utilizing modern relaying and switching equipments, the transient-stability problem can be essentially narrowed down and isolated to those cases involving *electrically long lines* and *hydrogenerating stations.* (This excludes the developments associated with high-speed, three-phase and one-phase reclosing for single-circuit lines.) The installation of 5,000-kva synchronous condensers is made—by and large—in subtransmission and distribution areas which are electrically remote from hydrogenerators and long lines. Even adjacent to long lines and hydrogenerators, the character of the condenser in having electrical and mechanical inertia, results in only a second order effect on stability.

2. *Voltage Stabilization.* I feel that the field of the smaller synchronous condenser is practically limited to those cases imposing a difficult regulating job, such as found in substations feeding shovel loads, arc furnaces, and so forth. Under these conditions, the capacitor aggravates the voltage disturbance, if unswitched, and if switched, its switching equipment cannot follow the voltage changes anyway, so for this use it is ruled out. And the synchronous condenser in providing voltage stabilization in this instance holds a strong position in system design.

Summarizing—I feel the use of the switched capacitor as a regulated reactive source will be used more and more in the future, when engineers have accurately studied their system requirements as have these authors, and when more experience has been obtained with the switched capacitor. And as indicated in the paper, as well as in my discussion, each system needs to be studied as a *complete unit,* making it necessary that the distribution engineers, who have been the large user of capacitors so far, fully co-operate with the generation and transmission engineers in designing a system to meet its requirements, namely that of supplying the two commodities, kilowatts and kilovars.

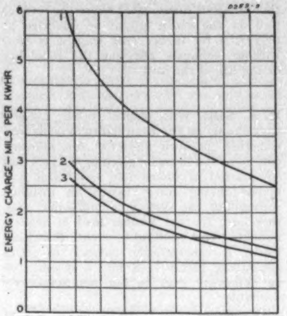


Figure 3. Curves showing economic dividing line between the application of synchronous condensers and capacitors rated 5,000 kva

Capacitors are more economical in regions above each line. Cost difference is capacitors minus condensers

Curve	Cost Difference	Demand Evaluation
1	$4 per kva	0
2	$2 per kva	0
3	$4 per kva	$100 per kw

G. S. Lunge (General Electric Company, Schenectady, N. Y.): It is interesting to note the increasing attention that is being given to reactive power (kilovar) dispatching. Although kilowatt load dispatching is taken for granted in modern utility systems, it is only in relatively recent years that reactive power has been similarly treated from an operating standpoint.[1]

From the manufacturers' standpoint an interesting by-product of this trend is the increasing use of varmeters (reactive kilovolt-ampere meters) for indicating and recording the flow of reactive power. A varmeter gives complete information regarding the reactive power flow, without resorting to any calculations, whereas a power-factor meter reading is of little significance unless considered in combination with the associated wattmeter reading. From an instrument-accuracy standpoint, the use of varmeters is worthy of encouragement. The varmeter is essentially built like a wattmeter and measures the quantity with a high degree of precision, regardless of the load. On the other hand, the power-factor meter, which measures the angle between current and potential, has a high degree of precision only when the load is above one fifth of its current rating. Except for application to synchronous motors, it appears that, in the future, power-factor meters will be used only rarely.

Although the use of telemetered readings of kilowatts has become quite common as an aid to power dispatching, such installations seldom include provision for telemetered readings of reactive power (kilovars). Nevertheless, this paper shows that for maximum utilization of the system facilities, it is essential that the reactive power dispatching be handled as thoroughly as the active power dispatching. The simplest and most effective means for giving the dispatcher a continuous picture of the reactive power flow in the system is to provide telemeter recording receivers in front of him to show:

1. The reactive power output of each major station or substation containing sources of reactive power.

2. The grand total of such readings.

3. The reactive power flow in each tie line interconnecting with neighboring systems.

In certain cases, a few additional kilovar readings from key transmission circuits inside the system itself will be helpful in making maximum utilization of the carrying capacity of those circuits.

When wire line channels are used for telemetering purposes, provision of a reactive power reading in addition to an active power (kilowatt) reading ordinarily doubles the expense of providing such facilities. However, when audio-frequency-modulated carrier channels are used, it is possible to use the same carrier channel for two simultaneous readings, so that the principal additional expense involved in adding the kilovar reading is merely that of the telemeter transmitter and receiver for reactive power (kilovars).

When the carrier channel is of such a nature that it cannot be used without further expense for the transmission of more than one telemeter reading at a time, it is entirely possible to change the telemeter-transmitter potential connections at regular intervals, say, every ten minutes, from wattmeter connection to varmeter connection and vice versa. When the associated (single) receiving instrument is of recording type, there is no difficulty in distinguishing the two records from each other on the same chart, except in the case of tie lines, in which case the use of separate kilowatt- and kilovar-recording instruments is highly desirable because of possible overlapping of the record.

REFERENCE

1. OPERATING ASPECTS OF REACTIVE POWER, **J. Allen Johnson.** AIEE TRANSACTIONS, volume 52, 1933, pages 752–7.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The recommendations listed in the paper by Messrs. Sels and Seely for the improvement in system protection will be recognized as being generally consistent with results obtained from studies made of other systems.

There is a question in regard to the stability calculations on which a little more detailed explanation would be appreciated. The statement is made: "The studies show that the phase-angle displacement is much more dependent on the turbine power output than on the generator power factor." This statement would seem to apply for stable transient operation through a severe fault but would not apply for the case of steady-state pull-out under the condition of carrying a high kilowatt load or a suddenly increased kilowatt load with low excitation. This latter condition has been considered an equally important consideration as a possible limitation for the application of unswitched shunt capacitors to a power system as the transient stability. In fact, this is a more important limitation than the transient case when quick relaying and switching are used. Accordingly, if quick switching and relaying are used with generator voltage regulators, it becomes possible to increase further the stability limitation toward the steady-state case and therefore to allow for a greater ratio between fixed and variable corrective reactive kilovolt-ampere from a stability standpoint. In this connection, also, I would like to ask the authors what is meant in the same paragraph by "pull-out angle." Is this steady-state pull-out angle or transient fictitious pull-out angle, and how is it determined?

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Sels and Mr. Seely have clearly outlined the more important factors which should be considered in connection with determining the ability of a power supply system. An accurate knowledge of the operating limitations of kilowatt- and kilovar-producing equipment is of course essential.

The increasing of power-factor and kilowatt ratings of generators will, of course, result in a reduction in their ability to provide reactive kilovolt-ampere load. It will be found, however, that although many machines are operated at kilowatt loads considerably in excess of their name-plate ratings, they can still provide substantial quantities of reactive kilovolt-amperes.

The excitation requirements at kilowatt loads in excess of the generator rating and at high power factor are usually considerably less than those for name-plate kilowatts and power factor. Therefore, from the standpoint of rotor heating, appreciable reactive kilovolt-amperes is available from a machine which is already carrying a kilowatt load equal to its kilovolt-ampere rating. For many machines, especially those operated at 3,600 rpm, the temperature rise of the stator will be increased only slightly.

For example, a 3,600-rpm, 20,000-kilowatt, 25,000-kva, 0.8 power-factor generator can be operated at 25,000 kilowatts and at 0.9 power factor without increasing the temperature rise of the rotor and with only 110 per cent rated amperes in the stator. Since generators are designed so that they may be operated at 95 per cent rated voltage without excessive heating, the 10 per cent increase in stator current actually represents a five per cent increase in what might be considered a normal operating current. In a 3,600-rpm generator the stator I^2R loss is extremely small compared to the total losses in the machine, and it will be found that this increase in stator amperes will affect the temperature rise of the stator only a slight amount.

In view of the fact that overload operation of machines may be required only during peak load periods or under emergency conditions when other generating units are out of service, it would appear that this type of operation should not cause any noticeable effect upon the life of a generator. In fact, there may be many cases where the rotor as well as the stator might be operated at overloads for rather long lengths of time without seriously affecting the reliability or the life of the unit.

H. K. Sels: With respect to Mr. Lunge's suggestions on the desirability of using telemetering for both kilowatts and kilovars, consideration must be given to the fact that summation of distributed switchable and nonswitchable static capacitors installed throughout the system is impossible, and, therefore, the net kilovar load is indeterminable to a considerable extent. Likewise, where a considerable number of small generating plants feed into a system, the net kilowatt load is not simultaneously available.

In attempting to reach the upper limit of unswitched capacitors which allows the generators to operate at unity power factor during light load as suggested by Mr. Butler, the total generator load power factor probably cannot exceed 95 per cent without certain generators having to operate at unity or leading power factors. However, in general, it is agreed that faster switching times and the use of generator voltage regulators will allow a greater use of unswitched capacitors at light load. With respect to the selection of synchronous condensers in preference to switched capacitors, this question has not been exhausted. The low installation cost of this first group of synchronous condensers in existing buildings was competitive with the cost of switched capacitors located at substations. A portion of the cost of the greater losses in synchronous condensers could be credited to building heating. The voltage-stabilizing characteristics of a synchronous condenser, as compared to a capacitor, came in for some consideration. The initial installation of synchronous condensers offered the possibility of later using the lagging capacity of the machines to regulate the installation of additional unswitched capacitors near their terminals. Some of these may be pertinent factors which Mr. Butler has overlooked. Nevertheless his contribution of a criterion on which to determine the proper proportion of different forms of kilovar supply is welcomed.

The proper proportion of switchable and nonswitchable static condensers, synchronous condensers, and kilovar loading on generators is largely an economic problem, including all factors of capital, operating, and maintenance costs. However, the problem of unstable voltage regulation which may be obtained under all conditions with the use of too much switchable and nonswitchable static condenser capacity must not be overlooked.

In regard to the relative importance of transient and steady-state stability discussed by Mr. Crary, all of the stability studies made to determine the proper kilowatt and kilovar loading of individual generators were based on the transient pull-out angle calculated from a regular a-c network analyzer set up since the steady-state pull-out of the closely coupled Public Service system has not appeared to be a problem. However, it is possible that steady-state conditions should be considered further in the light of Mr. Crary's comments.

As Mr. Dissmeyer points out, there is some temporary overload capacity in most generating units and these so-called short-time proved overload ratings are known, but to use them some sacrifice must be made in stability margins. Undoubtedly, these ratings will be used in extraordinary emergencies, but somewhere the last straw that breaks the camel's back, such as broken blading, is reached, and these ratings cannot be considered as determining the normal system capacity. The use of autotransformers and higher generated voltage firms up these ratings to a considerable extent from the standpoint of thermal and stability limitations. It is believed that full account has been taken of these overloads by planning for only 10 per cent reserve capacity, the lowest in the country, and if these emergency ratings were included in the balance sheet of load and capacity, reserves would probably need to be boosted to as much as 15 per cent. Therefore, no over-all net gain would be effected.

Progress in Design of Electrical Equipment for Large Diesel-Electric Locomotives

Discussion and author's closure of paper 42-23 by G. F. Smith, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 130–1.

R. Tom Sawyer (American Locomotive Company, New York, N. Y.): Mr. Gerald Smith's paper was an excellent paper. We have needed such a paper for some time to sum up the progress in the design of electrical equipment used on Diesel locomotives.

In the early days when the first Diesel-electric locomotives were built, traction motors designed for street cars or other uses were placed under the Diesel-electric locomotive. The insulation was poor, and the ratings very low. Today the traction motor is especially designed for Diesel-electric locomotive service and especially designed for the particular class of service utilized. Today, practically every piece of electrical equipment of the Diesel-electric locomotive is especially designed for its particular service—certainly a very marked step in the advancement of the Diesel-electric locomotive.

I should like to elaborate on Mr. Smith's paragraph on overloading capacity. We must agree that the traction motor of the Diesel-electric locomotive is still the vulnerable spot when it comes to overloading the locomotive. Tonnage ratings are based upon two factors.

1. The rating should be of the proper size in order to maintain the schedule.
2. The rating should stay within the limitations of the traction motors to prevent them from overheating.

For example, the American Locomotive Company and the General Electric Company have one survey department which calculates the performance of their Diesel-electric locomotives and thereby sets a tonnage rating of these locomotives for any given profile. Frequently these calculations have been checked with actual service conditions and have been found to be extremely close. When it is possible to calculate so closely, we find it advisable to not leave any leeway; that is, we expect the railroads to operate the locomotives at the tonnage rating given.

In Mr. Gerald Smith's conclusion he states that a freight locomotive may have twice as many traction motors as a passenger locomotive. This, of course, is generally the case; however, a passenger locomotive geared for 120 miles an hour can be converted into a freight locomotive and made to handle twice the tonnage simply by changing the gearing to 60 miles an hour. The New Haven Railroad is a good example; in this case the locomotives are geared for 80 miles per hour and handle both passenger and high-speed freight service.

G. F. Smith: It may be desirable to explain in detail why more motors are sometimes used on freight locomotives than on passenger locomotives. A 4,500-engine-horse-power locomotive geared for maximum of 80 miles per hour, with worn wheels, for operation on roads with 0.4 per cent (compensated) prevailing upgrades could haul 750-ton trailing load of light passenger cars at balancing speed of approximately 70 to 75 miles per hour, or 1,200 tons of heavy passenger cars at between 60 and 65 miles per hour, or 4,000 tons of freight trains at approximately 25 miles per hour. Such a locomotive would be very useful for passenger and freight service in rolling country with light grades.

If the number of motors is doubled, the locomotive weight would be increased by approximately 40 to 50 tons, and it could also haul freight trains of 4,000 tons on one per cent grades and 2,000 tons on 2.2 per cent grades at approximately half speed.

Doubling the number of motors without change in gear ratio permits operation of passenger and freight trains over a wider range of grades with corresponding reduced speeds on the heavy grades.

A Fast Circuit Breaker

Discussion and author's closure of paper 42-38 by D. I. Bohn and Otto Jensen, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, March section, pages 165–8.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): As a rectifier engineer, I feel greatly indebted to the two authors who were instrumental in developing a new breaker device for protecting mercury-arc rectifier systems.

In 1938 the Allis-Chalmers Manufacturing Company put into operation, at the Alcoa plant of the Aluminum Company of America, ten mercury-arc rectifier units, consisting of twenty 12-anode, 4,580-ampere tanks. These units were protected by standard oil circuit breakers on the primary side, and by high-speed d-c cathode breakers. After a few months of operation extensive tests were carried out to verify the computed characteristics of the circuit and the estimated currents obtained during possible short circuits and backfires. The tests confirmed our computations and also the fact, which was pointed out in the paper, that the increase in current takes place at the rate of over 5,000,000 amperes per second. In spite of the fact that the cathode breakers were able to limit the current in about half a cycle, the current during disturbances would sometimes exceed 50,000 amperes, and, in consequence, greatly stressing all the equipment connected to the system. Shortly after these tests were completed, the customer decided to enlarge this station to double capacity, but it was felt after analyzing the test data, that this high concentration of power should make it necessary to improve the protective means still further.

An investigation was started by our company, along the line of using grid blocking for further protection, and at the same time by Mr. Bohn of the Aluminum Company of America, to develop an anode breaker to be connected between the transformer and the rectifier. It was found that by means of grid protection, the internal short-circuit current in the rectifier, flowing during a backfire (see Figure 2 of the paper) could be interrupted in about one-sixth of a cycle. However, it was found difficult to increase the speed of the large-capacity cathode breaker, which during a backfire has to interrupt the current feeding from the parallel connected units into the faulty anode. In spite of the fact that at that time no suitable d-c breaker was available which could be used as an element of the anode breaker it was decided to develop such a breaker and introduce it into the anode circuits.

Furthermore, it was deemed advisable to equip the rectifier units for this new expansion with high-speed blocking by means of grids, and, in order to reduce the number of breaker elements, to use anode reactors in connection with a six-phase rectifier transformer. The application of anode breakers, and grid-blocking devices as used in this new station, designated as number 4, can be best seen from the paper entitled "Alcoa Rectifier Installations" by Mr. J. E. Housley and Mr. H. Winograd presented at the AIEE Southern District meeting in New Orleans, December 3, 1941.

It can be seen from Figure 2 that as each element of the breaker opens, the anodes are prevented one by one from firing and from feeding into the faulty anode. The protective action of this breaker is, therefore, similar to that action of the grid-blocking device by means of which, as in the first scheme, the anodes are prevented one by one from firing as soon as the grid is made negative. It can, therefore, be seen that both protective devices support each other in the clearing of disturbances which may be very advisable for some installations.

I would like briefly to enumerate some of the points which had to be taken into account when designing this new type of breaker and which are based on experiments made in the first installation referred to above. This will show that we do not here have a 600-volt d-c breaker, as superficial consideration may lead us to believe. From Figure 2 it can be seen that during operation between some of the anodes, a potential double the secondary phase voltage appears, and, therefore, the insulation between poles had to be designed for a voltage of 2,300 volts peak in order to take care of the increase in voltage in the interphase transformer during operation with grid voltage control.

Furthermore, when designing this breaker it had to be taken into account that during normal operation, the current may reach a maximum value of 2,500 amperes in about one fifteenth of a cycle and will drop to zero approximately at the same rate after one third of a cycle. During disturbances, the breaker was supposed to interrupt in about one cycle so that the current could not exceed 50,000 amperes, although it was estimated that the inductance of the circuit would be about one quarter of a millihenry. The tests also showed that such a breaker had to be able to interrupt the above current following a forward current of the magnitude of 25,000 amperes, and that during such interruptions a voltage of 3,000 volts was sometimes exceeded. This voltage had to be taken into account when the breaker was being designed, the reignition of the arc being borne in mind. The maximum interrupting time was set not to exceed one cycle, and it will be seen from Figure 11 that this value was reduced to 0.64 cycle, after further improvements were incorporated by the authors.

We feel, therefore, that a great step forward was made not only in the art of circuit interruption, but still more so in perfecting the protection of rectification systems for large blocks of power.

C. H. Black (General Electric Company, Philadelphia, Pa.) and **Lysle W. Morton** (General Electric Company, Schenectady, N. Y.): This paper is particularly significant to operators of rectifiers and to rectifier application and design engineers. The authors are to be complimented on the paper which treats of the development and design details of an ultrahigh-speed air circuit breaker.

As the rectifier art progressed during the past twenty years, the need for high-speed interrupting devices in the anode circuits gradually became apparent to many of the engineers interested in rectifiers. Desultory attempts were made to solve the problem from time to time, using then available medium-speed or semihigh-speed circuit breakers. However, these did not meet the requirements fully, and it became evident that what was needed was a high-speed multipole breaker equipment designed especially for use with mercury-arc rectifiers.

Some of the general specifications that such a breaker should meet may be listed as follows:

1. The individual poles must be trip-free.

2. Tripping action should be essentially for reverse current. The first and only always completely reliable evidence of occurrence of arc back in the circuit of a rectifier equipment is the reversal of current in the anode connections. Hence a polarized form of reverse-current trip is necessary to distinguish arc-back current from forward overcurrent.

3. High-speed current interruption should be provided both from the standpoint of time required to start reducing the overcurrent and time required to extinguish the arc. This requirement is dictated by several factors. When rectifiers are connected to the same d-c bus with other rectifiers or conversion apparatus, not only do other anodes of the affected rectifier feed reverse current into the faulted anode, but all of the other equipment connected to the bus may contribute current of high magnitude. The value of this current is limited only by the copper resistances of the circuits and the short-circuit impedances of the contributing equipment. For this reason, the rapidly increasing current must be interrupted before damage occurs to apparatus, and before it gets out of hand in magnitude.

In order to provide continuity of operation in larger installations, where several conversion units operate in parallel, the interrupting action should be fast enough to prevent associated equipment from going out of service due to overcurrent, and also prevent "sympathetic" or "simultaneous" arc backs on other rectifiers due to the short-circuit imposed. In many cases, two rectifiers are fed from a single transformer, and the anode circuit breaker, if its action is fast enough, may eliminate the faulty rectifier so quickly that the other rectifier may remain in service. These operational advantages may be obtained if high-speed anode breakers are used, by adjusting overcurrent devices associated with the a-c and d-c switchgear to obtain the proper selectivity.

4. Adequate insulation must be provided between the individual poles of the multipole circuit breakers to take care of the well-known voltages which occur in rectifier transformer circuits.

5. The complete multipole circuit-breaker equipment with its operating mechanism should be simple in construction and require minimum space. It should be so reliable in operation that it does not complicate or increase maintenance of the over-all rectifier installation.

The company with which we are associated is so thoroughly convinced of the real need for and advantages of high-speed anode circuit breakers that we have made available apparatus of this sort. Particularly, for rectifiers whose voltages are above 300, we regularly advocate and apply this type of high-speed switchgear.

One important fact, which we believe should be kept in mind, should be stated here. That is, high-speed switchgear in the anode circuits must not be considered a substitute for adequate a-c switchgear or other protective d-c switchgear. Arc backs are not the only faults which may be encountered with these equipments. In common with all other electrical apparatus, high-quality full-capacity a-c switches are still just as necessary to protect for such things as transformer ground faults, as ever. It is also just as necessary to provide d-c switchgear to protect for overloads and d-c system faults as ever.

D. I. Bohn: Mr. Morton's comments, in which he gave very complete specifications of the requirements of this type of device, need no further enlargement.

With reference to the last paragraph of his comment regarding the desirability of installing a full capacity a-c switch ahead of the rectifier transformer, this is largely a matter of opinion involving type of load, seriousness of infrequent short-time interruptions, and other angles which are essentially economic.

It, therefore, presents a problem apart from the rectifier and associated equipment, this protection being complete with the anode and cathode breakers themselves.

The particular scheme outlined in this paper furnishes adequate protection against transformer ground faults as well as overloads on the d-c system.

High-Capacity Circuit-Breaker Testing Station

Discussion and author's closure of paper 42-26 by J. B. MacNeill and W. B. Batten, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 49–53.

J. R. North (The Commonwealth and Southern Corporation, Jackson, Mich.): We agree wholeheartedly with the authors that methods of testing which merely approximate the breaker rating have very definite limitations. Our experience also has indicated that it is not always satisfactory to extrapolate interrupting data taken at less than rating to determine the full rating.

Circuit breakers may be considered essentially as current-interrupting devices, and it seems particularly important that interrupting devices be tested up to at least their full rating, especially interrupting devices which have a high-ampere interrupting rating. We feel very strongly that the selection and application of circuit-interrupting devices should be made only on the basis of authentic interrupting test data, which clearly demonstrate the ability of the devices to interrupt the duty expected at the actual operating service voltage.

We have been privileged to witness interrupting tests which demonstrated that the interrupting devices as designed and built easily met their rating, and this was subsequently substantiated by actual operating experience. On the other hand, several serious cases of trouble have resulted with devices which had not been adequately tested. For example, it was planned to modernize a group of breakers, and the design of the modernized parts was based on extrapolated data. Interrupting tests on a sample breaker at the proposed service voltage showed the performance to be unsatisfactory and prompted several important design changes. These tests thus proved very advantageous to both the manufacturer and the purchaser.

In another case, breakers which were to be used on service voltage considerably below their rated voltage were found on test to be inadequate to interrupt the higher short-circuit currents at the lower service voltage, although tests at the breaker rating had indicated satisfactory performance. Design changes were made which resulted in satisfactory operation at the lower service voltage and improved operation over the entire application range.

Comprehensive field tests and operating experience are invaluable supplements to factory tests; however, the number of tests which can be made in the field is generally very limited as compared to the number of tests which can be made in a factory test laboratory. Furthermore, in analyzing operating experience it must be remembered that circuit-interrupting devices are generally applied on the basis of short-circuit duty which it is expected will exist in the future, with adequate allowance for system development and growth. Thus for several years after installation a circuit breaker may not be called upon to interrupt currents anywhere near its rating, and the experience indication may therefore be of little value.

J. D. Wood (Roller-Smith Company, Bethlehem, Pa.): I wish to discuss the paper presented by Messrs. MacNeill and Batten describing the additions to their short-circuit testing station making possible the testing of high-capacity circuit interrupters.

Many of us can remember the time when manufacturers had no testing facilities, and design data were accumulated from experience on devices which were installed or an occasional short-circuit test conducted by a utility. These data were sketchy and conflicting, because they were collected under uncontrolled conditions, and the progress of the art was necessarily slow.

With the advent of permanent short-circuit testing facilities by manufacturers has come a period of rapid development. The advance in design and construction of modern circuit breakers is progress of which the engineers of this country can justly be proud.

The high-capacity station described in this paper will be found very useful, not only when testing larger breakers, but also in testing small breakers under conditions comparable with large concentrations of power, as found in present-day utility practice. It should be expected that these testing facilities will result in still further improvements in the apparatus offered for sale.

I would like to take just a moment to describe to you another type of testing station which has been built by one of the smaller manufacturing companies—Roller-Smith. It differs from previous practice in that it is designed to use power directly from a high-capacity bus on a utility property and is adapted for the testing of medium-size circuit interrupters. It is a permanent installation, entirely owned by the manufacturer and operated entirely under his supervision (Figure 1). As will be seen from Figure 1, the station takes power at 66,000 volts over a line approximately a quarter of a mile long, from a high-powered bus in a centralized switching station. With this arrangement voltage is maintained at a high value on the primary of the test transformer. Transformers are provided to step the voltage down to the desired test voltage. A backup breaker is provided to clear all faults, should any trouble develop in the device under test. Reactors with taps are provided to limit the short-circuit current to the desired value. Each of these is in a

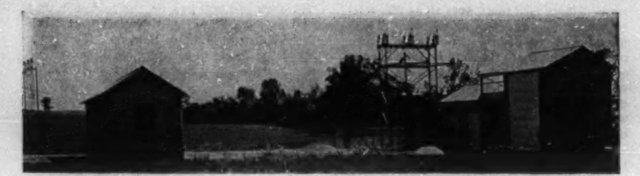


Figure 1

separate room, and a third room is provided as a test compartment. A separate building is provided for the oscillograph and other important recording and control devices sufficiently removed to provide safety for all operating personnel.

This station provides testing facilities from 300 volts to 23,000 volts, and up to 250,000 kva. A detailed description has been published in *Electrical World* (January 3, 1942).

I think the advantages of this testing station, located as it is, to subject breakers to the conditions usually found in an actual installation and to do it with a very economical setup, will be apparent to all of you. This station has just recently been completed, and something over 100 short-circuit tests have been made. The equipment for measuring transient voltage conditions is not yet completed. It can be expected that the station, operated in the future, will provide much interesting, useful data.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): Messrs. MacNeill and Batten have done an excellent job of providing in somewhat limited space for a large number and variety of the tests associated with the development of circuit breakers.

Most important among these is the short-circuit testing of circuit breakers approximately to full interrupting capacity. It is unquestionably desirable to be able to determine in the laboratory the interrupting capacity both of conventional circuit breakers and of breakers of new types. This can be done, as Messrs. MacNeill and Batten have done it, by providing sufficient generating capacity to make the test on a conventional circuit. It can also be done with somewhat less generating capacity by the use of a dual power supply, one part of which supplies high current for application during the arcing period, while the other part supplies the recovery transient and the sustained normal frequency voltage. This procedure has been described before the Institute in two previous papers, the discussion of which is quoted as a reference by Messrs. MacNeill and Batten, and further experience with it is mentioned in one of the papers presented earlier this morning.

These papers have shown that the circuits can be switched with sufficient speed and accuracy to realize a transient-recovery voltage rate of several thousand volts per microsecond. Such recovery rates have been read from the oscillogram on both oil and air-blast circuit breakers.

Theoretically, there is some modification of the current wave form as it approaches final zero. But, practically, with most modern breakers, an effective increase of several fold in the capacity of the testing plant can be obtained with a modification which is hardly perceptible. Furthermore, the tests' results may be read from the oscillogram in such a way that any error lies on the side of extra severity of the test, so that the net result is an increase in the breaker's margin of safety. Confirmation of this is given in the three comparisons cited in this morning's paper, which show the synthetic test to be at least as severe as the conventional circuit.

The performance of this circuit is illustrated by the oscillogram shown in Figure 2 of this discussion. In the early part of the film a rather low voltage is shown on the voltage trace, corresponding to the open-circuit voltage of the high-current circuit. This drops to zero when the short circuit is initiated, but, upon interruption, it rises to approximately double its initial value, corresponding to the voltage of the high-voltage circuit, and superimposed upon this is a recovery transient which rises almost to double the instantaneous normal frequency recovery voltage at the time of interruption. It will be observed also that the current wave is not perceptibly distorted.

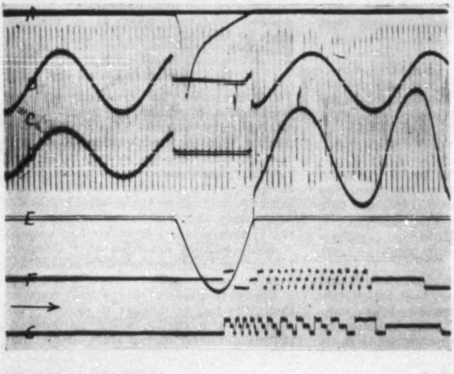


Figure 2. Oscillogram of a synthetic test

6,000/12,000 volts
33,000 amperes
A—Trip-coil current
B—Alternator voltage
C—Oil pressure below baffle
D—Recoveryvoltage
E—Breaker current
F—Piston travel
G—Contact travel

This circuit has been employed to good advantage in a number of important developments, and its use is expected to continue on the increase.

H. A. P. Langstaff (West Penn Power Company, Philadelphia, Pa.): The authors' description of the high-capacity circuit-breaker testing station at East Pittsburgh seems quite brief and limited to one who has seen the equipment in actual operation. Comparing this with the facilities and surroundings of the Wilmerding tests certainly shows the progress of the Westinghouse organization. Everything pertaining to this testing station seems to be about the last word and this removes practically all of the doubt in an operating man's mind as to the present-day ratings. In discussing this installation with members of our organization, none of us seems to have any suggestions or additional ideas which should be embodied in such a station. The results obtained give the operating men a great deal more confidence in the equipment; in fact, we believe that breakers today are gradually approaching the rating as I outlined at the time of the Wilmerding tests; namely, the standby rating or one such that the operator may stand by the breaker when operating at maximum rating.

W. S. Edsall (Allis-Chalmers Manufacturing Company, Boston, Mass.): In reference to the paper by Messrs. Strang and Skeats on field tests on high-capacity air-blast station-type circuit breakers, and also that by MacNeill and Batten on high-capacity circuit-breaker test stations, it is my opinion that the industry is now faced with the need of a more precise definition of circuit-interrupting factors and conditions, such as it faced almost 25 years ago. Then the famous Hewlett, Mahoney, and Burnham paper delivered here in February 1918, set up the methods of calculating short circuits to enable proper application of circuit breakers for interrupting duty. It pointed out, however, that there were many variables such as transient-recovery voltages and system conditions which could not be taken into account by a general formula, without making all designs so costly as to be economically prohibitive for general use.

Now we have interrupting-capacity testing stations and have a better grasp of these same variables of transient-recovery voltages and system conditions than we had 25 years ago, but the probability of adverse effects from these conditions is greater today, because of the much higher concentration of power to be handled.

The paper by Messrs. Strang and Skeats advocates the single-phase synthetic method of interrupting tests on circuit breakers, while MacNeill and Batten are satisfied with nothing less than a three-phase full-capacity test. Actually, the one great difference between these methods is a question as to the severity of circuit conditions such as transient-recovery voltage. Up to date there is no exact definition or yardstick of transient-recovery voltage against which a commercial circuit breaker must perform on interrupting tests.

In considering plans for extension of our own interrupting testing laboratory, we feel the need of a specification to tell just what we must test for, so far as rate of recovery voltage rise is concerned. It seems that a common basis of test standards and methods must be arrived at. Perhaps as a preliminary step, the best thing to do would be for the General Electric engineers to take their air-blast circuit breaker and test it in the Westinghouse company's laboratory, with the Westinghouse standards of measurement, and for the Westinghouse Company to do the same with their breaker, with the General Electric Company's standards of measurements. I might also add that we might also contribute to such an exchange of breaker and laboratories. The results, I am sure, would make a most fascinating Institute paper, and would give data to establish a sound basis of circuit-breaker testing and application.

J. B. MacNeill: The writers of this paper wish to thank those who have discussed the project, several of whom have visited the laboratory and witnessed tests therein.

A high-power laboratory such as described in this paper requires, in addition to a large first cost, a considerable maintenance and operating expense and occupies space that would be valuable for other purposes. The Westinghouse Company in providing these facilities believes that the investment is justified by improved switchgear and similar electrical equipment resulting from the laboratory operation.

Until the industry grew to a point where this expensive equipment could be absorbed in reasonable costs, it was necessary to rely on occasional tests on operating systems. This was not a satisfactory arrangement for several reasons:

1. Tests could only be made in small numbers and when system conditions provided a minimum of hazard to connected machinery and personnel.
2. The design of circuit-interrupting devices was incomplete, and development tests in large numbers rather than proving tests were required.
3. Available short-circuit concentrations were below the requirements of a growing industry.

The most comprehensive set of field tests ever made on an operating system were those on the Consolidated Gas, Electric Light, and Power Company lines at Baltimore during 1920 and 1921. A total of 200 short circuits in capacities up to 500,000 kva was made on approximately ten different types and sizes of circuit breakers. The cost to manufacturers and operating companies of running the tests probably was not less than $150,000, and the results in improved apparatus were considerable.

Looking back at these tests we realize that the results were in some cases extrapolated incorrectly to meet other conditions for which data were not available. To illustrate: These field tests were all at 25 cycles, and certain conclusions were drawn regarding 60-cycle operation, which were inaccurate. Again, the tests were at 13,000 volts, and subsequent work extrapolated the results to 25 kv with rather disastrous consequences. Again, restored voltage transients were in most cases relatively low and resulted in inaccurate findings.

However, these tests marked a great advance over previous data and the 1,500,000-kva breakers at Hell Gate and other large stations supplied during the 1920's were a result of this work. They have given satisfactory service, and the Hell Gate breakers today are operating without essential change from the time they were installed in 1921.

In contrast today we can under some conditions make 100 tests in an eight-hour shift. Large numbers of tests are necessary, since the variety of conditions which arise with transient troubles on a power system is very great, and the switches must be good for all. On one difficult design we made 3,000 short-circuit tests. This took a period of months, but we were analyzing what went on during $1^{1}/_{2}$ cycles while the breaker operated. The total elapsed time we were studying then for the whole 3,000 tests was only a minute and a quarter.

This explains why the laboratory described in this paper has already been subjected to over 100,000 tests. On the other hand, all the high-power switchgear tests made on all the operating systems in the country over the 20-year period from 1920 to 1940 probably did not exceed 1,500. We see then that the development of modern switchgear is quite impossible without this expensive and rugged tool called "a high-power laboratory."

But you may say: "Even if you have a Laboratory, why does it need to be so large?" The answer is found in the requirements actually being placed before us today for switchgear performance. One customer is changing existing breakers to 3,500,000 kva. Another has just split his system in two to keep within a limitation of 2,500,000 kva.; shortly, he will need larger capacity. A third is now operating with bad voltage regulation, because his switching capacity will not permit adding ties to his system which would increase the interrupting duty. In the not too distant future 3,500,000 kva will be necessary at our more important transmission voltages rather generally. The laboratory here described is the most adequate one in the world to meet these requirements.

Field Tests on High-Capacity Station Circuit Breakers

Discussion of paper 42-12 by H. D. Braley, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 31–5.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): We are all impressed, I am sure, with the magnitude of the undertaking related by Mr. Braley in shorting the Hellgate Bus with 294,000 kva on the test stub and tied in with the rest of Hellgate through Waterside and Niagara-Hudson. This is by far the greatest concentration of power for such purpose ever put together at 13 kv for a field test.

Previous circuit-breaker tests at station voltage have given 25,000 to 30,000 amperes. New York got 65,000 amperes in the arc on one shot. However, the Baltimore tests in 1920, giving 25,000 amperes, resulted in the circuit-breaker design now in use at Hellgate, and these breakers have been satisfactory. It seems reasonable then to assume that these later tests should provide data on which adequate designs for the future can be based.

The paper itself makes clear the precautions taken before the tests and relates certain incidents which show the necessity for care in such a project.

The phraseology is excellent in places and carries plenty of meaning to those of us who have witnessed field tests over a period of years—"The psychological effect of the flash and the accompanying sound effect, in connection with the arc in the test breaker, undoubtedly lent some emphasis to the need for more caution." This could hardly be improved upon. As to the advisability of field tests on circuit breakers, there may be a difference of opinion in some cases. Practically all such tests so far have been worth-while in the development of the art, certainly the Hellgate tests described by Mr. Braley resulted in desirable improvement which, otherwise, might not have been so promptly made. However, as currents increase, the stresses between bus runs, and so on, go up at the current square, and the possibility of damage also increases. Perhaps then it is not reasonable to expect tests on utility systems much beyond those here related.

A particularly valuable type of field testing during the last two or three years has been the work done at 220 kv, some of which has been described to the Institute. There are certain phenomena connected with extremely high-voltage switching such as arc prestriking, restriking tendencies, and characteristics of charging current opening, which are not present in lower-voltage work, and some of which cannot be simulated exactly in laboratories at this time. The knowledge of these factors gained recently in field tests at 220 kv has been a most valuable contribution of that close co-operation between designer and user which is so much in evidence in these papers you are here considering.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The thorough advance planning by those familiar with the details of design, application, construction, and operation, unquestionably played a major part in the success of the short-circuit tests described by the author. Aside from the principal objective of the tests, the discovery of weak points in the system protection, which had unquestionably escaped detection after adequate low-voltage tests, was probably of sufficient value to justify making them.

Careful planning of short-circuit tests[1] reduces the risk of damage to sound equipment to a negligible value. Unforeseen defects disclosed during short-circuit tests are not likely to cause serious system trouble, since a high concentration of specially trained personnel will be on guard at many points. The same unforeseen difficulty, if allowed to persist until an accidental system fault occurs, may result in serious service and equipment troubles.

Material gains are usually associated with appreciable risks, but the very low ratio of risk and benefit which is obtained with short-circuit tests is not so widely appreciated as it deserves. The results of many short-circuit tests made in the past were not published, probably for reasons other than those concerned with the technique and value of the test procedure. It is suggested that the appropriate AIEE subcommittee might wish to compile an informal list (from the information which may be readily collected) of short-circuit tests performed during the past few years, giving names of companies, dates, and a brief statement of the nature of the tests.

REFERENCE

1. TESTING OF HIGH-SPEED DISTANCE RELAYS, E. E. George. AIEE TRANSACTIONS, volume 52, 1933, page 802.

R. L. Webb (Consolidated Edison Company of New York, Inc., New York, N. Y.): With reference to the paper by Mr. Braley describing the setup for field tests on a 15-kv indoor air-blast circuit breaker, it may be of interest to know what recovery voltages were maintained.

Mr. Braley has described the circuit connections in detail. It will be noted that the breaker test house had to be located at a position where it was necessary to use two three-conductor lead-covered cables in parallel for the main test circuit. Each cable was approximately 600 feet long. Also, this connection contained no lumped reactance near the test breaker, since it was desired to obtain the highest short-circuit current that could be provided with the available capacity.

The circuit constants, particularly the cable capacitance, were such that only low recovery-voltage rates could be expected.

Table I of this discussion shows the current in the first phase to open, the approximate short-circuit kilvolt-amperes on a three-phase basis, the calculated recovery-voltage rate and the recovery-voltage rate obtained during the tests with the cathode-ray oscillograph. These are given for the 1941 tests only, though the values obtained in 1940 were of the same order of magnitude. The recovery voltages on all the tests were lower than those expected in an application of these breakers at Sherman Creek station. At this location the feeder breaker recovery-voltage rate has been calculated to have a maximum value of 2,400 volts per microsecond, on 500 megavolt-ampere breakers.

Table I. Field Tests on High-Capacity Station Circuit Breakers Calculated and Actual Test Recovery-Voltage Results

			Rate of Rise Recovery Voltage Volts Per Microsecond	
Test	Current Amperes*	Approximate 3-Phase Short Circuit-Kva	Calculated	Actual Test
1-A	24,000	600,000	270	230
2-A	22,000	580,000	270	250
3-B	47,000	1,200,000	360	310
4-B	47,000	1,180,000	360	390
5-C	62,000	1,560,000	330	290
6-C	58,000	1,480,000	330	250
7-D	26,000	615,000**	210	160

*Initial arc current in first phase to open.

**Line to ground test—kva is equivalent three-phase value.

The maximum recovery-voltage rate expected on 1,500-megavolt-ampere and 2,500-megavolt-ampere main bus and bus-tie breakers, in the same station, is indicated by calculation to be 4,600 volts per microsecond. We, of course, expect that the breakers will handle these recovery rates.

The main test circuits were set up a second time, with no voltage applied, to permit the General Electric Company to apply low voltage tests with their recovery voltage analyzer.[1]

We are informed the results agree substantially with the cathode-ray oscillograph records obtained on the actual tests.

REFERENCE

1. THE RECOVERY-VOLTAGE ANALYZER FOR DETERMINATION OF CIRCUIT RECOVERY CHARACTERISTICS, G. W. Dunlap. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 958-62.

Field Tests on High-Capacity Air-Blast Station-Type Circuit Breakers

Discussion and authors' closure of paper 42-30 by H. E. Strang and W. F. Skeats, presented at the AIEE winter convention, New York, N. Y., January 26-30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 100-04.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Strang and Skeats outline very well the actual development in the factory and field of a large compressed-air breaker. The design is neat and compact and lends itself well to station layout. The tests referred to are up to the rating of 1,500,000 kva under discussion.

Since breakers of the same type of 2,500,000 kva are being installed shortly in New York, a statement of the bearing of this work on the higher rating would be of interest to designers and users.

The authors discuss the synthetic method at some length as corroborating the results of field tests. Since this method was made public several years ago, I assume it was available before the first series of Hellgate tests. Is this a reasonable assumption, and if so was it used before these tests?

A fair statement of the limitation of the synthetic circuit is made by Strang and Skeats. They dispose of these limitations rather too briefly however. The interpretation of test results referred to in the paper detracts from the value of the device as a demonstration of ratings. Again, Figure 4 of the paper on recovery voltage transients shows three curves, none of which exceeds at any point the normal open circuit voltage. Are we to understand that this is typical of the synthetic circuit?

On the breaker design itself, which in general is an excellent physical structure, one major consideration is the tortuous air path. The compressed air starts on top of the structure, proceeds through relatively small tubes to the interrupters, changes here from vertical flow to generally horizontal flow, and then at the exhaust changes again to vertical flow. This results in loss of air efficiency against a straight air-flow device and no doubt is the reason that recourse has been taken to 250 pounds pressure. It has been found possible to retain the 150 pounds pressure tentatively agreed to by the industry for indoor applications by an improved design of the air passages so that the lower pressure is adequate. A discussion by the authors of this point would be appreciated.

L. R. Ludwig (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors of this paper have devoted considerable attention to methods of circuit-breaker testing, particularly with respect to compressed-air breakers. These devices are comparatively new to American engineers, and consequently special interrupting tests have more than the usual importance in proving the interrupting adequacy of compressed-air designs.

Long time study of the problem of circuit interruption has proved that, to establish the interrupting ability of a particular circuit breaker, the following sequence of breaker and circuit action must be satisfactorily completed:

1. The breaker contacts must part and form an arc.
2. The current in this arc must be equal to the current interrupting rating.
3. Immediately following current zero, recovery voltage must be applied across the breaker contacts as a function of time. The rate of application of recovery voltage must at least equal that of the actual service circuit as modified by the conduction of the breaker itself after current zero.
4. During the application of recovery voltage, the conduction current in the breaker must not be limited by the inadequacy of the test circuit.

The methods of testing the breakers described by the authors are,

1. Tests in the field.
2. Tests using a synthetic circuit of presumably equal severity.

The field tests as described in the paper, "Field Tests on High-Capacity Station-Type Breakers" by H. D. Braley, were made with the breaker set up in a special sandbag structure in the yard so that approximately 600 feet of cable were used to connect the breaker to the station bus. It is recognized, of course, that this type of test setup was essential for safety. Unfortunately, however, the effect of this cable is to greatly modify the circuit voltage-recovery rates in favor of the breaker. Therefore, it appears that the previously mentioned condition 3 for demonstrating interrupting ability is not necessarily met. For example, in actual installation of a 1,500,000-kva breaker a reactor is often used adjacent to it, and the circuit recovery-voltage rate may reach 12,000 volts per microsecond. Even without the reactor the recovery-voltage rate as determined by the generators may reach 3,000 volts per microsecond and possess sufficient energy so that the damping effect of the circuit breaker is negligible. With the breaker and circuit set as described, however, calculations indicate that the circuit voltage-recovery rate would be only 400 volts per microsecond. This is so very different from the rate if the breaker were actually installed in its own cell that one must raise the question seriously as to the complete proof of the breaker performance for all types of circuits.

I would like to ask the authors if they have cathode-ray oscillograms and voltage-recovery rates of the field test conditions and what values of recovery voltage were obtained.

We have deliberately modified circuit recovery-voltage conditions while testing compressed-air breakers and have found considerably more difficulty in building the breaker to satisfactorily interrupt circuits having high recovery-voltage rates.

The synthetic method of testing is a difficult one to control when attempting to meet the four conditions required to prove adequate interrupting ability. Equivalent voltage-recovery transients can only be established in the synthetic circuit so long as the breaker has not just conducted a high current. We have found that the compressed-air breaker differs quantitatively from other types of interrupters in that

1. **It is strongly affected by the rate of rise of recovery voltage.**

2. **It passes many amperes leakage current following normal current zero.**

Therefore, the breaker itself can and does modify the recovery transient originally established by the circuit alone, whether it be real or synthetic. In the real circuit, this modification is simply accepted. However, with the synthetic circuit, which does not have comparable impedance, how is one to judge that the breaker is subjected to the equivalent duty of resisting construction? The authors have offered no criterion for isolating the effect of the circuit on the arc space of the breaker. The leakage current increases greatly with the kilovolt-amperes to be interrupted, and, in consequence, a check of synthetic test methods with laboratory tests at say 500,000 kva does not imply that the methods will also check properly at 1,500,000 kva.

The authors state, with respect to Figure 4 of the paper, that one of the cathode-ray oscillograms—which unfortunately is reproduced without time or voltage scales—indicates a failure of the breaker to interrupt. There is no definite fall of voltage to arc-drop value and associated evidence of a continuous arc in the breaker, however. It may be that the greatly modified recovery-voltage transient is simply the result of large conduction current in the breaker. One cannot be certain. "Failure" may or may not have occurred. But if the circuit had been real, no room for doubt would exist.

The authors refer to interruption, in conjunction with the synthetic test circuit, associated with "high recovery rates." They have given no specific rates, however, and further definite data would be appreciated.

The authors' third conclusion calls attention to the close agreement demonstrated between the factory synthetic test and the field interrupting performance. However, the reasonable question regarding the sufficiency of either set of tests, with respect to recovery voltage, still leaves a great deal to be desired before accepting the check as proof of the breakers' performance.

H. V. Nye (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The paper by Strang and Skeats on a breaker of such similar type, voltage, and capacity is interesting, not only because of the method of testing used, but also because of the differences in arcing time and break distance shown by the two tests. One can hardly be blamed for wondering whether the lower arcing times shown in the latter paper are due to the test method employed, or to the higher air pressure used. Certainly my experience leads me to agree with the statement in this paper that a higher pressure does increase the margin of safety in insuring a consistently short arcing time.

In conclusion, it may be interesting to note that my company is at the present time constructing two outdoor air-blast breakers which will be put in service on a 13,800-volt system requiring an interrupting capacity of 1,000,000 kva, although the breakers are insulated for $34^{1}/_{2}$ kv. We all hope that the operating experience with the large capacity air-blast breakers described in these papers will bear out the promise of the laboratory tests which have produced them, and warrant a more general acceptance of the compressed-air breaker.

Joseph Slepian (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In spite of the scarcity with which actual test data are given in this paper, some interesting conclusions can be drawn as to the properties of arcs near current zero in actual circuit breakers, and as to the value of the proposed so-called synthetic tests. Concerning the synthetic tests, a conclusion completely opposite to that of the authors' conclusion 3 should be arrived at.

The idea behind synthetic tests is that after current zero the arcing space presents predominantly dielectric properties and only needs to be given a dielectric-strength test at sufficiently high voltage to prove its adequacy for bearing normal circuit voltage. The arc space after current zero, however, differs from usual insulating materials in that just before current zero it has been carrying heavy current and has had a very high conductivity. One certainly should expect, therefore, that just after current zero there might still be some conductivity left, and therefore, in carrying out a synthetic test one should be sure that the dielectric-strength testing means has sufficient capacity to really impress voltage on the space in spite of the dielectric leakage current. Everybody knows that an ordinary "shooting box" cannot test leaky insulation.

This point, along with other points of equal importance which are not being repeated in this discussion, has been raised before and was met with the statement that in the circuit breakers being tested by the advocates of the synthetic test, because of a displacement principle, the arc space was occupied by fresh insulating material which had practically zero conductivity or leakage, right up to its dielectric failure point.

But now, we have presented in this paper actual experimental information as to the leakage resistance of the arc space after current zero in an actual circuit breaker of the displacement principle type.

For, by a lucky chance, the circuit used, while not at all a test on the interrupting capacity of the breaker, is just the kind of a circuit which one would devise if one set out with the research problem of measuring the leakage resistance of the arc space after current zero. With the cathode-ray oscillograph the voltage impressed on the arc space by the test circuit is determined; from this the voltage drop on the series resistance of the test circuit and, therefore, the current supplied by the test circuit becomes known; and with voltage and current in the arc space known, its resistance is determined.

So we look at Figure 4 of the paper and particularly curve *C* with great interest. This is said to be for "breaker failing." This conclusion is not justified. The arc space as the oscillogram shows, is quite able to bear full voltage as fast as the synthetic circuit is able to supply it. If it represents the failure of anything, it is the failure, because of insufficient capacity, of the synthetic circuit to give an adequate dielectric-breakdown test on the arc space. But it does very nicely give us information on the leakage resistance of the arc space, after current zero. It shows that under the circumstance of that test, and where, therefore, the breaker was operating satisfactorily in a conventional circuit, presumably on the displacement principle, the arc space resistance begins by being small compared to the series resistance of the test circuit and becomes large compared to this resistance only after three divisions on the time scale are shown. The authors do not give us this time scale, but I guess that the time involved is several hundred microseconds!

Now the series resistance of the test circuit, according to the data given in the paper, was varied from 300 ohms to 3,000 ohms. Hence we conclude that the arc space resistance was of this order of magnitude for a length of time after current zero corresponding to three divisions of the time scale of Figure 4.

But this contradicts the displacement principle!

In Figure 5, the "border-line–synthetic tests" curve marks only the limits of the sensitivity of the synthetic circuit for measuring the resistance of the arc space. It gives no information whatsoever, of the capacity of the breaker for clearing a conventional circuit.

W. S. Edsall: See discussion, page 407–08.

H. E. Strang and **W. F. Skeats:** The discussion of this paper divides itself readily into two parts: that concerning the breaker itself, and that primarily concerned with the synthetic testing circuit. That regarding the breaker will be considered first.

As Mr. Ludwig calculates, the recovery rate in the field tests was comparatively low. Supplementary tests have been made, however, with very much higher recovery rates.

Mr. MacNeill asks about the significance of the field tests with reference to the 2,500,000-kva breaker. In the course of a development such as this, many tests are made, independently varying such factors as pressure, recovery rate, contact speed, shape, number, and location of barriers and coolers, and so forth. When the design of the 1,500,000-kva breaker was arrived at, it was subjected to a large number of synthetic tests to determine its performance under controlled conditions. After the field tests were completed, these results were compared with the factory test results under similar conditions, and it was found that the field performance, if anything, was slightly better than would have been expected, based on the synthetic test background.

The close agreement between the full-capacity field tests and the synthetic tests under similar conditions up to the 1,500,000-kva duty established beyond doubt the validity of this method of testing. The results of the synthetic tests are interpreted at their face value with the knowledge that the test procedure is at least as severe as service conditions and may result in a slightly increased factor of safety.

With this benchmark established, the development of the 2,500,000-kva breaker proceeded on the same basis. As was to be expected, an increase in duty of 67 per cent required some changes in design before satisfactory results were obtained, but the basic principle of determining performance applies to this rating as well as to the smaller sizes.

Before accepting the advantages which the configuration of this breaker gives in station arrangement and design, a careful study was made of the effect, if any, of the so-called "tortuous air paths" leading from the air tank to the contacts and from there to the exhaust. Vague references are frequently made to the effect that bends or restrictions in air passages introduce losses, so that the problem remained one of determining the magnitude of such losses. Obviously they must be confined to two kinds, both of which are susceptible of measurement.

(a) Loss of pressure.
(b) Loss of time.

Figure 1 of this discussion shows schematically the principal functional parts of such an air-blast breaker system: the tank, blast valve, pipe, manifold, and chutes. The chart shows results of actual measurements of pressure at various parts along this system. The lower curve was taken with an early design of blast valve, and shows over 40 per cent drop in the valve itself, compared with about 8 per cent in the "tortuous paths." An improved blast valve gave results shown in the upper curve. The drop across the valve itself is greatly reduced but remains about twice the drop through all the rest of the system. Blast valves are an essential part of any air-blast breaker system, and their design must be such as to reduce this drop to a minimum. However, actual tests have proved conclusively that the drop in pressure resulting from the bends in the air paths is negligible.

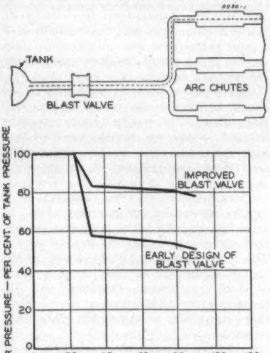


Figure 1. Pressure losses in blast valve and piping

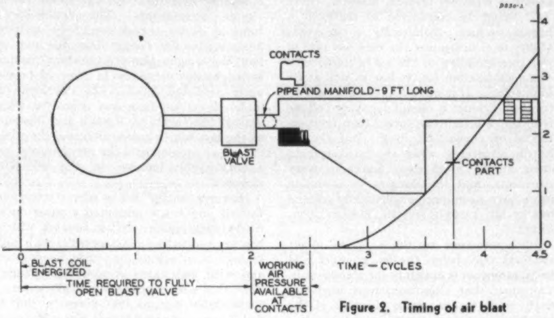

D850-2
CONTACTS
PIPE AND MANIFOLD - 9 FT LONG
BLAST VALVE
CONTACTS PART
0
1
2
3
4
4.5
TIME — CYCLES
BLAST COIL ENERGIZED
TIME REQUIRED TO FULLY OPEN BLAST VALVE
WORKING AIR PRESSURE AVAILABLE AT CONTACTS
0
1
2
3
4

Figure 2. Timing of air blast

Figure 2 of this discussion also portrays schematically the principal functional parts, in this case plotted to a time scale. From the blast valve on to the right, reference to the scale below will indicate the time at which normal working air pressure arrives at the various parts. Something in the order of two cycles from the time the trip coil is energized is required in this particular breaker for the blast valve to fully open. Less than a half cycle later full air pressure has arrived at the arcing contacts, after having travelled down the backpipe and along the "tortuous paths" through the header to the contacts. Blade travel is indicated at the right and shows that over a cycle later the contacts part, drawing the arc. In other words, full working air pressure has been available at the contacts in this design for a full cycle before the arc is started.

From such a study, based on actual measurements and not on surmises or speculations, it has been proved that this arrangement is not responsible for any substantial losses either in pressure or time, and that compared with the outstanding advantages which it affords in the way of simple and flexible terminal arrangements, such losses as do occur are unimportant.

A large part of the criticism of the synthetic circuit is based on the effect of leakage current after the final current zero. Up to a certain point the analysis on which this criticism is based is sound. The authors, too, have observed leakage conduction after current zero, and certainly this will affect the recovery transient. Certainly also the resulting difference between the ideal circuit recovery transient and the test transient actually realized will be greater with the relatively high impedance circuit which supplies the recovery voltage in the synthetic circuit which was used than in the low impedance circuit corresponding to field duty at equivalent kilovolt-amperes and recovery rate.

The authors agree, therefore, that it may be very misleading with the circuit shown in the paper to determine the ideal circuit recovery rate for the synthetic circuit, then make tests and, if the tests are successful, assume that the breaker will clear satisfactorily a field circuit with the same kilovolt-amperes and recovery rate. Because of the unsoundness of this procedure, the authors have shunned it, relying instead only upon recovery transients actually observed on the cathode-ray oscillogram taken at the time of the test. Comparing these with unmodified circuit recovery transients to be anticipated in the field is a very much more reliable procedure. It is based on the following logic: Suppose that upon interruption of a given current, a certain test recovery-voltage transient has been obtained on a synthetic laboratory test and recorded on the oscillogram. Now suppose the breaker on which this transient was obtained is to be applied in the field at the same current and on a circuit incapable of producing a recovery-voltage transient which exceeds at any time the test transient recorded on the oscillogram in the laboratory. The application of this circuit recovery transient can certainly generate in the arc path no more heat due to leakage current than is generated by the leakage current accompanying the test transient. The arc path will, therefore, gain dielectric strength at least as rapidly under the field conditions as under the laboratory conditions and, hence, will interrupt just as successfully.

Mr. Ludwig states that there is no definite fall of voltage in curve "*C*" of Figure 4 of the paper, which represents a breaker failure on the synthetic circuit. Apparently he has overlooked the substantially vertical line dropping practically to zero from the "*A*" and "*B*" curves about one millimeter to the right of the voltage axis. This is a part of the "*C*" curve and is the definite fall which Mr. Ludwig could not find.

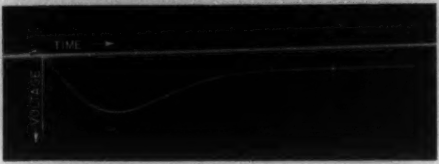


Figure 3. Recovery transient obtained on synthetic circuit showing overshoot to 2.7 times normal

Even if this definite fall and the rise preceding it were not present, however, curve "*C*" would be considered to represent a breaker failure. Admittedly it represents ability to clear against the very low rate of rise corresponding to the curve itself, with due consideration for its low as well as its high points. It might even represent ability to clear against a circuit recovery-voltage transient a little more severe than is represented by curve "*C*" itself. But that is hardly of interest when the breaker must clear against much more severe recovery transients, and the dangers in assuming more have been quite emphatically pointed out by Mr. Ludwig and Dr. Slepian themselves.

With reference to the question about synthetic tests before the first group of field tests, attention is drawn to the statement in the paper that alterations were inadvertently made in the arcing chamber of the breaker just prior to the field tests and after the synthetic tests at the factory. Under these circumstances, any comparison of the field tests with tests made in the factory would obviously have little significance.

One of the attractive features of the synthetic testing scheme is the flexibility with reference to the recovery-voltage transient that results from separating the high-current circuit and the recovery-voltage circuit. Among other things, this makes possible overshoots not only up to twice the instantaneous value of the normal frequency wave, the highest ordinarily encountered in the field, but far beyond in case that should be required. Figure 3 of this discussion shows an oscillogram on which the overshoot reaches 2.7 times the instantaneous normal frequency value.

It is believed that the synthetic testing procedure will find a field of great usefulness both in proof tests on circuit breakers and, as Dr. Slepian has suggested, in telling us more of the fundamental properties of an arc.

Field Tests and Performance of a High-Speed 138-Kv Air-Blast Circuit Breaker

Discussion and authors' closure of paper 42-9 by Philip Sporn and H. E. Strang, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 1–6.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The Sporn–Strang paper makes an important contribution to our American literature on high-speed oilless circuit interrupters. The double-break form of device tested should be entirely adequate for the ratings discussed and, in fact, the complication of a two-break device seems hardly necessary in view of previously published work. The development under discussion here was presented with limited test values by Rankin and Bennett at the last winter convention, and the present paper supplements the previous one by a considerable increase in test kilovolt-amperes.

However, at the last winter convention Ludwig and Baker presented a paper on a single-break compressed-air breaker which had been tested to 8,900 amperes with 132 kv impressed across the single interrupter and with high rates of recovery voltage. Since this value represents kilovolt-amperes considerably beyond that given by Sporn and Strang, one wonders if the additional complication of two breaks is justified.

The review of circuit-breaker development given in the early part of this paper is of interest, as it shows the considerable effort on the part of one operating company to advance switchgear design. Equally important work was conducted at somewhat earlier dates on other properties. Thus the AIEE paper by Baker and Wilcox presented in January 1930 dealt with field tests with 66 kv across a single-pole unit that uniformly gave two cycles or less of arcing, and, when equipped with a mechanism of normal speed, would give less than eight cycles of breaker operating time. Further results of field tests reported by L. W. Dyer in *Electrical World* for April 19, 1930, gave additional data on the Duquesne Light Company system tests of October 12, 1929, and included tests on the Alabama Power system up to 1,170,000 kva at 110 kv with arcing time well below eight-cycle breaker levels.

An especially important contribution was the Plymouth meeting tests on the Philadelphia Electric Company system of November 1929 in which eight-cycle breaker time was met on 220 kv.

These relatively early tests, including those referred to in the Sporn-Strang paper, have in the past been lifesavers in determining the adequacy of interrupting devices. Of late years, however, field tests have been relatively infrequent, as manufacturers' laboratories have demonstrated their ability to handle the test situation. In fact several important utilities who have been approached on the subject of field tests have not felt the hazard to their systems was justified.

The writer is of the opinion that in some respects factory tests against adequate power form a more difficult basis of approval than do field tests. For instance, the Ludwig-Baker paper reported circuit transient-recovery voltage rates as high as 3,420 volts per microsecond. We know of no field location where similar recovery rates have been had on circuit-breaker tests at high voltage.

The two methods of conducting interrupting tests both have advantages. The factory test can be made conveniently without hazard to a large operating system and in a minimum of time. The field tests on the other hand have a large value in operating experience gained during the tests, which points out possible weaknesses in operating setups under fault conditions as well as demonstrating the device on test. Therefore, for many of us the discussion of system effects given by Sporn and Strang is of particular interest and is conclusive evidence of the desirability of high-speed switching for heavy power concentrations.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I am sure that all of us are glad to see the progress and development of air-blast breakers, for none of us wants to use oil in electrical equipment, particularly in circuit breakers.

We have used it, it is true, and are still using it, but we just don't want oil circuit breakers.

The paper by Sporn and Strang covers the disadvantage of oil circuit breakers in a very clear and complete manner, and no repetition is necessary.

The data presented in this paper, as well as in the associated papers at this technical session, certainly give encouraged hope that the *great day* is nearing when we can buy at a reasonable price circuit breakers without oil for *all our requirements*.

We utility people want them, the steel mills, mines, and industrial plants want them, and the manufacturers want to build them; and all of us in the electrical business have an investment problem in writing off our oil circuit breaker and associated investments, and manufacturing plant therefore.

I would like to emphasize very strongly the need for continuing with the development of these breakers without oil and would like to show one slide, Figure 1, portraying the unsatisfactory performance of oil circuit breakers during tests to destruction, just to impress upon you the possibilities of a great catastrophe that might result from the use of oil circuit breakers, particularly beyond their capacity.

This Figure 1 shows a picture of an oil circuit breaker being tested to destruction. This oil throw with its accompanying fire and smoke was several hundred feet high and quite wide, as you can readily see. Such an explosion in an operating station would be intolerable.

There were many types and makes of breakers tested by our company about 15 years ago, and this entire test program covered duties up to 500,000 kva at 24 kv. This test program was arranged and carried through quite satisfactorily, even in spite of these spectacular explosions, and the breakers were tested below, at, and beyond their capacities to determine their performance.

I might say, as all of you familiar with oil circuit breakers will realize, that such breakers are not on the market now and you couldn't buy them if you tried. I doubt if any of you have breakers similar

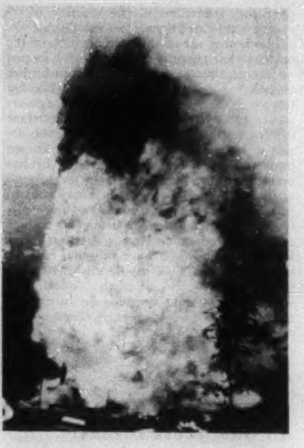


Figure 1. Fire and explosion resulting from breaker failing to interrupt a heavy short circuit

to these (I am not going to tell you what they are), for they have undoubtedly been fixed up so that they are really oil circuit breakers now.

The photo just illustrates something that might happen as a result of an oil circuit breaker attempting to interrupt larger and larger duties due to the system behind them growing. Even with the very best of maintenance and attention, an oil breaker is something we just don't want to keep around.

While these comments are referred to Mr. Sporn's paper, they apply equally to the companion papers on circuit breakers presented at this meeting.

W. S. Edsall (Allis-Chalmers Manufacturing Company, Boston, Mass.): Four years ago the company with which I am connected conducted its first commercial witness interrupting-capacity tests on a 15,000-volt, 250,000-kva air-blast circuit breaker. Three years ago, we made the first installation of a 15-kv, 500,000-kva air-blast circuit breaker. At that time we advocated compressed air for power circuit breakers. Then we were alone in our recommendations of the air-blast principle. Now we have a lot of good company, as is evidenced by the papers presented here today. I congratulate the engineers who have presented papers here at this session on the air-blast circuit breaker they have produced. While the different designs have much merit, the outstanding fact is that we manufacturers are all seemingly in agreement that the air-blast principle is safe, sound, practical, and efficient, as is amply demonstrated by the laboratory and field tests presented today, and by the thousands of interrupting tests made in our own laboratories. It seems we were also in agreement that compressed air is the only medium other than oil which can be applied over a wide scope of interrupting and voltage ratings. Air-blast breakers as manufactured by ourselves and by foreign manufacturers with whom we have been associated for many years are in operation today in this country and in Canada, extending from 100,000 kva at 2,300 volts indoors, up to 2,500,000 kva at 220,000 volts outdoors.

We think that the Sporn and Strang paper on field tests on the 138-kv air-blast circuit breaker is of particular interest, because it is a summation of one manufacturer's efforts to reduce arcing and reclosing time to a minimum. We appreciate the courage with which the American Gas and Electric Company's engineers faced the matter of making interrupting and reclosing tests of such magnitude, upon a system carrying important loads. The tests indicate the ability of the air-blast breaker to do substantially the same job as the oil circuit breakers of equivalent rating. However, the interrupting time of the breaker described seems quite long. In the outdoor air-blast circuit breakers of our associated companies before referred to and now operating in Canada, the 138-kv and the 220-kv breakers now in operation have interrupting times in the order of $3^{1}/_{2}$ cycles. This compares to five cycles for the breakers described in the paper by Sporn and Strang. Also, on the 220,000-volt breaker having interrupting time of approximately $3^{1}/_{2}$ cycles the reclosing times can be set to be quite low, that is, in the neighborhood of 12 cycles. Reclosing may be done on the single-pole basis if desired, and as has been reported in other discussions before the Institute, reclosing tests on a single-phase basis were conducted by our associated companies some years ago.

A further advantage of the breaker of which I am speaking is that reclosing is not done on exterior isolator contacts with the consequent brilliant visible display, but rather is accomplished within an interrupting chamber under 225-pound pressure. This eliminates the visual display and the possible hazard of attempting to reclose in normal atmospheric pressure by isolator contacts moving comparatively slowly and making circuit in the open air.

The authors' statement that it took 30 years to develop a five-cycle, high-voltage oil circuit breaker, but that the same accomplishment was achieved in two years in the air-blast circuit breaker draws our attention. It is felt that it would be well to acknowledge that several of us manufacturers had resident or visiting engineers in Europe, where these high-voltage air-blast circuit breakers were first developed, and that each one's designs are largely based upon European experience. However, as we have shown in our own case, the European experience and actual practice on many high-voltage breakers already installed in Europe, and in Canada give interrupting speeds not of five cycles, but of three to four cycles.

E. W. Knapp (The Shawinigan Water and Power Company, Montreal, Que., Canada): The authors state that, "the success of this development on 138 kv points encouragingly to the prospect of development at higher voltages, such as 230 kv or even higher." The Shawinigan Water and Power Company have in service at this time two different designs of air-blast circuit breaker operating on a 220-kv system. This company has been interested in the development of this type of circuit breaker for some years and has on a number of occasions made available sections of their power system for primary tests.

One-air blast circuit breaker now in service is quite similar to the 138-kv design discussed in the paper. It is rated as follows:

Voltage rating	220 kv
Current rating	400 amperes
Rupturing capacity	2,000,000 kva
Closing Time	17 cycles (60-cycle basis)
Opening time (contacts part)	$3^{1}/_{2}$ to $4^{1}/_{2}$ cycles
Arcing time	0.5–1.0 cycle
Air pressure	250 pounds per square inch

Automatic reclosing, single-pole or three-pole

Satisfactory primary tests have been conducted on this unit with one phase to ground, with and without the automatic reclosing feature in service, up to approximately 100,000 kva at 245 kv and up to approximately 1,000,000 kva at 220 kv.

It is noted that the 138-kv air-blast circuit breaker operates on an air pressure of 350 pounds per square inch, as against 250 pounds for the 220-kv air-blast circuit breaker. Have the authors tested the 138-kv unit at pressures below 350 pounds, and may we assume that operating at pressures above 250 pounds on this particular design does materially increase the rupturing capacity or produce other advantages?

The second type 220-kv air-blast circuit breaker now in service on the Shawinigan Water and Power Company system is rated as follows:

Voltage rating	220 kv
Current rating	600 amperes
Rupturing capacity	2,000,000 kva
Closing time	15 cycles (60-cycle basis)
Opening time (contacts part)	$3^{1}/_{2}$ cycles
Arcing time	one-half cycle
Air pressure	115 pounds per square inch

Not arranged for automatic reclosing

This design uses the cross-blast principle and is somewhat similar in operation to the type described by L. R. Ludwig, H. M. Wilcox, and B. P. Baker.

A series of primary tests was conducted on this type during the development stage which resulted in a considerable modification of the original design. The unit now in service has been tested on the power system to approximately 900,000 kva at 220 kv. Although the tests appeared to indicate satisfactory performance at the specified ratings, there has been insufficient operating experience to insure satisfactory service under all conditions.

During the primary and secondary tests on air-blast circuit breakers on the Shawinigan Water and Power Company system, a number of conditions were noted which might be of interest. This applies particularly to outdoor service in the rather severe climatic conditions of Canada.

Air-drying facilities and convenient supervision of the same is important.

Special attention may need to be paid to the problem of condensation.

Using the correct materials is important, not only for satisfactory operation, but also for weathering. Machined parts must be carefully chosen and adjusted to prevent sticking under extreme variations of temperature. Air pressures from 100 to 350 pounds per square inch are being used. It would be very desirable to standardize on a closer range of pressures.

Philip Sporn and **H. E. Strang**: It is true, as Mr. MacNeill has stated in his discussion, that improvements in manufacturers' laboratories have increased their ability to handle the testing of circuit breakers. On the other hand, it is our impression that field testing of circuit breakers has not fallen off either in frequency or importance in later years; also some very important utilities have in late years become very much converted to the value if not the necessity of making occasional field tests on their own systems. It is still true, of course, that many other important utilities are not now and never have been willing to subject their systems to staged interrupting capacity tests.

Notwithstanding our gratification on the good results obtained from the test on the air-blast circuit breaker, it is our belief that Mr. Jones has passed sentence rashly and altogether too severely on the oil circuit breaker, considering the present state of development and experience with both oil and air. Without discounting the encouraging and promising results obtained with air, it is still true that the modern oil circuit breaker is doing an outstanding job. Oil fires of anything like the nature indicated by Mr. Jones' photograph are extremely rare for really modern breakers when subjected to duties within their ratings. As a matter of fact, Mr. Jones' discussion lends support to the feeling which has been maintained for some time that factory tests and field tests supplement each other, and each have an important and distinct place in the development of circuit breakers. During the same general test program which Mr. Jones has described, and with the same setup and operating conditions, one breaker equipped with up-to-date arc-control devices which had been developed with the aid of factory test equipment was tested. It successfully passed these tests without any such disturbance as shown in the photograph.

As Mr. Knapp has suggested, the industry undoubtedly should standardize on such matters as air pressure in due time. Development tests which have so far been made have indicated that within certain limits interrupting performance improves with an increase in air pressure. Future designs may make possible the same kind of performance with lower pressures, but, for the time being, it does not appear advisable for the industry to standardize on this factor, since it might result in a handicap rather than a benefit. Pressures of 250 or 350 pounds per square inch are not difficult nor costly to obtain or maintain and represent an economical way of building an increased factor of safety into a line of air-blast circuit breakers.

There is no particular mystery about the design problems in connection with making available a satisfactory air supply. There is no reason to suspect that a straightforward engineering approach with a thorough check of each step will fail to produce results here. At least four years ago an indoor air-blast breaker was mounted in an unheated outdoor switchhouse, together with the necessary compressor and control facilities, and installed during a whole winter in northern Canada. Experience gained during that time has been of great assistance in the design of satisfactory equipment of this kind.

Mr. Edsall has rather jumped at conclusions in assuming that the successful performance reported for outdoor air-blast breakers and high-capacity indoor breakers naturally means that this principle may be extended over the entire range of circuit-breaker ratings. For duty below 500,000 kva, self-contained magnetic-type breakers have been in regular use for several years, and their popularity is steadily gaining.

The test results submitted with this paper include all of the tests which were made in the field without exception. This report was made for the purpose of clearly and completely defining the present state of the art as far as this particular development is concerned. If Mr. Edsall has available results of other tests which show faster operation on some other type of breaker, it is hoped that in the interests of providing the industry with all available information, he will make a similar complete disclosure of all the factors concerning such tests.

As Mr. Edsall has pointed out, our statement in which it was inferred that the entire air-blast circuit breaker development had been accomplished in only two years is not quite correct when interpreted in that way. The authors' comparison of the 30 years of oil circuit-breaker development work to a development of only two years with the air-blast circuit breaker was made primarily with regard to the particular circuit breaker and manufacturer under discussion. However, as pointed out by Mr. Edsall, the statement was probably misleading, particularly if interpreted to include all of the development work done on air-blast circuit breakers. We, therefore, wish to acknowledge the excellent work which has been done over a considerably longer period by other manufacturers including Mr. Edsall's company.

Both Mr. Edsall and Mr. MacNeill have brought up the question of the influence of transient recovery voltage rates on circuit-breaker tests, and Mr. Edsall has suggested that some rule or yardstick covering transient recovery voltage should be set up for testing purposes. While the severity of recovery voltage conditions is important, the bulk of evidence which we have been able to gather to date, with respect to the operation of modern types of circuit interrupters, seems to indicate that these breakers are capable of handling the highest rates that have been encountered. In both field and laboratory tests on modern types of breakers, where conditions have been controlled to obtain both high and low recovery rates, the actual effect on performance as measured by arcing time, appears to be relatively small. For example, in the tests listed in this paper, the recovery voltage rate was varied from less than 200 volts per microsecond, to approximately 2,000 volts per microsecond without any marked change in breaker performance. Furthermore, in connection with a recent survey of voltage recovery rates on power systems, it was found impossible, although an attempt was made, to obtain any definite correlation between recovery rates and breaker distress by studying actual operating records. This does not mean that there have not been any known instances where breakers, particularly of older plain-break designs, have been in trouble under conditions of known high rates of recovery voltage on other systems not included in this survey. It does, however, bear out the general conclusion that most modern breakers are capable of doing their job, regardless of the severity of recovery rate conditions.

In view of all of this, we are not of the opinion that there is anything to be gained by attempting at this time to standardize recovery rate values for circuit-breaker testing. While it is true that voltage recovery rates constitute one of the important factors to be taken into account in the design of circuit breakers, most of the known results to date seem to indicate that this has been done successfully, even for the highest rates encountered. As for a criterion as to what rates may be encountered, the results of the above-mentioned survey of voltage recovery rates made by the Association of Edison Illuminating Companies has already been made available to the circuit-breaker manufacturers, and certainly should constitute a fairly reliable guide for future developments in circuit-breaker design.

A 2,500,000-Kva Compressed-Air Powerhouse Breaker

Discussion and authors' closure of paper 42-41 by L. R. Ludwig, H. M. Wilcox, and B. P. Baker, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 235–41.

R. M. Bennett (General Electric Company, Philadelphia, Pa.): This excellent paper presents a remarkable description of the factors entering into the design of a cross-blast interrupter.

It is of particular interest to note, that although two groups of designers have both employed air-directed across the arc, their solutions to the same problem constitute radically different interrupting devices.

As the authors have stated the problem, and I quote, "A 60,000-ampere arc in an arc chute two inches wide will liberate sufficient gas from the splitters to cause the flow of gas to reverse a distance of six inches against a driving pressure of 150 pounds per square inch." In order to handle these large volumes of gas the authors have placed the arc, as indicated by the sketch on the left in Figure 1, at a point where the throat is very wide, so wide in fact that the operating pressure of the breaker, which is 150 pounds per square inch, can drive away the large volumes of gas that are produced by extremely high currents.

The 15-kv air-blast breaker described by Mr. Braley and Messrs. Strang and Skeats, however, interrupts a 60,000-ampere arc, not in a two-inch throat, but in one approximately half this wide. Air flow through this narrow throat is maintained by a radically different method. As shown in the sketch at the right (Figure 1), an orifice is placed upstream from the arc chute and acts as a flow stabilizing means. In the other view the arc chute would appear to diverge rapidly on the inside, and the orifice configuration would be more apparent. Upstream from the orifice the air pressure is very high since it comes from a storage tank at 250 pounds per square inch. Downstream from the orifice in the region of the arc the pressure is normally very much lower. This positive difference of pressure directly back of the arc chute maintains the flow of air under the most extreme current conditions, because, in order to stop the flow, the pressure in the throat would have to rise to the pressure back of the orifice. The drop in pressure at the orifice also acts as an effective blocking means to prevent particles of metal and carbon from being driven backward where they might impair the insulating properties of the incoming air line.

Under light current conditions, the interruption problem is quite different. The air pressure and velocity required to produce satisfactory interruption is entirely a function of voltage and rate of rise of recovery voltage and depends not at all upon the width of the throat. It is this fact that explains the significant differences between the interrupters. Both of the above designs must maintain virtually the same normal or no-load air pressure at the arc in order to interrupt light currents. But the interrupter shown on the right in Figure 1 employs much smaller air passages than the one at the left, with the inevitable consequence that it consumes substantially less air, in spite of the fact that the air in the tank is at higher pressure. In brief, the interrupter shown at the left permits much of the air to by-pass the arc, while the one at the right with its narrow channel forces air to flow through and across the arc stream.

This fact is reflected throughout the design of the two breakers, in the size of the arc chutes, in the air storage and compressor systems, and, incidentally, in the acoustic shock that results during interruption. The design described by the authors employs three blast valves and very short supply tubes in order economically to deliver large volumes of air to the interrupter. The one at the right employs a single blast valve and supply pipe together with a manifold to deliver air to the three arc chutes. It is this fact that permits the small over-all size and the extreme flexibility of terminal connections that are possible with the latter design.

With reference to the "reversal of flow a distance of six inches," the authors state: "If the rate of rise of recovery voltage of the circuit is slow, the stalled flow will have time to recover velocity, and dielectric will be restored. However, if the recovery rate is fast, the space below the splitters will remain clogged too long, and reignition will result." No matter what theory of operation applies, it must be evident that interruption cannot take place while the interrupter chamber contains incandescent gas. Yet the authors state that this gas is removed after the recovery voltage has started to rise.

What is more remarkable, the removal of six inches of gas, which must be accomplished at least before the recovery voltage reaches its peak value, would require a velocity of air flow of some 5,900 feet per second, or say six times the velocity of sound. This is true if the recovery voltage rises at the relatively slow rate of 500 volts per microsecond. At 5,000 volts per microsecond, it would require a velocity of 59,000 feet per second. It is to be presumed, therefore, that the arc chutes are not so badly clogged with hot gases as the authors believe, and that the majority of hot gas is removed before the current zero.

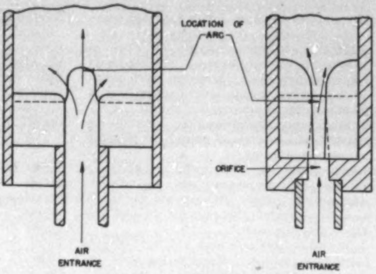


Figure 1. Arc chutes for air-blast circuit breakers

H. V. Nye (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The group of papers submitted this morning on air-blast breakers of large interrupting capacity, both at 15 kv and 138 kv, is very interesting, as demonstrating the claim made in one of these papers that compressed air seems the only practical interrupting medium, except oil, for use on a line of breakers from small to large capacity and throughout the commercial range of operating voltages. This is the conclusion reached several years ago by the engineers of the company with whom I am associated and explains why our developments along the line of oilless breakers have been along the line of the air-blast type.

Some six years ago my company built and tested an outdoor air-blast breaker at 37,000 volts across a single-pole unit. Even with this experimental breaker, operating speeds were very satisfactory, the average time from trip to arc extinction running in the neighborhood of two cycles. Our tests at that time, however, indicated that a great deal of work needed to be done to make the mechanical design of the breaker and its associated parts sufficiently rugged and reliable to make it commercially practicable. Since that time we, as well as other manufacturers, have been working toward the development of thoroughly satisfactory designs. The papers here presented are a good indication that this work has produced results, and it is now practicable to build air-blast breakers for the more usual breaker ratings.

The test results given in the paper by Messrs. Ludwig, Wilcox and Baker are valuable, not only because of the high capacities in kilovolt-amperes interrupted, but also in the wide range of currents and recovery voltages employed. They show a consistency in performance under varying conditions of load and recovery voltage which I think is typical of the compressed-air type of breaker. On the commercial breakers which we are now building up to 500,000 kva we have repeatedly demonstrated this consistency in performance.

On studying this paper I noticed the statement "that the breaker never failed to clear the circuit when the contacts were more than a fraction of an inch apart." As the tabulated data all show a separation of contacts at interruption of anywhere from 1.1 to 3.8 inches on the three-phase tests for the last phase to clear, I would be interested in having the authors explain as to just what meaning they intended to convey by this statement.

In the notes under Table III a statement is made that 58 consecutive tests, as outlined in the tables given, were made without any maintenance on the breaker. This is a record of performance which I think should be particularly mentioned. As an illustration of the fact that smaller types of air-blast breakers are also capable of repeated operations under interrupting duty, I would cite a series of tests recently run on one of our 150,000-kva five-kilovolt air-blast breakers. This unit was rated for a maximum interrupting amperes of 37,500. A series of 97 close–open short-circuit tests was run on this breaker with 25 per cent of the shots at each of the following values—10,000 amperes, 20,000 amperes, 30,000 amperes, and 40,000 amperes. There was no maintenance on the breaker during these tests, and at the end the breaker was in operating condition and capable of taking further shots.

H. A. P. Langstaff (West Penn Power Company, Philadelphia, Pa.): The authors have presented an excellent story on a new development which should prove of great value to the industry. A great deal of theoretical and practical principles have been applied to this compressed-air breaker.

High-capacity and quite complete factory tests, simulating operating conditions, have materially assisted in proving the breaker capable of meeting its guarantees; yet we must apply these breakers in our powerhouse laboratory where any remaining deficiencies will be developed, resulting from factory assembly and so forth.

Comparing the functioning of this compressed-air breaker with that of the ordinary oil circuit breaker and the functioning medium, namely, air versus d-c control voltage, I raise the following question. The tripping of an electrically operated oil circuit breaker has a guaranteed range of 90 to 140 volts, but in the majority of cases it will trip at a much lower value than 90 volts. Its rupturing capacity is not thereby affected, whereas in the case of the compressed-air breaker, it may trip on low pressure, but this, in turn, materially affects its rupturing capacity. I understand control circuits are arranged such that the breaker is now allowed to trip if the air pressure drops below a certain value.

In the paper the authors state that: Enlarging the throat of the arc chute or increasing the air pressure will obviously improve conditions; however, both result in an increased air consumption for a nominal gain in interrupting ability.

Since the kilovolt-ampere interrupting capacity is a function of pounds air pressure at certain opening of the arc chute, how much would the interrupting capacity be affected if the air pressure was reduced from 150 pounds to say 100 pounds or to 75 pounds provided the opening of the arc chute was the same?

Would a 1,000,000 and a 2,500,000-kva breaker of the same voltage rating have the same arc chute and opening? I am thinking of standardization.

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): This paper describes another stepping stone in the commendable progress that the circuit-breaker designers have made in the march toward adequate oilless switchgear, starting some 15 years ago when the company with which the authors are associated first produced commercially the Westinghouse air De-ion breaker.

The tests described are the first, so far as I am aware, where 2,500,000-kva has been delivered by a laboratory for testing a circuit breaker with the ability to repeat such a severe test as often as desired by those in charge of the test.

One shortcoming is apparent; one, however, that it may be impractical to overcome. These heavy short-circuit tests were made at circuit voltage-recovery rates, but a small fraction of the 12,000 volts per microsecond that may be expected in large generating stations. Such high recovery rates were obtained, but at kilovolt amperes only a fraction of the breaker's rating. Of course, the testing engineer must choose between "his cake or his penny" as the reactor which gives the high-voltage recovery rates cuts down the short-circuit kilovolt amperes. The third from last test given on Table III shows high-current and high-voltage recovery rate occur in what appears to be a more severe combination than any other test recorded. Is it just a coincidence that the arcing time is relatively low?

While the above paragraph points out that the tests do not duplicate the maximum conditions that may be met in certain large generating stations, I wish to pay high tribute to the fact that these tests, so far as I know, approach nearer to such conditions than any other laboratory or field tests ever made.

Progress made in the design of the air-blast circuit breaker gives comfortable assurance that as interrupters they may be relied upon to do what is expected of them, but, if any uneasiness still lurks in the user's mind on that score, it should be offset by the realization that any failure that may remotely occur will not be accompanied by a fire which could spread the damage far beyond the immediate confines of the circuit involved, a consideration of value at any time, but particularly when the possibility of bombings hangs over us.

The description of the compressed-air system in section IV indicates that care and forethought has been given to this important element, and it is well that this has been done, for it is in the air system that most of the troubles may be expected. This is the place where a major number of minor difficulties have already been found in several air-blast breakers already in commercial operation.

L. R. Ludwig, H. M. Wilcox, and **B. P. Baker:** Mr. Nye has made reference to experimental work and limited use of compressed air in this country for a period of about six years. Compressed-air breakers have, of course, been used in Europe for 12 years or more, and this, naturally, raises the question as to why this form of breaker has not been made commercially available at an earlier date in America. The European breakers have not been called upon to have the high interrupting kilovolt-amperes required for indoor breakers in American practice. This has led to European constructions of the nozzle type, which in general seem to possess a current limitation of the order of 25,000 to 30,000 amperes. At 13 kv this restricts the rating of the breaker to approximately 750,000 kva. In order to extend these limitations, it has been necessary for American designers to develop entirely different forms of interrupters. The cross-blast type, which is capable of reaching 2,500,000 kva, represents a satisfactory answer, but until this type of improvement was made, application of compressed-air breakers in this country was necessarily limited.

Mr. Nye points out that the tabulated data show that the contact separation at the time of interruption reached as much as 3.8 inches and questions this in view of the statement that the breaker never failed to clear the circuit when the contacts were more than a fraction of an inch apart. Detailed examination of the procedure during interruption quickly indicates, however, that these data are not inconsistent. For example, the contacts may part and form an arc just prior to a normal current zero. At the time of the current zero the contacts may be separated only three-eighths inch, and interruption will not take place. Because of the high mechanical opening speed, the contacts can then be 3.8 inches apart at the time of the next current zero. Clearly, the interruption can only take place at normal current zeros. An examination of the data with this point in mind does lead to the conclusion that the breaker always interrupted at the first current zero after the contacts were more than a fraction of an inch apart.

Mr. Bennett has contrasted the interrupting action in two forms of cross-blast compressed-air breakers. With reference to his sketches, it should be considered that the one at the left represents the arc chamber of a 2,500,000-kva breaker, whereas the one at the right shows the arc chamber of a 1,500,000-kva device. Naturally, the 2,500,000-kva arc chamber is wider.

When small currents are being interrupted in the chamber at the left, the air is so directed that it flows straight across the arc and does not diffuse toward the sides, as indicated by the arrows. When large currents are interrupted, the pressure in the region of the arc core (which is central with respect to the arc chamber) develops high counter pressure. During the portion of the half cycle when this counter pressure is sufficient, the air stream is then diverted sidewise as shown by the arrows. The effective action of the air stream at this time is to remove the extraneous ionized gases as they are formed and prevent them from "backing up" into the throat. As current zero is approached, the diversion of the air stream sidewise ceases, and the actual interruption near current zero is quite similar for both high and low currents.

This explanation should make clear the way in which the arc chamber recovers dielectric strength after current zero. It is seen that there is no need to remove six inches of ionized gas after the current zero, since this is done by properly diverting the air stream prior to current zero.

The fourth paragraph of the second section of the paper and Mr. Bennett's remarks in his last two paragraphs refer to the type of arc chamber in which there is no side diversion of the air stream. Mr. Bennett points out that velocities of 5,900 feet per second would be required for interruption, if ionized gas "backs up" six inches. Actually, interruption would probably not occur. The problem can be solved by the use of higher air pressure (250 pounds), which should prevent ionized gases from "backing up" six inches. The authors' solution, however, of "diverting" arc products, permits operation with only 150 pounds pressure.

In the design of the two breakers, the arc chute sizes are reasonably comparable for the same kilovolt-ampere rating. Furthermore, the quantity of air used in cubic feet per interruption, based on atmospheric pressure, is very nearly the same. The air is, however, used in an entirely different manner. The use of mechanically operated blast valves makes it possible to closely time the opening and closing of these devices, so that the duration of the air flow can be made quite short. Therefore, the quantity of air when it is flowing can be made high. The interrupting ability of the compressed-air breaker seems to depend upon the rate of air flow, and, naturally, the higher this rate, the greater the margin of safety. The air-storage tanks in the compressor system can obviously be made of different sizes, but the larger equipment has the advantage of providing the greater number of operations.

Mr. Langstaff has inquired relative to the kilovolt-ampere interrupting capacity of the breaker at lower air pressures. The designs, in general, are based on satisfactory interruption of full kilovolt-ampere rating, starting with tank pressures of 100 to 125 pounds per square inch. The nominal pressure used is 150 pounds. Since the drop in pressure does not exceed 25 pounds per operation, two complete operations are possible without supplying any additional air from the compressor equipment. It has been found that designing on this basis has not handicapped the size and cost of the breaker, because the curve of interrupting capacity versus tank pressure is comparatively flat in the range of 100 to 150 pounds with the type of design used.

Mr. Langstaff has further inquired regarding breakers of various kilovolt-ampere rating for the same voltage service. The arc chute width, the diameter of the blast valve, and the diameter of the air-supply tube are all increased progressively, as the kilovolt-ampere rating is increased. This means, of course, that greater quantities of air are used for interrupting the higher kilovolt-ampere capacities. Pressure is held constant, however, for all kilovolt-ampere ratings, and the design factors are worked out in such a way that standardization should not be a difficult problem.

Mr. Hentz has pointed out that it is difficult to simultaneously obtain high kilovolt-ampere interrupting tests and extremely high recovery voltage rates. As he has stated, we believe the tests shown in Table III of the paper, in which 53,000 amperes were interrupted with a circuit voltage recovery rate of 7,100 volts per microsecond, are the most severe tests ever made. The variation in arc time shown in the entire table is the result

1. Of contacts parting at various points in the half cycle.
2. Of the erosion of the first two splitter plates as the result of the large number of tests previously made on the arc chamber.

Therefore, the short interrupting time of the particular test referred to is to a large extent probability.

Mr. Hentz has pointed out that these tests still do not appear to reach the voltage recovery rates and currents which can be obtained in the field. In this connection, attention is called to the statement in the paper that, "All three of these circuit conditions represent double-frequency transients, but, in the second and third oscillograms, the higher frequency is so greatly damped by the conduction current of the breaker following current zero that little trace of the oscillation appears in the recovery-voltage transient recorded by the oscillograph." This damping out of the higher frequency component indicates that, if the circuit is modified in an attempt to further increase the recovery voltage rate, no actual increase will result, and the interrupting requirements placed on the breaker will not be made essentially more severe than they were during this particular test. If the current is increased, complete damping takes place at lower recovery rates, and, in view of these considerations, it is believed that the tests actually shown do indicate the ability of the breaker to withstand the highest recovery rates which may be encountered, up to full kilovolt-ampere capacity.

Acoustics and the Quiet Train Ride

Discussion and author's closure of paper 42-56 by William A. Jack, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, pages 382–92.

F. C. Lindvall (California Institute of Technology, Pasadena, Calif.): In the development of the new pendulum-type suspension which is incorporated in three passenger cars just put in service on the Santa Fe, Burlington, and Great Northern, the problem of acoustical treatment was given particular consideration. Mounting of the car body on a truck in this new suspension system described in February 1942, *Railway Mechanical Engineer*, permits the use of liberal amounts of rubber in all connections between the truck and car body. This rubber can be operated with quite large static deflections so that transmission of truck noise into the car body through attachments is greatly reduced over what is possible in standard equipment in which the body weight is concentrated at the truck center plates.

Preliminary measurements with two experimental cars showed that the noise level in the vicinity of the trucks was far higher than at other points outside the car body. Consequently particular care was taken with floor acoustical treatment. Calculations made by Dr. V. O. Knudsen showed that a substantial reduction in transmitted noise, about five decibels, could be made by treating the under side of the car in the vicinity of the trucks with absorptive material exposed toward the truck. The theory behind this suggestion is that the intensity of sound in the space bounded by the car floor and the ground can be minimized by preventing sound reflection at the under side of the car. The transmission loss through the floor itself is not much affected by the presence of the exposed acoustical material, but the intensity of sound at the under side of the car is substantially lower than it would be if a hard surface were used under the floor exposed to the truck which is the sound source. Road tests showed that the material known as Airacoustic was mechanically suitable for use under the car floor. Behind this material is a stainless steel sheet which seals the floor heat insulation against moisture penetration.

The car floor is a fairly rigid plywood slab uniformly supported on sponge rubber pads. The floor static deflection is small, but the rubber support is helpful in reducing the transmission of high-frequency disturbances. Comparative tests made with these new cars coupled in with standard modern lightweight equipment indicate a noise level of eight to ten decibels below that of the standard equipment. In the new cars not enough attention was given to acoustical treatment of the air-conditioning system with the result that the noise level, though it be low, is due largely to air noises.

Walter C. Keys (United States Rubber Company, Detroit, Mich.): Apparently noise vibrations travel through rubber and steel to an extent represented by the inverse of the velocities of sound through the two media. In steel, sound travels some 30,000 feet per second; in rubber of 50-durometer hardness (comparable to rubber heels), the velocity is 210 feet per second.

Mr. Jack indicates, in the section entitled "Vibration Isolation," that a bonded rubber isolator used in shear was less efficient than another using a controlled spring in combination with rubber. Experience with the millions of bonded rubber mountings used in modern automobiles indicates that bonded rubber mountings, when properly designed and applied, provide the most practical and efficient known means for isolating both vibration and transmitted noise.

We have a record of an installation of rubber at the center bearing of a six-wheeled truck having performed satisfactorily for 12 years. This is perhaps unusually long service but it illustrates the possibilities where stresses in the rubber are kept low. Insulation at center bearings, side bearings, truck pedestals, draft gears, stems, buffers, pantagraph supports, brake linkage, and so on, are essential to the quiet ride in railroad passenger cars. In addition, all auxiliary equipment such as blowers, motors, and so on should be adequately insulated.

It is well-known that vibrations of any frequency require a definite minimum flexibility to be adequately isolated. Some materials which have been extensively used cannot provide adequate deflections; hence, their value consists chiefly in interrupting metallic continuity.

The writer has too often been disturbed at night by people talking outside of a standing car; this indicates that the "enclosure" effect of the car body could be improved which would lower the noise level within the car.

The remarkable quieting effect of several inches of light snow is well-known. It is hoped that this beneficial effect can be approached or equaled by the use of some inexpensive product which may be easily applied.

A material known as Permacell, capable of sustaining moderate loads for long periods of time could be used as a support for an inner body which could be nonmetallically joined to the outer body. The writer believes that such an assembly can be designed and that it could provide one of several means for reducing the interior noise level.

William A. Jack: Mr. Keys' remarks on the successful use of rubber in several forms are very heartening. The quiet train ride will be obtained by designers, working in conjunction with such experts who know the proper uses of their materials and, in this way, include them in an early stage in the planning at the several points where acoustical experience indicates the noise problems can be attacked. The velocity of sound, as the discussion points out is much lower in rubber than in steel. This fact, in itself however, is not believed responsible for the usefulness of rubber to the acoustical engineer. The velocity of sound in a medium is a function of its elasticity and its density. If the attenuation of energy per unit length were the same, and if the same amounts of energy were introduced into each material, a rubber rod would deliver just as much energy to its far end as would a steel rod, although it would do it more slowly. However, the attenuation of energy per unit length is greater in rubber, and more of the vibrational energy is converted to heat. Furthermore, it is difficult to get large amounts of vibrational energy into rubber in the first place. Pieces of compliant material provide isolation when placed under the feet of a compressor, for example as brought out in the paper. The feet of the machine at the higher-frequency components vibrate much as they did when rigidly mounted. Rubber yields under these small-amplitude high-frequency oscillations in such a way that much of the energy is reflected, not absorbed. The author agrees that if energy does get in the rubber, most of it will be converted into heat before it travels far.

Professor Lindvall's remarks on the application of acoustical principles in the pendulum-type suspension cars are interesting, and the author certainly agrees with the steps taken. The complaint about the air-conditioning system might not have been noticed if the other disturbances had not been so well-reduced. It seems that there is always some smaller noise hiding behind a larger one. Perhaps the conditioning system, in question, has a sufficient run of duct so that Airacoustic may be installed to reduce this noise. As sometimes happens, the trouble may be an air-rush sound from the grilles. This is too far along for the acoustical engineer to be of much help, and the problem lies in the field of redesign.

A New Instrument for Recording Transient Phenomena

Discussion and author's closure of paper 42-57 by S. J. Begun, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 175–7.

E. A. Blomquist (Consolidated Edison Company of New York, Inc., New York, N. Y.): We investigated briefly the possibility of using a magnetic tape recorder for recording transients last August. We found that satisfactory reproduction at 60 cycles was not possible on the outfit we had, which was designed for sound recording. We did no further work on it but referred the matter to the manufacturer.

Mr. Begun's method of getting around the difficulty of recording the low frequencies is very ingenious. I would like to ask what accuracy can be expected from the device, and also how well it will hold its calibration over periods of time.

In applying this device to the recording of power-system transients, it would be desirable to obtain records up to several seconds. As I see it, this would preclude the use of a cathode-ray oscilloscope for viewing the record, due to the limited screen size and the limitations of the persistency of the screen. However, I see no reason why the record could not be taken off on a magnetic oscillograph. I would like to ask Mr. Begun if the recording and reproducing heads and tapes are sufficiently uniform so that a tape containing a record could be taken to the laboratory and there transcribed onto a film using a different head from that with which the record was made. If this were possible, recorders could be set up at various points on the power system, and when a fault condition occurred, the tapes could be removed and brought to the laboratory for transcription.

Another question that comes to mind is the possibility of obtaining more than one record on a tape. In power system studies, six to nine records are usually needed. Possibly this could be done by widening the tape and staggering the recording and reproducing heads.

It seems to me that this method of recording has decided possibilities in the field of power-system measurements, although further development may be necessary before it is practical.

S. J. Begun: It is very gratifying to hear that Mr. Blomquist feels that this method of recording transients has some possibilities in the field of power system measurements. As a matter of fact, during the development stages of the transient analyzer, this application was kept in mind.

The difficulties in recording low frequencies, to which Mr. Blomquist referred, are not surprising, but by the use of the carrier frequency method, very satisfactory results are obtained.

With regard to the problem of how to make the transient visible, the immediate observation, by means of an oscilloscope, is, in very many cases, desirable; but where this cannot be done, because of the length of record required, there is no reason why the signal should not be supplied from the tape to a magnetic oscillograph.

From our observations so far, we feel that the tape, as well as the heads, can be made sufficiently uniform so as to assure consistent results. The tape may be recorded on one instrument and reproduced on another without any difficulty. Furthermore, for all practical purposes, the record life is infinite.

Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers

Discussion and authors' closure of paper 42-59 by F. J. Vogel and Paul Narbutovskih, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 133–6.

H. L. Prescott (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): In this paper, the authors have presented some interesting data which add considerably to our knowledge of the variation of gradient and temperature rise with load. The application of these data to operating practices for transformers will, as the authors have shown in Figure 7 of the paper, permit a given transformer to carry considerably higher overloads for short times than has previously been allowed. The authors have shown by their example computation of the temperature at 200 per cent load that to compute the permissible overload for a given transformer under varying operating conditions is a long and tedious job and therefore is not a very practical basis for emergency loading of a transformer.

The winding temperature is determined by the sum of the oil temperature and the winding gradient. The temperature of any other conductor placed in oil and carrying current is determined in the same way. The Westinghouse company has developed a relay, which operates on this principle. The operating element of the relay consists of a bimetal conductor through which is circulated a current proportional to the winding current. The gradient between the bimetal and the oil, therefore, has a definite relation to the gradient between the winding and the oil and, by using the data similar to figures 3 and 4 of the paper, the desirable relation can be accurately predetermined. The bimetal is mounted in the same oil as the winding, so that the oil temperature factor which determines the winding temperature also determines the bimetal temperature. The relay, therefore, takes into account all of the factors included in the sample calculation given in the paper and provides a practical means of operating the transformer to take advantage of its inherent overload capacity. By adjusting the relay so as to obtain a proper relation between the bimetal and the winding gradients, the relay is made to automatically allow higher winding temperatures at higher overloads for shorter times in accordance with an approved time and temperature schedule such as, for example, that shown in Figure 7 of the paper. As the transformer approaches this predetermined schedule, the relay automatically operates an alarm signal to warn of approaching danger and later trips the circuit breaker if the predetermined operating limit is reached to prevent transformer burnout.

L. Wetherill (General Electric Company, Pittsfield, Mass.): This discussion is limited to the circulation of oil in self-cooled oil-insulated core-type transformers. There are several possible assumptions which serve to simplify the subject, and, in general, the broader the assumption the less closely calculations agree with tests. Three assumptions which have been used will be discussed.

Case I. The broadest assumption, and one which has proven adequate for many purposes, is that temperature differences within the oil can be neglected. Oil-temperature rise above ambient- and winding-temperature rise above oil can be calculated independently and added. It is particularly helpful in analyzing transient thermal per-

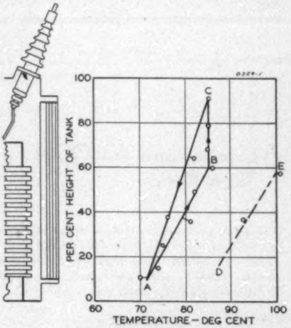


Figure 1. Thermal conditions in 2,500-kva self-cooled transformer

Steady state
——— Oil temperature
— — Copper temperature
o o o Test points

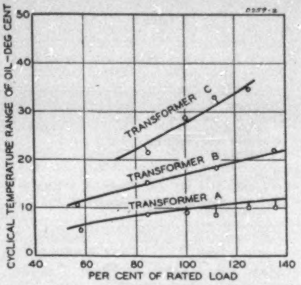


Figure 2. Comparison of design calculations and test results

Steady state
——— Calculated characteristic
o o o Test points

formance, since the resulting equations may be made identical with those representing transient performance in electrical circuits when current is abruptly changed in a circuit containing resistance and capacitance in parallel.

As a result, we obtain thermal transients with exponential decrement which can be superposed on the steady-state condition. The authors have made use of this procedure in analyzing transient behavior, and this procedure is in fact the most satisfactory which has been described in published literature.

Case II. It has long been known that calculation according to the foregoing procedure may lead to errors. Three such cases are as follows:

(*a*). Transformers in which the core is nearly as high as the tank.

(*b*). Transformers in which there is appreciable loss generated above the top of the core.

(*c*). Transformers undergoing heavy short-time overloads.

To predict with reasonable accuracy the higher temperatures which may result from these conditions, the assumption that temperature differences within the oil can be neglected should be replaced by the more accurate assumption that the core and coils can be represented by a single channel in which heat is absorbed as the oil moves upward, and that the tank cooling surfaces can be represented by a single channel in which heat is dissipated as the oil moves downward.

Figure 1 of this discussion shows ultimate steady-state temperatures observed in the oil of a 2,500-kva transformer at 135 per cent rated load. Starting at *A* (in Figure 1), the oil moves upward through the windings increasing in temperature at an approximately uniform rate to *B*. The oil then moves on to *C* at the top of the tank with no appreciable change in temperature. From *C* the oil moves down through the cooling tubes, or past the cooling surfaces, decreasing in temperature at an approximately uniform rate, to *A*; completing the cycle.

The temperature of the copper is shown by the curve *DE* in Figure 1 as a function of the height above the bottom of the tank. It is assumed that the temperature rise above adjacent oil is the same for all parts of the winding. Actually, this is frequently incorrect on specific transformers, but it serves to illustrate the nature of the phenomenon.

If the watts per square inch of winding surface is low, and the copper is at nearly the same temperature as the adjacent oil, reference to Figure 1 will show that the average copper temperature may well be lower than the top oil temperature. This doubtless accounts for the so-called negative gradients referred to in the paper.

The pressure difference which maintains the continuous circulation of the oil is the weight of the column of downward moving oil minus the weight of the column of upward moving oil. The magnitude of the pressure difference is

$$F = \rho\alpha a$$

where

F = pounds per square inch
ρ = density of oil at 0 degrees centigrade in pounds per cubic inch
α = thermal coefficient of expansion of oil per degree centigrade
a = area of oil circulation curve in inch-degrees centigrade

When, as in Figure 1, the oil circulation curve is approximately triangular the area is

$$a = RH$$

where

R = cyclical temperature range of oil in degrees centigrade (temperature at B minus temperature at A)
H = height of center of gravity of cooling surface above center of gravity of generated loss in inches

If the center of gravity of the generated loss is abnormally elevated, due to a tall core in proportion to the tank, or to appreciable auxiliary losses generated above the top of the core, the circulating force is reduced, the rate of oil circulation is reduced, the cyclical temperature range of the oil is increased, the top oil temperature is increased, and the hottest copper temperature is increased.

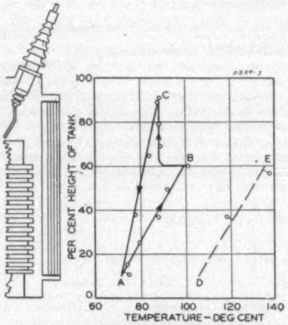


Figure 3. Thermal conditions in 2,500-kva self-cooled transformer

Short-time overload
——— Oil Temperature
— — Copper temperature
o o o Test points

If the resistance to oil circulation is known, it is possible to calculate the cyclical temperature range of the oil. A comparison of such calculations with test results is shown for a number of cases in Figure 2 of this discussion.

Figure 3 shows data corresponding to Figure 1 for a transformer undergoing short-time overload. Oil moving upward through the transformer winding from *A* to *B* rises in temperature and is discharged into the region above the core and coils at a temperature considerably above that of the oil already occupying this region. The streams of hot oil become dispersed and serve to bring about a slow increase in the temperature of the mass of oil between the core and the tank cover. Oil then moves down, from *C* to *A*, through the cooling tubes, completing the cycle.

Therefore, temperature of top oil is likely to be misleading when used in determining hot-spot temperature before thermal conditions have become constant. Temperature of oil in the cooling duct adjacent to the coil furnishes a better basis.

Case III. There are conditions for which the assumption of a single oil path is too simple to give results in reasonable agreement with test data. Such cases fall in the following categories:

(*a*). Transformers in which one winding is exposed much more freely to oil than another winding.

(*b*). Transformers in which a part of the heat dissipating surface is exposed much more freely to oil circulation than another part.

For these cases it is necessary to represent the transformer by a network of oil channels. For each oil channel the rate of oil flow is governed by the following equation:

$$P = rG + \rho V(1-\alpha T) - \frac{\rho\alpha M}{105G}$$

where

P = pressure at inlet minus pressure at outlet in pounds per square inch
r = flow resistance of channel in pounds per square inch per gallon per minute
G = flow through channel in gallons per minute
ρ = density of oil at 0 degrees centigrade in pounds per cubic inch
V = height of outlet minus height of inlet in inches
α = thermal coefficient of expansion of oil per degree centigrade
T = temperature of oil entering channel in degrees centigrade
M = moment of loss absorbed or dissipated by oil about horizontal axis through outlet in watt-inches

Performance of a network of channels can be determined from the foregoing equation by a process analogous to the application of Kirchhoff's laws.

Evolution of the concept of thermal behavior of oil in transformers requires creation of new terms. For many years the term "top oil rise" was used as though it were the only quantity necessary to specify the thermal condition of the oil. In recent years we have added the term "effective oil rise," and we have recognized the distinction between top oil rise and effective oil rise. A thorough analysis of thermal behavior of transformer oil would involve recognition of at least four different oil temperature rises as follows:

(*a*). "Effective dissipating oil rise" is the average rise of the moving oil adjacent to the tank dissipating surfaces. The "effective dissipating oil rise" is the dominant factor in the steady-state thermal performance of oil-insulated self-cooled transformers, and it can be calculated with a high degree of accuracy.

(*b*). "Effective absorbing oil rise" is the average rise of the moving oil adjacent to the coil dissipating surfaces. It is not necessarily the same for all windings. In normal transformers the "effective absorbing oil rise" is not appreciably different from the "effective dissipating oil rise" and they can be frequently assumed equal.

(*c*). "Effective thermal oil rise" is the average rise of all the oil. It determines loss absorbed by the oil under transient conditions.

(*d*). "Top oil rise" is the rise of the mass of hot oil at the top of the transformer.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): This paper is a very timely one and one that is of particular interest to me, because I have spent considerable time during the past year in obtaining data on hot-spot temperatures in oil-immersed transformers. Altogether about 50 heat runs have been made on three different transformer designs, each under a different condition such as self-cooled and forced air-cooled for both ultimate conditions and for short-time heavy overloads.

The authors show in Table I the gradient of "average copper to top oil." This leads, in some cases, to the assumption of negative coil gradients, a condition that cannot exist when the coils are dissipating heat. Average winding rise should, of course, be based over average oil temperature (not over top oil or maximum oil temperature) and hot spots in coils near the top oil should either be based over top oil or adjacent oil temperature.

The authors also show in Figure 6 that the difference in degrees between the hot-spot and the average winding temperature remains constant for loads ranging from 100 to 250 per cent. My tests show that this difference always increased with load, and under some design conditions it increased as the loss raised to a power ranging from 0.7 to 0.8, particularly under short-time overload conditions.

I would like to ask the authors how the losses of the 600-kva transformer were supplied to the oil. If the losses were supplied in a manner that caused the center of heat to change with load with respect to the center of the cooling tubes, this would affect the hot-spot rise over the average winding temperature and could easily account for some of the discrepancies between our results.

The authors used the method of calculating hot-spot temperatures given in my 1930 AIEE paper, "Loading Transformers by Temperature."[1] While not rigorously correct under some conditions, as pointed out by Mr. Wetherill, recent tests show that this is still the most practical method for obtaining reasonably accurate results.

I cannot agree with the statement made by the authors that the results of their tests make it possible to recommend much higher emergency overloads than those given in the American Standards Association Guides for Operation of Transformers. In the first place, they apparently did not use their test data on short-time overloads in their calculations of short-time overloads. In the second place, they used the same method of calculating overloads (as used in calculating the present ASA curves), except for the expedient of not making any correction for increased losses with temperature, since they assumed that the change in oil viscosity counteracted the increased loss. The effect of this however is small—being in the order of three to four per cent of overload values obtained.

The larger overload values proposed in the paper are mostly due to:

1. Using higher temperature limits than those used in calculating the ASA curves as for example 125 degrees centigrade instead of 115 degrees centigrade, for two-hour overloads.

2. Using 61 degrees centigrade hot-spot rise and 50 degrees centigrade top oil rise, instead of 65 degrees centigrade hot-spot rise and 45 degrees centigrade top oil rise as used in preparing the ASA curves. The difference obtained in the overloads when using 11 degrees centigrade, instead of 20 degrees centigrade, hot-spot rise over top oil (at rated load) is quite large.

While there are many small low-voltage transformers that meet the 11-degree hot-spot rise over top oil, there are also many large high-voltage transformers that approach a 20-degree hot-spot rise over top oil at rated load. Therefore, the ASA emergency overload curves were prepared for and apply safely to transformers that have a 20-degree hot-spot rise over top oil temperature.

Using the hot-spot temperature limits given in the paper, although in my opinion somewhat higher limits can probably be used, Table I of this discussion gives the calculated overload values of large high-voltage transformers in comparison with the overload values shown in Figure 7 of the paper apparently intended for small low-voltage transformers.

Table I. Emergency Short-Time Overloads

Time in Hours	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current Following			
		Small Low Voltage		Large High Voltage	
		No Load	Full Load	No Load	Full Load
1/4	140	3.0*	2.35*	2.4	2.0
1/2	135	2.6	2.10	2.20	1.80
1	130	2.25	1.90	1.95	1.65
2	125	1.9	1.71	1.65	1.45
4	120	1.55	1.45	1.4	1.32
8	115	1.33	1.33	1.25	1.23
24	110	1.2	1.2	1.16	1.16

*Values given in paper.

The characteristics applying under rated load conditions are shown in Table II.

Table II

	Small Low Voltage	Large High Voltage
1. Loss ratio	2:1	3:1
2. Top oil rise (degrees centigrade)	50	45
3. Hot-spot rise (degrees centigrade)	61	65
4. Time constant	4	3
5. Ambient (degrees centigrade)	30	30
6. Hot-spot rise over top oil and top oil rise over ambient vary as loss$^{0.8}$		

For intermediate classes of transformers intermediate overloads would of course apply.

It will be noted that the overload values for the large transformers are in general appreciably lower. This shows that different overload values should be used for different classes of transformers.

While I do not claim that one can calculate the exact amount of life taken out of a transformer during an overload period by the eight-degree rule, I feel that it is a useful tool—one that enables us to make progress. This suggests the consideration of even greater overloads than those shown above in cases where interruption of service cannot be allowed.

During the war period there may be cases where it would be advisable to take out of a transformer considerably more life than that used up for the overloads shown in Table I, to prevent a shutdown in a war plant or a station supplying power for war work. In fact a munitions manufacturer recently wished to purchase a spare bank of transformers of *minimum size* to carry the load during a period of one or two weeks in case a saboteur put his present bank out of commission. He was willing to sacrifice 100 per cent of the transformer's life.

I believe, therefore, that it is time for us to start studying the question of allowable overloads that will take out various amounts of the transformer's life. While one user may be willing to sacrifice say only one per cent of life during an emergency period, another user may be willing to sacrifice 10, 25, or 50 per cent and still another as mentioned above may be willing to sacrifice 100 per cent of the transformer's life to prevent a shutdown. Of course, caution would have to be used in applying such overloads to transformers already built, since there may be limitations other than thermal in such units.

REFERENCE

1. LOADING TRANSFORMERS BY TEMPERATURE, **V. M. Montsinger.** AIEE TRANSACTIONS, volume 49, 1930, pages 776–90.

H. W. Hartzell (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper has been read with considerable interest, because every contribution to the knowledge of loads that transformers may safely carry is especially valuable at this time when installed equipment may be called upon during emergencies to serve loads greatly in excess of name-plate ratings.

Continuity of service, prevention of costly shutdowns, and conservation of transformer equipment are some of the items which depend directly upon intelligent transformer application. With many loads growing rapidly and new loads being connected to power systems every day, it would seem that much thought should be given to the overloads that transformers can actually carry under the various conditions of operation.

Table III. Calculated Life for Transformers Operating Continuously at Hot-Spot Temperatures Indicated

Hot-Spot Temperature (Degrees Centigrade)	Calculated Insulation Life (Days)	
	Open Type	Closed Type
140	21	107
135	33	168
130	51	260
125	78	398
120	123	628
115	192	980
110	297	1,520

The authors have presented a table of suggested times and associated hot-spot temperatures which determine emergency overloads that might be applied to a transformer. Although these are of value, the overload ability of a transformer for longer lengths of time is of considerable interest. Momentary overloads occurring during system switching operations would probably be carried without too much regard to the transformer life consumption due to high overloads. The emergency ability of transformers for overloads having a duration of several daily load cycles warrants considerable attention, since under these conditions a substantial amount of transformer life may be consumed.

It would appear that the values given in this paper, although allowing greater loads than the proposed operating guide of transformer standards ASA-*C*57, may be extremely conservative. To illustrate this point Table III has been made from calculations which follow those of Nichols[1] for open-type transformers and Montsinger[2] for closed-type transformers.

With a hot-spot temperature of 130 degrees centigrade Table III indicates that the total life of the insulation in an open-type transformer would be $51\times24=1{,}224$ hours. If the transformer sustained two hours of operation per year when the hot-spot temperature remained at 130 degrees centigrade, this condition recurring annually for say 30 years, the total operation at this temperature would be 60 hours or $60/1{,}224\times100=4.9$ per cent of the calculated life of an open-type transformer operating at this temperature. For a closed-type transformer the life consumption would be $60/6{,}240\times100=0.97$ per cent, or practically a negligible amount for so many emergency operations. If calculations were made for the transformer insulation life loss during emergency loads limited by the temperatures and times proposed in this paper, since these temperatures would be attained only at the end of the time shown instead of continuing throughout, the loss of life would be much less than that indicated in the above illustration even though the unusually large number of emergency operations during the life of the transformer is considered. It would seem, therefore, that greater loads than those suggested in this paper should be considered for transformer emergency operations.

REFERENCES

1. Effect of Overloads on Transformer Life, **L. C. Nichols.** AIEE Transactions, volume 53, 1934, December section, pages 1616–21.

2. Loading Transformers by Temperature, **V. M. Montsinger.** AIEE Transactions, volume 49, 1930, pages 776–90.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The general method of determining permissible overloads outlined in this paper is correct, for it is a method which has been in common use for the past few years. However, the application of the method by the authors contains serious inaccuracies.

Nontypical transformers, having unusually low temperature rises were selected as a source of data. Values of temperature gradient even lower than those shown by these data were assumed and used for calculating allowable overloads. Consequently, the conclusions drawn are not conservative as claimed.

A brief summary of the most serious errors made in this paper follows: The examples in Table I of the paper are not typical standard 55-degree transformers, since all these transformers have temperature rises of less than 35 degrees centigrade. Any transformer user will testify that seldom, if ever, has he had a standard 55-degree power transformer which had a tested temperature rise of less than 35 degrees centigrade. Consequently, the data used as a basis are not data on typical standard transformers, but on transformers which contain more material than standard transformers and are, consequently, more expensive than standard transformers.

Even the data so obtained, however, were not correctly applied, but lower values of gradient between the copper and oil temperatures were assumed than were obtained on these special transformers. In the tabulation of test given in the paper, two of the measured gradients were above eight degrees at full load; these, however, are disregarded and a five-degree gradient is assumed for determining allowable overloads. A similar liberty was taken in determining the hot-spot correction. In the words of the authors, "On the average, the difference between the hottest copper and the average copper was eight degrees." Immediately following, the data are disregarded, and the authors state: "It appears that actual hot-test spot temperatures are in the order of six degrees or less above the average copper temperatures." This figure of six degrees is used in calculating the overloads instead of the eight-degree figure shown by their data.

While these differences are small in degrees, they are large in percentage and, consequently, they make a large error in the values obtained for allowable overloads.

The authors' claim in the conclusion that a more accurate method than previously used for calculating temperature rises has been derived is inaccurate, since the method used has been published before. To name one specific instance, this method was used in the December 1939 issue of the Allis-Chalmers *Electrical Review* in an article entitled "Simple Method of Calculating Transformer Temperature Rise Under Variable Loads."

Because of these fundamental errors, in applying the method, the conclusions reached in the paper are not conservative as claimed.

F. J. Vogel and **Paul Narbutovskih:** Mr. Sealey, in his discussion of our paper, has severely criticized it on the basis of the data furnished and the methods used. Typical of his criticism is his statement that "nontypical transformers having unusually low temperature rises were selected as a source of data." It was not claimed or intended that these were typical of commercial practice, but they were selected because the data for these transformers were available and were used to show that the methods of calculations commonly used are in error. Contrary to Mr. Sealey's further discussion, test data indicate that the conclusions reached in the paper are conservative, and there is a definite possibility that similar values will be recommended for general use.

Mr. Wetherill furnishes some very interesting data in his discussion regarding the thermal behavior of transformers. His analysis of the various cases is good, even though one might wish that the experimental data were given more completely. The fundamental considerations underlying the theory of his first case are essentially in line with those given in the paper. The data given on Figure 2 of the discussion were very interesting to us. The data furnished for transformer *A* seem to be a confirmation of our results. The results given for transformers *B* and *C* do not furnish as good a confirmation, but it is to be noted, that the actual oil temperatures are not given, and so there is no way of comparing the results with the theory outlined in our paper on the effect of viscosity. However, in both cases of transformers *B* and *C*, the variation of the difference between the top oil and the average oil over the range of loads from 100 per cent to 140 per cent load is not over four or five degrees centigrade. It also appears to us that transformers *B* and *C* are not entirely typical. Particularly in the case of transformer *C*, there might be a difference between the average oil and the top oil of nearly 15 degrees at 100 per cent load, and this in turn would lead to the transformer having a hot spot at least 15 degrees above its average temperature, which is not in conformance with the present standards.

In regard to the effect shown on Figure 3 of the discussion it is somewhat difficult to understand why this effect will appear on short-time overloads only. One is likely to encounter this effect when the oil circulation is partially impeded around one of the windings, such as an internal winding of a core-type transformer with a narrow vertical duct. But in this case the effect will appear in varying magnitude at all loads. The difference between the temperature of oil issuing upward from the vertical ducts and the tank oil elsewhere on the same level would be a variable quantity, depending on the magnitude of the load and a change in the oil flow as affected by the pressure difference and the friction head, including the effect of viscosity. A practical way of taking care of this phenomenon in computing the winding temperatures is to treat it as a component of the hot-spot gradient, although it will not vary quite as much as the 0.8 power of the losses. It may be estimated that this component at 100 per cent load may amount to about three degrees centigrade.

Mr. Montsinger questions the data in Figure 6 of our paper, wherein we showed that the difference between the hot-spot temperature and the average winding temperature remain constant for loads ranging from 100 to 250 per cent. He states that in his tests the difference always increased with load. We have confirmed the results obtained in Figure 6 with tests on a number of other units and feel that Figure 6 is in reasonable agreement with the facts. A small proportion of the losses of the 600-kva transformer was supplied above the coils, but, due to the other tests made, we believe that this had little effect. Further, additional study of the results themselves confirmed this belief. There are some conditions, however, under which this effect will not hold. Two of the simplest of these are:

1. The case where the top coil is more heavily insulated than the coils in the stack.

2. If shielding or other construction details prevent as free coil ventilation in the top coils as in the stack.

However, Mr. Montsinger has made a more serious comment than his remarks regarding the accuracy of the test data. He does not agree that the results of our tests make possible recommending higher emergency overloads than those given in the ASA Guides for Operation of Transformers. We did use our test data in that we showed that many transformers had lower hot spots than had previously been believed or, at least, used in similar calculations. Also, we did show that the increased losses, due to increased copper resistance at higher temperatures, were counteracted by lower gradients and increased oil flow, due to changes in oil viscosity. The fact is that higher temperature limits than those considered in the present Proposed ASA Guide for Operation are given in the paper, and this presentation is in agreement with the papers and discussions of "Temperature Limits Set by Oil and Cellulose Insulation," by Dr. Charles F. Hill, AIEE TRANSACTIONS, volume 58, 1939, September section, page 484, and "Loading Transformers by Copper Temperature," by Mr. H. V. Putman and Mr. W. M. Dann, AIEE TRANSACTIONS, volume 58, October section, page 504.

It was not intended that the characteristics given in the paper and used in the calculation of Figure 7 of the paper necessarily applied to all transformers, nor that additional classifications might not be desirable. For this purpose, the authors are prepared to suggest a list of several types of transformers with their respective characteristics. The authors believe, however, that for any of these types, a 20-degree gradient suggested by Mr. Montsinger is high.

Mr. Hartzell's discussion is very interesting to us, since it shows that some operating engineers are also thinking that higher overloads can be carried for emergency service than given in the operating guides in the proposed ASA Standards. We agree that the values proposed in the paper are conservative. We hesitated to go to the extremes, although we may have fundamental data which would indicate possibilities, because

1. It would be a radical departure from past practice.

2. There are other factors involved, such as, contacts, bushings, tap changes, and so forth.

High-Speed Single-Pole Reclosing

Discussion and authors' closure of paper 42-24 by J. J. Trainor, J. E. Hobson, and H. N. Muller, Jr., presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 81–7.

S. B. Crary, L. F. Kennedy, C. A. Woodrow (General Electric Company, Schenectady, N. Y.): The detailed technical information at present available on the problem of high-speed reclosing seems to be well understood. There appears to be no outstanding differences in the results of the stability analyses presented in the Trainor, Hobson, and Muller paper and in our paper. Manufacturers can supply as standard equipment either single-phase or three-phase reclosing breakers. It is agreed there are fields of application for both.

However, there does appear to be a difference of opinion in the interpretation of the technical data. This difference is in the extent of the field of application for single-phase reclosing breakers as compared with three-phase reclosing breakers.

In most stability studies double line-to-ground faults and three-phase faults have been used as a basis for determining the stability of the system. It would appear, if all at possible, that faults involving more than one phase should continue to be the criterion for reliable power delivery. This philosophy of system design, if continued, would result in the emphasis being placed on high speed of clearing, with possible shorter deionization times, rather than on the development of single-phase slower speed reclosing systems for single line-to-ground faults only.

If this approach is followed, reclosing will become much more beneficial to the over-all performance of the system and give it a more important place in system protection, since it would generally allow for successful operation through even the most severe faults rather than only line-to-ground faults.

J. Trainor (Public Service Company of Indiana, Inc., Indianapolis, Ind.): The transmission system of Public Service Company of Indiana, Inc., as it now exists, contains 1,837 miles of 33-kv line, 796 miles of 66-kv line, and 409 miles of 138-kv line, with 120 additional miles of 138-kv line under construction and 45 additional miles planned for immediate construction. When the expansion of the 138-kv system was undertaken, careful consideration was given to the available means for improved operation. The importance of these lines as major links in the interconnection of the systems of Louisville Gas and Electric Company, Cincinnati Gas and Electric Company, American Gas and Electric Company, and Northern Indiana Public Service Company and Public Service Company of Indiana, Inc., makes it necessary that their operation be of the best. For example, our load in the Newcastle area is 25,000 kw, but the duty on the Lenore–Newcastle line has reached 65,000 kw during power transfers for Cincinnati Gas and Electric Company and Indianapolis Power and Light Company to the system of American Gas and Electric Company.

Because of existing insulation levels, ground-fault neutralizers could not be applied to the system with reasonable cost. We have watched the operation of three-pole reclosing on neighboring systems in Indiana with great interest and we recognize its value. However, we felt that definite improvement in operation would be obtained by the use of single-pole reclosing:

1. The disturbance to the rest of the system attending the dropping of load being carried by the line when a fault occurs will be decreased if the amount of load dropped is decreased.

2. While the amount of load to be carried by the line may not be near the stability limit when the line is constructed, major tie lines are often called upon to carry unforeseen amounts of power during emergencies—that is, loss of generating capacity or loss of other tie-line capacity—and any increase in the stability limit of the line may prove invaluable at such times.

3. It is our belief that single-pole reclosing offers all the advantages of three-pole reclosing, and will prove even better in most cases.

J. E. Hobson and **H. N. Muller, Jr.**: We are glad to have the comments of Crary, Kennedy, and Woodrow regarding the type of fault to be used as a basis for determining system stability. It has, of course, been the practice to predicate system design on the ability to endure a double line-to-ground fault. It might seem logical to revise this basis somewhat, as additional data regarding line performance become available, and to base system design on the probability of instability for all types of faults. This approach would include considering the likelihood of occurrence of the various types of faults, the probability of faults occurring, anticipated loads on the system as a function of time of the day and time of the year, and so forth. The basis of system design would then be an estimated allowable probability of outage, taking all of these factors into consideration. This philosophy of design can become more and more effective as additional data regarding line performance become available. Under the present system it hardly is reasonable to base the stability performance of a well-protected, high impulse-level line and the stability performance of an improperly shielded, low impulse-level line on double line-to-ground faults. The probability of occurrence of double line-to-ground faults per mile per year on the latter line may be even higher than the probability of occurrence of single line-to-ground faults per mile per year on the well-designed line, having well-co-ordinated line-ground and line-line insulation. The actual performance of the two lines, each carrying a block of power calculated as the permissible limit to "ride through" a double line-to-ground fault, would be far different. Since the proportion of all faults which will be of the double line-to-ground type is likely to be low on a well-designed line, the probability of any fault occurring is low, and the combined probability of a double line-to-ground fault occurring at the time of peak transmission may be very low, it is perhaps not illogical to base the stability limits of such a line on single line-to-ground faults.

Analysis of the Application of High-Speed Reclosing Breakers to Transmission Systems

Discussion and authors' closure of paper 42-31 by S. B. Crary, L. F. Kennedy, and C. A. Woodrow, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 339–48.

Eric T. B. Gross (College of the City of New York, N. Y.): When comparing high-speed reclosing with ground-fault neutralizers, it seems to me that all advantages of both systems should be considered. In this country, I believe, all earth-fault coils which have been put into service are being designed for a very short period of operation only, whereas hardly any coil in service anywhere abroad has been designed for less than two hours operation, but most of the coils are for continuous operation. In many cases it has been a distinct advantage that the operation could be continued for a few hours until the point of the fault had been located,[1] and the line-section with the fault switched off the system without any "shock" resulting from a ground fault. In such systems there are no automatic outages for any single ground fault so that the protection is extended from the self-clearing ground fault to all single-phase ground faults.

The temporary operation with two-phase wires at full line-to-line voltage above ground does not bring severe additional stress on the insulation of the system; in hardly any case will this occur for a period longer than 12 hours altogether in a year (the sum of the durations of all single-phase ground faults during 12 months). It seems to me that in this country the field of application of ground-fault neutralizers lies within systems of lower voltages, that is up to about 66,000 volts, and in such systems the cost of the neutralizer should be small in comparison with the cost of relays and apparatus for quick reclosing at each end of the line sections.

REFERENCE

1. SENSITIVE GROUND PROTECTION FOR TRANSMISSION LINES AND DISTRIBUTION FEEDERS, Eric T. B. Gross. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 968–71.

J. E. Hobson and **H. N. Muller, Jr.** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Power-system and application engineers appreciate the benefits to be obtained in increased transient power limits and reliability of service from the use of ground-fault neutralizers and high-speed reclosing, both gang- and single-pole operated. The scheme to be used in any situation will be determined after a careful evaluation of the engineering and economic factors involved. The curves presented by the authors, resulting from a-c network calculator studies, will be useful in helping to evaluate these engineering factors.

Since the most commonly encountered problem today is that of raising transient power limits and improving reliability of service on existing transmission systems, and since those existing systems 110 kv and above are, almost without exception, operated with a grounded neutral, the ground-fault neutralizer is usually uneconomical compared with other schemes. This is caused by the extensive system changes necessary to convert a grounded-neutral system to utilize a neutralizer.

Several of the generalized statements contained in the various sets of conclusions presented in this paper merit discussion. A few of the more important conclusions, relating particularly to the choice between three-pole or single-pole reclosing, will be considered.

The authors state that 50 per cent longer deionizing time may be required for satisfactory arc extinction on a 100-mile line if single-pole rather than three-pole reclosing is used. It is recognized that charging current from the sound phases will flow in the arc while the faulty phase is isolated, and it is also recognized that this may be an important or even limiting consideration when very long lines are involved. Existing data on time for arc deionization and on critical arc currents are so meager that the discussers question whether it is possible to state any figure such as the 50 per cent presented by the authors. It should also be remembered that charging current is not only a function of line length, but a function of the square of the voltage, so that any limitation imposed by line length has little meaning unless it is qualified to apply to a particular voltage class. It is not believed to be practical, in view of the absence of definite data, to state for what line lengths and voltage classes arc deionization becomes significantly longer when single-pole rather than three-pole reclosing is applied. We believe this factor is not likely to be important for the shorter lines usually encountered.

The authors state that single-pole reclosing appears to offer important advantage, only on single-circuit interconnecting lines, and when the generating capacity of one of the interconnected areas is not much greater than the load on the interconnecting line. We have performed calculations on a system where the receiver was assumed to be infinite, and where the sending end capacity was several times the load to be transmitted over the interconnecting line. Rather large gains in transient power limits for single-pole as compared to three-pole reclosing resulted for reclosing times of both 35 and 20 cycles. For 35-cycle reclosing this gain was 42 per cent, and for 20-cycle reclosing 22 per cent applies.

The authors also state that single-pole reclosing offers no advantage over gang operation for faults involving more than one phase. Our calculations show that for double line-to-ground faults the gain is smaller than for single-phase faults, but in particular cases this gain may still be quite significant.

Transient power limits obtainable with 20- and 35-cycle three-pole reclosing are compared with the limits obtainable with 35- and 60-cycle single-pole reclosing. Conclusions drawn by the authors from such a comparison are somewhat exaggerated in favor of the three-pole operation, even if the 50 per cent longer deionizing time given in the paper is assumed to be necessary for satisfactory single-pole operation, since 35-cycle operation permits over twice the de-energized time that 20-cycle reclosing permits. A similar comparison exists for 60- and 35-cycle reclosing speeds. Even then, a conclusion drawn from this comparison states that a gain is realized by the use of 35-cycle single-pole as compared to 20-cycle gang reclosing.

We suggest that the more rigorous method of symmetrical components could be used to advantage in calculating the arc current in the faulted phase during the de-energized period, rather than the approximate method used by the authors. The quantity V_a' in the equations of appendix III may differ considerably from 0.5 V_b under the unbalanced condition with one conductor isolated.

J. J. Trainor (Public Service Company of Indiana, Inc., Indianapolis, Ind.): These authors seem to prefer the use of ground-fault neutralizers to that of single-pole reclosing.

Probably no one in the country is better aware of the operating advantages and the improvement in line outages and service interruptions to be gained through the use of ground-fault neutralizers than we. In the United States, the fifth installation of ground-fault neutralizers and the sixth neutralizer, was placed in service by Public Service Company of Indiana, at New Castle, Indiana, on December 6, 1936, on a 33-kv delta transmission system having about 160 miles of wood-pole line without static wires. After a few months' observation of the operation of this neutralizer, its value was appreciated, and four additional neutralizers were ordered from the manufacturer. The seventh installation in this country, involving these four units, was completed on August 15, 1937, for the protection of approximately 1,300 miles of 33-kv line without static wires on a delta system. At this time, we had 45 per cent, or 5 of the 11 neutralizers in service in the country, protecting 54 per cent, or 1,460 of the 2,700 miles of line so protected.

It may be of interest to note that we operate some 1,200 miles of 33-kv delta line in parallel with three ground-fault neutralizers on the parallel system. This operation was not at all practical before the installation of the neutralizers, and we were obliged to separate this section of the system into three isolated parts. It may be of further interest that we note no appreciable difference in the ratio of line outages to total system faults on the system having three neutralizers in parallel to the same ratio on the two isolated sections protected by individual units, the ratio being 60 per cent to 65 per cent in either case. Up to December 31, 1941, the neutralizers had successfully cleared 1,687 of a total of 2,634 system faults, or 64 per cent.

However, the present discussion is concerned with the operation of 138-kv transmission systems, most of which are designed and insulated for grounded-neutral operation. The application of ground-fault neutralizers on such a system requires that all line, insulation, and arrester voltages be increased to the value required for delta operation on the entire system, or that these be increased to the required value on that section of the system to be protected by the neutralizer, and an insulating transformer installed between that section and the remainder of the system. In view of the practically complete interconnection of the entire 138-kv systems in the eastern half of the country, involving thousands of miles of line and numerous substations, the isolation of particular systems by means of insulating transformers would be most expensive. This difficulty would be further complicated by the number of points at which interconnections occur. In our own system, we would require five insulating transformers for complete isolation, the locations being Lafayette, Kokomo, New Castle, Cincinnati, and Louisville. Such transformers would limit the useful capacities of the lines to the capacities of the transformer banks.

S. B. Crary, L. F. Kennedy, and **C. A. Woodrow:** The discussers indicate some differences of opinion as to the interpretation of the network-analyzer stability results, although not in the results themselves. This is not surprising, as the application of these results depends upon all the factors including the economic ones which affect the individual system or situation. Our objective was to show the technical factors which enter into the selection of means for improving reliability and the necessity of basing a selection upon the system characteristics.

Messrs. Hobson and Muller believe that a 50 per cent longer deionization time for satisfactory arc extinction on a 100-mile line, if single-phase rather than three-phase reclosing is used, is overly conservative or pessimistic. This assumption was made to illustrate the use of the curves in obtaining a comparison between single-phase and three-phase reclosing. The curves are such that any assumed de-energization time may be used. However, the general conclusions will be unchanged whether the single-phase reclosing time is or is not appreciably increased over three-phase reclosing. For the line-to-ground fault case, the rate of change in power limit with de-energization time is not very great. Messrs. Hobson and Muller point out that they have calculated rather large gains in power limits for single-phase as compared with three-phase reclosing, in one case 22 per cent gain in limit for 20-cycle reclosing. Although they do not specifically say so, this is undoubtedly for a line-to-ground fault. For the conditions they have outlined of an infinite receiving end and a sending-end capacity several times the transmitted load, it may be questionable whether this additional increase in transient limit for a single-line-to-ground fault is necessary. We have found that for systems of this kind the line-to-ground fault power limit with single-phase reclosing is usually appreciably higher than the load required to be transmitted over the circuit. This, we believe, is an important consideration when determining the advisability of using single-phase reclosing rather than three-phase reclosing. For systems of this type, as pointed out in our paper, three-phase reclosing can adequately provide stability for even the more severe three-phase faults with 20-cycle reclosing for the required line loadings. For short lines, where the thermal limitations of the circuit set the maximum power which can be carried, it may not be economical to use means which increase the stability limit for line-to-ground faults above the thermal load limit.

For double-line-to-ground faults we cannot agree with Messrs. Hobson and Muller that the gain may still be quite significant over three-phase reclosing. In the first place the gain is small even for the same reclosing time. Secondly, on many systems the number of three-phase faults may be as great or greater than the number of double-line-to-ground faults. Thus the advantages of single-phase reclosing for other than line-to-ground faults are very questionable.

In our paper results are shown for wide-range reclosing times. In one section of the paper we interpreted these results in terms of present-day standard breakers. Since the present application of single-phase reclosing breakers has been for longer reclosing times than three-phase reclosing breakers, it was natural for us to compare 20- and 35-cycle three-phase reclosing breakers with 35- and 60-cycle single-phase reclosing breakers, respectively. However, any other sort of desired comparison can readily be made from the data included in our paper. We do not believe that reasonable variations in practical comparisons will give results which are inconsistent with our general conclusions.

Messrs. Hobson and Muller suggest a more rigorous method be used for calculating the arc current in the faulted phase during the de-energization period rather than the approximate method used in appendix III of the paper. In using the network analyzer we represented the system as three separate phases with a neutral return circuit representing the ground and ground-wire return. With this representation we were able to impose on the circuit the simultaneous dissymmetries which occur, due to clearing line-to-ground or double-line-to-ground faults with single-phase switching. On the network analyzer the currents in the arc and the fundamental frequency recovery voltage after the fault is cleared were easily obtained during the angular swing when the faulted conductor is de-energized, while the method of symmetrical components becomes quite involved when the number of simultaneous dissymmetries exceeds two. These results indicated that for the angular displacement at which reclosure must take place in order to maintain stability, the magnitude of the arc current and the steady-state recovery voltage was not reduced very much from that given in appendix III. Because of this confirmation, it was felt that the approximate method gave a reasonably correct quantitative value for the arc current and recovery voltage which should be allowed for during single-phase de-energization. Furthermore, the current and voltage at the arc would conform most nearly to the approximate method under those conditions of light system loading and would be (except for effect of distributed constants) almost entirely correct at no load.

We regret that the limited reference to ground-fault neutralizers has lead Messrs. Hobson, Muller and Trainor to emphasize the limitations of this device under some conditions where high-speed reclosing appears to be the sound solution. We had no intention of attempting to offer the ground-fault neutralizer in place of reclosing. It was felt, however, that it should be mentioned as it provides an easy means of minimizing the effects of single-phase-to-ground faults where applicable.

Mr. Gross's discussion calls attention to differences between foreign and domestic practice in the operation of ground-fault neutralizers. The question of length of time to operate a ground-fault-neutralizer system with a permanent ground fault depends on many factors. In general, operators in this country prefer to switch permanent faults off quickly. This is the case even when neutralizer coils have been provided with a continuous rating. The type of system influences this considerably. If the system is of relatively low voltage 33 to 66 kv, for example, and essentially radial in character covering a large area, it becomes good practice to switch permanent faults off quickly in order to avoid simultaneous faults and an increase in the amount of load interruption. Other advantages also result, such as reduction in telephone interference, patrol time, and number of incorrect relay operations due to simultaneous faults.

Performance of Ground-Relayed Distribution Circuits During Faults to Ground

Discussion and authors' closure of paper 42-8 by C. L. Gilkeson, P. A. Jeanne, and J. C. Davenport, Jr., presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 40–8.

William Shuler (Dayton Power and Light Company, Dayton, Ohio): I wish to give an operating man's impression of the value of instantaneous ground relaying on distribution circuits during faults to ground. We started originally with ground relays connected on two 12-kv rural circuits. These relays operated in from one to two cycles, and, after clearing the fault, the breaker was immediately reclosed. The average time from the initiation of the fault to the reclosing of the breaker was approximately 35 cycles, and an operation for the second opening, should the fault persist, was controlled by time-induction relays which were set to permit main and branch-line sectionalizing fuses to blow. We were so well satisfied with the success of this scheme that ground relays either have been or are being applied to all of our four-wire feeders, including our heavily loaded power circuits in urban territory.

Some tests were made on one feeder supplying a customer who has a large load consisting of squirrel-cage, slip-ring, and synchronous motors, as well as rotary converters. A fault was placed on this circuit which was cleared, and the breaker reclosed without losing any motor load except two motors whose service depended upon contactors energized from the a-c source. The 250 volt d-c rotary converter, which was loaded only to 25 per cent, went through the interruption without flashing over or dropping out of step.

As I see it, the use of ground relays and instantaneous reclosures on overhead circuits has two advantages:

1. It gives a type of service almost equal to duplicate service.
2. It very materially reduces the damage to overhead conductors because of fault currents.

In applying ground relays to circuits having main or branch-line sectionalizing fuses, it is necessary that maximum short currents available be known, and that a fuse be selected having a time current characteristic which will permit the fuse to go through one operation without damage before the relay and circuit breakers clear the circuit.

In some cases it has been necessary for us to install reactors at the substation to hold the short-circuit current down to a value which will protect the fuses. On those feeders where this has been worked out carefully, no trouble has been experienced, and, although our rural feeders are very heavily fused, we have relatively few main or branch-line sectionalizing operations.

In the paper mention is made of the fact that 40 amperes is about as low a fuse as can be used successfully. It is my opinion that fuses having a lower rating may be used on long circuits since it is only a question of the fuse being able to carry without damage the short-circuit current available for longer than seven cycles.

I also want to mention the fact that we are now occupying poles jointly with The Ohio Bell Telephone Company, in territory near our urban district but removed from our four-kv system where we need only one phase wire and a neutral of a 12-kv feeder and can fuse the branch lead at a value which is satisfactory to the telephone company engineers. An engineering study is made of each case, but as a result of several years' co-operation we now have many poles jointly used.

I want to express my appreciation for the work done by the project committee of the Joint Subcommittee on Development and Research that collected the data which formed the basis for this paper. Our company is of the opinion that this work was of a very considerable value to us.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The widespread use of instantaneous reclosing relays has greatly enhanced the value of any operating scheme which permits substituting additional short-time interruptions in order to reduce the number of sustained interruptions. The comprehensive oscillograph information of the four companies' studies is excellent confirmation of the good performance secured by a large number of operating companies with instantaneous reclosing and ground relaying.

Several years' experience on the system of the former Tennessee Electric Power Company convinced the writer that even more sensitive ground settings than the author's suggested limit of 25 amperes may be used without danger of "tree grounds" causing unnecessary operations. Many three-phase, three-wire, 11-kv grounded neutral circuits on that system were operated with ground relay settings as low as 10 amperes primary.

There is some advantage in following the practice of one of the companies studied, namely, that of permitting the high-speed relays to be in operation for the instantaneous reclosure, as well as for the original disturbance. Objects such as tree limbs which are blown onto the line will sometimes fail to fall away from the line in the brief interval of instantaneous reclosing, and the second operation of the fast ground relay prevents an unnecessary sustained interruption to the affected branch at the expense of a few seconds' interruption to the entire feeder.

Fortunately, the telephone cable or conductor of a joint use installation provides a low enough resistance path to ground so that reliable operation is obtained with ground relays set on the order of full load current. It is much more difficult to insure that a fallen conductor in contact with the ground will be cleared properly, since contact resistance may limit the fault current to a value materially less than the full load current. In the case of three-wire circuits, or even four-wire circuits with neutral grounded at the supply station only, ground relays may be set independent of load current, and standard devices provide as sensitive settings as practicable. However, devices and methods are still lacking for providing sensitive ground relaying on the more common three-phase, four-wire circuits, especially at the lower distribution voltages.

G. Fred Lincks (General Electric Company, Pittsfield, Mass.): Messrs. Gilkeson, Jeanne, and Davenport's presentation of operating data indicates the advantages of a system of relaying that apparently is being adopted by an increasing number of utility companies. Their data support the conclusions as to the improvement in service continuity obtainable with such relaying as presented in my paper, "Relative Values of Different Types of Overcurrent Protection for Distribution Circuits." Table I of this discussion, giving additional data to that in the curves of my paper, shows the relative improvement obtainable with single-element fuse cutouts employed at each of the branches or at a number of the sectionalizing points connected in series, when nonreclosing relays, reclosing relays, or reclosing relays with the instantaneous ground relaying facilities are used. The authors only refer to branch-line fusing. Table I shows that relaying—so the substation breaker opens once ahead of all sectionalizing or branch protective devices and then provides time delay so these devices will clear ahead of the second opening of the breaker—generally provides greater benefits in the improvement in service continuity with line sectionalizing than with branch protection. Such relaying permits securing greater reductions in consumer minutes outage with less costly branch and line-sectionalizing equipment than is required with nonreclosing or reclosing relays and breakers. For example, single-element sectionalizing fuses with the instantaneous ground relaying are more effective than an equal number of three-element reclosing cutouts with just a reclosing relay.

The authors state that "instantaneous ground relays would not be expected to save branch-line fuses below 30- or 40-ampere rating." It is assumed that this limitation applies to the system for which the data are reported. In setting up such a relay and line protective scheme, the minimum fuse rating would be determined by comparing the total clearing time for the relay and breaker with the melting time-current characteristic curves for the fuses. A 25 per cent factor should be applied for variables (see Figure 14, "Trends in Distribution Overcurrent Protection" by G. F. Lincks and P. E. Benner, AIEE TRANSACTIONS, volume 56, 1937, January section, pages 138–52). The minimum fuse rating which will provide the desired selectivity of operation will depend on the relay setting and breaker opening time and the current available at the point on the system where the line protective device is located. In studies on actual circuits

Table I. Comparison of the Effectiveness of Substation Protective Equipment

With Single-Element Cutouts and 75 Per Cent Temporary Faults

No. of Branches or Sectionalizing Points	Substation Protection			Maximum Protection Obtainable (Permanent Faults Only Cause Outage)
	Nonreclosing Breaker	Automatic Reclosing Breaker	Reclosing Breaker Set for 1st Opening Ahead of Line Fuses	
	Per Cent Consumer Minutes Outage*			
Protecting 5-Mile Branches				
0	115.7	101.0	101.0	100.0
1	85.0	75.0	73.9	72.2
2	65.5	57.8	56.1	54.8
3	51.8	46.0	44.2	42.8
4	42.0	37.2	35.6	34.5
5	34.8	31.0	29.4	28.4
6	29.2	26.1	24.7	23.8
Protecting at Sectionalizing Points				
0	115.7	101.0	101.0	100.0
1	100	92.6	74.3	64.0
2	92	87.0	64.5	53.2
3	87.5	83.4	60.0	48.0
4	84.2	81.3	57.0	44.0
5	81.7	79.5	54.8	42.3
6	80.8	78.5	53.5	40.9

$$\text{* Per cent} = 100 \times \frac{\text{Consumer minutes outage caused by permanent faults plus temporary faults for any specific system setup}}{\text{Consumer minutes outage caused by permanent faults alone with no branch protection or line sectionalizing}}$$

it has been found that with instantaneous ground relaying, proper selectivity of operation is possible with fuse ratings as low as 10 amperes where such fuses are located well out on the line where the available short-circuit currents are low.

The authors' report on the high quality of the performance of fuses is indicative of the improvement that has been made in this product during the past few years. It shows that modern fuse links are meeting the requisites of greater dependability and accuracy demanded by such co-ordination practices.

C. L. Gilkeson, P. A. Jeanne, and **J. C. Davenport, Jr.:** The authors wish to thank Messrs. Shuler, Brownlee, and Lincks for their interesting and instructive discussions of this paper. In reply to the question regarding the statement in the paper that instantaneous ground relays would not be expected to save branch-line fuses below 30- or 40-ampere rating, the authors were referring to relatively high kilovolt-ampere capacity feeders, such as those on which most of the observations were made. Where the fault current is limited to relatively small values, either due to long feeders or neutral impedance, smaller fuses could be protected, as Mr. Shuler stated.

In suggesting minimum ground-relay settings of 25 to 35 amperes, the authors wished to be conservative, particularly with respect to four-wire multigrounded neutral feeders. As Mr. Brownlee states, lower settings may be feasible, and have been used on three-wire unigrounded neutral systems.

The following errors appeared in this paper as published in the AIEE TRANSACTIONS (volume 61, 1942, January section, pages 40–8):

1. Table II. The subheading "Instantaneous" under "Ground Relays" applies only to the column just preceding "Reclosure Practice."

2. Table III. In the footnote ## "cleared" should read "clear."

3. Table B. In the title "Local" should read "Load."

Relative Value of Different Types of Overcurrent Protection for Distribution Circuits

Discussion and author's closure of paper 42-2 by G. F. Lincks, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 19–26.

R. O. Loomis (Georgia Power Company, Atlanta, Ga.): Line-sectionalizing devices provide an economical means of reducing interruptions to electric service, which means improving the quality of the service. As in practically all engineering problems, the cost is the important element. Since investments in distribution lines are an important part of total system investments, opportunities for reducing these investments cannot be overlooked. Mr. Lincks has compared in this paper the relative value of different types of devices, enabling selection of the method providing the greatest improvement at the lowest cost. Mr. Lincks has made a definite contribution by providing a method of comparison. This paper is very pertinent at the present time when restrictions in material will prevent more expensive methods of improving service.

In applying sectionalizing devices, it should be remembered that proper co-ordination is essential. I suppose that one of the assumptions employed was that perfect co-ordination was obtained, and that no fuses broke mechanically or blew because of poor mechanical condition of cutouts. Some forms of cutouts tend to overstress the links mechanically upon closing. Cutouts in poor condition may overheat. Mechanical failure of reclosing fuses to reclose occurs. It will be necessary to use only one make and type of link on a given line. This usually means the use of only one make and type of link by the entire company, or at least by one operating district. Attention to such details is necessary to approach perfect operation. I would like to know what the effect on the results would be if an assumption had been made that, say one fuse link out of ten blew incorrectly, or unnecessarily.

Assumption 6 states that a trouble crew is available to start instantly. In rural areas where much of the sectionalizing is employed, this is seldom true. The additional time required apparently increases the benefits of sectionalizing.

In assumption 7, permanent faults are repaired very rapidly. Sometimes such faults require a crew larger than the trouble crew, and sometimes they are difficult to find, so I am inclined to think 30 minutes on the average is low. Increasing this time apparently increases the benefits of sectionalizing. Sectionalizing helps in locating the faults difficult to find, which is an added advantage.

Assumption 10 is often not true in rural areas. On the other hand, in urban areas where short-circuit currents are larger, and relay current and time settings are higher, because of larger loads, and to co-ordinate with larger transformer fuses, branch fusing may prevent burning down lines, since the branch fuse may blow in a time short enough to prevent damaging the conductors, where the substation breaker will not open quickly enough to prevent burning of conductors. This advantage is also obtained by using pole-mounted oil circuit reclosers, but they are not so adaptable to urban distribution as sectionalizing fuses. Although mechanical breakdown of conductors by automobiles, trees, and so forth, occur, I have not found a case where conductors burned down when protected by oil circuit reclosers.

The curves presented deserve careful study. They show the effects of a high percentage of temporary faults, particularly on line sectionalizing. I am surprised that the percentage of temporary faults does not have more effect in branch fusing as shown in Figure 5 of the paper. The conclusion that "Single-element fusing of branches provides approximately 85 to 95 per cent of the total improvement obtainable" is rather startling to me. I would like to know what effect the percentage of temporary faults has on branch fusing when all the customers are on the branches: for example, when all branches at the end of a trunk feeder are fused. Other conclusions appear reasonable and justified.

C. R. Craig (General Electric Company, Pittsfield, Mass.): Because of the fact that conclusions drawn from a mathematical study of operating conditions must necessarily be tempered by experience and inherent characteristics of individual systems, it should be borne in mind that the benefits of the different types of protection presented in Mr. Lincks' paper for a system as a whole may just as well be applied to only a portion of a particular system. If this portion of the system were subject to an especially high percentage of temporary faults, as compared to the system as a whole, an efficient combination of protection on the troublesome branches or small section of feeder would materially reduce the percentage of temporary faults on the entire system, thereby lowering the over-all outage time.

In paragraph 5, under the heading "Of What Value Is Branch Protection," the author calls to attention one of the most important conclusions. This shows that single-element cutouts furnish the major percentage of the total improvement attainable by branch protection. From the standpoint of initial cost, this means that the single-element cutouts give the most protection for the money invested. For example, let us assume some comparative costs of protective equipment, and then determine the comparative benefits of the combinations of protective equipment.

While actual costs may vary a great deal, for the sake of illustration, the following somewhat arbitrary values are assumed for initial installed costs:

One-element cutout	$ 20 each
Two-element cutout	30 each
Three-element cutout	70 each
Line automatic recloser	120 each
Nonreclosing substation	
Breaker and relays	1,000 each
Automatic reclosing substation	
Breaker and relays	1,350 each

The division of the total cost of branch protective equipment by the cost of substation protective equipment alone will give the per cent increase in initial cost of combined substation and branch protection over that for no branch protection. This result can be compared with the per cent improvement in system protection as shown in the paper for the corresponding system setup (see Figures 5 and 8 of the paper). A summary such as Table I of this discussion, constructed from the above assumptions and data calculated for the paper, serves to illustrate the comparison between attainable improvement in protection and the cost of the protective equipment.

Even though the above assumed comparative costs may not be exact for cutouts or breakers, it is evident from Table I, for example, that with a nonreclosing substation breaker, single-element cutouts on branches furnish 52.7 per cent improvement in protection with only 6 per cent increase in total installed costs over no branch protection. An increase in cost of 15 per cent (21–6 per cent) for three-element cutouts over the cost of single-element cutouts is necessary to obtain only 1.3 per cent (54–52.7 per cent) increase in protection furnished by the three-element in place of single-element cutouts. Similar comparisons may be made with the

Table I

		For 75 Per Cent Temporary Faults			
Substation Protection	Three 5-Mile Branches† Branch Protection	Consumer-Minutes Outage in Per Cent of "Yardstick"	Initial Cost	Per Cent* Improvement in Protection	Per Cent Increase in Cost
Nonreclosing breaker ($1,000)	None	110	$1,000		**
	3 One-element cutout ($20 each)	52	1,060	52.7	6
	3 Two-element cutout ($30 each)	51	1,090	53.6	9
	3 Three-element cutout ($70 each)	50.6	1,210	54.0	21
	3 Automatic recloser ($120 each)	50	1,360	54.5	36
Automatic reclosing breaker ($1,350)	None	100	1,350		#
	3 One-element cutout ($20 each)	46	1,410	54	4.5
	3 Two-element cutout ($30 each)	45	1,440	55	6.7
	3 Three-element cutout ($70 each)	44.7	1,560	55.3	15.5
	3 Automatic recloser ($120 each)	44	1,710	56	27
Permanent faults only		43		##67	

* Per cent improvement in protection = attainable decrease in outages, expressed as a per cent of the per cent consumer-minutes outages attainable with no branch protection. Example—$\frac{110-52}{110} \times 100 = 52.7$ per cent.

**100 per cent base cost = $1,000 for nonreclosing breaker, relays, etc. (Substation protection only.)

#100 per cent base cost = $1,350 for automatic reclosing breaker, relays, etc. (Substation protection only.)

##67 per cent in this case represents the maximum per cent of decrease in outages attainable (all temperature faults cleared immediately).

†Assume 7,500-volt single-phase, line-to-ground neutral, requiring only one protective unit per branch.

other types of equipment and with the automatic reclosing breaker at the substation as shown in the table. This example illustrates how actual costs may be applied in the author's mathematical study to be used as an aid in actual system planning.

To the loss of revenue and initial costs, there must be added the cost of maintaining the protective equipment on a system. It would be possible and advantageous to determine comparative maintenance costs for various combinations of protective equipment from a similar mathematical study with assumptions and conditions parallel to those in the paper under discussion. Calculations of actual outage time alone would serve as a basis for figuring labor costs for restoring service after outages with the different combinations of protective equipment. Auto and truck mileage would form a portion of the maintenance cost, but from the assumptions in this paper they would be uniform and proportional to the number of miles of line, because the number of outages and consumers per mile were assumed uniform. While it might be difficult to introduce actual costs into a maintenance study, because of the diversity of setups of distribution systems, comparative percentages would show the relations between the values of different types of overcurrent protection and the labor time necessary to maintain the protection.

Bruce O. Watkins (nonmember; Rural Electrification Administration, St. Louis, Mo.): Mr. Lincks is to be congratulated for this contribution to the solution of distribution-line-sectionalizing problems.

The so called "area-electrification" program such as practiced by the Rural Electrification Administration opens up an entirely new field in distribution engineering. So far distribution engineers have only scratched the surface on this subject.

Rural distribution systems financed by the Rural Electrification Administration vary widely in characteristics, but for purposes of discussion, a "representative" system as such usually involves between 300 and 400 miles of line, served by a single substation. Most systems are sectionalized by means of fuses. The substation protection consists of a three-shot reclosing fuse cutout, with the line sectionalized by two or three-shot cutouts. Branch laterals are usually controlled by a single-shot cutout. Operating practices on each system vary to suit local conditions, but for the purpose of this discussion the following practices are considered:

1. Substation is unattended.
2. Very little or no communication facilities are available.
3. Roads are usually poor, particularly during wet weather.
4. Line-crew personnel live a great distance apart or are usually out connecting new consumers or maintaining the lines.
5. Ends of lines may be 60 miles or more from the central office.

It can be seen that the above conditions are vastly different from those encountered in city or urban distribution.

Systems fused in the above manner have not given the results that have come to be expected for good service, particularly in lightning areas. Due to inadequate communication facilities, outages are not always reported promptly, and when they are reported, the problem of assembling a crew delays the restoration of service. The actual patrol of the lines to locate the trouble, and the travel over poor roads further increase the outage time.

To solve the communication problems, REA has been experimenting with two devices:

(*a*). A carrier-current outage recorder which immediately notifies the office of the location of an open sectionalizing device.

(*b*). A carrier communication system with portable transmitter–receiver and coupling capacitor in the trucks and with fixed equipment in the office.

Single-pole automatic reclosing circuit breakers, described by Mr. Lincks as "resetting reclosers," were installed on some of the earlier Rural Electrification Administration systems in conjunction with gap-protected transformers. Though many of these reclosers failed (failures largely due to improper insulation co-ordination), the results on the whole were so encouraging that in 1939 a program for installing an unprecedented number of such reclosers on systems located along the eastern seaboard was begun.

Table I indicates the results of a survey in one of the midwestern states where this type of recloser is being used. Gap-type transformers are in use on most of these systems. Since the recloser design was changed during this period, the two types of reclosers in use are differentiated by the terms 1 and 2, number 2 being the later design. All reclosers are of the same make.

On the eastern seaboard projects the reclosers are installed both on feeders and branches so that each one controls about 20 miles of line. No fused cutouts are installed on the load side of the recloser. In some cases reclosing fused cutouts are used for feeder sectionalizing and branch protection near the substation. Most of these systems have conventional arrester-protected transformers.

So far there has not been sufficient data accumulated to draw any final conclusions on the performance of these reclosing circuit breakers installed along the eastern seaboard, but the available information indicates much better operating results, both in shortening the length of interruption and decreasing maintenance costs, than with the previously fused systems. Quantitative results are being accumulated, but have not yet been tabulated; indications are that the per cent of lockouts to total operations is less than 10 per cent, and in some cases less than 5 per cent, during the lightning season. Only one complete recloser failure has occurred to our knowledge out of about 1,500 installed. Unfortunately, quantitative results for the previous fused cutout operation were not obtained, and hence no numerical comparison can be made. However, operating reports are not complete, but they do indicate that service has been improved considerably with the reclosing breakers and would seem to bear out the conclusion reached by Mr. Lincks that a very substantial improvement over fused cutouts is possible by using the recloser for sectionalizing, where there is a high percentage of temporary faults.

I agree with Mr. Lincks that continuity of service is always an operating engineer's major object, but there is also another angle to economical system operation. How much can we spend to eliminate fuse replacements? Estimates from Rural Electrification Administration system managers indicate that a few trips to refuse a distant cutout will cost much more than investment and maintenance costs on the recloser.

Since most faults are temporary, the recloser eliminates the majority of the previously necessary refusing trips.

There are still disadvantages to the presently available reclosing single-pole breaker:

1. As Mr. Lincks points out, it is not always practicable to provide automatic selectivity between the smaller size reclosers and the transformer fuse. This is particularly true for internally fused transformers.

2. There is an insufficient number of recloser sizes which will co-ordinate for proper sectionalizing on lengthy lines.

Both of these difficulties can be overcome, to some extent, by the introduction of inverse time delay in the recloser opening. New design reclosers have appeared on the market with such inverse time delay, but the experience with them is insufficient as yet to form any conclusion. Rural Electrification Administration is instituting further development of this inverse-time-delay feature in the recloser opening.

Table II. Report of Single-Pole-Reclosing Circuit-Breaker Operation on 20 Rural Systems in a Midwestern State to November 1, 1940

Most Systems Are Equipped With Gap-Protected Transformers

Total number of circuit reclosers reported	417
Total reported circuit recloser months of operation	5,098
Total number of operations reported	20,973
Total number lockouts reported	271
Per cent of lockouts to operations	1.29
Total operations where operations and number of reclosers were both reported	20,973*
Total number of reclosers as of above	301*
Average number of operations per breaker where both were reported	69.6
Average number of operations reported per recloser per month	4.11
Total reported number of recloser failures due to:	
Lightning	24
Worn parts	2
Oil	
Other	21
Total reported recloser failures, including those not segregated	59
Percentage of total recloser failures to number installed where both were reported	16.1
Percentage of total recloser failures per 12 months, where number of failures and months of operation were both reported	13.5
(Excluding system 18 where installation date was not given)	
Total reported failures:	
Design 1 reclosers	56†
Design 2 reclosers	3
Percentage of total design 1 recloser failure per 12 months, where number of failures and months of operation were both reported	21.8
Percentage of total design 2 recloser failures per 12 months, where number of failures and months of operation were both reported	2.04

*Number of reclosers and operations not always simultaneously reported.

†Systems 10 and 14 indicate failures but do not give the number. Other information on hand for system 10 indicates not more than two failures occurred, both due to recloser misapplication in interrupting rating.

Mr. Lincks states that overlapping the substation reclosing protection with single-element fusing at the branches or sectionalizing points approaches very closely to providing the minimum consumer minutes outage attainable.

Such a scheme is used on Rural Electrification Administration systems in the Tennessee Valley Authority area and works very satisfactorily. However, I do not feel that it is advisable on an extensive system.

1. Outages, even if momentary, are objectionable if over a large system.

2. It is difficult to reach all the way out on the system with the substation breaker.

3. The cost of the reclosing substation breaker would cover a great many single-pole reclosers.

In my opinion, definite advances can be made in rural distribution sectionalizing by the development of a small reclosing breaker similar to the present recloser, but with practically instantaneous first opening and inverse time delay on succeeding openings. The first reclosure should be as rapid as possible. Such a device should be inexpensive and should reset automatically. In order to reduce outage time on temporary line faults, the use of this breaker is advisable on most rural distribution feeders and long branch lines. By means of the instantaneous first opening and inverse-time-delay second opening, all short branch lines then could be protected with single-shot fuses, thus achieving the best over-all results. This idea seems to agree fairly well with the conclusions reached by Mr. Lincks concerning a reclosing breaker-fused branch combination, although Mr. Lincks discusses a different type of breaker. Also, I believe that to reduce outage time, the device should be single-pole, and, in order to prevent three-phase motor burnout with this device, effective and economical three-phase motor protection should be simultaneously developed.

Perhaps some new type of apparatus can be developed which will accomplish these results. At any rate continued research on the problem should advance the rural circuit sectionalizing art.

L. G. Smith (Consolidated Gas, Electric Light and Power Company, Baltimore, Md.): The author has prepared an excellent paper. In the field of distribution engineering there is a need for more papers of this type. In such studies the analytical approach based upon operating data should lead to conclusions that indicate:

1. Where to spend capital and maintenance dollars to obtain the best operating results by directly comparing operating benefits with the cost.

2. Where capital and maintenance dollars should not be spent.

3. What can be done to obtain the maximum improvement in continuity to service.

This paper should inspire further engineering studies of this type. However, additional operating data are necessary on which to build premises for further analytical studies.

The author refers to temporary faults varying from 15 to 85 per cent of the total. It is believed that in the usual distribution system the percentage is certainly above 50 and probably more of the order of 70.

Table III. Per Cent Consumer-Minutes Outage. Relative Effect of Various Methods of Branch Fusing

Three 10-Mile Branches

Station Protection	Fusing of Branches	75 Per Cent Temporary Faults (Per Cent)	85 Per Cent Temporary Faults (Per Cent)
Nonreclosing breakers	none	107	113
	1 element	33	40
	2 element	32	38
	3 element	31	37
Reclosing breakers	none	101	103
	1 element	30	35
	2 element	29	33
	3 element	28	31
Reclosing breakers set for instantaneous trip on first opening. Branch protective device delayed beyond first opening of breaker	none	101	105
	1 element	28	31
	2 element	27	30
	3 element	27	29

With the help of summary tables in a preliminary copy obtained from the author, Table III was prepared. If I have correctly interpreted the data, and if the data on one curve are comparable with those of another, I question the validity of two apparent conclusions derived from the author's data:

1. The data indicate less than a 10 per cent reduction in customer minutes outage by changing from a nonreclosing to a reclosing station oil circuit breaker as compared with about a 60 per cent improvement for branch fusing. The operating data of the company with which I have been associated show that 70 per cent of feeder troubles are restored without a lockout by reclosures of the station breakers.

2. In view of experience with reclosing station breakers, it is believed that actual operating experience would show a greater advantage in two or three element branch fuses.

G. F. Lincks: The author greatly appreciates the contributions made by those discussing the paper. There have been a number of points brought out in these discussions which should enhance the paper's value for those interested in the improvement of service continuity on distribution systems.

As Mr. R. O. Loomis points out, the study presented in the paper is based on securing perfect co-ordination. It has been experienced by a number of utility companies which, in addition to standardizing on only one type and make of fuse link for a given line, eliminated the possible causes of mechanical breakage of the fuse links, and poor mechanical and thermal conditions in the cutouts as well. Without doubt, Mr. Loomis had in mind reviewing the possible benefits to be derived from such elimination of defective devices, in raising the question as to the effect on the results "if, say, one fuse link out of ten blew incorrectly or unnecessarily." "Incorrectly" might imply improper sequence of operation without any increase in the actual number of outages, whereas "unnecessarily" would tend to indicate an increase in the number of outages. In either case, the effect on the results would vary with the location of the cutout which was causing the improper operation. In analyzing this point, it was assumed that the causes of such difficulty would be localized to one or two specific cutouts and would not be spread evenly over the whole line.

In case one out of ten devices operate "incorrectly," it was assumed that the circuit was opened at the next point closer to the substation than at the point where incorrect operation occurred. With line sectionalizing, this would tend to decrease by about one-tenth the improvement provided by the defective single-element or reclosing sectionalizing device. With single-element or reclosing branch protection, the station breaker would open, taking out the whole line instead of isolating the fault to the individual branch. Thus, the improvement over the "Yardstick" as provided by the branch protection employed under the specific system setup would be reduced by one tenth. The effect of defective equipment which causes incorrect operation is considerably greater at branches than at sectionalizing points.

In the case of devices operating "unnecessarily," and thus causing an additional number of outages to the one per mile used in the calculation, in the ratio of 1 to 10, it would be necessary to add from two to six units to the percentages shown on the curves for line sectionalizing with protective devices connected in series. The two should be added if the troublesome sectionalizing device is located close to the substation, and the six added if the troublesome device is located at the last sectionalizing point on the line. It varies about proportionately in between. With branch protection the effect is negligible, amounting to the addition of one unit to the percentages shown on the curves. With either branch fusing or line sectionalizing, the improvement provided by a defective reclosing device would be reduced by one tenth of the difference between the percentage shown on the curves for the actual device and for a device having one less reclosure.

Mr. Loomis is interested in the effect of the percentage of temporary faults on branch fusing, when all the consumers are on the branches, rather than evenly distributed over both the branches and the main feeder, as in the paper. A further study indicates that under such conditions, branch protection provides two or three units greater improvement for single-element cutouts, but no greater relative improvement for different devices or different percentages of temporary faults.

Apparently there was a misunderstanding of some of the values given in the paper on the part of Mr. L. G. Smith, which led to his questioning of the validity of two of the conclusions. Mr. Smith indicates that the operating data of the company with which he has been associated shows that 70 per cent of the feeder troubles are restored without a lockout. This operating experience lines up quite well with the values given in Table IV in the appendix of the paper. The values used in the calculations for determining the consumer minutes outage from which the curves were plotted are shown, in Table IV, to line up with this operating practice. This shows that while the reduction of the number of outages by reclosing equipment may be a very high percentage of the total, it does not necessarily indicate the same reduction in consumer minutes outage when related to the whole system. Consumer minutes outage takes into consideration the time it takes to go out and locate the fault, make repairs, and so forth. A large proportion of this time is required for permanent faults even with a high percentage of temporary faults. Single-element line sectionalizing and branch fusing reduce this time as well as decreasing the number of consumers affected. With branch fusing the consumer minutes outage caused by fuse blowing on temporary faults is a very small percentage of the total for the whole line, since only those on the one branch are affected. Therefore, even though reclosing devices on branches do restore service on 70 per cent of the faults without lockouts, the improvement in consumer minutes outage is very small when related to the system as a whole. However, as pointed out by Mr. Craig, the treatment of an individual branch as a unit would show a much greater improvement for reclosing equipment, and this might be sufficient to warrant special attention where some peculiar condition exists on that branch. It might even improve the consumer minutes outage on the whole system, if the percentage of temporary faults on the branch were particularly high as compared to the percentage of temporary faults on the system as a whole.

Mr. B. O. Watkins brings out some of the problems involved in rural electrification. He draws the conclusion that the automatic resetting recloser provides the real answer to the rural problems. As partial substantiation of these conclusions, he cites some data based on circuits employing gaps for lightning protection of the individual transformers. It should be recognized that such a setup has a fictitiously high percentage of temporary faults, since the automatic reclosing devices are called upon to give not only overcurrent protection, but also a part of the function that proper lightning protection should give. A tabulation of the number of successful reclosing operations without lockout should show up well, because the arc across the gaps is very likely to go out, as quickly as the recloser opens the circuit the first time, and is not likely to be re-established. The data presented in the paper are based on a normal system with the better conventional lightning protection, where the reclosing protective devices are not called upon to do double duty in interrupting the accidental plus gap-created short circuits. Therefore, the improvement indicated for reclosing devices in the paper will fall short of the improvement shown by data which include numerous additional operations to clear on the first reclosure arcovers on gaps employed for lightning protection.

Mr. Watkins stresses again the limitation in the number of reclosers that can be connected in series and the difficulty of coordination with the fuses at the transformer, which is brought out in the paper. He gives his idea of an ideal design which would have instantaneous operation on the first opening and then inverse time-current characteristics on subsequent operation. Such a device would have some advantages over the present design, although it would not provide for any increase in the number of devices that could be connected in series. The instantaneous operation on the first opening would retain the advantages of the present design in the preventing of burning down of lines, which is mentioned by Mr. R. O. Loomis when he states, "I have not found a case where conductors burn down when protected by oil circuit reclosers." It would probably have a similar advantage as regards the burning back of gaps employed for lightning protection, which might become excessive more quickly with reclosers having a longer time of operation on the initial opening. Actual operating experience is available only with the rapid opening.

All of these factors should be considered when employing the curves of the paper for the study of the relative value of different types of protective devices, when the characteristics of the devices or the type of the application vary from the assumptions of the paper.

Mr. Craig has presented some additional information and suggestions for applying the data presented in the paper, which should extend its value.

Relays and Breakers for High-Speed Single-Pole Tripping and Reclosing

Discussion of paper 42-35 by S. L. Goldsborough and A. W. Hill, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 77–81.

Eric T. B. Gross (College of the City of New York, New York, N. Y.): In connection with the application of relays acting upon the phase-angle shift between two sets of phase-sequence quantities, it might be of interest to mention that a similar problem arises in the problem to secure correct operation of distance relays in delta-wye connected systems. One of the solutions to this problem makes use of wattmeter-type phase-sequence relays.[1]

Reference

1. Influence of Wye-Delta Transformation on Distance Protection of High-Voltage Systems, **E. Gross.** *Elektrotechnik und Maschinenbau*, volume 55, 1937, pages 333–8.

Evening Courses at Graduate Levels—a Challenge to Colleges of Engineering

Discussion and author's closure of paper 42-34 by Robin Beach, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 88–94.

P. L. Alger (General Electric Company, Schenectady, N. Y.): This paper is very welcome for its record and advocacy of advanced education for engineers in industry. It is obvious that any four-year or even six-year course in college is only the beginning of an engineer's education. All who aspire to professional standing, therefore, must continue their studies for many years after leaving the halls of the university. Heretofore, most graduates have found it sufficient to learn from the hard knocks of experience and the advice of their associates, supplemented by occasional periods of individual study. It must be expected, however, that very many men in the future will wish to accelerate this process of continued learning, and will welcome the help of experts in so doing.

There are several reasons why the colleges should seize this opportunity for expansion, besides the mere filling of a growing educational need. For one thing these older students, engaged in practical engineering work, will bring to the university definite ideas on the utility of various sorts of knowledge, and numerous problems whose solutions are needed. Thus, the college professors will be jarred out of any academic ruts and stimulated to extend their own knowledge. In this atmosphere research programs will naturally grow up, industry support for them will develop, opportunities for student employment will arise, and undergraduate education will become more realistic.

Also teaching practicing engineers how better to perform their daily work is a public service of immediate economic value. The tuition fees received in consequence are likely to be multiplied many times in the college income, merely because of the community of interest and co-operative spirit thus created between the university and the town.

Any step that lessens the time between spending money for education and reaping its rewards reduces over-all costs and strengthens the financial position of the college. To advocate as some do that the government finance large numbers of students for a fifth or sixth year of college residence is, therefore, economically unsound, and, by the same token, evening courses for graduates are economically sound. They open the way for many young men to learn things they could not otherwise afford to study.

I believe that the future trend of these evening courses will be to include many more civic and cultural subjects that will make the engineer a better citizen, a man of affairs rather than a technician. It is only after an engineer has begun to carry business and family responsibilities that he can put to use a knowledge of law and accounting, or of history and government, or indulge his tastes for art. He should study these things after college, therefore, rather than postpone his technical courses on their account. The largest field of future service for the colleges may be the civic and cultural education of mature and busy men. If the colleges undertake this work, the professors will gain a far better understanding of business economics, and the business men will lose some of their commercialism, to the great benefit of us all.

F. R. Stansel (Bell Telephone Laboratories, Inc., New York, N. Y.): As a recipient of the doctor of electrical engineering degree from Professor Beach's institution, I wish to express my gratitude for the opportunities afforded to me by the Polytechnic Institute of Brooklyn and to add a few observations from the student's point of view.

A year or two after graduation many young engineers find themselves faced with a dilemma. They realize that in any of the fast-growing fields of engineering, the basic training acquired in their four years of undergraduate study is insufficient, but they often feel that it is impractical for them to now leave their jobs and go back to school for advanced training. Besides the obvious reasons of money and family relations, there are many other considerations including the realization that industry itself is a vast school. The engineer while realizing the need of more training in the basic principles for his future advancement, wonders if the training in engineering practice offered by industry is not of more immediate importance. Evening graduate courses of the type outlined by Professor Beach offer a solution to this dilemma, allowing the student to broaden his knowledge of the fundamentals and, at the same time, retain his position in industry.

From the financial angle, evening graduate study is peculiarly advantageous to the young engineer. The cost is automatically spread over several years without the necessity of seeking loans or scholarships, and the engineer is able to retain a job much more remunerative than part-time employment which he could obtain as a full-time student.

Professor Beach referred to evening graduate students as differing "from the conventional graduate student of the day session in a number of essential characteristics." One of the characteristics he refrained from mentioning, however, is the student's fatigue from having already accomplished a full day's work. Due to this fatigue, problems of classroom ventilation and illumination assume major proportions. Another great help in this matter of fatigue would be the more wide-spread use of mimeographed notes covering the more complicated mathematical formulas, and particularly drawings, thus relieving the student from acting as a transcribing machine, and allowing him to concentrate on understanding the material placed before him.

Finally, the picture of evening graduate study would be incomplete without a mention of one other group of persons—the wives and girl friends of the students. Many a graduate schedule has been completed only because of the unselfish willingness on the part of the student's wife to give up the companionship of her husband for one and frequently more evenings each week.

Robin Beach: The unusually large attendance at this meeting, as well as the active participation in discussion, both by members of industry and of the teaching profession, have clearly demonstrated the wide-spread interest in this field of graduate education through evening courses.

The general approbation of the progress already made by a number of engineering colleges in graduate study through evening programs of courses was indeed encouraging, and it should serve as a stimulus to those who contemplate entering this challenging field in the near future.

The author takes this opportunity to express his appreciation of the thought which has been directed to his paper by all of those who contributed so interestingly and so richly to this educational session.

Utilization Voltages

Discussion and author's closure of paper 42-69 by H. P. Seelye, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 147–51.

W. J. Russell (Westinghouse Electric and Manufacturing Company, Mansfield, Ohio): Mr. Seelye has made a very interesting presentation of voltage-variation data, which should be of considerable value to the industry. As manufacturers of domestic appliances, we would like to introduce a short discussion of the effects of voltage variation on small appliances (small portable devices).

The effect of voltage variation within reasonable limits on small appliances is, as a rule, of little practical concern. According to Mr. Seelye's investigation, the voltage range for appliance use is approximately 110–125 volts. Since most small appliances are now designed for 115 volts, the cases of underwattage will be less than those of overwattage. It is the former that is the basis for most customer complaints.

The trend toward automatic or thermostatically controlled devices has minimized the effect of voltage fluctuation. With automatic temperature control, sufficient wattage may be used to provide for proper operation under any normal voltage condition without danger of overheating on the higher voltages.

Nonautomatic devices are usually of lower wattage, and voltage variations are of more concern. However, we have found from long experience that cooking operations may be slowed or accelerated without harmful results within the range of known voltage variations.

The allowable wattage for standard wall receptacles is limited to 1,650, and, on some of the larger portable appliances, such as roasters, broilers, and double hotplates, this has caused trouble with slow operation when low voltages are encountered. However, improvements in efficiency have been made in the past few years which have largely overcome this trouble within the normal voltage range.

In the design of all appliances, sufficient factor of safety is included to take care of voltage variations so that they will have little effect on the life of the device. Life tests on our devices are conducted on an average voltage of 125.

We believe that with the existing voltage range, as shown by Mr. Seelye's study, we should continue to use 115 volts as the design voltage for small appliances, inasmuch as with this voltage the slowing effects of underwattage are not serious, and the overwattage can be taken care of by proper design. An increase in the design voltage to $117^1/_2$ or 120 volts at the present time would increase the field complaints, particularly on nonautomatic devices, due to slow heating in cases where the service voltage is on the low side of the range.

C. A. Powel (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Seelye's paper is quite timely in view of the work being done by the joint EEI-NEMA committee on preferred voltage ratings on the revision of the 1930 report. His plea for a general conception of utilization voltages as a band rather than a fixed value should command attention. In the past we have spoken of "nominal system voltages" and "rated circuit voltages"; it would seem desirable to revise this idea and speak only of a "nominal system voltage," which would be expressed as a band or would be understood to constitute a band, leaving the term "rated voltage" to be applied only to the equipment used on a system of the corresponding nominal voltage. There appears to be no good reason for having a single voltage value assigned to a system, particularly since it is admitted that of necessity we cannot select any such value which will be correct for all parts of the system of a given voltage class.

Utilization appliances must, of necessity, be given a single voltage rating at which their performance is specified, but, to a large extent, they are self-compensating and will give quite satisfactory performance over a fairly wide range of voltage variation. The life of the appliances is also not noticeably affected by reasonable voltage variations.

In regard to the lamps, it is desirable to keep down the number of special lamps which have a relatively small market. Orders for small quantities of these special lamps are quite burdensome to the manufacturers in that the stoppage and adjustment of equipment required for their manufacture interrupts and delays the production of higher-demand units. Moreover, they constitute a problem in that stocks have to be maintained in most cities and towns.

A. H. Kidder (Philadelphia Electric Company, Philadelphia, Pa.): Those who are interested in utilization voltage problems are especially indebted to Mr. Seelye for his progressive contributions and for his preparing so fine a review of the work. There are only two points which I think may deserve more consideration than has been given in the paper. The first is the effect of emergency operations upon supply voltage, and the second is the problem of the odd ratio.

Emergency Voltage

Provision for motors to start at 90 volts was made a part of both the maximum utilization voltage spread and the preferred voltage spread for residential customers. The need for the provision is not self-evident, and I do not recall mention of it in the paper, for the summary shows the maximum voltage drops in normal operations alone, as reported by the 14 companies. With the increasing dependence of customers upon continuous electricity supply, the time has come when emergency operations generally satisfy the need for at least partial service restoration, long before it is possible to complete the repairs necessary to restore normal conditions. When the supply to a distribution circuit has been interrupted, therefore, it is usual practice to transfer the load to adjacent circuits, if possible, and to supply it in the meantime, despite a possibility of low voltage.

To illustrate, assume that the normal voltage drops are the minimum values given in the author's table; that is, $2^1/_2$ per cent in the transformer, 2 per cent in the secondary, and 7.5 per cent total. If it is then assumed that the last transformer has failed on the circuit illustrated in Figure 3 of the paper, and its load has been transferred temporarily to the adjacent transformer, it will be seen that the load on the adjacent transformer will have doubled while both the circuit length and the load will have become three times normal in the secondary branch that is used to pick up the load which had been lost. Under this emergency condition, the voltage drop in the transformer would increase to 5 per cent and that in the secondary would increase to 18 per cent (2 per cent ×3×3), or an overall increase of $18^1/_2$ per cent above the $7^1/_2$ per cent normal voltage drop. In a system designed for 125 volts maximum, the refrigerator or oil-burner motor whose thermostat calls upon it to start under such conditions would have not much more than 90 volts. There are, of course, many possible examples, but this one is thought to be sufficient to show that secondary design for greater voltage drop probably requires provisions for dividing the load of the circuit in trouble between more than one adjacent circuit, if the load at the time is sufficient to require the additional emergency load transfers. Because urban distribution designs more often observe the limitations imposed by emergency rather than normal operations, it is quite natural that the normal voltage spread in urban distribution, and particularly in networks, may be somewhat less than in suburban distribution.

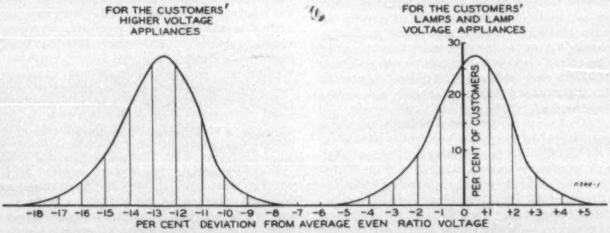


Figure 1. Illustrative distribution of voltages supplied by the odd-ratio urban circuit

Odd-Ratio Utilization Voltages

Figure 4 of the paper illustrates very well the nature of voltage spread for even-ratio equipment under normal operating conditions at even-ratio voltage. It does not, however, represent the much wider range of voltages which is faced by even-ratio equipment on circuits which supply either even or odd-ratio voltage—a situation the paper mentioned very briefly in the comments on 240-volt equipment. Motors are now almost the only utilization equipment which has not been made altogether adaptable to odd-ratio supply. Since the odd-ratio circuit is not yet used extensively outside of urban areas, it may be well to consider the problem in terms of the urban voltage spread of Figure 4, which needs only to be extended, in the accompanying chart, to illustrate the place in which even-ratio equipment finds itself on the odd-ratio urban circuit. For this purpose the reference point is taken to be the average voltage for the even-ratio connection on the odd-ratio circuit, and all deviations from that average are expressed as percentages.

From the above illustration it will be seen that the even-ratio motor, which is required to operate interchangeably on either even-ratio or odd-ratio voltage, must be prepared to meet satisfactorily a voltage range of +5 per cent to −18 per cent or a range of voltages up to 1.28 times the minimum odd-ratio voltage in urban distribution of the characteristics represented by Figure 4. This range is about midway between the 1.24 range, from 197 to 245 volts *preferred*, and the 1.30 range, from 193 to 250 volts maximum which the author mentions as being considered for such motors. All of these ranges are greater than that of 1.22 between the 90 per cent and 110 per cent limits established in the American Standards for Rotating Electrical Machinery, as being the allowable variation from rated voltage for motors. The American Standards must be revised, therefore, to permit use of any range greater than 1.22, no matter how carefully the rated voltage may be selected. Provision in the American Standards, for a greater voltage range (and retention of the standard "208-volt" single-phase motor line for difficult odd-ratio situations) seems to be the only practical solution to the odd-ratio problem in single-phase motors for which some such compromise solution seems to be inevitable.

On the other hand, discussion of the odd-ratio problem in three-phase motors has led to the suggestion of a possibility that the speed-torque-excitation characteristics of such a motor can be made to be exactly reproducible when it is supplied from any three-phase circuit connection that happens to be at hand, be it even or odd-ratio voltage. It may, therefore, be possible that the odd-ratio problem in three-phase motors is susceptible of an exact, rather than the compromise solution necessary for the single-phase motor. If an exact solution should be found to be practical, there would appear to be no justification for adding any special, approximate, or halfway measures to the present American Standards for three-phase motors.

The proponents of an exact solution for the three-phase motor argue quite convincingly that the differences between even-ratio and odd-ratio voltages in three-phase circuits are due entirely to differences in the coil connections, which could be adjusted almost as easily in a suitable motor as in its supply transformer. They point out that the exact solution can be attained by building the present so-called "208-volt" line of motors with the same number of coils and only two more taps than are standard in the present line of 220-volt three-phase motors. With such connections the motor could be connected so as to have exactly the same performance characteristics on each one of the three possible three-phase secondary distribution voltages: lamp-socket voltage, twice lamp-socket voltage, or odd-ratio (1.73 times lamp-socket voltage), as the case may be.

Such a motor, if practicable, would be seen to include one connection which is so nearly 110 volts as to make it quite probable that the now separate standard 110-volt line of three-phase motors could be discontinued altogether. It would also have the advantage that the American Standards for Allowable Variation from Rated Voltage for Motors would not need to be revised, except possibly for the special case of the single-phase motor. I believe the suggestion may be worthy of more serious consideration than it seems to have received.

Hamilton Treadway (Rural Electrification Administration, Washington, D. C.): The problem of standardization of utilization voltages presented by the author becomes critical in the development of large rural systems spread over several hundred miles. In such systems the spread in voltage must necessarily be wider than in the urban and suburban communities or even in the nearby high-density rural area. As the rural area becomes thinner, the economic gains to be realized by closer voltage control are much smaller. The difficulties in standardization of utilization voltages consequently are increased.

This suggests another approach to the problem; namely, voltage co-ordination. By voltage co-ordination is meant co-ordination of the appliance rating voltage with the voltage prevailing at the consumer's outlets. Approaching the problem from this angle would suggest perhaps three or four utilization voltage spreads to be used by the manufacturer, for example 105 to 115; 110 to 120; 115 to 125; and 120 to 130. This may require a change in our fundamental thinking and may place a greater burden on the supplier of electric service to advise the consumer of the proper appliance voltage rating to be used on his premises. Whatever change may be required in this direction, it is probable that voltage co-ordination lends itself more readily to supplying adequate and efficient service than does the establishment of a single utilization voltage spread.

In light of the author's figures on over-all voltage spread, it appears that the trend toward the purchase of 120-volt lamps might well be reversed if economic light production is to be obtained by the consumer. Undoubtedly the real reason for the past and present trend is not so much due to a difference in the schools of thought on the proper voltage at which to operate incandescent lamps as to a lack of information in the hands of the user on the most economical voltage at which the incandescent lamp should be operated. There is likewise a similar lack of knowledge on the part of the consumer on the most economical voltage at which to operate other appliances. A program of system voltage co-ordination is again the suggested solution.

Approaching the problem of utilization voltages from the voltage co-ordination view should permit the manufacturers to design more efficient appliances for consumer use. It should also reduce investment in lower consumer density areas without impairing the quality of service rendered. Of course the need for a narrowing of the utilization voltage spread throughout an operating system and particularly between systems cannot be overemphasized.

Howard P. Seelye: The fact that the discussions of this paper which have been presented contain very little criticism would seem to indicate quite general agreement on the main ideas offered, namely, the need for standardization of utilization voltages, the desirability of considering the voltage standard as a band of voltages rather than a single voltage, and the specific values suggested for that band. Only one, Mr. Treadway's, proposes the very different approach of setting up a multiplicity of bands. As a general practice, however, this would seem to be a step backward, adding confusion and hence cost in manufacturing and distributing appliances. The survey of operating systems and the fact that service conditions are, in general, quite satisfactory does not indicate that any such step would be justified.

Mr. Kidder's analysis of the operation of even-ratio equipment on odd-ratio systems is logical but does not take sufficient account of the fact that odd-ratio systems, being essentially three-phase, four-wire, are almost entirely confined to areas of high load density where the voltage is well regulated. The minimum voltage is likely to be 115 volts or more, giving at least 200 volts for the three-phase motors. Past practice has shown that 220-volt motors have, in most cases, had little difficulty in operating as such voltages. There is at present no evidence of any tendency toward the spread of the use of odd-ratio systems to more lightly loaded areas, although, of couse, it cannot be guaranteed that this will not occur eventually.

Mr. Powel brings out a point which was mentioned in the paper but which should be emphasized. It is necessary, of course, that any appliance have a voltage rating, designated as a single voltage, to which its other ratings and characteristics are referred. Such ratings may be different for different appliances. On the other hand, it is highly desirable for general use that a single name be given to the whole class of appliances which will correspond with that of the system on which they are used—in other words a *nominal* voltage, which could well be 120 volts in view of the preponderance of the 120-volt lamps. This nominal voltage would merely designate the standard band or spread of voltages and should not be considered as a mid-point, which would be actually $117^1/_2$, if the proposed spread is adopted. If voltage *ratings* for appliances can be made to coincide with the nominal voltage, possibly by giving a different tolerance for plus voltage than for minus, confusion would be avoided, but if this is not practicable, the nominal voltage should be emphasized for general use, the rating being relegated to an auxiliary position for technical purposes.

Electric-Power Distribution Systems in Wartime
(42-60)

Underground Distribution Systems in Wartime
(42-62)

Power Supply to Distribution Substations in Wartime (42-42)

Distribution Substations and Wartime Necessities
(42-61)

Overhead Distribution Systems in Wartime
(42-45)

Discussion and authors' closures of paper 42-60 by Philip Sporn, paper 42-62 by L. R. Gaty, paper 42-42 by H. P. St. Clair, paper 42-61 by F. C. Poage and M. W. Reid, and paper 42-45 by Harold Cole; presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 105–07; 107–12; 112–17; 117–22; 123–6.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I can assure you that Mr. Sporn's paper covers many ideas that are really worth-while and that will help the utility people with their problems in these trying wartimes. We in our company have been using many of the methods he calls attention to. Appreciable reduction in material requirements has been obtained by the methods I will mention.

Sacrifice in Voltage Rendered to Customer

We have been maintaining a very good voltage range which was a rather narrow band in most cases, on a peacetime basis, but we found that we could let the voltage drift in many cases and still render *usable* voltage to the consumers. Actually, in many cases we changed the transformer taps at the industrial consumer's plant to raise the voltage to compensate for the increased load on *his* distribution system. This is not necessary normally and, in some cases, does raise the light-load voltage somewhat, thus providing a wider band of voltage. Entirely satisfactory performance has resulted from these changes, and system changes have not been required, nor have any materials been required.

On the distribution feeders with feeder regulators at the supplying substation, we have in the past been increasing their voltage range from five to 10 per cent to stay within a narrow band rendered the consumers, but now, in some cases, we are not changing the regulators for voltage reasons and are letting the voltage drift a little during heavy load periods.

In our state (Pennsylvania) we have certain voltage limits prescribed by the Pennsylvania Public Utility Commission. We are well within these limits and in peacetime have spent money and performed construction and used materials to keep the very narrow band of voltage even above such requirements. Now we are permitting the voltage to approach the commission requirements before we use materials to improve service.

Relative to loading of equipment we have been crowding our equipment for the last few years and have been doing considerable research work to discover means of crowding it still further. We have determined individual ratings for power transformer banks in transmission substations and in distribution substations, and also for induction feeder regulators, both indoor and outdoor, and we find that a considerable increase in capacity can be obtained by operating these pieces of equipment on the thermal basis governed by the particular load cycle. We discovered that this would relieve our material requirements appreciably, as well as save the man-hours required to design and construct new installations. Ventilation is a field well worth exploring, and our experience has been that this is well worth any research work that you do. It is surprising how little *air movement* is required to get an appreciable increase in capacity. We also find that you will probably have to do a considerable amount of local research, due to the particular conditions surrounding your equipment, such as age, conditions of equipment, natural ventilation, indoor, outdoor, load cycle, and so forth.

Similar investigations have been going on on overhead lines and underground cables, and appreciable increases have been developed.

Emergency Conditions

Naturally, the same amount of reserve and emergency cannot be provided in wartimes as in peacetimes, because of the scarcity of materials and man power, as well as the lack of time to provide said reserve. Normally, of course, all of us try to render good service under the most extreme adverse conditions, but the ability to do so has gone "since Pearl Harbor."

To build two lines and provide two transformer banks to industrial customers, in most cases does not appear to be justified, and I suspect you would have a hard time getting an approved priority application to provide for this reserve. However, this does not mean that we should sacrifice on the quality of design or the strength built into the installation. Because of the fact that there is some reduction in reserve, the design should be at least equal to the good old peacetime quality if possible.

I seriously question the advisability of providing reserve, in many cases, for breakdowns which can be repaired in a relatively short time, such as a transmission-line burndown, which can be repaired in a matter of 8, 24, or 36 hours. Of course, such cases must be tempered by the business of the customer, for an interruption to a plant which merely curtails production, where production can be resumed practically immediately upon restoration of power is one thing, whereas an interruption to a plant where curtailment means considerable loss of material and requires a large amount of time to restore production, or endangers the lives of a large number of employees (such as coal mines) might require some degree of reserve over and above others.

Materials

The utilization of different materials from what you would normally use is necessary, of course. We must use wood structures if we cannot get steel; we must use different size and type transformers if we cannot get what we want to have. We must use the different type and styles of circuit breakers with necessary control interlocking and sequential relaying schemes to keep the duties within the capacity of the breakers; we must use fuses instead of breakers if we don't have the breakers. By and large, we must use materials that we have or can get, and must not make our material requirements a burden on the already overburdened material situation. Some suggestions have been made that the excess materials (if any) among utility companies and others be pooled and used where most urgently needed. This, of course, is merely good sound horse sense necessary to help win the war, which we must do.

Planning

All planners, particularly in utility companies, must plan the system in an entirely different manner, generally along the lines outlined above, and our company has been doing this for quite a while, and we find that it is satisfactory. Of course, we are going to get complaints here and there due to outages, low voltage, flickers, and so forth. Such complaints will just have to be tolerated where their correction cannot be justified under present conditions. Of course, too many complaints or too serious a complaint must be taken care of.

We may have to tolerate voltage flickers to a greater degree, even to the point where they are slightly objectionable to lighting consumers, but if too many objections are made, the voltage flickers will have to be corrected.

Load Shifting

Load shifting to ease the burden of demands on equipment is a possibility, and individual systems will have to investigate and see what can be done. Obviously spreading the same loading over twice the time reduces it in half, but this is merely academic and not practical. You can really shift the peak load on an electric utility system by staggering working hours in different manufacturing plants, or by shifting some of the day work to night work. Curtailment of nonessential loads is also a fertile field which, I believe, will be cultivated before the war is won.

While these comments are directed to Mr. Sporn's paper, some of them apply to companion papers presented at this meeting, particularly those of Messrs. St. Clair and Cole.

Conserving Equipment Necessary

I was particularly interested in many points brought out in Mr. St. Clair's paper relative to loading the equipment to the absolute safe thermal limit, and even beyond, to some extent. In these wartimes we have to sacrifice some life of the equipment rather than to replace it with larger equipment, or to consume materials needed elsewhere in order to manufacture this larger equipment.

Our company has been doing many of the things Mr. St. Clair mentioned, and we have had particularly good success with them. We are just purchasing a 3,000-kva portable substation, 22/4 kv, for use in emergency and for maintenance and repairs. The secondary of this transformer has various voltage combinations which will permit using it on many installations. Actually, as a result of this portable substation being available soon, we have curtailed the amount of transformer capacity installed in some of our newer semirural districts—all at an appreciable savings in capital investment and in savings in materials.

Switching Equipment

I can assure you that considerable economies in investment and materials will result from the selective relaying, wherein a group breaker is tripped instead of the replacing of individual feeder breakers with larger ones. We made a very careful analysis of this some years ago, and the evidence disclosed that there were very rare instances wherein the failures occurred at locations which would produce short circuits in excess of the small feeder breaker capacity, and, therefore, such group breaker tripping would not affect service adversely except in very, very rare instances. We have, therefore, been employing this practice with marked success for a number of years, and just last year we finally completed this selective relaying scheme in our four-kilovolt distribution substations. In fact, I am very glad we did, for I doubt if we could obtain larger breakers.

We have also adopted sectionalizing tripping for larger transmission substations, which means that the bus tie breaker or breakers are tripped first and practically instantaneously, and then the individual line breakers are well within their capacity. This latter feature has been in force on our system for 10 or 12 years.

Relative to tapping transmission lines with distribution substations, we believe that this is an excellent plan for curtailing expense and material requirements. We have just recently installed a 4,000-kva, 66/4-kv distribution substation and connected it onto our 66-kv transmission ring, because we had no 22-kv line anywhere near the proper location for the new substation. This has worked out quite satisfactorily, even though we did encounter considerable opposition in our own company to connecting such loads onto the so-called "sacred" transmission ring. However, economies justified it, and we had to proceed with it.

Certain benefits, of course, have resulted from the revising of our operating practices, particularly with reference to taking equipment out of service. We just do not take it out of service now until we absolutely have to do some work on it.

One comment on Figure 3 might be of interest. A modification of this would be for the step-down bank and the four-kilovolt bus to be in the same location rather than 0.7 mile apart. We have several of these substations, one at 66 kv, as referred to earlier, and several relatively similar to this at 22 kv. We have installed on the high-voltage bus a single-phase grounding disconnecting switch, which is actuated by the differential relays and upon their operation, actually places a ground on the high-voltage system. This is done, because a fault on the low side of the bank, but not within the area of the differential protection, might be of such low magnitude, particularly where the bank is small, that the high-voltage transmission-line relays at the nearby substation would not get sufficient current to operate. We developed this switching method in our own laboratory and made several trial installations and put it through its paces. We found it quite satisfactory and have been using it for some time.

Harold Cole (The Detroit Edison Company, Detroit Mich.): I want to describe a method of reinforcing a subtransmission system to enable it to supply increasing loads, a method which The Detroit Edison Company has recently applied to good advantage, and which may have application to other systems. We are just completing the changeover of the transmission network serving nearly half of our total service area from 24-kv to 40-kv operation. This changeover is being made at a very low cost, considering the increased capacity obtained.

The conditions were, of course, very favorable to the adoption of this scheme. The area involved, the so-called "Thumb" district of Michigan, has a low-load density, and the load growth will probably be slow. The distance from power sources to the extreme limits of the area are over one hundred miles, and voltage regulation had been a serious problem for several years. All substation transformers in the area were connected delta-delta, and about the only expense involved in changing to a wye-delta hookup was the cost of changing bushings. Circuit breakers at switching stations in the area had to have new bushings and be modified to obtain higher interrupting capacity and better clearances. Very little line reinsulation was found necessary for operating at the higher potential. Autotransformers were purchased to step up the voltage at the points of connection between the 24-kv and 40-kv system. These have a low impedance, so that the total over-all regulation of transformer and line will be improved as much as possible over the regulation of the lower voltage line alone.

As a result of this change, the lines in this area can carry more than twice the load they formerly carried before reaching limits of voltage regulation. The cost of making the change is much less than any other of the possible expedients studied, although some of the other expedients would have made possible a greater increase in load. If it becomes necessary later to reinforce the supply to this area, however, the higher subtransmission voltage will be very helpful in keeping line costs to a minimum.

We are now planning to use this same method of increasing line capacity to take care of new defense loads coming on in several suburban areas where distances from supply sources are too great for economical supply at the 24-kv level. The savings in the use of vital materials are obviously large, and if, as in our case, ways can be seen to fit the autotransformers required into the future changeover of a large portion of the suburban system to higher voltage levels, the scheme has great possibilities in contributing to the war effort without any great sacrifice in ultimate economy.

G. H. Fiedler (Rochester Gas and Electric Corporation, Rochester, N. Y.): I have reviewed Mr. Cole's paper on "Overhead Distribution Systems in Wartime" which I think is very timely.

We have found that a considerable amount of conductor could be saved by running two-wire services rather than three-wire, unless the customer has equipment which actually requires 240 volts. It has been our policy for many years to install three-wire circuits if a residence had four or more lighting circuits. We are still encouraging the installation of a three-wire riser and distribution panel for these customers but are extending only two service wires at present and installing two-wire meters. In some instances, the extension of the third secondary wire is saved, and to date we know that several thousand feet of conductor have been conserved on services alone. Very few voltage complaints have resulted and we intend to continue this practice until critical conditions are relieved.

G. S. Van Antwerp (Philadelphia Electric Company, Philadelphia, Pa.): Mr. Cole is to be congratulated for having stated clearly and concisely the principles that must be observed in extending and maintaining overhead distribution systems with smaller amounts of critical materials. It is difficult to believe that anyone can object to using any or all of these methods where at all applicable to his individual problems. With a reduced and limited amount of material available, either new or reclaimed, there is set up a real challenge to line departments.

Should we leave customers "out of service," because there are no bright and shiny arm braces, guy clamps, secondary racks, splicing connectors, guy rods available? Or shall we straighten the bent and dirty braces, hand-serve the stranded guy wire, use a wood bracket or arm in place of the rack, make up a wrapped joint for the connector, forget the guy rod and run the guy wire to a buried dead man?

It is a fine feeling to get a supervisory group together, enlist their enthusiasm, patriotism, loyalty, and co-operation to carry out a program such as this. But what a different reaction one gets "near the belt line," after finding that the fine words and elaborate procedures still build scrap piles of reusable materials—mounds of good intentions gone wrong.

Sell the supervisory group, yes, but that is not enough. Before positive results can be achieved, the man on the job, the layout man, the estimator, the storekeeper, the foreman, the lineman, and the lowly helper must be with you and Uncle Sam. That's where they want to be—helping; so give them a chance. Not only will they show you more ways of saving materials, but I suspect they will fight for reclaimed materials to—

Keep those kilowatts rolling!

Floyd M. Fuller (Pennsylvania Power and Light Company, Allentown, Pa.): To a distribution engineer of an operating public utility, the papers presented are very interesting reading, as describing current good practice which, no doubt, is being followed by many engineers.

All distribution engineers know, either from experience or from rules promulgated by governmental bodies, the limitations in regulation, and from their executives, the continuity of service required. In most cases limits of construction are imposed either by regulatory bodies or "codes" which, in many cases, are now used as the standard of construction. These restrictions have been imposed upon the distribution engineer in his practices, and we all know how difficult it is to change such codes and regulations.

As an organization why cannot we take some action and take it promptly to obtain a relaxation of present requirements. For example, in the Commonwealth of Pennsylvania voltage regulation must not exceed plus or minus five per cent of the standard voltage. If this is exceeded, and all of us in the industry know it may be exceeded to a certain extent, why not have the regulations waived for the duration, as long as the customer can live with the regulations Another example is guying over railroad and telephone facilities where electric lines, except for the lower voltages, are able to be installed with guys having a factory of safety of three, which in our estimation is excessive.

For improving the voltage, Mr. Cole, in his paper, very wisely stresses the need of saving material in new construction and the use of regulators, boosters, and capacitors. He might also to good advantage have brought out in his paper the benefits accruing from increasing the voltage. In the case of changing from a three-wire 2,300-volt to a four-wire 4,000-volt system using a common neutral (in many cases the existing secondary neutral), very little material will be required, in fact a lightning arrester and fuse cutout will be salvaged from each single-phase transformer installation and this increase in capacity may eliminate the need of replacing existing conductors with larger ones to provide additional capacity.

Similarly increased capacities may be obtained by changing from 6,900, three wire, three phase to 12,000 volt four wire, three phase without replacing transformers. It may also be advisable in certain cases to obtain the additional capacity by converting a system from 2,300 or 4,000 to 12,000 volts.

Another way to conserve in material is in street lighting: where the work is necessary and justifiable. By the installation of modern luminaires, having double the efficiency of the old equipment and using higher mounting heights, one-half the number of lights now existing on the boulevard standards can be installed, and the old obsolete equipment salvaged for defense.

The distribution engineer can and will change his practices in the interest of the conservation of material, but he could go much further in such conservation, if the walls of regulatory bodies and the requirements of reliability of service are lowered for the duration of war. An earnest request is made at this time that suitable action be taken by our officers with this in view.

H. E. McDowell (nonmember; Ebasco Services, Inc., New York, N. Y.): In the several papers, pages have been devoted to stating the problem and the conditions that are responsible. Simply stated: Many of our systems are short of capacity in certain respects or locations. There is a shortage of materials. Hence, the utilities will have to get themselves out of their predicament as best they can.

Bench Marks

Occasionally, one hears about the bold and courageous steps that must be taken to meet the present emergency. Without any desire to disparage the thought, it might be considered that many, if not most, of the measures that have been suggested will closely parallel practices that are characteristic of companies whose finances never have permitted gold-plate or chromium trim. Bold practices in the eyes of some are but customary in the eyes of others.

Select Plans That Have Multiple Advantages

It should be obvious that the times call for extreme simplification. Further, it is not too late to work toward a higher degree of standardization. This refers to practices, basic designs, flexibility of application, rather than to detailed specifications.

Whereas this discussion represents no attempt to boost the fortunes of the manufacturers of capacitors, it is worthy of consideration that the capacitor presently is the most useful article for increasing system capacity, improving voltage, or providing load relief that can be obtained on short delivery. It carries the advantages of *multiple uses*, referred to above, as well as the benefits of mobility or salvageability, means of reducing system losses—all being things that you are familiar with but which you may not have added up. It is well nigh axiomatic that a company cannot make a mistake in buying somewhat too many capacitors so long as its load is growing. They unquestionably have a great variety of uses.

A recent example that will illustrate the point is that of a company short of generating capacity, this being due in part to lack of reactive kilovolt-ampere capacity in its generators to supply lagging components of loads, together with the necessary reactive required to import available power from neighboring systems. In order to release this "captive" kilowatt capacity, amounting to some 15,000 to 20,000 kw, plans were made to install 35,000 kva of capacitors. At the same time, load growth generally had been such as to require load relief on approximately twenty four-kilovolt feeders. This would involve under usual practices a considerable construction program, including new feeders and allied improvements. The installation of capacitors, which were fully justified by virtue of salvaged generating capacity, so altered the program for distribution-system construction that but one new feeder became necessary, the remaining program consisting of shifting feeder regulators, installation of automatic switching on certain groups of capacitors, and other minor improvements. Cash required for the distribution-system program, exclusive of the capacitors which were justified by reason of generating capacity salvaged, was a fraction of that which otherwise would have been required. The saving in use of critical materials was in like proportion.

Another case in point involved the installation of condensers, capacitors, or both, to strengthen transmission channels to a large section of the company's territory. Choice lay between 15,000 kva of condenser capacity, 15,000 kva of capacitors, or some combination of the two. The selection made was one 5,000-kva condenser and 10,000 kva of capacitors, most of the capacitors being installed on distribution circuits (voltage regulation being largely controlled by existing synchronous condensers). The net result so far as the distribution systems were concerned was to obviate any general improvements to take care of the substantial load increases experienced.

Make Due Allowance for Flexibility in Planning

Planning with an eye to future changes or growth always has been smart—how much more so when conditions are changing with such rapidity now! This refers not to overbuilding but to:

1. Provision of ample ground space (usually a proportionately small item of cost).
2. Flexibility of arrangement of structures, circuits, and so forth, for easy rearrangement.
3. Mobility of equipment The accomplishment of these objectives is a matter of skill rather than of free use of money.

Maximum Use of Transformers

The writer questions whether due emphasis has been placed upon the possible benefits to be derived from paralleling distribution transformers as to capacity saving, voltage improvement, or both. The majority of utility companies in this country has been reluctant to adopt this method of operation, but the writer calls to mind a company that has for more than 20 years judiciously, but quite thoroughly, pursued this practice with entire success. This company operates in a metropolitan area and is in the class that at least enjoys a modest amount of "chromium trim."

Considerable discussion has been given to more thorough utilization of substation transformers. The writer calls to mind one company that averaged between 300 and 500 individual transformer shifts each year during a period of several years when load growth was rapid. Perhaps that might be charged to poor judgment or planning, but that particular system always enjoyed an exceedingly high degree of utilization of all of its components.

Elimination of Nonessentials

It is perhaps trite to say that the way to save materials is to exclude nonessentials, but there can be little question that an astounding saving could be made by eliminating from the conventional systems those gadgets that the simplest systems do not have. This reference in the main is to certain classes of apparatus that have for their justification service improvement. It is suggested that a good start on such a policy might be a visit to some of the poorer country cousins.

Salvaging Operations

But few companies have indulged in a comprehensive way in salvaging used materials. Such companies as have done so justify the expense in part as an offset to pensions and by the greater satisfaction and happiness to old employees in being usefully occupied rather than idle. It would seem that all companies in the present circumstances are duty-bound to embark on a thoroughgoing salvage program, and they might well do so with an eye to its available "pension" features.

R. G. Hooke (Public Service Electric and Gas Company, Newark, N. J.): About three weeks ago, at the request of the power group of the New York section of the Institute, I gave a paper on "Wartime Electric-System Planning in a Major War-Industries Area,"[1] and the remarks which I wish to make by way of comment on the papers presented this afternoon are supplementary thereto.

First, as to methods of limiting the short-circuit duties on oil circuit breakers, there are two suggestions. We have for many years resorted to sequential tripping of breakers on substation transformers in cases where the breaker on one side was likely to be overstressed, while the switch on the other side was adequate. More recently, in a particular situation, we have effected a major postponement of investment by adoption of a plan to operate with a tie circuit between two substations, open at one end under normal conditions. This involved two substations each supplied by four or five 13-kv cables and with a tie line between, which under normal conditions is only lightly loaded. The addition of several thousand kilovolt-amperes in war demands to one substation necessitated installation of a new transmission line, which in turn increased the short-circuit duties on the breakers beyond the values considered safe. Since the tie line between the two substations delivered heavy short-circuit currents in either direction, due to the many feeds to each substation from the source, and since it is an important part of the transmission network only when a circuit to one group or the other is unavailable, it was decided that under normal operation it might be left open at one end; in case of a fault, this end would immediately be closed. After the fault, with one line in the network from the source out of service and the tie circuit in, duties on the breakers were just within rating.

There are three schemes for avoiding investment in the circuit breakers which would ordinarily be considered necessary with increasing loads. All of these involve using available breakers with paired connections to them. Substation transformers may be paired on one breaker on each side with disconnecting switches, the arrangement being such that in case of a transformer fault, a defective unit can be disconnected, and the other quickly restored to service. Meanwhile short-time emergency ratings on other available transformer banks would be recognized.

While we feel we have overcome the likelihood of simultaneous outages of open wire circuits on one pole line, due to lightning, we have not found any means of controlling the auto driver who occasionally takes down our poles. It has, therefore, been our policy to provide in any transmission network for complete loss of any pole line. From this it follows that, if new transmission is needed in a network containing two circuits on a pole line as the maximum outage to be provided for, we may save the installation of switches for the new line by pairing the two open wire circuits on one switch at each end. By doing this we have lost nothing in firm capacity to the area, and if disconnecting switches are installed at the terminals of the paired lines, the loss of flexibility in the transmission system is almost negligible.

In other cases we have found it economical to pair on one breaker at each end small-capacity transmission cables, when a new circuit to the substation is required, and when the capacity of the new circuit is approximately equal to the sum of the capacities of the two lines to be paired. This same policy has been followed in situations where added load in future is expected to be handled by an increase in transmission voltage, thus making it desirable to prevent installation of more breakers than will be needed when the voltage change comes about.

In the matter of investment in transformers, our experience like that of most other companies, has been that failures almost never occur, and that, consequently, every means should be employed to secure higher average use of the units without jeopardizing service, in case one should be unavailable for a brief time. About two years ago we accepted emergency ratings based on 115 degrees centigrade maximum hot-spot temperature. We have also investigated each substation to determine the frequency of high loads and established a "probability allowance factor" for transformer ratings, based on the relative infrequency of occurrence of high peak loads. For example, in a residential area that peak load may occur only on Christmas or New Year's Eve. In such a situation, the probability of concurrence of peak load and transformer outage is very remote. We, therefore, consider that an increased emergency rating is justifiable, recognizing that if a fault should occur at the peak time, we would abnormally impair the life expectancy of the transformer.

We are very much interested in this matter of equipment life and have recently carried out some studies for transmission, similar to those discussed by Mr. St. Clair for transformers. The problem, of course, is to balance the annual cost of new investment against the increased maintenance costs and retirement charges which will result from emergency operation at higher temperatures. It will be found that, in a great many cases, the investment required, if operating temperatures are to be held down to present accepted values, is far more costly than is the higher maintenance and retirement cost which results from higher temperatures and shorter life.

In the transmission system, many of our circuits are limited in capacity by short sections of cable at the ends. Where the lines are short so that voltage drop is not a problem, we have found it highly economical to increase the capacity by either replacing cable sections with cables of larger size, or by putting in additional cables and pairing them at the ends with the present ones, so that one open wire circuit may have two cables in parallel wherever underground construction is required.

By following these expedients and many of the others mentioned in the papers here this afternoon, we have since 1938 expanded our system to meet a 37 per cent increase in peak demand at a total over-all cost from coal pile to customer's meter approximately equal to what, 15 years ago, was considered a reasonable charge per kilowatt for a new turbogenerator.

REFERENCE

1. WARTIME ELECTRIC-SYSTEM PLANNING, R. G. Hooke, E. C. Plant. ELECTRICAL ENGINEERING, volume 61, April 1942, pages 192–6.

E. V. Sayles (The Commonwealth and Southern Corporation, Jackson, Mich.): The five papers dealing with the effect of wartime construction and operation on electric distribution systems should prove to be of much value, if reviewed and studied by engineers charged with the design and operation of electric systems. While much of the material presented constitutes a review of practices, some of which have long been in common use by some companies, it is doubtful if all companies have employed all of these suggestions to the fullest extent. Detailed study of these papers in the light of the present wartime status of the country should unquestionably be helpful.

It would appear that the greatest effect of war on construction programs for transmission and distribution systems is to change us from our normal basis of "economic design" to a new basis of "material conservation." Efforts to conserve materials can be carried to great lengths, and there is little question but that all construction should be carefully reviewed to insure the minimum use of critical materials. In the case of transmission and distribution circuits, this may result in the use of smaller conductors and greater energy losses than would be permitted from an economic standpoint. However, reduction in conductor sizes cannot be carried too far, because voltage drop immediately assumes control, although in many instances voltages may be permitted to be just a little bit lower "for the duration" than might otherwise be considered proper.

With the industry now confronted with carrying greater loads on electric distribution systems, one factor of principle importance—perhaps of greatest importance—is the inherent capacity built into distribution systems for the purpose of providing duplicate service to principal load centers, or for carrying these loads when certain normal facilities fail. For example, there are occasional primary distribution circuits supplied from one substation, which during emergencies can be served from another primary circuit supplied from another substation. In some cases there has been a tendency to keep these circuits lightly loaded in order that they could be used as tie circuits between these substations, in the event of failure of power supply to the distribution substations. While this is a frequent practice, it would seem that transmission-and-distribution-substation design should be such as to permit greater confidence in it, thereby releasing primary-circuit capacity for the supplying of greater loads. It will be recognized that this general philosophy can readily be applied all along the line, starting from the low-voltage secondaries up through the transmission systems. Perhaps a relatively small expenditure to improve the reliability of a single transmission circuit may obviate the necessity for construction of a duplicate circuit.

Each of these papers emphasizes the conservation of materials. This is particularly evident in the case of transformers and transformer loading—something which cannot be stressed too greatly. Mr. Cole has discussed this rather completely for line transformers and has referred to the ratio of demand on a circuit to the total installed transformer capacity. While it is true that this ratio is not a proper measure of loading of individual transformers, nevertheless, it is a figure which is readily available and, if used with discretion and judgment, may reveal a general condition of excess or possibly lack of line-transformer capacity. Obviously, if this ratio is 40 or 50 per cent on one primary circuit and 100 per cent or more on a similar circuit, an investigation is warranted, although such differences may be expected and justified on circuits serving loads of dissimilar characteristics.

Many companies have thoroughly studied the overload abilities of substation transformers in the light of load curves and have already taken advantage of increased loading of these power transformers, actually finding that loads in excess of name-plate ratings were not overloads at all. Excellent data are available regarding permissible loadings of power transformers, and a study of particular transformers may indicate that substantial overloads for short periods may result in no sacrifice in useful life or perhaps a small sacrifice which may be justified in the light of present conditions. In such analyses, proper allowances should be made for ambient temperatures at time of peak, possible low temperature rise of the transformer at full load as determined from original test data, possibilities of different types of forced cooling for short periods, and other factors brought out in these papers.

Mr. Cole has stressed the matter of secondary banking, and Mr. Gaty has called attention to the possibilities of secondary networks. Underground secondary networks have received much attention in the past few years, and their possibilities must be now generally appreciated. Much can be accomplished with overhead secondary networks by occasionally banking transformer secondaries in the less populated urban areas to take care of spotty poor voltage conditions. There should be many opportunities where extending three-wire secondaries "around the corner" or installing a "couple of spans" to connect two small transformer areas may obviate the necessity of changing transformer sizes, establishing new secondary districts, or changing secondary conductor sizes. The results of such practice may be surprising. It would seem that much greater consideration should be given to a limited use of such "baby networks," particularly in view of the excellent service record of low-voltage secondaries, which, when properly installed, cause little or no trouble.

None of the papers has referred to the increased capacity of a distribution system which results by changing from delta to wye operation. The entire history of overhead distribution is intimately associated with the changing of distribution systems from 2,400 volts, three-wire delta, to 4,160 volts, four-wire wye, with a common primary and secondary neutral. Similarly, in recent years some 4,800-volt delta systems have been changed to 8,320 volts wye, and it has been common practice to change 6,900- and 7,200-volt systems for wye operation. In the case of transmission lines, additional capacity has been secured by changing 24,000-volt transmission to 41,600 volts, and so forth. In many cases, studies have been made to determine whether some of these changes could be justified economically, and, if adverse decisions regarding such changes have been made, perhaps they should be reviewed in the light of material conservation. Certainly the capacity of a distribution system can be increased substantially by such a practice without the addition of copper. In some cases, generating equipment, synchronous condensers, or large motors now in operation may make such a change more costly, but the possible reconnection of this equipment or the use of autotransformers may be feasible.

It would seem that in this series of papers there should be some reference to the service voltage furnished to residential customers. While two-thirds of the lamps sold in the country are rated 120 volts (indicating voltage levels based on 120 volts), many systems are still operated on a 115-volt base. Since many of the complaints about low voltage result from unsatisfactory operation of appliances, would it not be opportune in the case of so-called 115-volt systems to consider raising voltage levels to 120 volts? By so doing, an improvement of from three to five volts can be secured for all types of utilization equipment without appreciably increasing complaints of high voltage except possibly for lamps. The 115-volt lamps would, of course, burn out more rapidly, but lamps sold in the area would be changed to 120 volts rating, and the voltage levels could be raised gradually over a period of several months until the 120-volt level is reached. Therefore, it is believed that consideration should be given to this suggestion, especially in view of the recent utilization voltage recommendations by committees of the Edison Electric Institute.

Messrs. Poage and Reid have pointed out the desirability of power-customer co-operation to help reduce system peak loads. Power-customer co-operation should also be encouraged to the greatest degree in connection with the power supply to new plants and the facilities which a large power user should require in order to guarantee dependable service. Surely the transmission, distribution, and substation design engineers should be able to give these customers the benefit of much valuable advice in planning their substation facilities, to the end that much material and equipment can be conserved by simplified layouts, with due consideration being given to continuity of service. This co-operation might extend to helping the customer determine his own distribution voltage, type of switching equipment, and outdoor substation facilities with special consideration given to the kind of transformers used, their proper taps, the necessity for spare units, duplicate busses, duplicate metering, and so forth.

The use of automatically operated air-break switches to take the place of high-voltage oil circuit breakers as pointed out by Mr. St. Clair is worthy of much study by many companies. It is surprising how much can be done by properly planned installations of this type at enormous savings in materials used, cost of equipment, and time required for construction.

M. M. Samuels (Rural Electrification Administration, St. Louis, Mo.): The worst feature of this symposium is the fact that it did not take place three years ago or even five years ago, but I suppose that if it were presented then, the authors would have been called warmongers or even worse names, and possibly would have lost their jobs.

The symposium is certainly timely. The industry is struggling to keep up with production of additional generating capacity, even though some ostriches still claim that there is no shortage. There was no shortage of steel or copper either. Everything was lovely.

But very little has been said about a shortage of distribution capacity. These papers, indeed, present a very pessimistic picture, but in the last few months we have become accustomed and acclimated to pessimistic pictures. We got so that we can take it, and let us hope that we can take this one too.

The seriousness of the situation cannot be overemphasized. Reports from cities in which there is an enormous war activity indicate that distribution systems find it difficult to carry the existing loads, to say nothing of carrying additional loads, in existing industrial war plants and for war plants yet to be built.

Stretching will not solve the great problem of distribution capacity, particularly when the peak periods are no longer just 30 minutes or one hour but become four or five hours. We are in for a long war. Stretching may solve the problem for a very short time but not for a long time. When we get the additional generating capacity, the new powerhouses may be all dressed up with no place to go, because there will be very inadequate distribution. The problem must be considered as a part of our great war problem in general. No individual can decide or should take it upon himself to decide how important his activity is in the great problem of the war. This can only be decided by those who are responsible for running the war. If additional capacity is required to follow through with the government program of production, those in charge of running the war will have to see to it that this capacity is made available, and they will see to it.

Two years ago, before the Engineers Club at Raleigh, North Carolina (November 13, 1939) and again before the Florida Engineering Society (April 12, 1940), I spoke of "Power for National Defense" and had to be careful not to use the word "war."

I called attention to the need of preparing for this emergency. At that time many things could have been done by every utility that would have made the present problem easier. But there is no use of even talking about spilt milk. They just were not done, and it is never too late to start.

Mention is made in some of the papers of mobile substations as high as 15,000 kva. As an indication of how far behind we are in this respect from the viewpoint of a world war, I merely want to mention that in the London magazine, *The Engineer*, October 14, 1938, there is a photograph of a German portable substation of 100,000 kva, 220,000-volt to 10,000-volt, with all kinds of taps, and it was reported that the Germans had quite a few such substations. This is big-time power for war purposes, and we have to begin to think in terms of big-time power for war purposes.

Another item, a very important item, which is overlooked in all these discussions, and to which I called attention in North Carolina as well as in Florida, is the fact that in time of war, we must plan what we are going to do in case part of a power system is destroyed, either by enemies or by crackpots from within.

Nothing is mentioned in the papers about how the power that is left will be scheduled to the most important places, and whether or not the circuits are so arranged that they can be so scheduled. How will it be possible to bring whatever power is left to such places as hospitals, cold-storage plants, police radio, traffic lights, water supply, and so forth?

In case of a network, separate sources of supply to unusually important loads are very difficult to provide. I suggested on many occasions that every city, particularly those that have network systems, should provide themselves with a number of small mobile powerhouses that can run through the streets like fire engines and then bring power immediately to hospitals, first-aid stations, bomb shelters, and other such places. Very little has been done along this line, and it is never too late to start. One of the reasons, probably, why utilities did not take up the idea is psychological. Most of the small units are Diesel-driven, and the Diesel to a utility man has always been like a red flag to a bull.

To show what these little mobiles can do, I will recite an incident that happened only about a week ago. The army was about to start construction on a $26,000,000 camp and could not get a power supply brought it on time. One of the rural electrical co-operatives, quite a distance away, had 2,100-kw mobiles in temporary operation, pending completion of an incoming power circuit. The army asked for the mobiles to be able to start construction. Everybody worked that night to compete the circuit. In addition, some complicated legal papers had to be made out. In spite of that, the mobiles were on their way next day and got to the army site ahead of time. I have been told that they were pulled on the highway at 50 miles an hour. You all know, of course, that the Germans and the English have hundreds of small mobiles. We, too, should have had hundreds of them by this time.

Mention is made in one of the papers of load-curve control. This, too, is a very important item that has received insufficient attention. According to the paper, the particular company has been able to secure voluntary co-operation of the customers to reduce the peak. It should be possible everywhere to secure such co-operation and reduce peaks, thus adding considerably to the capacity of the power system.

We hear so much lately about a kilowatt-hour shortage as against a kilowatt shortage. When you speak of water power, a kilowatt-hour shortage can only mean a shortage of water. We had that. If you speak of steam, a kilowatt-hour shortage may mean either a shortage of fuel or a low plant factor brought about by a high peak. By load-curve control, whether voluntary or by force, it should be possible to reduce peaks considerably, and when peaks are reduced, both generating capacity and distribution capacity can take care of more energy.

In conclusion, I may say that, based on the papers presented here today, immediate action should be taken by all utilities to correlate distribution to generation and transmission, correlate the whole system to the government war purposes, and co-operate fully with those who are responsible for running and for winning the war.

In time of war: "Our country, right or wrong." Always, why not: "Country before company"?

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): One of the main thoughts throughout the papers is to increase the loading on existing equipment in order to reduce sharply the amount of critical materials required for new installations. As indicated in my 1939 AIEE paper, "Load Ratings of Cable,"[1] we have been establishing since 1930 new and higher temperatures for emergency operation of various kinds of cable, in order to postpone installations of new lines. I have been actively interested in the matter of operation at high temperatures since about 18 years ago when I had something to do with the research on paper insulation at the Massachusetts Institute of Technology.

Nevertheless, I should like to call for caution and perspective on this whole matter of temperatures for transformers, contacts in circuit-interrupting equipment, cable, and so forth. Our planning from the standpoint of the war emergency has to be on a basis of several years, because our government is planning the national efforts on the basis of the war's lasting several years. A recent Gallup poll showed that half of our citizens thought the war would last more than two years.

Tests and operating experience indicate that if one wishes to have a life of say 50 years for a given piece of equipment, then the operating temperatures may be anything up to a certain value, depending on the equipment. I have indicated this point at *A* in Figure 1. The limit may be determined by physical and chemical deterioration of the insulation as in a transformer, or by ionization as in an impregnated-paper-insulated cable, or by sheath cracking in the manhole as for a lead-covered underground cable, or by mechanical troubles as in a large alternator.

With further increases in temperature, the life of the equipment decreases uniformly, perhaps on a logarithmic basis down to a point, *B*, as indicated in the figure. In specifications and standards for cables and transformers in this country during the past few years or so, the industry has established temperatures for emergency operation which correspond to temperatures approaching point *B*. These temperatures are usually 10 or 20 degrees centigrade above our previous limits for normal day-in-and-day-out operation. Research at the Massachusetts Institute of Technology for The Engineering Foundation–AIEE project on insulating oils and cable saturants has shown that with continuous exposure of oil-impregnated insulation to oxygen, charring of the insulation occurs in a matter of a couple of weeks at 105 or 115 degrees centigrade, which is the range of the upper emergency temperatures recently established for transformers. Other researches, including a current one by the Commonwealth Edison Company, have shown that with no oxygen present, the rapid loss of the mechanical integrity of the insulation begins to occur at 120 or 130 degrees centigrade.

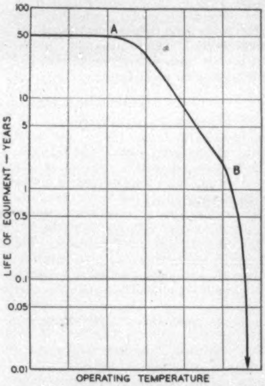


Figure 1. Temperature scale and shape of curve varies with equipment

If, during the war, equipment is normally operated at temperatures up to *A* and a little higher and equipment is operated at toward *B* during emergencies, in line with recently established limits, then we will increase our loading of existing equipment over past practice and cause some shortening of the life. However, the equipment will generally last without any particular trouble for the duration of the war. On the other hand, operation normally with temperatures toward *B* with occasional operation at temperatures above *B* during emergencies will greatly increase the likelihood of failures during the next few years. The new established temperatures for emergency operation are, incidentally, much above the limits in European specifications and standards.

Our operation in general should not be such that within a couple of years, and before the war is over, reliability of service would become very poor, and much equipment would be ruined and would need to be replaced when the country is short of critical materials and labor. Instead, installations should be made judiciously and consideration should be given to measures to avoid very excessive temperatures by methods such as indicated in today's papers and discussions.

I have a few special comments to make about Mr. Gaty's paper, "Underground Distribution Systems in Wartime":

Method of Supply, beginning of first paragraph: Regarding the relative advantages of a network system over a radial distribution system, our experience with the radial distribution four-kilovolt system has been, in general, quite satisfactory from the standpoint of service and economy of materials. Emergency temperature limits have been established for the underground cable and overhead wire and distribution transformers. Also, arrangements are made for emergency relief through emergency connections to take care of a load of a given feeder that has failed by having the load transferred to adjacent feeders.

Reliability of Supply, fourth paragraph: We have installed 66- and 132-kv transmission lines in the same conduit as lower-voltage cables without any bad consequences. The cables are each fireproofed in the manholes, and the relaying is fairly fast and has operated almost always as expected.

Reliability of Supply, fifth paragraph: The comments that the stock of cable and spare materials should be higher than normal are interesting, but the requirements of the War Production Board preference order may not permit putting this idea into effect in many cases.

Reliability of Supply, seventh paragraph: In connection with making quick repairs, one scheme that has been successful for many years in Chicago has been to connect the conductors of a joint and apply tape over the individual conductors, and omit soldering the joint sleeve to the cable, and omit filling the joint with compound. These open-air joints have been left to operate for several hours, until nighttime usually, when the joint construction has been completed with the line made dead again. Such operation has resulted in no troubles. Of course, where the water level tends to be high in the manhole, steps have to be taken to keep the manhole pumped dry for the several hours of the emergency. This general scheme has been used on joints rated at 12 kv and less.

The general ideas in the paper on the need and value of further work on limitations of insulations, conduit, and sheath are, to my mind, very much in order. It has been such work that has enabled industry in the past several years to establish emergency cable ratings that are 5, 10, or even 25 per cent above normal ratings.

Also, I want to emphasize point 4 in the author's summary on the need to "maintain an adequate inspection and maintenance program." With higher loading of equipment, the chances of trouble are greatly increased, but we may be able to give fairly reliable service if steps are taken to make relatively small expenditures for inspection and maintenance. This point, like many other points in this interesting paper, is one that many people agree on but do not always put into effect.

REFERENCE

1. LOAD RATINGS OF CABLE, **Herman Halperin.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

M. S. Oldacre (Commonwealth Edison Company, Chicago, Ill.): Frequent reference is made in this group of papers to "defense industries" and "emergency conditions" or similar phrases. We should say "war industries, and so forth," so that it will be impressed on the minds of all of us at all times that we are faced with immediate problems and not with something we can do next week.

The authors have covered a wide field and necessarily have been able to devote only small space to each item. I should particularly like to comment on some of the transformer problems that we should face.

As pointed out in several places in the papers, most operating men try to avoid loading equipment so that any of the life will be consumed, and, in general, the nameplate rating is the limit. Under peacetime conditions this was justified as it has given some of the elasticity or slack in an electric distribution system that makes possible greater load capacity for war conditions.

The authors have made very little reference to how long the increased load is to be carried.

Shall equipment be loaded to the utmost, without regard to effect on the life of the apparatus, thus shooting all our ammunition in one shot, or shall the best available data be used as a guide to load the transformer to the limit that will still leave the transformer available for service as long as needed?

Most likely the latter method is the only one considered by most of us, and it is suggested that the recommendations in the American Standards Association standards for transformers be consulted as a guide for the average transformer. These data are based on the research of the larger transformer manufacturers and present, in more usable form, data partially covered in the references in the papers. The ASA recommendations are of necessity rather conservative, as they are intended to be applicable to almost any transformer, but I would not be surprised if many operating men voice loud objections.

For larger transformers and special conditions, study of the individual transformers is justified, and it will generally be found that more actual capacity is available than indicated by the name plate. In several instances such studies have delayed for two or three years additional installations costing a quarter to a half million dollars each. In still another instance a large transformer bank's capacity was increased one third to take care of a temporary condition that has now continued for 12 years.

Such studies develop along several paths, some of which are:

1. More extensive use of the transformers in their existing condition:

(*a*). This requires that the transformer will be loaded to the safe temperature limit for continuous operation, taking into account load cycle, ambient, and actual characteristics of the transformer.

(*b*). It requires further that for emergencies due to equipment failures, and so forth, the transformer will be loaded to at least the emergency limits of the ASA standards.

2. Providing additional cooling facilities of some of the types mentioned in the papers. In this case all additional facilities reasonably feasible should be added as one operation. Then loading for regular and emergency operation along the lines mentioned above should be employed.

New installations offer an additional problem that is well worth study and adoption where possible to save materials needed for war purposes.

In the larger installations load and load cycle is generally fairly well-known, and the transformers should be specified to provide conservative operation under normal conditions, but for emergency conditions the maximum temperature conditions that will still give a reasonable life should be specified.

This is best illustrated by an example: It is proposed for a specified load and load cycle that three transformers be installed, each capable in an emergency of carrying one half of the total maximum load. Normally all three will operate in parallel. An emergency may be of two days' duration to repair the underground cables that feed the transformers, or it may last for a month or two, if a transformer repair is necessary. It is proposed that for the emergency rating with forced air cooling, the temperature rise of the copper may be 75 degrees centigrade above the usual ambient of an average of 30, and a maximum of 40 degrees centigrade in 24 hours. This emergency rating would be about 60 per cent above the usual self-cooled rating that would be applied to the core and coils.

There is an indicated saving of materials of 35 to 40 per cent over a straight self-cooled transformer, and 15 to 20 per cent over the usual self- and forced air-cooled transformer. With other types of cooling, such as forced oil, circulation or spraying of water on the transformer might make further savings possible.

A good share of the saving will be in steel and copper—two strategic materials.

Philip Sporn: The striking fact brought out by all the discussions is not only that all utilities have been doing thinking on the problem, and that all have found particular ways and means of making existing distribution systems more effective, or ways and means of extending the capacity of distribution systems more economically than was felt desirable or even possible in peacetime, but that substantially they have been doing so for some time. One of the aims of the symposium was to bring all these ideas together and to place them as a complete unit before the distribution engineers of the power-supply systems. I believe the discussions show that it has accomplished that preliminary purpose. Once this has been done the final objective, that is, making existing distribution systems perform more work or making new systems perform necessary tasks more efficiently can, I am confident, be left to the skill, industry, and patriotism of those carrying on the detail task of designing, constructing, and operating the distribution systems of the country.

H. P. St. Clair: As we had hoped, the authors of the papers in this symposium have not been disappointed in the many valuable comments and suggestions which have been contributed by the discussers of these papers. To mention a very few, I should first like to endorse Mr. Halperin's timely warnings as to the possible dangers of going too far with the overloading of transformers to the extent that temperatures exceed or even closely approach the point B on the curve in Figure 1 of his discussion. While a considerable margin of safe overload capacity is undoubtedly available in many transformers, particularly on a short-time emergency basis, it is quite true that loss of life goes up rapidly, on a logarithmic scale with temperature, and also that temperature itself increases more rapidly as greater overloads are applied. It would indeed be false economy in the use of materials to allow failure of distribution or of transformer equipment to occur in the midst of our war effort. This only emphasizes the importance of a careful analysis of the overload capabilities of equipment so that the increased loading will be applied on a sane and sound basis.

Referring to the arrangement described by B. M. Jones, whereby a single-phase ground is intentionally applied by closing a disconnecting switch actuated by the differential relays of a transformer bank where high-voltage circuit breakers are not provided, I should like to say that the description of our typical "medium-capacity" transmission substation shown in Figure 4 failed to mention the fact that this station is, likewise, arranged for closing a disconnecting-switch ground when the differential relays operate. As a matter of fact, this scheme is employed on this and on one other 132-kv stepdown station on our system for the same reason as that described by Mr. Jones, namely, to insure clearing the high-voltage line in case of a transformer fault which would in itself not provide sufficient fault current to relay the high-voltage line.

The practice described by Mr. R. J. Hooke, whereby, in certain suitable instances, pairs of lines or cables can be paralleled on a single breaker and operated as a single circuit, is quite sound, and a proper addition to the various schemes mentioned in the paper. While it was not brought out as such, the arrangement shown in Figure 9 of the paper represents one such case where conditions were entirely favorable for this expedient. The practice has been followed in one other case, on the system with which the author is associated, and we are in full agreement that opportunities for such economies should not be overlooked.

Harold Cole: The discussion has brought out a number of valuable suggestions for conserving the use of material in overhead distribution systems which were not mentioned in my paper. Mr. Fuller and Mr. Sayles have pointed out the possibilities of converting 2,300-volt, three-wire delta systems to 4,000-volt, four-wire wye, and similar changes at higher-voltage levels, as a means of securing increased capacity with a minimum use of material. Certainly, expedience of this kind should be carefully studied, where the existing system connections are such as to make the change possible.

Mr. Sayles and Mr. McDowell have also advocated the "banking" of distribution transformers on the secondary side, as a means of conserving material. It has seemed to me that, in most cases where the practice has not been followed in the past, a considerable amount of new conductor material would have to be installed to do a proper job of "banking." It is, therefore, questionable whether a net saving of critical material would result. One of the principal advantages of "banking" is the reduction of voltage dips, and it has been pointed out that such minor inconveniences may have to be tolerated for the "duration." I hasten to add that I do not want to be taken as deprecating the practice of "secondary banking" as poor engineering in peacetimes. For many years the practice has been followed by The Detroit Edison Company, wherever the conditions were at all favorable, and its use justified from the point of view of economy as well as improvement to service.

Mr. Van Antwerp's comments on the need for encouraging ingenuity, organization, and enthusiasm, to carry out a program of getting the most use out of the material available, is certainly very much to the point. The practice cited by Mr. Fiedler of the use of two-wire services, which could later be readily converted to three-wire, is a good example of the type of ingenuity which accomplishes very worthwhile results. In this same connection, I understand that it is now possible to purchase meters which may be readily changed from three-wire to two-wire in place, at a very small increase in cost over the standard three-wire meter. This makes it possible to avoid the purchase of two-wire meters, which would later be difficult to use, because of the increasing requirement for three-wire services.

F. C. Poage and **M. W. Reid:** Mr. Samuels in attempting to cover the over-all situation, refers to shortages of facilities in almost universal terms. This is not a strictly accurate picture. There are also surpluses in certain locations, and there will be others in the future as many peacetime activities are curtailed. To the extent that facilities can be rearranged and reused, the apparent shortages will be relieved.

We have pointed out a few of the means for conserving essential materials and man power by the electric-power industry for "the duration" to the end that "the duration" may be shortened.

The discussion by Herman Halperin is particularly appropriate in this symposium, because of its clear exposition of the basic relation between operating temperature and useful life of organic insulating materials. It is the application of the principles which he has cited to the loading of equipment that we have urged in our paper.

It seems rather irrelevant, however, to speak of normal life of transformers and other equipment, when the life of the nation itself may be in jeopardy. If by loading equipment on a short-life basis, we can make more war materials and man power available now—not sometime in the future—to put into planes, ships, tanks, and guns, and thereby shorten the war by ever so little, the failures that may later ensue and the replacements to follow will be well worth their cost. The country needs the materials and the men *now*, and we must be guided at present more by the point of view of what we have to do now rather than by the strictly technical concept of what might otherwise be altogether prudent. The electric-power industry must and will, we are certain, make the necessary sacrifices to win the war and to win it quickly.

The Electric Strength of Nitrogen and Freon Under Pressure

Discussion and authors' closure of paper 42-33 by H. H. Skilling and W. C. Brenner, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 191–5.

G. C. Nonken (General Electric Company, Pittsfield, Mass.): The authors have expressed the breakdown voltage of the gases as tested between spheres in form of linear equations involving pressure and gap spacing. Their equations are of the form

$V = KPS + C$

when

V is the breakdown voltage
P is the absolute pressure (atmospheres)
S is the gap spacing in inches
K and C are constants for a particular gas

This equation is an expression of the well-known Paschen's law. Many research workers have found that their data deviate widely from Paschen's law at high pressures or at large gap spacings. Figures 1, 2, and 3 give the breakdown data of Freon, nitrogen, and air taken from the authors' and five other papers.[1–5] These tests are all made between spheres or uniform field gaps. The pressures range from atmospheric to 600 pounds gage pressure, and the spacings vary from 0.05 inch to 1 inch.

With few exceptions, these data lie on straight lines when the voltage gradient is plotted against the absolute pressure in logarithmic co-ordinates.

The general equation for these data is

$$\frac{V}{S} = CP^b$$

when

V is the breakdown voltage
S is the gap spacing in inches
P is the absolute pressure in pounds per square inch
C and b are constants. b represents the slope of the line and C determines its position vertically

The equation for the voltage breakdown of Freon is

$$\frac{V}{S} = 24.4P^{0.805}$$

and is plotted in Figure 1. For these data, the constant C varies from 21.7 to 28 or over 20 per cent, while the constant b changes only from 0.79 to 0.805 or slightly less than two per cent. The variation in C indicates the differences due to the different investigators' test setups, electrodes, and so forth, while the constant b is affected very slightly by variations in test procedure but is a constant typical of the particular gas.

The authors' equation

$V = 183PS + 4$

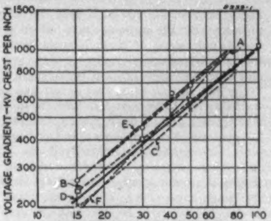


Figure 1. Breakdown voltage gradient of Freon

A—Skilling and Brenner—0.05-inch spacing
B—Trump, Safford, and Cloud—0.2-inch spacing
C—Nonken—0.39-inch spacing
D—Proposed equation $\frac{V}{S} = 2.44P^{0.805}$
E—Skilling and Brenner equation $V = 183PS + 4$ ($S = 0.05$ inch)
F—Skilling and Brenner equation $V = 183PS + 4$ ($S = 1$ inch)

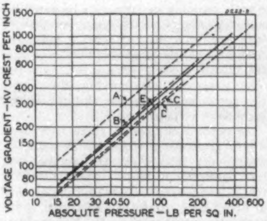


Figure 2. Breakdown voltage gradient of nitrogen

A—Skilling and Brenner	$S = 0.05$ inch
B—Nonken	$S = 0.39$ inch
C—Finkelmann	$S = 0.76$ inch
D—Goldman and Wul	$S = 0.079$ inch
E—Proposed equation	$\frac{V}{S} = 7.6P^{0.835}$

for the breakdown of Freon is also plotted in Figure 1. This equation has different values of V/S for each spacing. The authors have chosen the correct constant for a 0.05 inch spacing, which is represented by curve *E*. The curve for a one-inch spacing according to the equation is shown by *F*. It is necessary to choose different constants for each gap spacing to make this equation fit the curve.

Figure 2 shows similar data for the breakdown of nitrogen. The equation suggested for this data is

$$\frac{V}{S} = 7.6P^{0.835}$$

For this data, the constant C varies between 12.5 and 6.4, while the constant b varies from 0.8 to 0.835.

Figure 3 gives similar data for the breakdown of air. The equation suggested for this data is

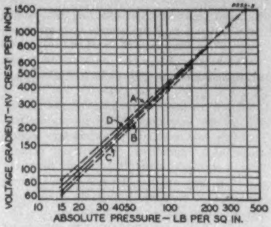


Figure 3. Breakdown voltage gradient of air

A—Howell	$S = 0.25$ inch
B—Trump, Safford, and Cloud	$S = 1$ inch
C—Finkelmann	$S = 0.76$ inch
D—Proposed equation	$\frac{V}{S} = 6.15P^{0.925}$

$$\frac{V}{S} = 6.15P^{0.925}$$

For this data the constant C varies between 5.6 and 6.9, while the constant b varies between 0.875 and 0.935. Paschen's law is expressed by the above equation when $b = 1$. Freon deviates the most from Paschen's law with an average value of $b = 0.805$, while the values of b for nitrogen and air are 0.835 and 0.925 respectively. The breakdown characteristics of gaps which have corona previous to breakdown do not follow such a simple law as the one above. As the authors suggest, caution must be exercised in drawing any general conclusions or extrapolating any data in connection with the breakdown of sharp-edged gaps in high-pressure gas.

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2. HIGH-PRESSURE GAS AS A DIELECTRIC, **Gordon C. Nonken.** AIEE TRANSACTIONS, volume 60, 1941, December section, pages 1017–20.

3. DER ELEKTRISCHE DURCHSCHLAG VERSCHIEDENER GASE UNTER HOHEM DRUCK, **E. Finkelmann.** *Archiv für Elektrotechnik*, volume 31, 1937, pages 282–6.

4. BREAKDOWN OF COMPRESSED NITROGEN IN A NONUNIFORM ELECTRIC FIELD—II, **I. M. Goldman, B. M. Wul.** *Journal of Technical Physics* (USSR), volume 3, 1935, pages 16–27.

5. BREAKDOWN STUDIES IN COMPRESSED GASES, **A. H. Howell.** AIEE TRANSACTIONS, volume 58, 1939, May section, pages 193–204.

G. W. Dunlap (General Electric Company, Schenectady, N. Y.): In these days of shortages and priorities much thought and effort are being expended toward the utilization of substitute materials, but in the field of insulations the search for new materials has been going on for many years. More particularly and more recently much consideration has been given to the use of compressed gases in place of oil. This involves the comparison of the two mediums on the basis of not only dielectric strength but of weight, heat transfer, fire hazard, mechanical construction, maintenance, availability, and cost. These cannot be discussed at length here but must obviously be considered in detail for any given application. Since the paper under discussion concerns the dielectric characteristics of gases, these remarks will also be confined to those characteristics.

Gaseous insulation has been used satisfactorily in a number of special cases including cables, capacitors, electrostatic generators, potential transformers, and probably with the most spectacular results in the new million-volt portable industrial X-ray unit described in AIEE TRANSACTIONS for December 1941. In addition, the increasing use of the air-blast principle in circuit breakers, while not ordinarily thought of in the same category with closed system insulations, is truly a great application of gaseous insulation. These uses amply justify the work which has been done and should encourage further investigation in this field where even now not too much is known. The following remarks, therefore, wherein they disagree with the authors should be taken as cautionary rather than discouraging in the application of gaseous insulation.

To take up the authors' conclusions in order, the relative strengths of nitrogen and air between smooth electrodes do not remain the same, even over the pressure range shown in Figure 1 if the length of gap is increased to the order of one centimeter. This is shown by Finkelmann[1] who found differences up to 20 per cent for one-centimeter gaps between flat plates 13.5 centimeters in diameter at 16 atmospheres, and up to 15 per cent at 10 atmospheres for gaps between 18 and 20 centimeters diameter concentric cylinders. Conclusion 2 is, of course, consistent with this.

Conclusion 3 regarding the relative strengths of Freon and nitrogen checks with the results of Charlton and Cooper (authors' reference 3) and Nonken,[2] and also with the experience of the writer which indicates that the 2.5 ratio holds out to spacings of at least four centimeters between 6.25-centimeter spheres. However, the formula $V=183PS+4.0$ does not necessarily hold for this same range as may be seen from Table I where some of the writer's data which are comparable with Nonken's are compared with values calculated from the formula.

Regarding mixtures of Freon and nitrogen (conclusion 4), the question might be raised as to the practicality of providing a fan for mixing the gases as the authors found necessary.

For the case of Freon between pointed or other corona-forming electrodes, irregularities of such magnitude exist that the data presented by the authors—data which do not include some of these irregularities—may give too optimistic a picture of the general situation. It is true that the strength of Freon may be four times that of nitrogen as stated by the authors, and, in fact, the ratio may be even greater under some conditions. For example, Nonken shows about six times for a six-centimeter standard rod gap at atmospheric pressure. However, under conditions of greater spacing and voltage than the range covered by the authors, Freon gives a "hump" in the pressure-voltage curve similar to that shown for air in Figure 7, except that the maximum occurs at a much lower pressure in the Freon. For this reason the strength of Freon may actually be less than that of nitrogen at some pressures, and the writer has taken data in which this was the case.

Table I. Breakdown of 6.25-Centimeter Sphere Gap in Freon

PS	Test Kv	Formula Kv	Per Cent Deviation From Formula Kv
		1 cm spacing	
0.39	90	76	+18
0.78	155	147	+ 5
1.58	225	291	−23
2.36	300	436	−31
		2 cm spacing	
0.78	130	147	−11
1.58	255	291	−12
3.15	350	580	−40
4.72	460	868	−47

In addition, and even more important in many applications, the high strength in the "hump" portion of the curve results in situations where the 60-cycle strength may be twice the impulse strength, and an impulse ratio comparable to that of transformer oil is rarely obtainable. This phenomenon has been verified by both Nonken and the writer. As the authors point out, this high 60-cycle strength is found with corona-forming gaps, but it would seem to be a case of more corona shielding in Freon due to decreased mobility of the space charge rather than an impeding of corona formation as indicated. A number of investigators[2,3] have measured the corona starting voltage in Freon and other gases and have found no discontinuities corresponding to the "hump" in the breakdown voltage curve.

As pointed out above, there have been several applications of gaseous insulation in which very satisfactory performance has been obtained. Experience shows, however, that it is necessary to study each application in detail with careful consideration of its particular requirements. If these requirements are such that conditions for optimum gas performance can be maintained, the use of gas may prove very advantageous. For such a case marked reductions in size and weight of equipment may be effected as was done with the million volt X-ray outfit.

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2. HIGH-PRESSURE GAS AS A DIELECTRIC, **G. C. Nonken.** AIEE TRANSACTIONS, volume 60, 1941, December section, pages 1017–20.

3. THE EFFECT OF PRESSURE ON THE POSITIVE POINT-TO-PLANE DISCHARGE IN N_2, O_2, CO_2, SO_2, SF_6, CCL_2F_2, A, He, AND H_2, **H. C. Pollock, F. S. Cooper.** *Physical Review*, volume 56, July 15, 1939, page 170.

Henry M. Hobart (Consulting Engineer, Schenectady, N. Y.): Conclusion 6 of the authors' paper states that, for dielectric purposes, compressed Freon would be inferior to compressed nitrogen at compressions greater than 150 pounds per square inch (10.5 metric atmospheres). At that pressure and for a 50-mil spacing between one-inch-diameter spherical electrodes, the 60-cycle electric strength of Freon, estimated by the authors' formula, is 2,000 crest volts per mil. For that gap and spacing it is the writer's opinion that the 60-cycle electric strength of nitrogen will not exceed 1,600 crest volts per mil, no matter how great a compression is employed.

Corresponding to a compression of 10.5 metric atmospheres, the condensation temperature of Freon is 44 degrees centigrade. The writer would value an expression of opinion by the authors as to any difficulties foreseen by them in employing Freon in cases where (for the attainment of a particular purpose) a temperature considerably greater than 44 degrees centigrade is required. For example, at a temperature of 100 degrees centigrade a compression of 25 metric atmospheres would not condense Freon gas, and its 60-cycle electric strength for a 50-mil spacing between one-inch-diameter spherical electrodes almost certainly would be considerably more than 2,400 crest volts per mil (that is, more than 50 per cent greater than nitrogen's limiting strength of not over 1,600 crest volts per mil for that spacing).

The application which the writer has in mind, relates to a compressed-gas transformer in which the gas shall partly or completely condense after each passage through the active material (core and windings) of the transformer, each time returning to its starting point cooled by having transferred its heat by the vaporization of a liquid of appropriately low vaporization temperature. It is evident that for such a cycle the conditions are improved, and the size and cost of the transformer and heat exchanger are decreased, the higher the permitted temperature. If, in such a vaporization-cooled transformer, there can be employed a temperature very much above 100 degrees centigrade, the liquid to which the gas transfers its heat, can be water, and we have the advantage of its wide availability, its usually almost negligible cost, and its very great latent heat.

The writer would value the authors' opinion (based on their considerable familiarity with Freon), as to how high a temperature could be employed for the Freon gas without encountering any prohibitive chemical difficulties. Owing chiefly to their higher condensing temperatures for given compressions, and on account of their price and of their dielectric characteristics at high pressures, the writer considers SO_2 and CCl_4 to be attractive alternatives. Consequently he would also much appreciate the authors' comments anent these materials for the purposes here described. Such a design obviously involves various compromises. It is believed that we now have available a considerable variety of adequate heat-resisting materials for the turn and layer insulations and for their impregnating constituents.

Amongst other features which will be embodied in such transformers is the decreasing of the core loss by employing the "Maclaren effect." Since this feature requires a core temperature much in excess of desirable winding temperatures, the core will be separated from the windings by a "thermal screen" of asbestos or fiber glass or Mycalex or other suitable material. High-voltage compressed-gas power transformers embodying these various features will not require any high-voltage entrance bushing but will be supplied through a surge-attenuating underground transmission line comprising compressed-gas-insulated conductors enclosed in a welded-steel pipe line.

Thus we arrive at a system providing 60-cycle underground conductors for the long-distance high-voltage transmission of great amounts of power and for power pools. No time should be lost for seeking economical alternatives for our present outmoded overhead, high-voltage, power-transmission system with its aboveground, outdoor installations of oil-type power transformers and switchgear. If the project here briefly sketched could be promptly developed and employed, then the impending annual expenditure of many hundreds of millions of dollars for the urgently required increase in the nation's electricity supply need not perpetuate the decades old constructions with their serious susceptibility to interruptions from lightning, from storms of ice and sleet and wind, from enemy depredations in wartime, and from interference with aircraft traffic and with communications systems. The high-voltage transformers no longer will require expensive and none too dependable inlet bushings of awkwardly great height, nor internal shielding for surge protection, nor will they contain inflammable and sludging oil which imposes a very low temperature limit. Their compressed-gas successors will be lighter, cheaper, and in all respects more dependable. The consequent conservation of the nation's oil supply is also a factor not to be overlooked.

Our present overhead tower systems and our oil transformers are carryovers from the historic Frankfort-Lauffen long-distance transmission installation built just 50 years ago. To meet the vastly more severe modern requirements the fundamentals of this pioneer line have been retained to a great extent but revamped to provide for more power and higher voltages. For their present tolerable adequacy thanks are due to the skill and ingenuity of many able engineers. The result, however, has been attained at a cost which seriously handicaps the desired far wider use of electricity and with a disappointing degree of dependability.

The writer recalls the interesting article by one of the authors of the present paper which was published in *Electrical World* a few years ago. The article was devoted to the description and advocacy of a compressed-gas underground system in which the high-voltage transmission conductors were pipe-enclosed. It would be of much interest to learn from Professor Skilling to what further extent he has gone with the development of that project.

The authors have done well in especially emphasizing the extremely important characteristic of Freon in virtue of which its electric strength is not so greatly impaired by increasing nonuniformity of the field as is the electric strength of nitrogen. Thus in analyzing the results published in Mr. Nonken's recent AIEE paper entitled "High-Pressure Gas as a Dielectric," the writer finds that with a pressure of nine metric atmospheres and a spacing of 800 mils, Freon's electric strength in the rod-gap test decreased to 40 per cent of its strength in the sphere-gap test, whereas the electric strength of nitrogen under these same conditions decreased down to only 30 per cent of its sphere-gap strength. The authors' tests with pointed electrodes as shown in their Figure 7, disclose the same trend, but much intensified, as they apply to a greater extreme of nonuniformity of field than corresponds to the rod gap.

This paper is certain to be of much value to those working on application problems involving the use of compressed gas as a dielectric. Already it has proved very useful indeed to the present writer.

Wm. C. Brenner and **H. H. Skilling:** The principal purpose of this paper is to present experimental results on the electric strength of Freon gas and of mixtures of Freon and nitrogen, giving information covering a region of pressure and electrode spacing for which adequate data have not been published. As a convenience in estimating the sparking voltage under the conditions of the experimental work, an equation for sparking voltage is given.

Much of the discussion is related to the applicability of this equation to other ranges of pressure and spacing. Mr. Gordon Nonken proposes an alternative equation in his discussion, of the form $V=CP^bS$. He suggests the application of this form to sparking in air and nitrogen as well as in Freon. He says very truly that many investigators have found that their data deviate widely from Paschen's law at high pressure and large spacing. This was emphasized in a previous paper by the authors on "The Electric Strength of Air at High Pressures—II" (reference 2 of the present paper). In that earlier paper a modification of the familiar form of Paschen's law was given to apply to the authors' results in air. Paschen's law, applicable at pressures up to atmospheric, and substantiated by physicists for over half a century, is of the form

$$V=75.4PS+1.7 \text{ kilovolts} \tag{1}$$

P is in atmospheres, S in inches. The modification to fit the authors' data at pressures up to twenty atmospheres was

$$V=\frac{76.0PS}{1+cP}+1.7 \text{ kilovolts} \tag{2}$$

This equation 2 has the advantage of reducing to the accepted form of Paschen's law at low pressure (c is a constant much smaller than unity). Mr. Nonken's formula does not share this advantage; in particular, the constant term is absent from his exponential form which approaches zero sparking voltage at low pressure. Paschen's law, to be sure, fails to apply if the pressure-spacing product is below 0.05 inch-atmospheres, but an exponential formula will deviate from observed data considerably sooner.

Equation 2 has the further advantage of taking into account the length of spark gap. It will be seen from Figure 3 of Mr. Nonken's discussion that the voltage gradient is *not* independent of the gap length; the authors' equation 2, above, takes this into account, and results computed from this equation may be plotted in Nonken's Figure 3 as a family of lines, one for each spacing. This appears somewhat more satisfactory for representing the family of experimental curves than does a formula giving a single line. Equation 2 gives excellent agreement with Howell's results, and good agreement with Trump's, if compared with the curves published by those authors. The equation also agrees with the present authors' measurements in air (reference 2) which could be plotted in Mr. Nonken's Figure 3 as a curve lying above all those shown but approaching them at very high pressure.

In devising a formula to apply to experimental results in Freon, the authors naturally sought a form similar to Paschen's law. It seemed desirable not only for its simplicity and familiarity, but also because it is a form so well-established in air and other gases. Thus the equation suggested is:

$$V=183PS+4.0 \text{ kilovolts} \tag{3}$$

It must again be emphasized that this equation does not apply near the pressure at which Freon condenses to a liquid and therefore should not be used above about four atmospheres.

Whether this form or another is used is a matter of choice at present. There is not yet enough data to permit a decision, although it seems likely that an equation, to take into account all the peculiarities of Freon, would necessarily be a fairly complicated one. The authors' equation 3 gives reasonable agreement with their own results. It gives even better agreement with the results published by Trump, Safford, and Cloud in their Figure 4 (of reference 9) at pressures below 60 pounds per square inch absolute. No attempt was made to derive a formula for a wide range of pressure and spacing, for it is our opinion that more information is needed before that can profitably be done.

Dr. Dunlap presents new and interesting data that substantiate this opinion; yet it is remarkable that the authors' equation 3 turns out to be as good as it is under conditions of board extrapolation. With extrapolation of the pressure-spacing product to five times that for which the formula was designed, the deviation between the experimental results and the formula is given by Dunlap as only about ten per cent.

Dr. Dunlap shows that in other cases the deviation is as much as 30 or 40 per cent, but since his data were obtained at pressures practically as great as the vapor pressure of Freon at room temperature, and quite beyond the range of equation 3, this is not surprising. It seems best that experimental sparking values in this pressure range be used directly, for generalization in any kind of formula would probably be unsafe.

Regarding the mixing of Freon and nitrogen, the authors suggest that in commercial applications of gaseous insulation it would be more convenient to mix the gases before injecting them into the equipment to be protected. When once mixed they will, of course, stay mixed, and there is no need for a fan within the vital parts of a cable or a million-volt X-ray outfit.

Mr. Hobart's interesting discussion brings to attention several points that might be clarified.

1. The conclusion 6 to which Mr. Hobart refers should have been qualified for the sake of clarity, for it was intended to show that Freon at room temperature was inferior to nitrogen at 300 pounds per square inch for the various stated reasons, and it was meant to convey the idea that where structural or mechanical limitations would not allow pressures above 150 pounds per square inch, Freon (at room temperature) should be carefully considered.

2. The equation $V = 183PS+4$ for the breakdown kilovolts (V) for sphere gaps (where S is in inches and P in atmospheres) was determined at temperatures ranging from 55 to 73 degrees Fahrenheit (considered to be constant at the time of measurement) and for pressures all below the vapor pressure of Freon at room temperature.

To extrapolate the curve for higher pressure regions, one should take into account the temperature effect realizing that the density of the Freon is the important factor. For constant temperature the density was adequately represented by pressure, but to be more complete the form of the breakdown equation should be of the type

$$V=\alpha\frac{P}{T}s+\beta$$

Again it should be mentioned that the original equation was not checked at the vapor pressure, and unless it is intended to use Freon at elevated temperatures where the gas has some degree of superheat, it would be well to check the equation for several points at the vapor pressure, lest unhappily saturation of the gas may be accompanied by some sort of discontinuity.

As to the stability of Freon and the other mentioned gases in Mr. Hobart's discussion, it is well to consult the manufacturer as to the thermal decomposition of the various gases. As to the effects of electrical disintegration, none need be considered unless there is an electrical discharge taking place, and the degree and seriousness of this should be determined by the particular application. For smooth surfaces with no or only occasional arcing, decomposition may be disregarded, but for applications where corona may possibly exist, the seriousness of the decomposition is to be determined by the effects of corrosion and the requirements of adding or changing the particular gas in question being used for its high insulating properties.

Referring to the proposed Freon-cooled transformer feeding into a Freon-filled cable, one thing must be remembered, and that is, that if high pressures are to be utilized, the entire interconnected system must be maintained at a temperature sufficiently high to prevent the Freon's liquefying, thereby reducing its high insulating strength.

It seems that Freon would best serve at ordinary temperatures and pressures below the vapor pressure where the entire system could be maintained at a constant pressure; otherwise, there would arise the above mentioned problem of maintaining elevated temperatures for the entire system, or the need for gas tight bushings between the various elements of the system. However, for special cases the use of high-pressure Freon may prove quite valuable and should bear further investigation.

Saturated Synchronous Machines Under Transient Conditions in the Pole Axis

Discussion and author's closure of paper 42-43 by Reinhold Rüdenberg, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 297–306.

S. B. Crary (General Electric Company, Schenectady, N. Y.): This paper presents an interesting graphical analysis of the effect of saturation on synchronous-machine performance. Any analysis of the influence of saturation must necessarily be approximate. When such approximations are necessary, the value of the analysis is increased when it is used with a full knowledge of the assumptions made. Dr. Rudenberg's analysis is necessarily limited to the effect of saturation in the main flux paths rather than in the leakage paths and is more nearly correct when the major part of the saturation is in the pole body. The analysis, therefore, can be used for transient disturbances, which are for periods of time in which the relatively slow field transient is important, such as in the calculation of alternator swing curves.

The method described in the paper is similar to that given by Mr. F. R. Longley in "Calculation of Alternator Swing Curves—Step-by-Step Method" (AIEE TRANSACTIONS, volume 49, 1930, page 1129), except that Rüdenberg suggests taking into account the additional effect of saturation by the addition of an equivalent field magnetomotive force acting along the direct axis. This method of taking saturation into account has been used in the calculation of swing curves with the network analyzer, except that the pole saturation was corrected, not only as a function of the direct-axis pole flux, but also as a function of the air-gap flux, in order more properly to take into account the effect of stator saturation. In terms of per-unit quantities and the nomenclature which has been established in this country, Rüdenberg's equation for calculation of the effect of saturation when a machine is connected to an external reactance would take the form of

$$T_{do}pe_{\psi}=e_{I}-\frac{x_{d}+x_{e}}{_{d}'x+x_{e}}e_{\psi}-e_{s}$$

where

T_{do} = open-circuit field time constant
e_{ψ} = voltage behind transient reactance
e_{I} = voltage corresponding to exciter voltage
e_{s} = voltage corresponding to saturation magnetomotive force

For swing-curve calculations the vector diagram is of the form shown in Figure 1. e_s can be determined as a function of the voltage back of Potier reactance, which may be less than the transient reactance, as well as of the direct-axis pole flux. Although this method, as well as Rüdenberg's method, is approximate, it does give an answer which more accurately describes the machine performance than if saturation were neglected entirely. The point of interest is that by using the main-field winding flux or voltage behind transient reactance, the calculations can be carried out, using the open-circuit time constant in the equation for the rate of change of field-flux linkages without concern as to the impedance of the external circuit. This was previously pointed out by Longley for the case of no saturation.

The method described by Rüdenberg is approximate in that:

1. It neglects the effect of voltage produced by the rate of change of stator-flux linkages which, as he mentions, produces a d-c current or natural frequency oscillation.

2. It assumes rotor flux as only a function of the open-circuit saturation curve and therefore neglects

(*a*) Effect of saturation of the leakage paths due to stator currents.

(*b*) Effect of field-current load saturation. Actual rotor saturation is a function of both field excitation and direct-axis rotor air-gap flux, as shown in reference 13.

(*c*) Effect of change in leakage-path saturation due to redistribution of field-leakage flux.

The errors due to these assumptions are particularly appreciable under short-circuit conditions, and it becomes necessary, in any analytical method which attempts to approach actual conditions, to include properly these effects. It has been previously shown that saturation of the leakage paths is the principal cause for low equivalent short-circuit reactance. It is not sufficient to assume, as Rudenberg has done, that the subtransient current can be determined at the first instant by including only the stator leakage reactance and neglecting the effect of amortisseur or rotor leakage reactance. Actually the stator leakage reactance is reduced, particularly in the case of turbine generators. Then the problem becomes one of determining the amount by which both the stator and rotor leakage reactances are reduced.

As will be readily recognized, Rüdenberg's method assumes that the transient reactance and Potier reactance are equal to the sum of the stator- and rotor-field leakage reactances. This is not the case. It is well known that Potier reactance varies, depending upon the amount of load saturation, and even at normal operating voltages is usually less than transient reactance ("Armature Leakage Reactance of Synchronous Machines" by L. A. March and S. B. Crary, AIEE TRANSACTIONS, volume 54, 1935, April section, pages 378–381).

R. Rüdenberg: The discussion by Mr. Crary indicates that he is still wholeheartedly on the ground of the classical linear theory, which has had its great merits in clarifying the principles. Significantly, he begins with the assertion that "any analysis of the influence of saturation must necessarily be approximate" and with similar statements of axiomatic character.

Fortunately, though, this is not the fact. The saturation in synchronous machines can be taken into account by methods having the same rigor as the classical methods have for the linear case. It is only necessary to develop a transient analysis which can be applied graphically rather than analytically,

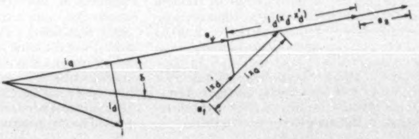

Figure 1

since for the most part the saturation functions of machines are given graphically.

If Mr. Crary will go over the paper again, free from the bias of a nomenclature of parameters developed solely for nonsaturated machines and not applicable therefore to the present case, I think he will be surprised to see how simply the rigorous consideration of the saturation under transient conditions actually works out.

It is by no means necessary to develop first a nonsaturated transient theory as most authors, including those referred to by Mr. Crary, have done for fear of a too complicated saturated analysis, and then try to correct such a linear theory by "additional effects" of saturation. It is the object of my paper to consider saturation from the very beginning as a fundamental element and to show that a rigorous and yet a highly practicable nonlinear analysis can be developed without the crutch of subsequent corrections.

Mr. Crary deduces as an analogue to my main relation (equation 14) a similar but not identical equation expressed in terms of his preferred nomenclature. However, his is not a relation of equivalent value, since it contains parameters which are defined for nonsaturated conditions only and actually are not constant for saturated machines. Thus, although the analysis sketched by Mr. Crary may be, according to his own words, an approximate one, due to his introducing saturation a posteriori, the method of my paper is definitely a rigorous one in considering saturation a priori. All the many omissions, assumptions, and approximations which Mr. Crary ascribes to the new method are by no means actually contained in it but are merely imaginary, caused by his viewing the present problem from an unsuitable angle.

This may be verified by two examples among many others. Mr. Crary asserts in his second and third paragraphs from the end that I have neglected saturation of the leakage paths due to stator currents. However, he seems to have overlooked paragraph 9 of section 4 of my paper, with equation 29 and Figure 16, where I show analytically and graphically the manner in which saturation of stator and rotor leakage paths enters this analysis and can be considered rigorously from the very beginning. Also, the last sentence of my paper, before the summary, generally treating nonlinearity with current, seems to have been overlooked by Mr. Crary in writing his discussion.

In his last paragraph, Mr. Crary asserts that my method would assume the "transient reactance" and "Potier reactance" as equal to the sum of the stator and rotor leakage reactances. This is contrary to my statements. Actually, I have proved in paragraph 7 of section 5, by means of Figure 19, that "transient reactance," a classical notation which I do not use elsewhere in my paper, with saturated machines always appears materially greater than the sum of stator and rotor leakage reactances. "Potier reactance," on the other hand, is, for good reasons, not used nor mentioned in my paper.

And so it is with all the other objections of Mr. Crary. It seems to me that by the simplicity of the new method, which considers saturation as a primary effect, Mr. Crary is misled in believing that this method would not cover the many secondary effects which apparently he has in mind. Simplicity, however, is not of itself a preventive of accuracy.

Formulas for the Magnetic-Field Strength Near a Cylindrical Coil

Discussion and author's closure of paper 42-27 by H. B. Dwight, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 327–33.

A. U. Welch (General Electric Company, Pittsfield, Mass.): The formulas in this paper form a very complete solution of the problem of computing magnetic fields within and near a cylindrical coil. Where great accuracy is not required, the desired information could be given much more conveniently by curves. The writer had plotted such curves a number of years ago and has found them very convenient. They are, therefore offered here in the hope that they may prove helpful to others.

These curves are plotted from formulas derived by the writer by integrating and differentiating well-known formulas for mutual inductance and magnetic potential. The formulas used were not so complete as those in the paper, and it was necessary to resort to numerical integration methods with formulas for circles similar to equations 12 and 13 in the paper to bridge the gaps between the limits of convergence of the different formulas. The curves used are as follows:

Radial Flux. It will be seen by differentiating and integrating that the radial

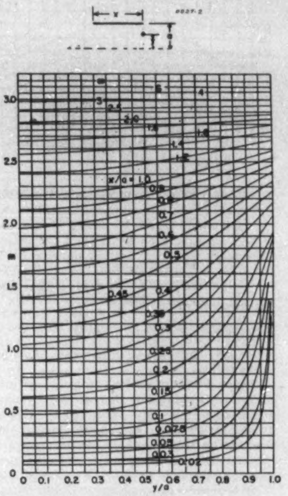

Figure 2. Axial flux density at a point in the end plane of a cylindrical current sheet, point having smaller radius than the current sheet

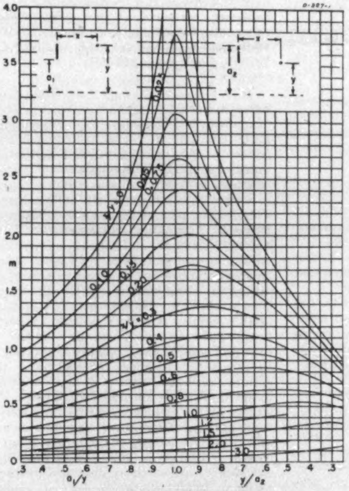

Figure 1. Curves of mutual inductance between coaxial circle and ring

Used for computing radial flux density

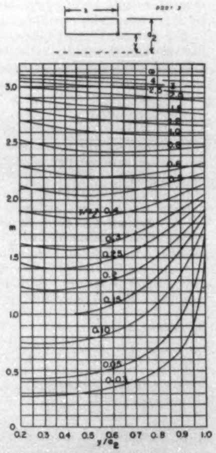

Figure 3. Axial flux density at the inside corner of a coil

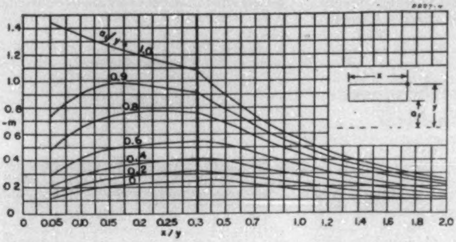

Figure 4. Axial flux density at the outside corner of a coil

flux density at any point is a function of the difference of the mutual inductance between the ring which is the boundary of one end of the coil and a circle through the point, subtracted from the mutual inductance of the other end of the coil and the same circle. If the quantity m be so defined that the mutual inductance is equal to $4\pi y m$, then

$$H_r = 0.2nI(m_1 - m_2)$$

where the definition of terms is the same as in the paper.

Figure 1 and the above formula permit computation of radial flux density at any point having the same radius as the inside or outside of the coil. (Note that in the sketches on the curves the dotted lines are the center lines of the coils, and only one side of the coils is shown.) By subdividing a coil into parts or by adding fictitious parts, if the point is beyond the inner or outer radius of the coil, and weighting the contribution of each part according to its radial thickness (number of turns), the field can be computed at any point within or near the coil.

The radial flux caused by a cylindrical current sheet of zero thickness becomes a function of the difference of mutual inductance of circles. Since complete tables of mutual inductance of circles are available,[1] no curve is necessary for this case.

Axial Flux. Figures 2, 3, and 4 give axial flux density at points in the end plane of cylindrical current sheet and at the inside or outside corner of a thick coil. Axial flux density

$$H_x = 0.2nIm$$

It should be noted that the direction of axial flux is opposite on the inside and outside corners of coils.

By superposition of parts of coils the flux density at any point within or near a coil can be calculated as outlined above.

The curves are unsuitable for computing fields at a large distance from the coil, since the result will be the difference between two nearly equal quantities. Formulas are available which converge rapidly for this case.[2]

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1. TABLES FOR CALCULATION OF MUTUAL INDUCTANCE OF CIRCUITS WITH CIRCULAR SYMMETRY ABOUT A COMMON AXIS, F. W. Grover. Scientific paper 498, Bureau of Standards.

2. Reference 2 of the paper.

H. B. Dwight: The curves presented by Mr. A. U. Welch are a welcome addition to my paper. They can save computation by the formulas, which is a rather long piece of work in some cases. His discussion indicates that this general problem has practical applications in engineering. For instance, the mechanical force on a corner conductor of a current-limiting reactor can be computed as the product of the current in that conductor and the magnetic field strength at the corner of the coil.

Example II of the paper is checked very quickly by Figure 2 of the discussion. At $y/a = 0.6$ and $x/a = 0.4$, m as read from the curves is 1.45. Then

$$H_x = \frac{2nI}{10} \times 1.45 = 2.90\frac{nI}{10}$$

Impulse and 60-Cycle Characteristics of Driven Grounds—II

Discussion and authors' closure of paper 42-22 by P. L. Bellaschi, R. E. Armington, and A. E. Snowden, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, pages 349–63.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In a discussion of a contemporary paper by Messrs. Robertson, Lewis, and Foust, "Lightning Investigation at High Altitudes in Colorado," the writer referred to effects of earth resistivity and geology. This relates also to the paper by Messrs. Bellaschi, Armington, and Snowden. The conductivity of the soil depends principally on the presence of moisture and salts. The ancient rocks are low in porosity, moreover during their long lifetime, soluble salts have been washed out. Where they lie near the earth's surface, resistivity is high. Where the ground resistance is low, there are moisture and soluble salts making electrolyte. In the writer's opinion, the reductions in ground resistance observed at high currents in soil of relatively low resistivity are caused largely by the change in resistivity of electrolyte produced at high currents, whereas in soils of high resistivity the reduction in apparent resistance is produced largely by sparking in the earth as discussed by the authors. In fact, during similar tests made at Trafford in 1929, sparking was actually observed visually on high-resistance grounds when surged with high currents. Those tests were not as exhaustive as the more recent ones described in the Bellaschi, Armington, and Snowden paper, but the results were of similar character. The publication of the early Trafford tests,[1] contained a caution which may bear repetition. The reduction in resistance of what is termed a high-resistance ground achieved at high-surge currents does not necessarily make it a good ground, since the discharge is accompanied by high voltage. The resistance as determined with low-voltage measurements is a good guide to the efficiency of grounds.

In regions of great geological age, the behavior of grounds is of particular interest, since they are inherently high in resistance. Data on this would be of value.

REFERENCE

1. *Railway Signaling*, April 1929, pages 145 and 146, and *Electrical World*, December 9, 1933, pages 751 and 752.

C. Francis Harding* (Purdue University, Lafayette, Ind.): This discussion emphasizes the importance of the use of laboratory tests and models in predicting probable characteristics of grounds in practice. All will recognize the desirability of having the tests made upon actual line installations in so far as possible. However, this is particularly difficult, expensive, and slow of solution in the case of surge potentials. Even in the case of the additional protection against lightning of 4,000-volt distribution lines by means of the interconnection of secondary neutral to lightning-arrester grounds, several years ago, before this Institute, Mr. Roper stated the fact that the laboratory investigation had solved the problem in many years less time and at much less expense than would have been possible by awaiting actual lightning strokes.

Professor J. H. Karr of Purdue University, among others, established a satisfactory close correlation between 60-cycle ground impedances in small volumes of various soils in the laboratory and in actual installations. With the apparently wide variations of such impedances with the shape and depth of grounding electrodes, and with the temperature, moisture content, density, and chemical analysis of various soils, it is obvious that laboratory tests should provide an early solution to such a problem. The authors of this paper have been fortunate in having several types of soil within reach of a surge generator. It is not evident from the paper, however, that ground electrode shapes other than rods of limited diameter were used.

Mr. J. R. Eaton, of the staff of the new high-voltage laboratory of Purdue University, in co-operation with Messrs. Lewis and Foust, is engaged in an attempt to secure further data involving the previously mentioned variables in volumes of various soils in the laboratory using the wide variety of grounding electrodes, sometimes adopted in wooden-pole transmission-line construction. It is hoped that such tests will determine the conditions which contribute most to low impedance offered to surges. Later, by means of a portable surge generator, it is hoped to secure correlations between such laboratory findings and actual installations. Such a procedure should be more rapid and much less expensive than a statistical analysis of actual lightning conditions. Both may be necessary for a complete solution of this important problem.

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*Deceased April 13, 1942.

Hamilton Treadway (United States Department of Agriculture, Washington, D. C.): This paper is a valuable extension of the author's previous work on this subject.

There is one point which may give rise to some controversy. The author proposes that the decrease in the resistance of driven grounds with increasing impulse current is due to an increase in effective radius and length due to the increase in voltage gradient. It cannot be doubted that the voltage gradient on the rod results in an increase in both the effective length and radius of the rod. However, it is doubtful whether this is the sole reason for the decrease in resistance.

1. This conclusion assumes that the shell of earth around the ground rod, after the breakdown occurs at the critical gradient, has the same conductivity as the rod, and that the contact resistance of this shell of earth to the surrounding soil is the same as that of the ground rod to the soil. This assumption is obviously incorrect but for all practical purposes may be justified. This point might be clarified by conducting tests on various size rods using values of crest current to give equal voltage gradients on each rod.

2. This conclusion ignores the relation between the leakage resistance of the soil, the contact resistance between the rod and soil, and the capacitance of the rod. The ground rod and lead circuit is essentially an imperfect capacitance in series with a resistance and inductance. The contact resistance is in parallel with the capacitance of the rod. Under transient conditions as the circuit approaches series resonance, the contact resistance is effectively shunted out, and the resistance indicated is essentially the leakage resistance of the earth itself.

The relation between the constants of the ground circuit is an important one. As pointed out by Mr. Hagenguth in his discussion of the author's first paper on this subject, the impulse impedance will rise rapidly during the first few tenths of a microsecond and in all probability will greatly exceed the 60-cycle value. The impulse impedance-time curve for the parallel combination of grounds *F*, *G*, *H*, and *I* in Figure 13 of the author's paper substantiates the existence of an initial impulse impedance exceeding the 60-cycle value. This initial rise in the impulse impedance may result in voltage stresses which will exceed the single-shot breakdown voltage of the insulation of low-voltage equipment. This may explain many cases of meter failure which have occurred in rural areas of high lightning intensity. Furthermore, with or without interconnection of lightning arrester and secondary grounds, great danger to life can result from this characteristic of the impulse impedance.

The relation between the inductance, capacitance, contact resistance, and leakage resistance of the ground circuit explains the *V* characteristic of the impulse resistance-time curves in Figures 11 to 15 of the author's paper. As the point of series resonance is passed, the impulse impedance of the circuit increases, and the impedance again approaches the 60-cycle value.

This *V* characteristic would be experienced, irrespective of the effect of voltage gradient. With much lower values of impulse currents, where the effect of voltage gradient would be minimized, Towne in his earlier work obtained similar results.

A need is apparent for further research in this field. Work on the evaluation of the ground-circuit constants for the various type soils and ground conductors is desirable. This information will be of great value in the future study of the protection of apparatus from lightning. It is hoped that the author's further investigation of the impulse characteristics of driven grounds will shed some light on the relationship between the circuit constants.

W. E. Berkey (Westinghouse Research Laboratories, East Pittsburgh, Pa.): The authors explain the lowering of the effective resistance of an electrical ground under impulse test in terms of an increase in effective area of the grounding electrodes caused by an electrical breakdown of the ground due to high-voltage gradients. With this assumption they are able to calculate the ground resistance of single or parallel grounds under impulse test conditions with a knowledge of the soil resistivity, the impulse current, and the dimensions of the rod.

It seems probable that the mechanism of conduction through the soil is mainly electrolytic. It is well-known that in electrolytes the conductivity increases (or the resistivity decreases) when the voltage stress is increased. Dilute solutions show a larger increase in conductivity than more concentrated solutions. The increase of conductance depends upon the mobilities, valences, and concentration. This is known as the Wien[1] effect and is not due to temperature or heating effects. For example, in Table I is shown the Wien effect for a barium ferrocyanide solution with conductance $k = 0.000125$ ohm^{-1} centimeter^{-1} or resistivity = 8,000 ohm-centimeters. The electrode spacing was 0.44 centimeter, and the time constant of the spark discharge was 10 microseconds.

This effect, of course, varies with different electrolytes but may account for the observed lowering of ground resistance in a qualitative way, when it is remembered that probably the conduction through the earth is through electrolytic film-coated particles, in which the voltage gradients may be much higher at the particle contacts than over the particle itself. Debye and Falkenhagen[2] discovered an increased conductance due to an increase in frequency in electrolytic conduction. The conductivity of soil was found by Banerjee and Joshi[3] to increase with frequency of radio waves.

Thus, it would seem possible to account for the lowering of the impulse resistance by assuming that the effective resistivity of the soil is a function of the voltage gradient. Have the authors considered this approach? In the case of ground *M* shown in the oscillogram of Figure 6 of the paper a real breakdown occurs, in which some of the particles actually flashed over, due to excessively high gradients caused by the abnormally high soil resistivity.

Table I. Wien Effect for $Ba_3Fe(CN)_6$

Kv	$\frac{R_x}{R_0}$	Kv X/Cm	
4.47	0.94	10	Data fits curve $R = AX^2 - BX^4$ where *A* and *B* are constants
7.96	0.83	18	
11.3	0.74	25	
14.3	0.65	32.5	
17.4	0.57	39.5	
20.3	0.50	46	
26.1	0.41	59	
31.7	0.33	72	
37.3	0.28	85	

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3. DIELECTRIC CONSTANT AND CONDUCTIVITY OF SOIL AT HIGH RADIO FREQUENCIES, **S. S. Banerjee** and **R. D. Joshi.** *Philosophical Magazine*, volume 25, series 7, 1938, page 1025.

P. L. Bellaschi, R. E. Armington, and **A. E. Snowden:** The authors want to thank all who have discussed the paper for their contribution to the subject.

Mr. Berkey cites interesting data on the lowering of the conductivity of a barium ferrocyanide solution with increasing voltage gradient. It is suggested that the resistance of grounds in soils may be affected to the same extent by purely electrolytic action. The results of impulse tests we have made on tap water having a resistivity (2,200 ohm-centimeters) which is representative of the ground water at the site of the grounds show that the resistance of the water is practically constant, independent of the voltage gradient. Impulse currents of a 15x30 microsecond wave were applied to electrodes immersed in the water and both current and voltage oscillograms recorded. The measurements cover gradients up to 28 kv per centimeter. The water broke down at 50 kv per centimeter. Within the range of experimental accuracy, all these data show that the resistance of the water remains constant.

The influence of the electrolytic effect in lowering the impulse resistance of natural grounds commonly encountered, similar to those investigated, does not appear to be of more than secondary importance. Exception to this would probably be the case of low-resistance grounds, such as grounds in fills and artificial grounds treated with salts for the specific purpose of lowering the resistance. That is, the exception would be the low-resistance grounds—a point Mr. Beck also brings out in his discussion.

The importance of geology, soil structure, and similar factors in determining the resistance of grounds in a given location, to which Mr. Beck refers, will require a good deal more attention and study. For the problem is of such wide scope that a clearer and more complete understanding of the underlying factors hardly can be reached without a combined approach of field experience, analysis, and actual tests.

As further progress is made, some of the factors Mr. Treadway suggests may be segregated and their respective influence and part in determining the characteristics of grounds better established. However, a good deal more experimental data will be required. In the opinion of the authors, contact resistance and capacitance are factors of secondary importance. There are some exceptions to this statement. For instance, grounds do have a capacitance effect which in very high-resistance soils such as rocks, has a certain contributing effect on the characteristic of the grounds.

Again, the capacitance is a factor of some importance when an abrupt front is considered, particularly in the initial stage of the front discharge.

We agree with Professor Harding that many a complex technical problem has been tackled effectively and economically in the laboratory. The complex nature of ground presents problems which are conducive to fundamental approach in the laboratory. Gradually, the investigation should be extended to the complex forms which the practical problem of grounds in the field present. We always favor correlating field and laboratory results. To accelerate progress and because of favorable conditions, the method and approach presented in the paper were followed. That is, grounds such as employed in service were driven in various soils adjacent to the laboratory and tested at current discharges corresponding to actual lightning.

Shielding of Substations

Discussion and authors' closure of paper 42-14 by C. F. Wagner, G. D. McCann, and C. M. Lear, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 96–100.

G. D. Floyd (Hydro Electric Power Commission of Ontario, Toronto, Ont., Canada.): The degree of protection given to a substation by masts or wires or by a combination of these has hitherto been largely conjecture. This paper should remove some of the unknown factors and permit design of shielding on a somewhat more sound basis. I do not think it is the authors' intention that the data presented should be taken too literally. For example, are not the curves for 0.1 per cent exposure so close to the vanishing point that for all practical purposes all points lying on or below them constitute perfect shielding? Some details of the method by which the authors arrived at a curve where only one stroke per thousand contacted the protected object would give the reader a better idea of the use which might be made of these data in design of shielding.

As a general rule the low-voltage low-capacity substation is not shielded deliberately; the data in this paper offer an explanation. This type of substation covers only a small area and is generally designed to have relatively low elevation. Both factors contribute to the relative immunity enjoyed by such a station. It is rather a nice point to decide whether shielding is justified in the first place, and having decided that it is, to determine the degree in which it should be applied. Whether one per cent exposure is the maximum to be permitted is questionable. I doubt if any substations have such excellent shielding, unless it has been acquired indirectly.

A very common form of shielding is by means of overhead grounded wires, supported either on extensions of the main station structures, or on separate masts. It will usually be found that this combination is cheaper than masts alone for the same degree of protection and can, in addition, be combined with the overhead ground wires of incoming and outgoing lines, to protect these lines as well as the live parts in the station proper. While the data of this paper would appear to cover this combination, I think the advantages of this arrangement might have been shown a little more in detail.

For a given coverage masts and wires are more economical than masts alone and, in certain cases, are also cheaper than structure extensions. A standard height and design of mast is also economical, regardless of the configuration of substation. The required shielding is provided in this case by adjustment in location and number of masts.

It is not quite clear that the cross-hatched areas of Figures 11c and 11d of the paper represent the protected areas for 0.1 per cent exposure. If the locus shown in Figure 11a represents the approximate limit for two masts, should not the corresponding total area for three and four masts be the superposition of the areas between each mast and every other mast? If this be correct appreciable areas shown protected in Figure 11c and 11d would actually be unprotected on the basis of 0.1 per cent exposure.

C. F. Wagner, G. D. McCann, and **C. M. Lear:** Mr. Floyd's first point is concerned with the choice of 0.1 per cent exposure as a basis for application. Because of the vagaries of lightning the phenomenon involved is statistical in nature. While it is possible to install a network of parallel overhead ground wires, so that perfect shielding is attained, the more conventional construction that usually consists of one or two ground wires leaves large gaps through which a stroke might "infiltrate." It is difficult to assign limiting configurations to such construction that just provide perfect shielding. This may be seen from Figures 3 through 7 in the paper which show considerable displacement between the 1.0 per cent and 0.1 per cent curves. Had sufficient data been taken so that 0.01 per cent curves could have been plotted, some displacement between these curves would still exist. The laboratory difficulties encountered in obtaining such data are evident, inasmuch as to provide data on one point for 0.01 per cent exposure would require something of the order of 100,000 shots. Thus it becomes impractical to obtain data on perfect shielding. The authors have, therefore, chosen the value of 0.01 per cent exposure, as it represents a low enough value upon which data could be obtained and, at the same time, a reasonable value for design purposes. Even these data involved some extrapolation.

The authors agree with Mr. Floyd in the opinion that supporting masts with overhead ground wires are, in general, more economical for the shielding of large areas than masts alone. Some station design engineers, however, fear the danger incident to the breakage of such overhead conductors. It would seem that this difficulty could be alleviated by sufficiently heavy construction.

The question has also been raised regarding the extent to which all points within the shaded areas of Figures 11c and 11d of the paper are protected. These areas were derived from general considerations provided by the test data. Since presentation of the paper, additional tests have been made which definitely establish that all points within the shaded areas have an exposure not over 0.1 per cent for the range of variables given in Figure 10c.

The Influence of Towers and Conductor Sag on Transmission-Line Shielding

Discussion and author's closure of paper 42-37 by R. W. Sorensen and R. C. McMaster, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 159–65.

C. F. Wagner and **G. D. McCann** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A paper presented before the Institute in 1940 by Wagner, McCann, and MacLane discussed the factors that must be correctly simulated in the laboratory so that tests on scale models can be used to obtain data regarding shielding. This paper also described test results concerning the shielding of transmission conductors by ground wires. This work has been extended by two papers presented at this session—one by Sorensen and McMaster which considers the shielding afforded by the presence of the towers and by the sag of the ground wires and conductors, and the other by Wagner, McCann, and Lear which considers the shielding of substations.

The data presented by Sorensen and McMaster is interesting in that it shows, for the particular configuration assumed, the extent to which the tower structure shields the conductors. For distances approximately two times greater than the tower height, little shielding is provided. This effect is epitomized in the curve of Figure 24 of the paper which presumes to indicate the variation of the integrated shielding effect for different span lengths. The ordinate of this curve is labeled "Per Cent Added Protection," which, expressed more clearly but not so concisely, might be called "The Per Cent Decrease of Strokes to the Conductor Because of the Presence of the Tower." The curve in Figure 24 is based upon the assumption that the relative shielding of the tower, as given in Figure 22 of the paper, is independent of the span length. Actually, however, this is not the case. As shown by Figure 7 of the paper entitled "Shielding of Substations," substantially perfect shielding should be obtained for span lengths less than twice the conductor height. The full

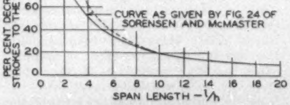

Figure 1. Added protection resulting from presence of towers for various lengths of span

line of the accompanying Figure 1 is reproduced from Figure 24 of the Sorensen and McMaster paper, and the dotted line indicates the more probable curve resulting when an estimate of the change in distribution with span length is taken into consideration. The normal range of span length to tower height lies between 6 and 12, between which values the presence of the tower reduces the strokes to the conductor between 15 and 40 per cent. Thus the previous work on the shielding of conductors by ground wires is conservative to this extent. This represents about the same order of accuracy as one would expect upon extrapolating laboratory results to actual systems. It is assuring, however, that the error by neglecting tower effects is on the side of conservatism. Correction for this effect involves an increase of but a few degrees in shielding angle, which is hardly worthy of consideration.

The curve and the data presented by Sorensen and McMaster apply only to the particular configuration tested which represents a shielding angle of 64 degrees. While this particular configuration was chosen to magnify the effects involved, it would have been interesting to have determined the tower effects for a smaller shielding angle, an angle between 35 and 45 degrees which is more representative of good practice.

Robert C. McMaster (California Institute of Technology, Pasadena, Cal.): Since the writing of this paper, a further investigation of the shielding effect of towers on short transmission-line spans has been completed. Results of model tests conducted under conditions identical to those reported in the paper, for a span of length equal to twice the tower height, are shown in the accompanying curves. Two identical model towers (see Figure 6 of paper for detailed dimensions), each 10 inches in height, were placed 20 inches apart and supported taut conductor and ground wire at a protective angle of 64 degrees as shown in Figure 7 of the paper. Cloud height was five times tower height, as before.

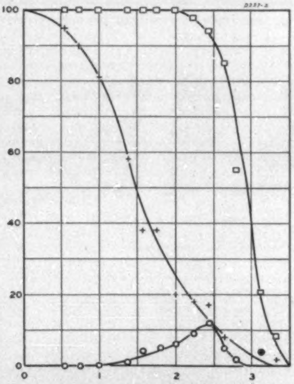

Figure 2. Stroke distribution in plane $B/h=0.5$, for span lengths of $2h$

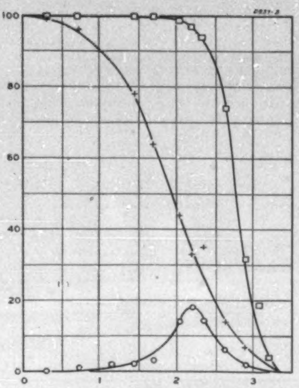

Figure 3. Stroke distribution in plane $B/h=1.0$, for span length of $2h$

With the cloud electrode in the plane of the tower, $B/h=0$, the distribution of Figure 8 of the paper was obtained for strokes to the system. With the cloud electrode in the plane $B/h=0.5$ (one-quarter the distance between towers) there was obtained the stroke distribution of Figure 2 of this discussion. In the plane $B/h=1.0$ (half the distance between towers) the distribution was found to be that shown in Figure 3 of this discussion. From the areas under these curves, Figure 4, showing the distribution of strokes along the span, was obtained.

The per cent added shielding, resulting from the presence of towers, is found by comparing Figure 4 of this discussion with Figure 21 of the paper, and equals 88.9 per cent for this span length. The result obtained from the investigation of longer spans, shown in Figure 24 of the paper, gave 80 per cent as the added shielding for this case, a conservative value which differs from 88.9 per cent by an amount which would have negligible effect upon the shielding calculated for an entire transmission line in which such short spans would be rare. Figure 5 of this discussion shows this correction to Figure 24 of the paper.

It is to be noted again that the results given in this discussion and in the paper apply quantitatively only for the 64-degree protective angle chosen for the test measurements, as stated in the paper. For other protective angles, the trends and relative shielding effects of towers and line sag can be determined only qualitatively from these data.

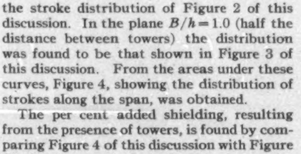

Figure 4. Distribution of strokes along line

Taut parallel ground wire and conductor with towers in place. Span length $2h$

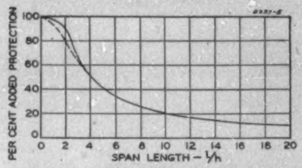

Figure 5. Added protection resulting from presence of towers, for various lengths of span, showing small correction at $l/h=2$

R. W. Sorensen: The several papers of the session have each made a definite contribution to our knowledge of lightning and what to do with it in connection with the operation of electric power systems, and structures to be protected. In 1926, some costly fires due to lightning occurred in oil-storage reservoirs, and a rather hurried study was made to determine the value of rods or towers as a means of providing protection against damage by lightning. One result of that study was the erection in southern California and other places of well-grounded towers to serve as lightning rods around oil-storage reservoirs.

In general, the towers were located so that any spot to be protected came within a circle, such that the distance from the rod on the level of protection was within $2^1/_2$ times the rod height from the rod. In other words, each tower is expected to protect around it an area included within a circle having a radius of $2^1/_2$ times the tower height. It is obvious that such a tower could not be in the center of the structure whose protection is desired. Therefore, in each installation, several towers surrounding the object to be protected were used.

These towers were placed at considerable distance from the reservoir to be protected, to guard against side flashing from the tower to the reservoir.

Naturally, the protective circular areas for the several towers were made to overlap so that all parts of the object whose protection is desired will have the protection of one or possibly more rods. No guarantees were made that absolute protection could be provided by rods, but up to the present time (15 years have elapsed) all places provided with the protection planned have been free from damage by lightning. The statistical data are therefore favorable.

The paper, "Shielding of Substations," by Wagner, McCann, and Lear, adds greatly to the information that was obtained when the protective plan for the oil-storage reservoirs was developed. Their presentation, combined with the information contained in each of the papers presented at the session, well augment our knowledge as to what can be done in maintaining lightning protection by the use of rods—in some cases supplemented by connecting grounded conductors.

A comparison of the data presented in McMaster's discussion, which show some slight changes from the data in the original paper, indicates that the original data regarding the amount of shielding due to towers and sag do not give an impression of more protection than might normally be expected in power lines with shorter spans.

With regard to the dependability of results as obtained from the tests on models as discussed in our paper, may I call attention to the fact that in studying actual lightning performances, as well as in the use of model tests, the results obtained are statistical. The probability law regarding the ratio of strokes that hit the rod, to strokes that hit elsewhere, holds both in model tests and in actual observation of lightning itself.

It therefore seems to me that, notwithstanding our inability to exactly duplicate all actual conditions with small scale models, the statistical measure of protection provided by the tests on models is well within the range of the statistical measure of protection upon which one can rely in using data as they apply to actual lightning. It therefore does not seem unreasonable to assume that we are fully justified in considering our model tests as indicative of actual lightning conditions, due regard, of course, being given to a reasonable engineering tolerance factor.

Lightning Investigation on 132-Kv Transmission System of the American Gas and Electric Company

Discussion and authors' closure of paper 42-18 by I. W. Gross and G. D. Lippert, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 178–85.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have presented a large amount of data accumulated over the last eight years during the course of their lightning investigations. These data should be very useful to the utilities as well as to the designers of equipment, since the large amount of data available and presented permit drawing rather definite conclusions as to the severity of lightning currents, the effectiveness of ground rods and counterpoises and the effectiveness of modern lightning arresters.

The authors mention, in connection with Table II, that the apparent line-surge impedance is of the order of 236 ohms or less when the calculation is based on the measured bus voltage and on the measured line current. Conversely the traveling wave voltage using line current at station and surge impedance of 400 ohms is of the order of from 800 to 6,000 kv.

I would suggest the following interpretation:

If traveling wave theory is used on a circuit represented by an incoming line and outgoing line with an arrester at the junction, and the arrester is represented by a resistance corresponding to its maximum *IR* drop, the effective station resistance R' is a parallel connection of outgoing line-surge impedance Z_2 and arrester resistance R or $R' = R \times Z_2/R+Z_2$.

In the case of the currents presented in Table II of the paper, the *IR* drop is of the order of 315 to 355 kv, the arrester resistance between 140 to 260 ohms, and the effective station resistance R' between 105 and 157 ohms if Z is taken as 400 ohms. The sum of the currents in the outgoing line and through the arrester is equal to measured line current.

$$i_R' = \frac{2E}{R'+Z}$$

where E is traveling wave voltage.

The current associated with the incoming traveling wave is $i_t = E/Z$; therefore, the ratio of traveling wave current to the total measured current is

$$\frac{i_t}{i_R'} = \frac{R'+Z}{2Z}$$

or

$$i_t = i_R' \ \frac{R'+Z}{2Z}$$

i_R' represents the sum of the incident and reflected traveling current wave which is measured as line current. R' and Z are known impedances, and, therefore, the traveling wave current, the traveling wave voltage, the current in the arrester and in the outgoing line can be calculated. Table I of this discussion shows these values for the five cases at Roanoke.

Therefore, the bus voltage represents the sum of the incident and reflected traveling voltage wave; likewise, line current represents the sum of the incident and reflected traveling current wave. Bus voltage divided by line current gives the value of the effective terminal impedance of the line.

The traveling wave voltages range between 560 and 800 kv which are reasonable values and are to be expected near a station on a 132 kv system.

The bus voltages measured are, of course, at least as high as the arrester-gap breakdown, which is 388 kv for AIEE lightning-arrester test. The *IR* drop following the arrester-gap breakdown is however, much lower for the low currents indicated in Table I of this discussion. The effective terminal impedance depends on the *IR* drop of the arrester rather than on the gap breakdown voltage.

The calculated and measured arrester current checks well for case 3 only. In all other cases, the calculated currents are higher than the measured values from 700 to 2,000 amperes. It is not stated whether the measured line current is the sum of the currents on the incoming and outgoing line. If it were the sum, then, of course, the calculated arrester currents would be considerably smaller. In case 3 for instance, the lightning arrester current would be 800 amperes instead of 1,400 amperes. In calculating Table I, line currents have been assumed to be measured on one line only.

The actual circuits involved in the terminal impedance at stations is much more complicated than the circuit used for these calculations. Especially if station capacitance is taken into account, the traveling wave current would be roughly one-half line current for relatively steep current waves, because the capacitance initially reacts as a short circuit to the incoming current wave with a consequent reflected wave of amplitude and polarity equal to the incident wave. On that basis, the calculated arrester currents check more closely the measured currents, and the traveling wave voltages will range between 400 kv and 640 kv for the examples in Table I of this discussion. A more exact calculation can be made when all the constants of the circuit are known.

The low bus voltage of 300 kv in the fourth case at Roanoke is indicative of a slow rate of voltage rise of the traveling wave. This is substantiated by the maximum rate of voltage rise of 92 kv per microsecond given in the table for this case, the calculated *IR* drop is 320 kv with an arrester current of 1,600 amperes. In the fifth case at Roanoke, the high line current and low bus voltage without arrester operation suggest that the incoming traveling wave was of the order of 800 kv or higher but of short duration, probably less than one microsecond. Such a wave could be reduced by the station and bus capacitance to the measured value of 233 kv and, therefore, would not cause arrester operation. It would be interesting to know whether records indicate a line flashover at the time this record was obtained. A short wave of only one-microsecond length would, however, have a relatively steep front of the order of 800 kv per microsecond or greater, while the wave-slope indicator indicated only 164 kv per microsecond.

Similarly, the high bus voltages shown for cases 1 and 2 at Roanoke would indicate high rates of voltage rise to exceed the arrester-gap breakdown voltage of 388 kv.

The rates of voltage rise indicated in the Table II of the paper for a few of the cases in Roanoke, therefore, seem to be too low to account for some of the other simultaneous observations in the station and on the line.

If a wave-slope indicator, such as shown on Figure 1A of the paper was used in this station, the calibration of the instrument would have a dual calibration on account of the 35-turn coil which probably has an inductance of the order of 13 microhenries and the fact that a protective gap is provided. With rates of rise producing inductive voltages with amplitudes smaller than sparkover of the gap, the calibration should be $de/dt = i/2C$. When the rates of rise are great enough to arc-over the gap, the current indication can be equivalent to several times the rate of rise indicated by the above equation.

On the basis of such calculations, it might be worthwhile to re-examine the records. Such an examination might show that a larger proportion of waves had steeper wave fronts. Due to the dual calibration characteristic, the correct rate of rise can be established only in such cases where other related data, such as in Table II of the paper, are available.

Finally, with respect to the authors' Table III, I would suggest a possible explanation for the apparent greater effectiveness of the tower legs to carry current than the counterpoise. If we analyze case 1 of Table III, we find that the tower-base voltages associated with tower leg, ground rod, and counterpoise currents are 360,000, 275,000, and 117,000 volts respectively if currents are multiplied by resistance. Obviously, the three voltages cannot exist simultaneously. Now we know from tests[1,2], that a counterpoise resistance varies between some 100 to 200 ohms initially to a value somewhat lower than the d-c resistance, provided the current fronts

Table I. Lightning Data at Roanoke Station as Calculated and Compared With Test Results of Table II of the Paper. Z = 400 Ohms

Measured Line Current (Amperes)	Measured Arrester Currents (Amperes)	Calculated Arrester Currents (Amperes)	Bus Kv Measured	IR Drop Calculated	Terminal Impedance Calculated	Traveling Wave Current (Amperes)	Traveling Wave Voltage (Kv)
2,000	500	1,220	472	314	157	1,400	560
3,200	350	2,360	525	336	105	2,000	800
2,200	1,350	1,400	397	322	146	1,500	600
2,400	450	1,600	300	320	133	1,600	640
2,000		No operation	233	314	157	2,000	800*

*Arrester did not operate. Indicates chopped traveling wave reduced to 233 kv by station capacitance.

are steeper than the length of travel of the current wave on the counterpoise. On the other hand, the 200 ohms resistance of the tower leg probably does not vary considerably since its length is short (ten feet) and the registered current of 1,800 amperes is relatively low. The resistance of the ground rod probably is initially higher than the d-c value, since its length of 60 feet is considerable.

Calculations based on these considerations would indicate a probable maximum rate of current rise of the total current to ground of 20,000 amperes per microsecond and an average rate of rise of 4,000 amperes per microsecond with a crest current of 10,000 amperes. In other words, the three current crests measured do not occur at the same time.

The rates of rise indicated above are well within the range of measurements made on the Empire State Building.[3] A rate of 20,000 amperes per microsecond or more was recorded in 36 per cent of the records. Since the values shown in Table III are undoubtedly the results of direct strokes, the results of such measurements seem to indicate a qualitative check to the measurements at the Empire State Building. If the data of Table III are interpreted in this manner, that the apparent high tower-leg conductivity indicates high rates of current rise, such measurements might be useful to obtain statistical data on rates of rise of lightning currents.

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1. COUNTERPOISE TESTS AT TRAFFORD, **C. L. Fortescue** and **F. D. Fielder**. AIEE TRANSACTIONS, volume 53, 1934, July section, pages 1116–23.

2. THEORY AND TESTS OF THE COUNTERPOISE, **L. V. Bewley**. AIEE TRANSACTIONS, volume 53, 1934, August section, pages 1163–72.

3. LIGHTNING TO THE EMPIRE STATE BUILDING, **K. B. McEachron**. AIEE TRANSACTIONS, volume 60, 1941, September section, pages 885–90.

W. W. Lewis (General Electric Company, Schenectady, N. Y.): In Figure 10 of the paper by Messrs. Gross and Lippert are given curves of rate of change of voltage at stations. These curves are based on a certain interpretation of the wave-slope-indicator records.

In a paper by Lewis and Foust, entitled "Lightning Investigation on Transmission Lines—IV," which appeared in AIEE TRANSACTIONS for August 1934,[1] Figure 1 shows the time to reach crest of 45 waves caused by lightning on the 220-kv Wallenpaupack–Siegfried line, as recorded at the end of the line by cathode-ray oscillograph, and Figure 3 gives similar information for 16 waves recorded at the mid-point of the line. I have taken the same data and calculated the kilovolts per microsecond of the wave front.

In Figure 1 of this discussion curve *A* shows kilovolts per microsecond as ordinates against percentage equal to or greater than ordinate for 44 waves recorded at the end of the transmission line near the station. Curve *B* gives similar information for the 16 waves recorded at the mid-point of the line. It will be noted that curve *B* lies considerably above curve *A*. Three points of small voltage amplitude but very short time to crest account for the upper part of curve *B*. A possible explanation for the difference in these curves is as follows: The transmission line was covered with two overhead ground wires for 20 miles near the end of the line. Most of the waves recorded traveled in toward the station from a long distance and consequently suffered considerable attenuation before reaching the oscillograph. At the mid-point of the line, however, there were no overhead ground wires and it is possible that the waves originated nearer to the oscillograph. Consequently, these waves were steeper than the waves at the end of the line.

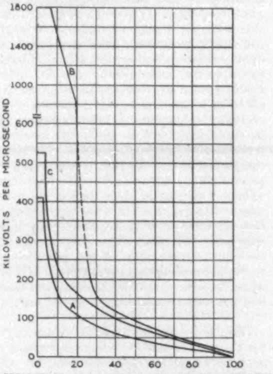

Figure 1. Comparison of wave-slope-indicator and cathode-ray-oscillograph records

Curve *A*. 44 cathode-ray-oscillograph records at end of Wallenpaupack–Siegfried line

Curve *B*. 16 cathode-ray-oscillograph records at mid-point of Wallenpaupack–Siegfried line

Curve *C*. 554 wave-slope-indicator records at stations on Glenlyn–Roanoke line

Curve *C* is plotted from 554 records secured on the Glenlyn–Roanoke line in 1939, 1940, and 1941, by means of the wave-slope indicator. These are the same data on which Figure 10 of the Gross-Lippert paper is based, but the data have been interpreted differently. The oscillatory surge interpretation has been used, making use of the data from all of the links.

The data of curves *B* and *C* were both taken under similar circumstances, that is, from traveling waves coming into stations on conductors shielded by overhead ground wires. These two curves check very closely and this circumstance gives considerable confidence in the wave-slope indicator and the oscillatory surge interpretation.

REFERENCE

1. LIGHTNING INVESTIGATION ON TRANSMISSION LINES—IV, **W. W. Lewis** and **C. M. Foust**. AIEE TRANSACTIONS, volume 53, 1934, August section, pages 1180–6.

J. T. Lusignan, Jr. (Ohio Brass Company, Mansfield, Ohio): In commenting on the results of Table II of the paper the authors mentioned the apparent discrepancy in arriving at line impedance values. I assume that the bus voltage is measured next to the arrester so that applying this to the line current should result in a value for arrester impedance not line-surge impedance. Could that be a proper assumption?

In the paper I note reference to the use of $^1/_{16}$-inch-by-2-inch flat iron strap for counterpoise. What are the advantages of this section? I am wondering whether there have been any corrosion problems with such a section as yet.

Should the range of 15.6–180 per cent for relative conductivity of the ground rods not be 31–180 per cent? The last column of Table III leads me to believe this.

The field-investigation data described in this paper should prove most helpful not only to the operating engineer and line designer but also to the equipment manufacturer. I was very interested in the figures given by the authors for rates of voltage rise recorded at the stations. While it is comparatively easy to design line insulators to resist puncture under exceedingly steep wave fronts, it is rather costly to do so with such station insulation as bushings. Often some sacrifice of other worth-while properties must be made. So far we have designed to meet voltage rises in excess of 1,000 kv per microsecond. We note that the authors only measured a maximum of 600 kv per microsecond. I am wondering whether they felt that 1,000 kv are still a reasonable figure. It is not that we want to drop it any. We would just not like to have it hiked much higher.

C. M. Foust (General Electric Company, Schenectady, N. Y.): The wave-slope measurements of conductor voltages at substations included in the paper by Messrs. I. W. Gross and G. D. Lippert were obtained through an extension of our now extensively used surge-crest ammeter technique.[1] Several steps in this adaptation, particularly as to details of calibration not included in the paper, are brought out in this discussion.

The general arrangement of field setup as shown in Figure 1 of the Gross-Lippert paper consists of magnetic links located in the ground connection to the coupling capacitors to measure maximum capacitor currents. The design of the coupling-capacitor wave-slope indicator is based on the following considerations:

1. A range of wave slopes from 25 to 5,000 kv per microsecond.

2. Measurements of capacitor currents reversing in polarity with a wide range of relative front- and tail-slope ratios.

3. Coupling capacitors of several capacitance values.

For a unidirectional voltage wave on the line the coupling capacitor current will reverse in polarity, and when the charging and discharging currents are of the same order of magnitude, a simple unidirectional current calibration for the magnetic links will be inaccurate, giving values generally below true wave slopes.

These considerations were provided for through the design of an eight-link recording station for each coupling capacitor with the links set in or adjacent to small coils in the ground connection of the coupling capacitor as shown in Figure 1A. The coil turns and link locations were designed to give a magnetization ratio of 2.5 to 1 between adjacent links. On a surge-crest ammeter of 100 divisions and proportional to magnetization current, this 2.5 to 1 ratio gives at least three adjacent link readings for each application of unidirectional current from 10 to 8,000 amperes. This range of current provides for wave-slope readings from 25 to 5,000 kv per microsecond with the available coupling capacitors ranging from 0.00025 to 0.0015 microfarad. Now with this overlapping of link magnetization, at the known link ratio of 2.5 to 1, an oscillatory calibration similar to those we have used for structure currents and checked by actual laboratory tests can be used to obtain crest-current values with reasonable accuracy.

On this basis of interpretation our field records range up to 525 kv per microsecond with the distribution of frequency of occurrence shown in Table II. These values do not depart greatly from those presented by Gross and Lippert in Figure 10 of their paper and based on a somewhat different interpretation.

As far as available records indicate, these slopes were the result of traveling waves incoming to the station from strokes to the line some distance out. For comparison with these distant strokes we have a direct-stroke oscillographic record made in 1930 of slightly under 2,000 kv per microsecond. This 2,000-kv-per-microsecond slope was a direct stroke to a high-voltage transmission line, the measurement having been made immediately at the point of a stroke to the line conductor of a 220-kv line. No overhead ground wires were in use at the time. The point struck was out on the line remote from the terminal stations, and no additional station capacitance was available to reduce the wave slope. This value of voltage wave slope, therefore, possibly ranges above that to be expected on well-protected lines at substations.

Table II

Wave Slope Kv Per Microsecond	Frequency of Occurrence Per Cent Above Magnitude of Column 1
525	Maximum recorded
220	10
150	20
100	40
60	60
35	80
25	100

REFERENCE

1. DIRECT MEASUREMENT OF SURGE CURRENTS, C. M. Foust and J. T. Henderson. AIEE TRANSACTIONS, volume 54, 1935, April section, pages 373–8.

J. G. Hemstreet (Consumers Power Company, Jackson, Mich.): This paper presents very valuable information pertaining to the effects of lightning on high-voltage transmission systems. We compliment the authors on the splendid way that they have collected and presented such a large amount of pertinent data and concur on the conclusions that have been derived.

The statistical curves for lightning currents appearing in the various elements of a transmission system should be of interest to all concerned with lightning protection and performance of high-voltage transmission facilities.

The writer has been associated for the past five years with an investigation of the relative effectiveness of radial counterpoise and deep driven ground rods. This investigation has been conducted on towers situated on a sandy plane where tower footings and counterpoise offer very high measured resistances to ground. Deep driven ground rods extending to depths of approximately 100 feet below the surface offer very low measured resistances and are connected in parallel with the counterpoise to each tower. Counterpoise and rods are so equipped that contributions of each to the total tower current may be measured. It has been found in our investigation that where the measured resistance of the ground rods is very low as compared to that of the counterpoise, the ground rods contribute three to four times as much current as does the counterpoise, thus indicating that the deep driven ground rods are very effective for dissipating stroke currents. The ratio of per cent current to per cent conductivity of the counterpoise has been found to be as much as 8.6 or more when the same ratio for the combined tower footings and ground rods is in the order of 0.6.

Data presented in Table III of the paper for cases 1, 2, and 3, when rearranged to compare the counterpoise contribution with that of the ground rods and tower footings combined, give ratios of per cent current to per cent conductivity for counterpoise as 0.72 to 0.78, while the same ratio for ground rods plus tower footings is about 1.6 to 2.0. This differs somewhat from our experience and is probably due to the different conductivity relations of counterpoise and grounding system.

It is, therefore, possible that the counterpoise may contribute much more current than a measure of its conductivity would indicate, while means for grounding the towers directly by ground rods and tower footings would carry current more nearly in the order of their conductivities.

The writer is curious to know the general nature of the surface and soil conditions for the towers listed in Table III, since the measured resistances of the counterpoise are somewhat less than either tower footing or ground-rod resistances in every case.

W. G. Roman (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. In discussing Table *A* of the paper, the authors attempt to estimate the conductor voltage by multiplying the observed line currents by an assumed surge impedance of 400 ohms. This leads to erroneous results, as the current recorded in the line is not a single traveling wave current, but the totalized current consisting of all traveling waves including reflections from arresters, flashovers, and junction points, as well as the original traveling wave current. The actual line voltage is determined by the impedance of paths to ground particularly the tower and tower ground or the station arresters and ground.

2. The same comments apply to the attempt to calculate the line-surge impedance from the observed line current and bus voltage in Table II of the paper. The bus voltage is determined by the impedance drop through the station arresters, the station ground, and the drop from the bus to the arresters.

3. In Table II, there are discrepancies between the observed line currents and the arrester discharge currents. Is this due to currents leaving the station on other lines connected to the same bus? If so, are records of these currents available, and do the sum of all the currents into the station bus correlate with the observed arrester currents?

4. When a traveling wave enters a station, the rate of rise of voltage on the bus is not necessarily the same as would be measured on a line when the same wave passed. A typical station is a complex network of parallel capacitances from the bus to ground through leads of various lengths. These station constants determine the rate of rise of voltage on the bus for a given traveling wave entering the station. For a given traveling wave, the rate of rise will be higher in a small station having low capacitance to ground than in a large station having high capacitance to ground. Also, a station having more than one line connected to the bus will show lower rates of rise than a station having only one line on the bus. For these reasons, caution should be used in applying data on rates of voltage rise obtained in a given type of station to stations or equipment in general.

C. F. Wagner and **G. D. McCann** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A single magnetic link can be used to determine the crest magnitude of a unidirectional current. When, however, the current is oscillatory or has values of opposite polarity subsequent to the crest, two or more links are required to estimate the crest magnitude of the current. The reason for this is the demagnetizing action of the current of opposite polarity.

A demagnetizing current of but one or two per cent of the original magnetizing current might produce an error of 25 to 50 per cent of the true indication, and larger currents larger errors.

The magnetic link in the wave-slope indicator described in the paper by Gross and Lippert will be magnetized by a current which is proportional to the rate of change voltage across a capacitor. The authors recognize the interference of switching surges in most of their links and limit their lightning measurements to the indications of a single link. Due to the complex nature of voltage surges, especially at terminal equipment, the magnetic links are, in all probability, subjected to demagnetizing influences subsequent to the maximum magnetizing force. This would, undoubtedly, throw considerable doubt upon the records so obtained. Most, if not all, of these devices were located adjacent to transformers protected by arresters. It is likely that the maximum rate of change of voltage occurs when the arrester gap breaks down. Curve I of Figure 10 of the paper is, therefore, more likely to be a measure of the rate at which the arrester breaks down, influenced, of course, always by the extent to which any subsequent rate of change of opposite polarity might affect the reading.

I. W. Gross and **G. D. Lippert:** In presenting the data given in this paper, the authors hoped that sufficient detail was included to enable others interested in the lightning field to analyze it and draw their own conclusions, which, it was recognized, might not in every case conform to that given in the paper. Comments along this line are certainly most welcome, as it is recognized, in exploring new fields in lightning field research with newly developed instruments and even with some of the older ones, there are discrepancies which have occurred and may continue to exist for some time to come.

Regarding the matter of correlating measured bus voltages with line currents and surge impedances, it is recognized that the technical solution of this problem involves complicated mathematics together with a knowledge of terminal constants which it is difficult if not impossible to obtain. In presenting data, therefore, such as given in Table II of the paper, it was realized in suggesting this analysis that complete correlation might not be obtained. However, it is impractical to attempt to calculate lightning voltages and impulses with any degree of certainty in a complicated network consisting, for example, of one incoming line, four outgoing lines—one being on the same tower circuit as the incoming, and, in addition, coupling capacitors, station capacitance, terminal equipment, and the like at the station.

Table II merely attempts to show what correlation, if any, does exist on a very simple basis. It is hoped that the presentation of the data in Table II and the discussions which have followed will lead to more extended work on this problem in an attempt to reach a much more simple and easy solution than now required by extensive mathematical calculations.

Mr. Hagenguth uses a relatively simple mathematical analysis which seems to have promise of placing a reasonable interpretation on the data. The currents recorded in the paper are actually measured currents in the line wires and are not a value obtained by the addition of currents in separate wires. In the fifth case of Table II, there was no evidence found that there was a line flashover associated with this record.

Regarding the possibility of the protective gap flashover affecting the results, there was only one field station where evidence of this gap flashing over was obtained.

The data obtained in our field investigation, when interpreted, give rates of current rise comparable to those obtained on the Empire State Building. It has always been a question in the minds of the authors whether or not it was possible to convert satisfactorily records in lightning strokes to a building such as the Empire State Building to practical application to an elevated transmission system such as a 132-kv line or circuit.

Dr. Lewis' comparison of rates of voltage rise measured at stations and out on the line with the data presented in our paper is most interesting, considering that they were obtained by two entirely different methods. We subscribe heartily to the suggestion that higher rates of voltage rise undoubtedly occurred near the region of the stroke. This means that on lines which are not shielded, higher rates of voltage rise may be expected than on lines partially or adequately shielded.

Regarding the use of galvanized iron strap for counterpoises on which a query has been made by Dr. Lusignan, this material was used on account of its low cost, expected freedom from theft, and the further belief that it would be reasonably unaffected by soil corrosion. Digging up and inspecting this material after some six years indicated that there was relatively no trace of any corrosion whatsoever.

The percentage range of 31 to 180 instead of 15.6 to 180 for the relative conductivity of ground rods is correct. Regarding the question raised by Dr. Lusignan as to whether the 1,000 kv per microsecond furnish a reasonable basis on which to design equipment, we believe that there is nothing in the picture as brought out from the data reported in the paper relative to natural rates of rise of natural lightning on transmission lines which would indicate that the 1,000-kv-per-microsecond figure is too low. Until further evidence is available, there certainly seems to be no justification for decreasing this value as a design basis.

The question of the method of determining rate of voltage change in the field with the surge-crest ammeter has been mentioned by Messrs. Wagner and McCann, and perhaps Mr. Foust's discussion has already cleared up this point. We recognize that this wave-slope indicator is a relatively new instrument in the field and will be subject to some scrutiny until records obtained therefrom have been correlated with other data obtained by different methods. Considering the relatively large amount of data that is obtainable by the use of this instrument at a reasonable cost, in comparison with a single cathode-ray field installation and the uncertainties coupled even with this more accurate measuring instrument, it is believed that the wave-slope indicator has yielded some very valuable information, although it may be later subject to some modification in the instrument itself or detailed refinement in calibration.

The results Mr. Hemstreet has mentioned in connection with his field work in measuring the effectiveness of driven ground rods, counterpoises, and tower resistances are interesting. The fact that they do not show the same trend as those reported by the authors may be due in part at least to different soil conditions. The conditions under which our reported measurements were made were in flat country with sandy loam-type soil. We hope to continue further investigation on this subject, and perhaps the results may show why there is a discrepancy between the grounding results shown in Mr. Hemstreet's field study and those reported by the authors.

Lightning Investigation at High Altitudes in Colorado

Discussion and authors' closure of paper 42-16 by L. M. Robertson, W. W. Lewis, and C. M. Foust, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 201–08.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The paper is very interesting from the point of view of extending lightning-stroke records into regions of high altitude. The conclusion is drawn that there may be no lightning strokes within regions of altitudes of 18,000 feet and above. I should expect that this conclusion is correct as far as currents of sizable amplitude are concerned. On the other hand, the existence of long continuous discharges of relatively long duration might be expected to take place between differently charged portions of the clouds, as well as between cloud and ground. Data are available. The fact that the terrain extends into the cloud should not preclude formation in the clouds, especially since air currents at such altitudes are frequently extremely violent.

It is interesting to note from Figure 1 and Table 1 of the paper that the average number of strokes per mile per year, over the whole section above 6,000 feet, is approximately 0.25; in the section 6,000–8,000 feet—0.43; 8,000–10,000 feet—0.1; 10,000–12,000 feet—0.17, and in the short section above 12,000 feet—1.75 strokes per mile per year.

In conclusion 2 of the paper, it is shown that of the 145 strokes registered, 56 per cent caused faults and 44 per cent caused no line disturbances. It would be interesting to know the distribution of faults with respect to the altitude on this line. From the data presented, it would appear that flashovers should occur frequently at altitudes, such as are attained by this line, because:

1. The number of strokes per mile is high.
2. The ground resistance of tower footing, and probably also of counterpoise, increases rapidly with altitude as shown in the paper.
3. The flashover voltage of the air decreases considerably at high altitude.

Therefore, the reduction in current amplitude should not have so great an effect on the fault record as would appear from the current data alone. I would appreciate the authors' comments on this phase of their investigation.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Robertson and his coauthors conclude that there is little likelihood of lightning strokes at altitudes of 18,000 feet or more. Although the data adduced in the paper are hardly complete enough to prove the case, there is some logic in the assumption that altitude influences lightning. Is it not likely that this influence varies with climatic conditions and geography? In the tropics, thunderstorms are reported to be of somewhat different nature than in the temperate zones, with a greater prevalence of strokes between clouds. This may be a manifestation of an effect as discussed by authors in so far as the freezing isotherms shown in Figure 10 of the paper are, no doubt, at higher altitudes than in the United States, so that the separated charges may lie at higher altitudes. It would be interesting if more data were made available on this subject.

Another factor that may color the distribution of the current records is the influence of earth resistivity and geology on the nature of the lightning currents that flow after the stroke has been established. Some lightning investigations and observations of arrester performance, as discussed before the Institute[1] last summer, indicate that such an influence exists. This is logical, since the earth, as well as the cloud, is a terminal of the lightning battery. In regions of ancient geology and high resistivity currents of lower amplitude may prevail. Such regions exist in Colorado, and the effect may be magnified on the tops of rocky mountains. The authors have mentioned geology, and continued investigation of its effect on lightning currents is of importance. This relates also to the paper by Messrs. Bellaschi, Armington, and Snowden, on impulse and sixty-cycle characteristics of driven grounds, presented at this meeting.

REFERENCE

1. Discussion by **Edward Beck** of LIGHTNING TO THE EMPIRE STATE BUILDING—II. AIEE TRANSACTIONS, volume 60, 1941, pages 1377–8.

C. F. Wagner and **G. D. McCann** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As stated by the authors, operating experience of transmission circuits at high altitudes over a period of 30 years has shown that there is less damage to equipment than at lower altitudes. The authors, from the data submitted, conclude that this is due to the absence at the higher elevations of lightning currents of large magnitude. It may be that the simple explanation is the less frequent occurrence of lightning strokes of all magnitudes; that is, the current distribution curve might be the same but the total strokes per season be fewer.

All the points obtained are tabulated in Table I and plotted in Figure 5 of the paper. For a given range in altitude, fewer records are obtained at high altitudes than at the lower altitudes. In the absence of information regarding the distribution of the number of instruments as a function of altitude, it is impossible to say whether this is due to fewer instruments at higher altitudes or fewer discharges per instrument. In any event, because of the statistical nature of such investigations, the number of records obtained above 8,000 feet is insufficient to warrant the conclusion that currents of large magnitudes do not occur. Had a larger number of instruments or a longer period of time been considered, currents of large magnitude might have been recorded.

The bench marks which form the points at zero elevation in Figure 5 were obtained from the low-altitude record of Figure 6 of the paper, the Lewis and Foust curve. The accompanying Figure 1 shows a similar curve obtained by Waldorf. If will be observed that this curve shows much smaller currents than that of the Lewis and Foust curve, and had the bench marks at low altitudes obtained from this curve been used, the straight lines of Figure 5 would have been entirely different.

In Figure 1 not only are the Lewis and Foust and Waldorf curves plotted, but also the mean curve for all the records of Table I, which represents data for altitudes in excess of 5,000 feet. Comparing this curve with the Waldorf curve shows that the effect of altitude is very small, the difference being much smaller than the difference obtained by different investigators at low altitudes.

Not being able to identify the particular points from which the different curves in Figure 6 were plotted, the authors replotted the data in Table I for the four ranges in altitude given. The results are shown in the accompanying Figure 2. These curves do not appear to indicate a very definite trend in the effect of altitude, the differences being smaller than are obtained from year to year or from season to season on the same line.

In conclusion, while the surmise of the authors may be correct that the magnitude of lightning strokes decreases with increase in altitude, the data presented appear insufficient to draw definite conclusions.

E. A. Evans (General Electric Company, Pittsfield, Mass.): The authors have stated in conclusion 1 that 36 per cent of the currents they measured were positive, while 64 per cent were negative. This is of considerable interest since, as they have also indicated, stroke currents measured on low-altitude transmission lines have been found to be roughly five per cent positive, and 95 per cent negative. They suggest as a possible contributing factor, the probability that the high-altitude lines may at times be within the clouds. Under this condition one might expect that the line would be nearer the upper positively charged portion of the cloud, and that it, therefore, would be more likely to receive discharges from it.

There is one section of the Shoshone–Denver line which I feel certain is seldom, if ever, within a thoundercloud. Therefore, a re-examination of the data on strokes to that part of the line will be of value in determining whether Colorado thunderstorms have charge distributions which differ from the distributions in low-altitude storms. The section referred to is that between 5,000 and 8,000 feet at the Denver end of the line. Judging from the authors' profile of the line, its length is about 27 miles.

This end of the line is subjected to the same types of thunderstorms which I studied from the top of Devil's Head Mountain, roughly 35 miles south of the line during the summers of 1930 and 1931. The two seasons were spent there, through the courtesy of the United States Forest Service, for the sole purpose of studying the phenomena occurring during thunderstorms.

Devil's Head Lookout is 9,348 feet above sea level, while the immediately surrounding country for some distance lies between 7,000 and 8,000 feet elevation. Although the lightning discharge centers of 37 storms passed within three miles of the lookout in 1930, and a number more in 1931, in no case was I within the thundercloud.

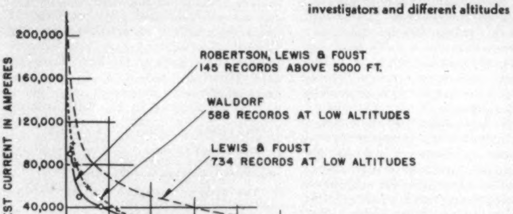

Figure 1. Comparison of current distribution curves for different investigators and different altitudes

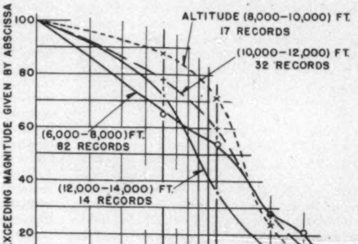

Figure 2. Cumulative curves of probable stroke currents as a function

Several measurements were made of the distance between ground level and cloud base. Most of these fell between 3,000 and 6,000 feet above the surrounding country and from 1,200 to 4,200 feet above the lookout. Only on very rare occasions were storms seen in which the cloud base appeared to be less than 1,000 feet above ground. These cases may have been optical illusions, for storms viewed at a distance, particularly when the rain area is dense, appear to be much lower than they actually are.

In view of this, it seems reasonable to assume that the section of line referred to was seldom within the cloud. Therefore, if a reexamination of data for it shows a considerably higher percentage of positive strokes than that obtained for storms at low altitude, a difference in charge distribution for low- and high-altitude storms may be indicated.

Based on my observations of thousands of lightning strokes, the shape of a number of which could be explained only by assuming that both positive- and negative-charge concentrations were present near the base of the cloud, and based on cloud field measurements, I am of the opinion that some of the positive currents measured by the authors were caused by discharges from centers of positive charge in the lower regions of the cloud.

Other positive records may have resulted from strokes which began as negative discharges from the base of the cloud to ground but ended as positive discharges, due to the discharge channels extending into the upper positively charged regions of the cloud. In Colorado on September 2, 1930, while watching a storm under conditions in which the cloud was clearly visible from its base to its top, I saw several discharges in succession which were probably of this type. In each case a stroke occurred from the lower part of the cloud to ground. Each stroke was followed by a progressive illumination upward through the cloud, which ended with several discharge channels visible near the top of the cloud. McEachron has recorded strokes to the Empire State Building[1] which started as negative discharges and ended as positive discharges. It is possible that the structure of Colorado mountain thunderstorms, resulting from their special conditions of formation, is such that this type of discharge occurs more frequently.

With regard to the decrease in current magnitude with altitude, the roughly sevenfold increase in tower-footing resistance between 7,000 feet and 11,000 feet possibly deserves some consideration. Using the tower-footing resistance as a crude measure of earth resistivity, a foot of counterpoise at 11,000-foot elevation should have of the order of seven times as much leakage resistance to ground as a similar length at 7,000-foot elevation. The difference in resistance offered to the discharge of a lightning stroke at the two locations will be less than this, due to breakdown of the air pores in the ground, but it should still be substantially higher at the 11,000-foot elevation. Considering the discharge of a stroke into a counterpoise (after Bewley[2]) as a combined leakage discharge and traveling wave phenomenon, the leakage resistance met by the stroke should be one that starts relatively high and continually decreases, as the surge travels along the counterpoise and thereby taps more and more paths in parallel to ground. Bewley's and Wade's[2] measurements of the velocity of propagation of an artificial lightning surge along a counterpoise indicate that after three microseconds between 900 and 1,000 feet of counterpoise should be picking up current from the earth. With this length of counterpoise in action, it appears doubtful that its resistance either at 7,000- or at 11,000-foot elevation would be high enough to substantially influence the maximum current of the stroke. Further evidence that a sufficient section of counterpoise probably comes into action quickly enough to offer a low-resistance path for the lightning current is given by one of the authors.[3] Still, in view of the nature of the ground under the Shoshone–Denver line at high altitude, ground resistance should not be entirely dismissed as a possible contributing factor toward the decrease in current with altitude found by the authors.

The authors' suggestion that decrease in temperature with altitude is a factor in limiting the formation of lightning is interesting. The rapid decrease[4] in moisture content of the atmosphere with altitude is also important. Based on the data given, it appears improbable that thunderstorms can form above an extended ground level of say 18,000-foot elevation, if the temperature at that level never exceeds 32 degrees Fahrenheit. Thunderstorms could, however, form at lower elevations and strike from the upper regions of the cloud to ground at that elevation as they moved toward it. Meteorologists believe, as shown by the generalized cloud diagrams in Figures 10 and 11 of the paper, that charge concentrations can occur in thunderstorms several thousand feet above the 18,000-foot level.

The study of corona discharge currents from points subjected to thundercloud fields appears promising as a means of increasing our understanding of the nature and strength of these fields. I would expect that refinements in calibration methods as the investigation proceeds will show the field strengths to be lower than those given in Table IV of the paper.

It should be possible to considerably increase our knowledge of the locations, polarities, and magnitudes of the charge concentrations causing thunderstorm fields by a continuation of the above measurements, combined with simultaneous observations of thundercloud position and characteristics, discharge center locations, and rain area locations. The records should also yield information on the charging processes going on within the storm cloud.

A few measurements of the discharge current from a 40-foot length of 0.011-inch-diameter bare wire, subjected to thundercloud fields, were obtained at Devil's Head in 1931. These yielded some interesting information on thunderstorm field characteristics. The maximum current which I measured was 314 microamperes. For this record a flow of positive electricity from wire to air was indicated showing that a negatively charged region in the cloud was dominating the field at the lookout.

REFERENCES

1. LIGHTNING TO THE EMPIRE STATE BUILDING, **K. B. McEachron.** *Journal* of the Franklin Institute, volume 227, 1939, pages 149–217.

2. THEORY AND TESTS OF THE COUNTERPOISE, **L. V. Bewley.** AIEE TRANSACTIONS, volume 53, 1934, pages 1163–72.

3. PICKUP AND RESISTANCE OF COUNTERPOISE SYSTEMS, **W. W. Lewis.** *Electrical World*, volume 116, November 29, 1941, pages 56–7, 102–03.

4. PHYSICS OF THE AIR, second edition, **W. J. Humphreys.** McGraw-Hill Book Company, New York, N. Y., 1929. Page 70.

L. M. Robertson, W. W. Lewis and C. M. Foust: Several of the discussers point out that the apparent decrease of lightning current with altitude may be due to causes other than those suggested by the authors, for example, climatic conditions and geography, influence of earth's resistivity and geology, and less frequent occurrence of lightning strokes of all magnitudes. Some or all of these points may have a bearing on the results.

Our data permit us to comment on the relative frequency of occurrence of strokes. All our results were obtained on the sections of line equipped with ammeter links, as indicated on Figure 1 of the paper, that is, on about 45 miles of the approximately 150 miles of line. With 145 total strokes recorded in four years, this would indicate about 0.8 stroke per mile per year. Bell[1] showed 356 towers flashed on the section of the Wallenpaupack–Siegfried line not equipped with overhead ground wires (about 36.9 miles) in a period of 13 years, or about 0.74 tower flashed per mile per year. Assuming each flashover represents a stroke, it will be seen that figures for high altitude in Colorado and low altitude in Pennsylvania are comparable.

In Mr. Hagenguth's discussion, he deduces from Figure 1 and Table I of the paper the average number of strokes per mile per year in the different altitude ranges. He overlooked the fact that data were secured only on about 45 miles of the line. The correct figures for strokes per mile per year would be approximately as follows:

Over the whole line	0.8
6,000 to 8,000 feet	2.05
8,000 to 10,000 feet	0.36
10,000 to 12,000 feet	0.4
Over 12,000 feet	1.17

Messrs. Wagner and McCann show a curve of lightning current against frequency plotted from Waldorf's data, and they compare this with the Lewis–Foust curve and with Figure 6 of the present paper. It is interesting to note that of the 734 records plotted in the Lewis–Foust curve, 457 records are from Waldorf's data on four systems. This is 62 per cent of the Lewis–Foust data and 78 per cent of Waldorf's 588 records plotted by Wagner and McCann. The remaining 277 records, or approximately 38 per cent of the Lewis–Foust data, are from four other eastern low-altitude power systems. From a comparison with various other curves we believe that the Lewis–Foust curve is representative of the range of current magnitude that may be expected at altitudes near sea level. It should also be noted, as mentioned in the paper, that 30 years operation indicated much less destruction, for example, shattering of wood poles and crossarms, at higher altitudes than at lower altitudes on the Colorado system.

We wish to thank the discussers for their interesting and constructive discussion. We intend to continue our investigation and hope to secure additional data which will confirm or revise our present conclusions.

REFERENCE

1. LIGHTNING INVESTIGATION ON A 220-KV SYSTEM—III, **Edgar Bell.** AIEE TRANSACTIONS, volume 59, 1940, pages 822–8.

Large Adjustable-Speed Wind-Tunnel Drive

Discussion and author's closure of paper 42-65 by C. C. Clymer, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, March section, pages 156–8.

M. M. Liwschitz (The Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): Mr. Clymer recommends in his very interesting paper that the rotors of both doubly fed machines in the wind-tunnel drive described by him be connected in series with the synchronous machine, in order to introduce a source of positive damping in the circuit of the doubly fed machines. Were the rotors directly connected in parallel, there would be only a negative damping in the circuit of the doubly fed machines, and these could oscillate independent of the total system without any restraining force.

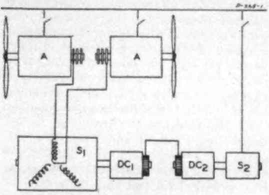

Figure 1

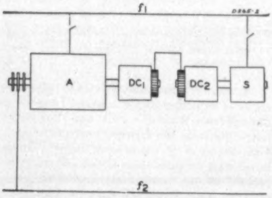

Figure 2

This statement by Mr. Clymer is correct as far as normal doubly fed machines are used, machines having the damping characteristics shown in the paper "The Doubly Fed Machine," presented at the AIEE winter convention by Messrs. C. Concordia, S. B. Crary, and G. Kron.[1]

But there is a possibility to build the doubly fed machine by using special windings, so that its negative damping is considerably reduced, and so that the damping torque of the system, consisting of the doubly fed machines and the fans, is not negative but slightly positive in the range between about 12 per cent slip and standstill. Doubly fed machines built in this way do not need a source of positive damping between the rotors and can operate without danger of nonrestrained oscillations, when their rotors are connected directly in parallel.

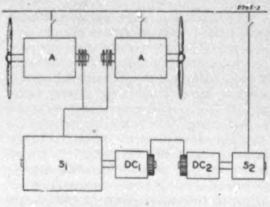

Figure 3

There is another point in the paper by Mr. Clymer on which I would like to make a remark. Mr. Clymer recommends the variable-speed drive as described in his paper for the use as frequency converter. For this purpose also the arrangement can be used that is shown in Figure 2. Two sets of this kind I have described in the *Siemens-Zeitschrift* 1926. The doubly fed machine is used here as a frequency changer only. The motors that drive the fans or pumps will be connected to the secondary line having the frequency f_2 and will be synchronous motors.

There is no danger of negative damping here, since the doubly fed machine is connected on one side with d-c machines, which produce positive damping, on the other side with synchronous machines which have damper windings.

REFERENCE

1. THE DOUBLY FED MACHINE, C. Concordia, S. B. Crary, and G. Kron. AIEE TRANSACTIONS, volume 61, 1942, May section, pages 286–9.

L. A. Umansky (General Electric Company, Schenectady, N. Y.): In reviewing these two papers, and in listening to the discussions, I was particularly impressed with one fact: a good and sound solution of an engineering problem depends not only on the kind but on the size of the equipment involved.

Take, for instance, the frequency changer described by Dr. M. M. Liwschitz. This arrangement is neat and probably is economical when the difference of frequencies between the two power supplies is small, and the capacity of the set is moderate. But suppose we must provide a 20,000-kw frequency changer to tie together a 60-cycle and a 25-cycle system. The d-c machines on the frequency changer and on the regulating set will be about 12,000 kw each. The high resulting cost is obvious; each d-c machine will have to be subdivided into three or four units. The whole arrangement will not be practical.

Or, let us take the remark made by Mr. Kilgore in reference to the slip-regulator control as a feasible application for wind-tunnel drives. Undoubtedly, for smaller drives of this kind, say of a few thousand horsepower, such control can be considered, particularly if a low efficiency is not a factor. But, for a 30,000-horsepower or a 40,000-horsepower drive, the matter of efficiency cannot be disregarded, and some means of slip-energy recovery should be provided. Furthermore, the slip-regulator control for a 40,000-horsepower drive will not solve the problem of starting current inrush which the described drives have successfully solved.

The system discussed here and first suggested by Mr. Clymer is not at all the Kraemer system. In the latter, as we all know, the slip energy of the main induction motor is converted into d-c power by means of a synchronous converter. Could this be done in case of 40,000 horsepower with a 6:1 or greater speed range? The capacity of the converter would be an equivalent of 30,000 kw; would have to be split into six or more units, and the problem of hunting, load division and so on, would be aggravated. Starting of the main drive would be just as difficult (considering the power system) as with a slip regulator.

C. C. Clymer: Mr. Liwschitz makes some very interesting observations concerning the possibility of obtaining damping on doubly fed machines operating in parallel. The use of a series connection for the rotor is, of course, only one way of achieving stability. There are other ways. For example, a six-phase synchronous machine, and the use of inductive coupling. The series connection is, shall we say, an economical and convenient method since it permits the use of perfectly standard induction motors with simply a reconnection of the synchronous machine.

Since Mr. Liwschitz does not describe the connection by which positive damping may be obtained with parallel rotor connections, a discussion on the various methods is not possible, however intriguing the subject may be.

Variable-Speed Drive for United States Army Air Corps Wind Tunnel

Discussion of paper 42-63 by A. D. Dickey, C. M. Laffoon, and L. A. Kilgore, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, March section, pages 126–30.

L. A. Umansky: See discussion, this page.

Equivalent Circuits for the Hunting of Electrical Machinery

Discussion and author's closure of paper 42-3 by Gabriel Kron, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 290–6.

S. B. Crary (General Electric Company, Schenectady, N. Y.): Kron's equivalent circuits for the damping and synchronizing coefficients of rotating machines already have been used in our company for many problems, and we can attest to their practicability and usefulness. We have obtained results which would never have been realized by ordinary numerical methods. Numerical methods may rapidly become impractical from an engineering standpoint when the number of factors to be taken into consideration increases only moderately. It may take more time to make an analysis in such a manner than it does to build the apparatus. This difficulty of the analytical method can be largely removed by methods which eliminate or simplify the numerical work. Network analyzers and differential analyzers provide means by which this may be accomplished.

The circuits developed by Kron for use on an a-c network analyzer may be used for an important class of problems: specifically, for the determination of the damping and synchronizing torques over the range of system conditions and design parameters. These parameters may include the frequency of oscillation, the effect of circuit elements to which the particular apparatus being studied is connected, and the effect of change in the design details of the apparatus or equipment itself. The method using damping- and synchronizing-torque coefficients allows a study of a particular part of the system and for particular modes of oscillation separately, without introducing the additional complexity of all of the other elements and all possible modes of oscillation at once. This then allows for a best design to be obtained for this particular element as it may be applied to different systems and under varying conditions. If necessary, additional tests for the stability and performance of the whole system may be made by using Routh's criterion, or a differential analyzer solution. The use of the circuits developed by Kron does not replace the use of other stability criteria and methods of analysis, but rather they increase their value by making them more easily applied.

Banesh Hoffmann (Queens College, Flushing, N. Y.): In his paper "Equivalent Circuits for the Hunting of Electrical Machinery," Kron transforms the nonsymmetric impedance of a machine to a symmetric form, which is then taken to be the impedance of an equivalent network. The manner of bringing Z to symmetric form has certain interesting features which seem worthy of discussion.

The process used by Kron has two essential steps:

A. The introduction of complex quantities, and the application of a symmetric components transformation.

B. The subsequent multiplication by a diagonal matrix n'^{-1}.

Let us discuss B first. In one place Kron regards this as the process of dividing each component equation of the vector equation of voltage by an appropriate scalar. In this form it has no tensor significance. I wish to show that the operation may actually be regarded as a valid tensor transformation, employing a well established tensor device known as a *mixed reference frame.*

Let the equation of voltage after step A has been performed be

$$e_a = Z_{a\dot{b}} i^{\dot{b}} \tag{1}$$

and denote the real, diagonal matrix n'^{-1} by $N_\lambda{}^a$. Since equation 1 is a vector equation, we may transform it to a new reference system using $N_\lambda{}^a$ as the transformation matrix. The tensor law of transformation gives e_a the new components

$$e_\lambda' = N_\lambda{}^a e_a \tag{2}$$

The *vector* $(Z_{a\dot{b}} i^{\dot{b}})$, treated as a unit, will have the new components

$$(Z_{\lambda\dot{b}} i^{\dot{b}})' = N_\lambda{}^a (Z_{a\dot{b}} i^{\dot{b}}) \tag{3}$$

It is thus a valid tensor transformation to write equation 1 in the form

$$e_\lambda' = N_\lambda{}^a Z_{a\dot{b}} i^{\dot{b}} \tag{4}$$

If we define

$$Z_{\lambda\dot{b}}' \equiv N_\lambda{}^a Z_{a\dot{b}} \tag{5}$$

we may write equation 4 in the form

$$e_\lambda' = Z_{\lambda\dot{b}}' i^{\dot{b}} \tag{6}$$

It is this quantity $Z_{\lambda\dot{b}}'$ which Kron takes as his symmetric impedance. A full tensor transformation on equation 1, applying to each individual tensor therein, would give

$$e_\lambda' = Z_{\lambda\dot{\mu}}' i'^{\dot{\mu}} \tag{7}$$

with

$$Z_{\lambda\dot{\mu}}' = N_\lambda{}^a N_{\dot{\mu}}{}^{\dot{b}} Z_{a\dot{b}} \tag{8}$$

and

$$i'^{\dot{\mu}} = N_{\dot{b}}{}^{-1\dot{\mu}} i^{\dot{b}} \tag{9}$$

In equation 6 the e has been transformed to a new reference frame, the i has been left in the old, and the Z has been placed partly in one frame and partly in the other. The quantity $Z_{\lambda\dot{b}}'$ is merely the impedance tensor $Z_{a\dot{b}}$ expressed in a mixed reference frame. Since $N_\lambda{}^a$ happens to be diagonal, the effect of the above transformation is merely to change the scales along the individual reference axes so far as the e's are concerned, while leaving the scales for the i's unaltered.

At this point let us recall the well-known theorem of tensor analysis which states that an unsymmetric tensor is unsymmetric in all reference frames. This theorem effectively prevents the transformation of Z to symmetric form by means of a full tensor transformation. But it is inoperative if we use a mixed reference system, and there seems no theoretical reason why the use of a mixed reference system should not alone suffice to bring Z to symmetrical form. On the other hand, in step A, by his introduction of complex transformations, Kron automatically takes the problem out of the restricted realm of tensor analysis into the more general one of spinor analysis. The impedance, when regarded as a spinor, has one dotted and one undotted index. For such a spinor symmetry is not an invariant property, the symmetry theorem being replaced by one which states that a hermitean spinor is hermitean in all reference frames, and a nonhermitean spinor nonhermitean in all reference frames. Thus, the introduction of spinors could theoretically alone suffice to bring Z to symmetric form. Nevertheless Kron uses step A merely to simplify the appearance of Z and makes Z finally symmetric by means of the mixed reference system introduced in step B. It would thus seem that he has been lavish in the theoretical resources used to combat the asymmetry of Z, since either the introduction of a suitable complex full spinor transformation alone or else the introduction of a suitable mixed tensor reference frame alone could theoretically make Z symmetric. When p is replaced by $j\omega$, and so on, the Z of a rotating machine and the Z of a network become alike in being nonhermitean, and this is an invariant characteristic, while the property of symmetry is neither invariant nor common to both. The use of a mixed reference frame obscures this invariant similarity. Therefore, in view of Kron's statement that only tensors (or more generally spinors) can be represented as measurable quantities in the equivalent network, it might ultimately prove of practical value to investigate the possibility of reducing Z to symmetric form by the use of a full spinor transformation only. The calculations required, though, would be of considerable length. Kron's main object was actually to make Z symmetric, and this he has done not only with elegance and real economy of labor, but also, as I have shown above, by the use of legitimate spinor operations.

The following remarks are the fruits of correspondence with Mr. Kron. They concern the reasons why the method by which Kron reduces Z to symmetric form is actually successful in accomplishing its purpose.

Initially we have equation 16 of Kron's paper, with R and L symmetric. If we apply a spinor transformation we are liable to destroy the symmetry of R and L. And if we do happen to preserve this symmetry under step A, we are liable to destroy the symmetry of R on multiplying it by n'^{-1} in step B. Clearly, therefore, the method of reduction cannot be generally valid. The following calculations show that it is actually made exactly to measure for the problem in hand.

By matrix multiplication we have

$$\begin{vmatrix} 1 & j \\ 1 & -j \end{vmatrix} \begin{vmatrix} a & c \\ c & b \end{vmatrix} \begin{vmatrix} 1 & 1 \\ -j & j \end{vmatrix} = \begin{vmatrix} (a+b) & , & (a-b)+2jc \\ (a-b)-2jc & , & (a+b) \end{vmatrix} \tag{10}$$

Thus only a diagonal spinor, originally symmetric, will remain symmetric under step A. Now R is always diagonal, provided there are no resistances common to any meshes, and L will be diagonal if there are no mutual inductances. But the transformation to symmetrical components is always applied to the ***d*** and ***q*** axes of a layer of winding, and since these are taken at right angles they have no mutual inductances. Thus step A does not disturb the symmetry of R or L. It does however change them from diagonal to nondiagonal form, as equation 10 shows. This is important since R', the new R, is multiplied in step B by the diagonal matrix n'^{-1}. By matrix multiplication we have

$$\begin{vmatrix} x & 0 \\ 0 & y \end{vmatrix} \begin{vmatrix} a' & c' \\ c' & b' \end{vmatrix} = \begin{vmatrix} xa' & xc' \\ yc' & yb' \end{vmatrix}$$

Thus $n'^{-1}R'$ will be symmetric only if either n'^{-1} or else R' is a multiple of the unit matrix. Now on the rotor it is assumed that the windings are symmetric. This means that R, and thus, by equation 10, R', is a multiple of the unit matrix, so that the condition is satisfied. On the stator, however, there may be asymmetries, and R is generally not a multiple of the unit matrix. But on the stator we have $v=0$, and this makes $\boldsymbol{n}'^{-1}$ a multiple of the unit matrix, thus fulfilling the condition in the other possible manner. Any asymmetries on the rotor, such as from an outside load, would destroy the effectiveness of the method.

Gabriel Kron: Mr. Crary's discussion brings out rather emphatically the fact that analytical methods of investigating engineering structures become, from a practical point of view, valueless just when these studies are most needed, unless the method offers a means also for quick numerical solution. The need for and usefulness of calculating devices and models such as the a-c network analyzer is attested by the fact that the number of such devices is increasing, and in our company the analyzer is being worked two shifts.

The limitation of the hunting equivalent networks, as presented here, is that they are restricted to one frequency of oscillation. However, these circuits may be transformed so as to be valid for all frequencies, that is, for transients, such as the sudden short-circuit and acceleration phenomena of machines and interconnected systems.

Professor Hoffmann's points are all well taken. It is an excellent suggestion that the multiplication of $\boldsymbol{Z}$ and $\boldsymbol{e}$ with $\boldsymbol{n}'^{-1}$ should be considered as a transformation to a mixed reference frame, in which the transformation consists in changing the scale of the voltages.

That the use of symmetrical components takes the problem out of the restricted realm of tensor analysis into the more general one of spinor analysis, as stated by Dr. Hoffmann, is indicated also in the introductory equations 1-4 by the use of barred indices.[*]

The question whether the two steps A and B can or cannot be replaced by one spinor transformation must be left open. I have tried repeatedly to discover such a single transformation, but did not succeed.

There seems to be no agreement as yet among authoritative mathematicians whether the word "tensor" should include physical (or geometrical) entities that are functions of both real and complex variables or whether it should be restricted to functions of real variables, while the word "spinor" is introduced to denote physical entities that are functions of complex variables. Personally, I prefer to use the expression "tensor" for all physical entities.

That $\boldsymbol{R}$ and $\boldsymbol{L}$ both must be of special form in order that the method of symmetrical components should reduce them to symmetrical form is, of course, well-known. There is a real need in electrical engineering for transformations that reduce more general types of tensors to symmetrical form, and, if possible, to diagonal form. So far Fortescue's method is the only workable tool available. Professor Hoffmann, because of his immense background of transformation theory, may be of help to electrical engineers along these lines of development.

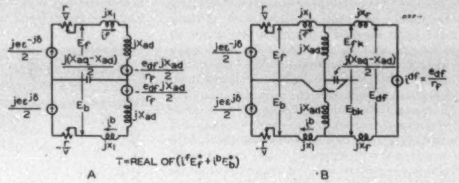
r/v
jx₁
i^f
$\frac{je\varepsilon^{-j\delta}}{2}$
E_f
jX_ad
$\frac{j(X_{aq}-X_{ad})}{2}$
$-\frac{e_{df}}{r_f}\frac{jX_{ad}}{2}$
$-\frac{e_{df}}{r_f}\frac{jX_{ad}}{2}$
$\frac{je\varepsilon^{j\delta}}{2}$
E_b
i^b
jX_ad
$-r/v$
jx₁
T=REAL OF$(i^fE_f^*+i^bE_b^*)$
A
jx_r
E_{fk}
$i^{df}=\frac{e_{df}}{r_f}$
E_{df}
E_{bk}
jx_r
B

Figure 1. Two forms of the equivalent circuit of the salient-pole synchronous machine

T real of $(i^fE_f^*+i^bE_b^*)$

A—With impressed field voltage

B—With impressed field current

He correctly points out that $\boldsymbol{R}$ must be a multiple of the unit matrix along the reference axes, in which the equivalent circuit is to be established, if it is to be brought to a symmetrical form. That condition is not satisfied with unbalanced load on the rotor even for the reference axes under discussion, and $\boldsymbol{Z}$ then becomes hopelessly asymmetrical.

However, it must be emphasized that it is not at all necessary that $\boldsymbol{Z}$ should be expressible by a symmetrical matrix in order to represent $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ by a stationary network. The only necessary (though not sufficient) condition that $\boldsymbol{Z}$ should be representable by a stationary network, is that $\boldsymbol{Z}$ should be a tensor (or rather a spinor). The condition of symmetry only enters if the requirement is to use a mesh network. But if one cares to use an orthogonal network (a network with both impressed voltages and currents) the symmetry of $\boldsymbol{Z}$ is not a necessary condition.

As an example, let the steady-state equivalent network of a salient-pole synchronous machine Figure 2a of the paper be considered reproduced in the enclosed Figure 1A. The original equation $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ in which $\boldsymbol{Z}$ is not symmetrical has been given in reference 5, equation 14 (dividing row $\boldsymbol{f}_a$ by v and $\boldsymbol{b}_a$ by $-v$) as

$$\boldsymbol{e}=\begin{array}{c|c|c} \boldsymbol{d}_f & \boldsymbol{f}_a & \boldsymbol{b}_a \\ \hline e_{df} & 0 & 0 \end{array}$$

$$\boldsymbol{Z}=\begin{array}{c|c|c|c} & \boldsymbol{d}_f & \boldsymbol{f}_a & \boldsymbol{b}_a \\ \hline \boldsymbol{d}_f & 4r_f & 0 & 0 \\ \hline \begin{matrix}1\\-\boldsymbol{f}_a\\4\end{matrix} & 2jX_{ad} & \frac{2r}{v}+j(X_d+X_q) & j(X_d-X_q) \\ \hline \boldsymbol{b}_a & 2jX_{ad} & j(X_d-X_q) & -\frac{2r}{v}+j(X_d+X_q) \end{array}$$

Even though this matrix with three rows and two columns is not symmetrical, it can be represented by the equivalent circuit of Figure 1B containing three meshes in which an impressed field current i^{df} also exists. That current remains constant while the speed v changes.

The disadvantage of the circuit of Figure 1B is that on the network analyzer, the generator supplying i^{df} has to be adjusted each time v varies, while on the circuit of Figure 1A, all generators remain unchanged as v varies. On the other hand Figure 1B with its constant field current corresponds more nearly to the physics of the actual machine. In fact, it gives the resultant flux E_{df} linking the field winding, that could not be represented on Figure 1A.

In the problem of connecting a synchronous machine to a transmission line $\boldsymbol{Z}$ is asymmetrical and E_{df} has to be known. Hence the circuit of Figure 1B must be employed then and not the simplified form Figure 1A, in which the row and column of $\boldsymbol{d}_f$ (and the corresponding mesh) had been eliminated.

Temperature and Electric Stress in Impregnated-Paper Insulation

Discussion and author's closure of paper 42-10 by J. B. Whitehead and W. H. MacWilliams, Jr., presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 10–13.

L. J. Berberich (Westinghouse Research Laboratories, East Pittsburgh, Pa.): Doctors Whitehead and MacWilliams have shown that oil-impregnated paper subjected to high stress and temperature undergoes large increases in power factor in a relatively short time. They offer the explanation that this behavior may be caused by electrolytic dissociation of the oil and the effects of temperature and stress in accelerating this dissociation.

This problem can be viewed somewhat more broadly when the behavior of oil-impregnated paper is contrasted to that of the modern capacitor dielectric which consists of paper impregnated with chlorinated diphenyl. It is common practice to operate capacitors at voltage stresses in the neighborhood of 400 volts per mil and temperatures only a little lower than the 80 degrees centigrade used by the authors are reached in service. Little or no increase in power factor over long periods of time is observed under these conditions, which appear to be disastrous for oil-impregnated paper.

What is the explanation for this difference in the observed behavior of the two dielectrics? As for the synthetic impregnant used in capacitors, it has a greater residual electrolytic dissociation than oil which is reflected in a greater ion content and conductivity. This dissociation is probably equally well accelerated by voltage stress and temperature. The answer may lie partly in the fact that the dielectric constant of the synthetic material is greater than that of oil, and consequently there is less stress on the liquid component in the case of the capacitor dielectric for the same over-all voltage stress. The rest of the answer may possibly lie in the fact that there is some evidence that the synthetic liquid has a greater affinity for paper than oil because of its polar nature. Both the higher dielectric constant and the greater interfacial tension between the synthetic liquid and paper should make the formation of gas pockets less likely than in the case of oil-impregnated paper.

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[*] See reference 7 of the paper, May 1935, page 239, where the method of symmetrical components has already been presented as a full-fledged spinor transformation.

At this point I should like to ask the authors if they have any definite evidence which would exclude the formation of gas pockets and the ensuing gaseous ionization as another possible cause of the behavior they observed? This evidence may be difficult to obtain, because even if dry spots exist in the paper while stress is being applied, reimpregnation may take place before they can be found by dissecting the sample. However, if the test is run for a long time, sufficient X wax should be formed to become easily visible. Assuming that gas pockets can form in some way, explanation of the observed behavior on the basis of gaseous ionization is easy. It is known[1,2] that gaseous ionization can cause a rapid increase in ion content of the oil which is reflected in an increase in conductivity and power factor.

Even assuming that gaseous ionization may be a possible cause of the power-factor increase, there still remains to be explained why the effect is less at lower temperatures. This may be simply a matter of greater viscosity of oil, making it more difficult for the oil movement to take place which is necessary for void formation. It would be particularly interesting to repeat this work with a lower viscosity oil which should answer some of the questions raised. It should also throw further light on the mechanism proposed by the authors.

Figure 9 of the paper shows a very large accelerating effect of temperature on the power factor increase. I should like to ask if the power-factor data for the various points were all determined at the same temperature?

REFERENCES

1. INFLUENCE OF GASEOUS ELECTRIC DISCHARGE ON HYDROCARBON OILS, **L. J. Berberich.** *Industrial and Engineering Chemistry*, volume 30, 1938, page 280.

2. CORONA DISCHARGE ON LIQUID DIELECTRICS, **J. Sticher, J. D. Piper.** *Industrial and Engineering Chemistry*, volume 33, 1941, page 1567.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): At the beginning of the third paragraph of this paper it is stated that "Little attention has been paid to the study of the long-time effect of combined high temperature and high stress." Then the paper proceeds to give the results of tests of durations up to about 100 hours with stresses of 400 volts per mil on the impregnated-paper insulation.

In Figure 4 of my 1939 AIEE paper, "Load Ratings of Cable,"[1] are given power factor data obtained in a series of tests on experimental high-voltage oil-filled 132-kv cable installed in conduit. The maximum stresses on this cable ranged from 220 to 259 volts per mil, which are somewhat above the highest stresses on commercial oil-filled cable in this country. The duration of the test was several years, as indicated in the 1939 paper, and the maximum total duration of voltage application to the termination of the tests was over 60,000 hours. The copper temperatures were 80 to 95 degrees centigrade average for the 1,000-foot lengths for total periods of 9 and 24 months on two samples, respectively, and in some portions of the cables the copper temperatures were 120 and even as high as 140 degrees centigrade, in contrast with temperatures of up to 80 degrees centigrade in the paper under discussion.

In addition, we have been conducting, in our high-voltage laboratory, tests of oil-filled cable with maximum stresses of 294 volts per mil and with heating up to 100 degrees centigrade. The tests have been going for many months, and only small changes in power factor have occurred.

On the other hand, the Whitehead and MacWilliams paper gives data showing a very high rate of increase in the power factor (nearly 0.04 per week) for insulation subjected to 80 degrees centigrade and voltage stress of 400 volts per mil. Then, after giving other data, it is stated in conclusion 1 that deterioration of impregnated-paper insulation due to temperature and stress may begin earlier and increase more rapidly than commonly supposed. An exhaustive study of stresses of impregnated-paper insulation in operation, not only of cable but of capacitors, together with a study of the enormous amount of data obtained with experimental cable, indicates definitely that for the temperatures and electric stresses in effect in normal operation of impregnated-paper insulation, deterioration and chemical instability do not start anything like as early as indicated in the article. Perhaps there is some phenomenon that is peculiar to higher stressing, that is, at about 400 volts per mil, such as was used by the authors.

Long-time tests of oil-filled cables in Chicago showed little effect of superimposed voltage during heating periods upon changes in power factor as compared with similar periods of heating without voltage. In fact, it was found that application of voltage produced a decrease in power factor for hours or even days, while removal of voltage caused corresponding increases. This finding appears to be in contrast with the conclusions from test 3. However, Figure 7 of the paper seems to indicate further increase with power factor after removal of the voltage. We tried to explain the phenomena found in Chicago by assuming that application of voltage causes a shift in the balance of oxidation or deterioration products. Further careful studies of these phenomena should give valuable information on the mechanism of deterioration of insulation.

The conclusion is drawn from tests 6 and 7 that oxidation plays a relatively small role in the deterioration process. This conclusion is in contrast with the results of studies of oil-filled cables in Chicago and with the results obtained in the research project on insulating oils and cable saturants at the Massachusetts Institute of Technology under the auspices of The Engineering Foundation and AIEE. It should be noted, however, that the reported amount of oxygen of 0.7 per cent by volume is small, and the length of the test of about five days is very short. In aging tests of oil-filled cable in Chicago it took for one sample about one year and for another more than three years before considerable increases in power factor developed due to presence of some oxygen.

From the results of power-factor measurements of the individual layers, the conclusion is drawn that there was no chemical action from the electrodes. These tests were made at room temperature. Studies in Chicago showed that radial power-factor curves at room temperature show very little variations, while at 60 degrees centigrade they may show significant changes.

In order to obtain a better understanding of the processes involved in the power-factor changes observed, careful studies should be made of the effect of such variables as electrode materials, purity of the paper, oxygen content, and purity of the oil.

REFERENCE

1. LOAD RATINGS OF CABLE, **Herman Halperin.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–53.

Robert J. Wiseman (Okonite–Callender Cable Company, Inc., Passaic, N. J.): I have read with considerable interest the paper by Dr. Whitehead and Dr. MacWilliams. They show that when their samples of impregnated paper, without oxygen or electric stress present, are heated for a week at 80 degrees centigrade, practically no change in power factor results; when tested without oxygen and with stress, there is a change in ionization with temperature and stress; and with oxygen and stress present, about the same results are obtained. These results are of interest, but it would have been helpful if the authors had been able to continue their time of tests for many weeks instead of one week. Each of us has our particular method of testing impregnated paper for its power-factor stability, and we find it is possible for the first few days to show a decrease in power factor and then a slow steady increase or even a very large increase, depending upon the type of oil being tested and the kind of metal used as electrodes. Perhaps at some time Dr. Whitehead will repeat his tests, varying the oil and kind of electrodes with samples under a high temperature for two to three months.

I like the explanation offered by the authors for the increase in power factor with stress, namely, possibly due to electrolytic dissociation; yet I wonder if it is actually dissociation—that is, is it a permanent change in the oil? We have made tests on samples of impregnated paper which we are quite certain permitted no chance of gaseous ionization to take place, and yet we did find a change in power factor with stress. It has been customary to assume that when there is a change in power factor, voids are present and gaseous ionization takes place. Dr. Berberich has referred to this condition in his discussion of the paper. I do not believe Dr. Whitehead's samples had gaseous ionization. Where we believe that there is no chance of gaseous ionization, and we do find a change in power factor for want of a better term of what happens, we call it "electrolytic ionization." The change in power factor is usually very much lower than takes place when gaseous ionization takes place. As we view it, it takes place due to the ionization by impact of free ions as the electric stress is increased, if one will accept the assumption that there are free ions present in oil. Perhaps Dr. Whitehead's electrolytic dissociation and our electrolytic ionization are synonymous.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.: During one period we tested a large number of impregnated-paper condensers in communication with the body of oil used for saturation of the paper. Like the condensers of the authors, these were subjected to voltage stress at an elevated temperature. We finally concluded that our tests were seriously affected by diffusion of oil between the condensers and the surrounding oil body which was freely exposed to oxidation and absorption of oxygen from the air. Altogether different results are obtained on condensers (such as a cable) when the dielectric is completely sealed from any communication with the atmosphere. I believe the authors should give careful thought as to the possibility that their results may also have been influenced by interdiffusion of oil between the saturant of the paper and the surrounding oil body.

Hubert H. Race (General Electric Company, Schenectady, N. Y.): The conclusions drawn from the data presented in this paper are quite contrary to information obtained either from service or other experiments on miniature cables. In my opinion, the reason is that the authors failed to recognize that when specimens are tested at 80 degrees centigrade in an open oil bath, oxygen is present in sufficient quantity to produce oxidation products in sufficient concentration to account for the results obtained.

At high temperature in an open bath the rate of transfer of oxygen from the air to the specimen is increased greatly because of convection currents set up in the oil bath. No doubt the specimens were prepared substantially oxygen-free but as soon as they were opened to air and submerged in an open bath of oil they started to absorb oxygen. Oxidation products of both the oil and the paper would contribute to increased power factor.

For this reason I believe oxidation was the cause of the results presented in Figures 1–5 which were marked "no oxygen" just as much as for the subsequent experiments in which a limited amount of oxygen was purposely admitted at the beginning of the experiments. For both types of experiments the observed time rate of increase in power factor probably is determined largely by the rate of diffusion of oxygen into the specimen. This would account for the fact that the authors found little difference between the specimens which contained no oxygen at the beginning and those to which oxygen was admitted before the heating and measurements started.

In Figure 9 the authors state that *in the absence of oxygen* they have found a rate of increase of power factor at 80 degrees centigrade of 0.02 per cent per hour or about 0.5 per cent per day. At the 1939 summer convention I presented a paper[1] summarizing life tests on oxygen-free miniature oil-impregnated-paper specimens *assembled, evacuated, impregnated, and tested in glass* in which the five specimens having longest life had been tested at high voltage between 400 and 800 days before failure. Up to July 1937 these specimens were heated to 60 degrees centigrade for about 20 hours each day and afterwards to 100 degrees centigrade. After dissembly, the oil-soaked paper taken more than 1 inch from the breakdown path in these specimens had a power factor of only about one per cent. Furthermore, many specimens had practically constant power factor throughout their life. These data are not consistent with those presented by Whitehead and MacWilliams. The differences are reconciled, however, if it is understood that continuous oxidation was possible in their specimens.

In the paper to which I refer we point out unexpected metal migration and concentration in the region of highest stress adjacent to the electrode surfaces. My explanation has been that under conditions of *high electrical stress* and elevated temperature, chemical reactions occur which would not otherwise be possible. Therefore, I am in complete agreement with Whitehead and MacWilliams in believing that the electrical stress is a very important factor in deterioration resulting from heating and oxidation.

REFERENCE

1. TESTS ON OIL-IMPREGNATED PAPER—IV, MECHANISM OF BREAKDOWN, **Hubert H. Race**. AIEE TRANSACTIONS, volume 59, 1940, pages 721–9.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): It is well-known from accelerated aging tests of cables that electric stress causes a deterioration power factor at a rate which increases rapidly with rise of temperature above 60 degrees centigrade, but these tests do not indicate rates even approaching those shown by the authors' tests. It has generally been assumed that this action has been due entirely to the increase of ionization caused by vacuums resulting from oil contraction after thermal expansion. That this is the main reason for the temperature effect is obvious from the fact that when precautions are taken to prevent voids, the deterioration of power factor is practically stopped at temperatures even higher than that used by the authors. The authors claim an effect apart from and additional to that caused by ionization of voids.

As load cycle tests are usually conducted at stresses of one half, or less, of the stress employed by the authors, the phenomenon they report must be peculiar either to the high stresses used or to their test method.

I cannot dismiss the test method, because I note from the characteristics of the oil used to impregnate the samples that, unless there is an error in the viscosity figures, the oil is of naphthenic base, and therefore while its power factor is not greatly susceptible to change by oxidation under the given test conditions, its power-factor stability against electrical discharges would be greatly impaired by the presence of oxygen. In view of the nature of the oil, I do not consider that the open tank method of test used by the authors is a satisfactory guarantee against *electrical instability* due to oxygen.

We have tested a well-known naphthenic base cable oil in the electric discharge cell described on page 209 of AIEE TRANSACTIONS, volume 60, 1941, and have found the results shown in Table I.

This may be the key to the authors' data.

It must be remembered that a paper-insulated cable is a completely enclosed device and that it is unsafe to draw conclusions about cable behavior especially in relation to power factor from tests on unenclosed specimens.

At Mr. Halperin's suggestion I made some load cycle tests with heat periods lasting over days instead of hours and found some increase in power factor during the 80 and 115 degree centigrade test periods, but the magnitude was not of the order found by the authors.

The other alternative is that something happens at stresses of 400 volts per meter that does not happen at 200 volts per meter, that is something different in kind, rather than in degree. The authors suggest that this may be "electrolytic dissociation." Undoubtedly a number of free ions dissociated from impurities in the oil, if not from the oil itself, will increase with increasing stress, because ions of opposite sign tend to segregate and so reduce the rate of recombination without reducing the creation of new ions.

Table I

Condition of Oil	Power Factor at 100 C Before Discharge Cell Test	Power Factor at 100 C After 1,000-Minute Discharge Cell Test at Room Temperature
New	0.86	2.2
Partially oxidized	5.00	Unmeasurably high at or over 80 C

It is also true that heat will further this action by agitation, but I can see nothing new happening at 400 volts per meter which does not happen although in less degree at 200 volts per meter, nor can I see why the agitating effect of 80 degrees centigrade should be vastly greater than that at 60 degrees centigrade, a difference of only six per cent in absolute temperature.

K. W. Wagner's AIEE paper of 1922[1] shows characteristic curves of impregnated-paper insulation in which, at high current densities the stress actually decreases with increasing current density, or to put it another way, the current density increases even with decreasing stress. Does this condition indicate a combined electrical and thermal instability, under extreme conditions, such as the authors assume?

I do not believe that the authors have given us sufficient data to permit us to give a final evaluation of their work.

The series of papers on impregnated-paper insulation which Dr. Whitehead and his associates have brought out over a number of years should be regarded as parts of a unit which is not yet complete. I hope that the experiments which are described in the present paper will be amplified in the light of the discussions offered, for some future number of the series.

REFERENCE

1. PHYSICAL NATURE OF THE ELECTRICAL BREAKDOWN OF SOLID DIELECTRICS, **K. W. Wagner**. AIEE TRANSACTIONS, volume 41, 1922, pages 288–98.

J. B. Whitehead: The results reported in the paper have been criticized as showing a temperature plus stress increase of the power factor of impregnated paper much higher than that found in load cycles on impregnated-paper power cables, and also in laboratory tests. The most important points made are:

(*a*) The relatively small values of power-factor increase found in long load-cycle tests on cables.

(*b*) That the results found by the authors are probably due to oil oxidation, the oxygen entering the sample by diffusion, since the tests are conducted under an oil bath with air at atmospheric pressure above it.

The principal data on (*a*) are those given by Mr. Halperin from the well-known and valuable cable stability tests conducted by his company. The particular results he cites are the tests on oil-filled cables; and from a large array of data on such cables he has selected a few in which the increase in power factor in long load-cycle tests is relatively small. In response to this it should be pointed out that from the same data there are a number of cases in which substantial values of power factor increase are evident. The text of the paper itself points out a number of such cases, and, in particular, Figure 1 of Mr. Halperin's paper shows results on a solid cable in which there is an excessive rise of power factor due to combined temperature and stress. Moreover, in many of the load-cycle tests the temperature cycle is 24 hours, which is scarcely long enough for the detection of power-factor increases such as we have reported.

Load-cycle tests on finished cables such as those of Mr. Halperin are not of the best type for investigating the combined influence of temperature and voltage. Tests on finished cable and under the usual load-cycle programs introduce many variables other than those here in question. It is quite probable that the oil-filled cables referred to by Mr. Halperin have good temperature-voltage stability, due either to the properties of the particular oil they contain, or perhaps due to the oil-filled type of construction. It should be noted that our own tests were made with a solid cable oil. Incidentally, the increases reported by C. F. Proos, referred to in our paper, were found in load-cycle tests on oil-filled cables, and the author emphasizes the noticeable effect of combined high stress and high temperature, an effect which is absent under the same stress at lower temperatures.

As to (*b*), the suggestion that our results are due to excessive oxidation, the principal comment is that by Dr. Race who suggests that within the six-day period of our test, enough oxygen will diffuse into our 0.04-inch thick sample to account largely for the rapid rise in power factor we have reported, as an oxidation process. In reporting our results we have assumed that a comparison of behavior of two samples, one practically entirely free of oxygen, and one containing over 500 times as much oxygen, would be sufficient to test a possible influence of oxygen on our results. In the latter case, the results did indeed show a substantially higher rate of increase due to high temperature alone, but not by any means comparable in amount to the increase of power factor found in both these samples when under combined influence of temperature and stress. However, Dr. Race goes further and states that it is possible that the amount of oxygen absorbed in the test in an open bath is so great that it completely masks the difference observed in our two samples, and that both samples are substantially the same in the matter of oxygen content.

We do not concur with Dr. Race in this view, and fortunately we have available the results reported by T. B. Jones, AIEE TRANSACTIONS, volume 56, 1937, page 1492) who, working with the same apparatus and a similar oil measured the time increase of power factor of impregnated paper for samples in which the oil had been saturated with oxygen at pressures up to 76 centimeters of mercury, and temperature up to 80 degrees, saturation having taken place before admission to the sample, and saturation of the paper taking place at the same temperature in an atmosphere of oxygen. The results of Jones show that the rise of power factor due to the absorption of oxygen from the bath open to the air is of the order of up to 0.01, that is, little greater than those reported in our paper for the oxygen sample, and the value observed at the end of one day in our combined voltage-temperature test. These values are on the assumption that the sample has become completely saturated with air from the bath, which fact we seriously question. It is also to be noted that in Jones' experiments on samples containing different amounts of oxygen, the measuring cell was evacuated immediately after each power-factor measurement and therefore remained throughout the test at the particular pressure corresponding to the amount of contained oxygen, except for the brief intervals in which power factor was measured.

Incidentally I may also mention that we have allowed cylindrical impregnated-paper specimens such as used in our accelerated life tests, to stand for weeks at a time submerged in an open bath, and have found no material change in the value of power factor measured immediately after impregnation.

Most important in the present work, however, is the difference in behavior we have found when the specimen is subjected to stress and when it is not. If our power-factor increases are due to oxidation alone, they should certainly show up in the absence of stress, and this they do not do. Is it possible that Dr. Race has overlooked this important fact?

With reference to Dr. Wiseman's preference for the term "electrolytic ionization," no serious objection can be taken to it. The word "ionization" has been overworked to such an extent that it applies to almost any unfavorable phenomenon in a cable. The word "ionization" arose first, I believe, from the phenomena in gases, and it was therefore natural that the term should be introduced to describe the phenomena going on in gas spaces in cables. However, it has always seemed to me undesirable to use this word when describing the behavior of liquids and solids, particularly those in which there is no free gas. Moreover, "dissociation" is a well-known term applying to all electrolytic solutions, and many authors consider that insulating liquids including oils have an inherent electrolytic dissociation. Dissociation in this sense, of course, means the continuous process of the separation of molecules into ions and the recombination of the latter, there being, however, on the average, always a certain number of free electrolytic ions. It is this type of ion that we have invoked in our suggested explanation of the phenomena reported in our paper.

We are glad to note that there is general admission that there may be conditions under which combined temperature and stress may lead to unlooked for increases in power factor. We heartily agree in the general comment that the matter should be investigated further with a variation of materials and other obvious variables. We recognize freely that we have tested only one oil and one paper, but we emphasize again that if *any* insulating oil presents the behavior such as we have noticed, attention should be called to it.

Field Investigation of the Characteristics of Lightning Currents Discharged by Arresters

Discussion of paper 42-17 by I. W. Gross, G. D. McCann, and Edward Beck, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 266–71.

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): This paper is another valuable contribution aimed at rounding out our knowledge of lightning and protective systems by filling in a vital gap concerning the wave shape of natural surge currents.

The difficulty of obtaining such data has been overcome by persistence and resourcefulness, and if we add patience we shall in time accumulate a firm foundation for sound design and application of lightning protective devices.

The low magnitudes and slow rates of rise so far measured explain the highly satisfactory over-all performance of modern lightning arresters. There is, nevertheless, a number of instances in which failure to provide protection demands currents whose rate of rise and magnitude greatly exceed those indicated. None of us is satisfied with such inferences, however, and it is to be hoped that some light will be thrown on this question by the authors in the course of continued investigations. It should be realized that direct strokes quite close to stations are required to produce such currents, and for this reason these data may not be secured for some time.

In many instances it is possible to plan economical protection for discharges of this character and if application engineers give this problem appreciative attention, we may expect protection methods to keep pace with fundamental research.

W. J. Rudge (General Electric Company, Pittsfield, Mass.): The data contained in this paper are a valuable addition to the basic information essential to the economic design and application of protective devices. It is hoped that the authors will continue their investigation in the future.

In Figures 3 and 9 of the paper, the authors compare the results obtained with those obtained by Gross and McMorris. It is pointed out that the difference in the two results may be due to the difference in instrument sensitivity. This is apparently true as the surge-crest-ammeter links used by Gross and McMorris were set to have a threshold of measurement at 300 to 400 amperes; therefore, discharge current below this value would not be recorded. In Figure 9 of the paper by Gross, McCann, and Beck, it appears that currents as low as 100 amperes or perhaps less were recorded. Since low currents occur more frequently than high currents, it is natural to expect that with a lower threshold of measurement a larger number of discharges will be obtained.

It is of particular interest to note that the highest current measured in this investigation is 9,600 amperes, although five of the installations described in Table I were on wood-pole construction with no overhead ground wires where one would expect the highest discharge currents to be measured. The highest discharge current recorded by Gross and McMorris through a single arrester pole was 15,000 amperes with a probable maximum of 18,000 amperes (if the currents were added in two arrester poles on the same phase). In view of these results, it appears that the lightning arrester subcommittee reports, publishing the performance characteristics of arresters available to the industry up to discharge currents of 20,000 amperes, cover the range of lightning currents most frequently encountered on high-voltage transmission systems.

H. A. P. Langstaff (West Penn Power Company, Pittsburgh, Pa.): The West Penn Power Company has had the equivalent of five to six years experience with fulchronograph installations on our 25-kv system, the first of three being installed August 1939. A number of records have been obtained, one of which I understand is the maximum rate of rise obtained to date: namely, 7,800 amperes per microsecond. From a total of 40 surge recorders on this same system during this past year we have obtained 11 records varying from 4,500 to 10,000 amperes; nine between 10 and 20,000 amperes; one between 20 and 30,000 amperes; three between 30 and 40,000 amperes, and one over 40,000. All arresters involved were of the line type, and a number of them were of the 20-kv rating. The one discharge over 40,000 amperes destroyed the 25-kv arrester. One 20–30,000-ampere discharge was through a 20-kv arrester with no resulting damage. We are continuing our application of 20-kv arresters on the 25-kv system with desirable economical results, and most all of the line-type arresters are mounted on suspension, thereby allowing all damaged arresters to clear the circuit and allow service to be re-established.

Loss-of-Field Protection for Generators

Discussion and author's closure of paper 42-20 by G. C. Crossman, H. F. Lindemuth, and R. L. Webb, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 261–6.

S. B. Griscom (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper brings out an interesting point in connection with the stability of metropolitan power systems. On transmission systems when stability is a serious problem, it is usually due to such factors as large initial phase-angle displacements, low inertia factors, reduction of synchronizing power by switching out lines, and large reductions of generator output by line short-circuits. These considerations are practically never a factor in the stability of metropolitan systems because of the close proximity of generating capacity and loads, the multiplicity of transmission circuits, and the inherently better stability characteristics of steam turbine generators compared with water-wheel generators.

As may be expected, general instability of metropolitan power systems is very rare, possibly occurring at intervals of from 5 to 20 years on any given system. When instability does occur, it is more apt to be caused by some major apparatus failure, or to an operating error. In other words, with metropolitan systems, the problem is not improvement of stability, but the elimination or neutralization of incidents which might cause instability. The most frequent causes of instability of metropolitan systems have been prolonged short circuits at points of high-power concentration and loss of generator excitation. Recent advances in bus differential and backup protection systems have largely solved the short-circuit problem, and the loss-of-field protection system described by the authors appears to satisfactorily solve the excitation problem.

The combination of an undervoltage relay with the reactive current relay is of particular interest in that it gives double assurance against false tripping. In the minds of most operators, this requirement is probably paramount.

Harold A. Bauman (Brooklyn Edison Company, Brooklyn, N. Y.): Those of us who are operators of generating stations are primarily concerned with improving the reliability of our service by reducing the possibility of serious system disturbances.

These loss-of-field protection relays were welcomed by all of our operators as filling a gap in our generator protection. The first installation, at Hudson Avenue station of the Brooklyn Edison Company, proved the extreme reliability of this equipment. The only maintenance required has been the half yearly operation check.

Tests made by actually tripping out the field of one of our generators while on the bus carrying load proved that the protective relays functioned so well that even the high board operators could hardly observe any disturbance on their station meters.

We are confident that this new loss-of-field protection will prevent serious system disturbances and that a long wanted need has been filled in our system protection.

Earle Wild (Commonwealth Edison Company, Chicago, Ill.): The use of this relay for locating complete or partial loss of excitation on a turbogenerator should be very valuable, as this is a rather rare and often a difficult type of trouble for an operator to analyze. This relay would remove any doubt in the operator's mind as to the cause of the severe oscillations on his generator. It should be pointed out, though, that it is neither desirable or necessary in the metropolitan operating company to remove the generator from the system. The ability of nonsalient pole turbogenerators to resynchronize themselves is very pronounced and the correct procedure would be for the relay to indicate by alarm the nature of the trouble. By either reducing the steam input or restoring the field, the generator would very quickly come back to synchronous speed. In the short interim the speed has been increased considerably to the slip speed of an inductive generator. On large systems the reactive requirements of an induction generator are met with only a slight drop in voltage for the few seconds before an operator could act.

This conclusion is based on the analysis of approximately 18 cases of trouble and tests on the Chicago system which were made over ten years ago and reported in a paper in the December 1931 Transactions of the AIEE.

E. E. George (Ebasco Services, Inc., New York, N. Y.): The scheme of relay protection outlined in this paper seems to provide satisfactorily for a reasonable range of operating conditions and to be adaptable in principle to different sizes and types of units.

Apparently, the scheme always operates to disconnect the generator if the field is lost or is reduced so low that predetermined conditions of voltage, current, and phase angle prevail. It is not clear whether any consideration has been given to reducing the turbine throttle opening so as to lower the generator kilowatt output and prevent instability in those cases where no hazard to equipment is involved in continued operation.

On hydroelectric machines it has been found possible to clear up an out-of-step condition in which the generator was slipping poles rapidly by merely reducing the turbine gate opening. This procedure was satisfactory even in the case where the field was completely removed from the generator, and it permitted the field to be restored without ever opening the generator oil circuit breaker.

There would seem to be cases in some systems where reduction in generator kilowatt loading might be preferred to opening the generator oil circuit breaker. This provides further argument for the idea of position limit interlocking between the generator voltage regulator and the turbine throttle or gate opening.

G. C. Crossman: In reply to Mr. George's comments on reducing turbine throttle opening to lower generator kilowatt output and thus prevent instability during loss or material reduction of generator field excitation, the authors would like to point out that such throttle control has been considered. However, since the system with which the authors are connected is designed to stand the loss of capacity represented by the disconnection of any one unit in any station, it has been felt that the added complication of such control could not be justified, especially in view of the speed with which serious voltage reductions can occur with loss of field on this system.

Mr. Wild has indicated that on the Chicago system it has not been felt desirable or necessary to remove an unexcited generator from the system since only a slight voltage drop is experienced for the few seconds before the operator could act to reduce generator load or restore field. As indicated in the paper, the authors recognize that there are some systems where loss of field may occur without serious voltage disturbance and where corrective measures may be taken without disconnecting the generator from the system. However, studies and experience indicate that on the New York metropolitan system serious voltage disturbances can occur within a relatively short time, sometimes as short as one or two seconds. Furthermore, when such a disturbance does occur, and the unexcited machine loses synchronism, it is often very difficult for the station operator to determine which machine is in difficulty and what that difficulty is. Several years ago loss of field occurred on a 160-megawatt 25-cycle unit. A serious voltage disturbance resulted over the entire 25-cycle system, with considerable equipment damage. Due to the confusion produced by the fluctuations occurring in practically all the station instruments, considerable time elapsed before the operator was able to identify the machine in trouble and remove it from the system.

In connection with Mr. Wild's comments as to restoring field without disconnecting the generator from the system, it should be pointed out that probably the majority of excitation system troubles cannot be corrected without removing the machine from the system. Of the six cases of complete loss of field which have occurred on the New York system, only two resulted from opening of excitation system breakers, while the other four resulted from such various causes as: field rheostat control circuit trouble; pilot exciter rheostat open circuit due to over travel and contact trouble; and contact failure of a pilot exciter brush during replacement of the paralleling brush.

At the time the paper was written, there had been no operating experience with the loss-of-field relays described in the paper. Subsequently, however, on the evening of January 23, 1942, loss of field occurred on a 65-megawatt, 60-cycle, 3,600-rpm unit equipped with these relays. The unit was carrying a load of 60 megawatts. Through an operating error in shutting down another machine, the pilot exciter breaker on this machine was tripped, causing complete loss of field. The relays quickly disconnected the machine from the system without appreciable disturbance in bus voltage.

Linear Couplers for Bus Protection

Discussion and authors' closure of paper 42-40 by E. L. Harder, E. H. Klemmer, W. K. Sonnemann, and E. C. Wentz, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 241–8.

C. D. Hayward (General Electric Company, Philadelphia, Pa.): Bus protection continues to be a most popular subject at the protective-relay sessions. Fortunately for the relay designer, the attention of interested engineers continues to focus (and rightly so) on the current transformer rather than on the relay as the piece of equipment most susceptible to improvement.

The paper on transient characteristics of current transformers during faults by Messrs. Concordia, Weygandt, and Shott[1] today clearly demonstrates the impracticability of using standard current transformers for generator bus protection.

At the Toronto convention last summer Messrs. Kennedy and Sinks[2] showed that a nonsaturating transformer with linear characteristics was necessary, if adequate bus protection was to be provided. They chose the air-gap-core current transformer in preference to the nonmagnetic-core or linear coupler, as described by the present authors, because of the higher level of power which it affords to operate the relays.

The nonmagnetic-core transformer, or linear coupler, is by no means a new device. The interesting magnetic properties of toroidal coils which make them attractive for use as current transformers have been known for a long time. The mathematical demonstration of the complete freedom of the ideal toroidal coil from mutual coupling effects has been used as a textbook example in volumes on electricity and magnetism[3] for many years. It has long been common practice to use toroidal loading coils in telephone circuits to reduce inductive "cross talk."[4] The use of air-core current transformers for differential protection was covered by United States patents[5] issued ten years ago.

The chief stumbling block which discouraged the use of air-core transformers, or linear couplers, in the past was the low amount of power which could be obtained from them to operate the relays. This trouble has been overcome in the air-gap-core current transformer by making the air-gap portion of the core just long enough to prevent saturation of the remaining iron portion on the highest fault currents to be encountered, but no longer, so that the power delivered is not unnecessarily reduced. Since the maximum power which can be obtained from a given sized current transformer on a given primary current is proportional to the square of the effective permeance of its total core magnetic circuit, and since the air gaps in a typical air-gap-core current transformer constitute only about one sixth of the total magnetic circuit, it will be evident that a current transformer with an air-gap core can deliver about 36 times as much power under optimum conditions as it could if its core were all air.

The authors of this paper have apparently accepted the low power level of their linear couplers as inevitable and have been forced, therefore, to use delicate low-energy relays in order to obtain pickup on acceptably low values of internal fault current. The much higher power level afforded by the air-gap-core current transformer makes it possible to use standard-type sturdy reliable relays and to obtain operation on lower fault currents.

The air-gap-core transformer due to the greater power available, can also readily be used with standard types of relays having a small percentage of through-current restraint where minimum settings for internal faults are desirable, whereas the extra burden of restraint coils and connecting leads would probably be excessive for the linear coupler. With the air-gap current transformer only a small percentage of through-current restraint is required, just enough to overcome the tendency to operate on the small differential current due to the slight mismatching of the mutual reactances of the transformers. It is not necessary to have a separate restraint magnet on the relay for each bus circuit, as it is nearly always possible to group the circuits so that the three separate restraints available on standard relays are sufficient for busses having any number of circuits. With this arrangement the tendency of the relay to operate on heavy through faults is entirely eliminated, leaving the sensitivity of the relay as the only limitation on the minimum fault setting. Thus with standard relays and the air-gap-core transformer, the setting can be made low enough for any practical bus installation.

Since theoretically the linear coupler should be free from mutual coupling effects of external fields, it would be interesting to know whether the authors have any explanation to offer for the seemingly high error of 1.6 per cent given as due to astatic effects, especially when the air-gap current transformer without its shielding case was only one per cent and was too small to be accurately determined with the shielding case in place.

Also the linear coupler would appear to be easier to manufacture with close tolerances than the air-gap transformer; hence, we are disappointed to see the authors state that the couplers can be built with an accuracy of plus or minus 1.5 per cent which is no better than that given for the air-gap transformer at the summer convention.

Since no greater accuracy is claimed for the linear coupler, and since its power level is very much lower than that of the air-gap-core transformer with similar linear characteristics, it would appear that considerably more development would be necessary before the linear coupler can be accepted as a desirable transforming means for bus differential protection.

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TELEPHONE COMMUNICATION, **Wright and Puchstein.** McGraw-Hill Publishing Company, Inc., New York, N. Y., 1925. Page 221.

5. United States patent 1,760,541, **Bruckel.** United States patent 1,766,048, **Trogner.**

G. Camilli (General Electric Company, Pittsfield, Mass.): The authors have described an interesting application of the linear couplers to the operation of differential relays for bus protection. Unfortunately, the output which can be derived from the toroids described in this paper is very limited and requires highly sensitive relays.

It might be of some interest to those not familiar with the art to know that linear couplers have been used in the past for the measurement of losses at low power factors (see: Method of Measuring Losses in Reactors at Low Power Factor, by K. G. R. Wilkinson & T. W. Carter), *BEAMA Journal*, May 1939, pages 145–50.

As in the paper under discussion, in this case also, the purpose of the toroids is to obtain a voltage which is directly proportional to the primary current.

The scheme of measuring losses is quite accurate, because practically no load is derived from the toroids. The voltage across their secondaries is balanced with the voltage obtained across the apparatus to be tested in a potentiometer network.

Careful adjustments and very accurate calibrations are necessary when couplers are used to supply even very limited loads.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): A bus-protection scheme, having the well-known advantages of differential protection and yet being free from transient effects, should be highly useful in overcoming the reluctance of many operators to install any bus-protective equipment. This reluctance is not too difficult to understand, in view of the fact that if one false operation of a bus-protective equipment occurs, perfect performance must then be secured for many years, if the scheme is to have as good an operating record as conventional transmission-line relays. Also the seriousness of an incorrect bus clearing is such that false operations are next to intolerable. Accordingly it is well to consider carefully whether a setting margin of six per cent is sufficient to take care of an expected manufacturing tolerance having a three per cent spread, assuming that there are no other possible sources of error.

In considering the desirable operating range of bus-protection relays, another factor in addition to the range of normal maximum and minimum connected generation is of importance. Particularly in the case of outdoor busses, it may be highly desirable after a bus-protection operation to make a brief inspection and then energize the bus from a single generator. The bus-protection relay should, of course, operate with positive margin for this condition.

The lower operating limit given by the authors for the low energy plunger relay is 1,500 amperes or about $7^{1}/_{2}$ volts. For a high voltage system (such as 138 kv), this setting amounts to some 360,000 kva which may not be low enough. The sensitive polar-type relay apparently has an available setting of about one third of this value. What is the opinion of the authors on the necessity of any special precautions in control cable elements to prevent the possible induction of some two volts in the high impedance relay circuit?

Mention is made of adjusting the mutual reactance of the couplers to the correct value after they have been wound. Details of the adjusting method would be of interest.

E. L. Harder, E. H. Klemmer, W. K. Sonnemann, and **E. C. Wentz:** The discussion by Mr. Hayward indicates that he believes that the paper on "Transient Characteristics of Current Transformers During Faults," by Messrs. Concordia, Weygandt, and Shott, clearly demonstrates the impracticability of using standard current transformers for generator bus protection. The wave forms and error currents calculated in the paper referred to are quite interesting in that they do not appear to be too much out of line with what is to be expected. However, to interpret the results of that paper in such a way as to conclude that generator bus protection using standard current transformers is impracticable is entirely out of line. It has been clearly shown by calculation, test, and operating experience, that the principle of utilizing variable percentage characteristics in relay design, together with utilizing fault current for restraint, is and has been quite feasible. Furthermore, standard current transformers are used, selected so that they meet well-established criteria with respect to a-c performance which are neither difficult to specify nor hard to meet. It should be borne in mind that the whole story is not given when a study of current-transformer characteristics shows that a false differential current will flow during certain external fault conditions. The picture is only completed when a satisfactory relay design has been evolved to meet these conditions, or when the problem is given up as being hopeless. The situation, as it exists today, is that the limitations in current-transformer performance under transient conditions have been satisfactorily offset by adequate relay design.

Mr. Hayward puts emphasis on the fact that the toroidal coil is not new. The justification for his attention to this point is probably the fact that in our paper we stated that a new principle was applied to bus differential protection. What the authors meant, of course, was that while the toroidal coils in themselves are not new, the application to bus differential protection is new in that it has not been done before. The fact that the principles involved in the accurate performance of air-core toroidal coils are not new is a positive advantage in that it adds an unquestioned background of accuracy fully justifying the application of these devices.

The fact that a limited amount of energy is available from the air-core transformers or linear couplers has undoubtedly been a deterrent against previous investigation on a serious basis. However, close examination of the facts brings out the interesting point that the difficulty anticipated in the past was a great deal more fancied than real. To substantiate this point, let us consider the relative values of energy levels required to operate several standard relay designs as shown in Table I.

The standard-energy close–open relay operates at 17 volt-amperes at tap value, and this is a figure with which most of us are familiar. The plunger-type relay and the polar-element relays were the ones used in the tests described in this paper. Both of these elements are standard designs as are found in many Westinghouse relays. The figures given for the type *D*-2 relay are rather startling because of the low energy level indicated, but this is easily verified from published information. For example, the resistance of the moving coil is given as 0.213 ohm, and a contact setting requiring only one millivolt across the coil for operation is easily made. From these values of volts and ohms the figure of 0.0000047 volt-ampere results.

Reviewing the above, it is seen that practical relays operate over a range of energy requirements of 3,620,000 to 1, this range being obtained by dividing 17 by 0.0000047. The most sensitive relay used in our tests was the polar-element relay requiring 0.02 volt-ampere, and this is 4,250 times the amount of energy required by the type *D*-2 relay. With this tremendous range in relay energy requirements to pick from, it seems to the authors to be rather pointless to make a vigorous effort to fill a portion of the magnetic circuit of the coupling device with iron, particularly in view of Mr. Hayward's statement that the best that can be done is to obtain a 36-fold increase in power level. Furthermore, when iron forms any part of the magnetic circuit, it introduces its characteristics into that part of the magnetic circuit so that the d-c saturation problem is not at all voided. The parameters of the circuit are merely moved to higher current levels, which merely give the current-transformer designer a little more freedom in certain respects, but which introduce other complications. For example, a parallel circuit must be used, bringing in such complications as temperature errors inherent in the application of modified current transformers of this type and the necessity for a multiplicity of adjustable impedances for impedance-matching purposes.

Mr. Hayward indicates that with the air-gap-core transformer there is plenty of energy to energize restraining coils of the differential relay in those cases where it may be desirable. In this respect, there is no difference whatever between the two schemes. Restraint is necessary only when the accuracy of the device is not sufficiently high to cover the desired range between maximum external fault and minimum internal fault. Experimental work has been carried out to a sufficient extent to show that should it become desirable to add restraint to the linear-coupler relay, the linear couplers can furnish the energy required without difficulty.

Table I

	Volt-Amperes
Standard energy close–open relay	17
Plunger-type relay	0.5
Polar-element relay	0.02
Type *D*-2 relay	0.0000047

Attention is called to what Mr. Hayward feels is a seemingly high error of 1.6 per cent given in the paper. If the authors could have made this test at a total error of three per cent, it would have been done. Actually, the only way we could get the maximum error of 1.6 per cent was to deliberately adjust the position of the poorest coupler of the entire lot for maximum error. A total of three per cent error would have been used as stated above for this test, if a number of couplers just meeting the limits which we have reserved of ±1.5 per cent had been available. Actually, the only way to operate under the maximum error of three per cent, in practice, would be to have all of the couplers in the circuits carrying current into the bus in error a maximum amount in one direction, and the linear coupler in the circuit carrying the current away from the bus in error the maximum amount in the other direction. For example, if on a six-circuit bus the linear couplers in five source circuits were 1.5 per cent high in their response, and the remaining linear coupler in the faulted circuit was 1.5 per cent low in its response, then the maximum error of three per cent would be obtained. The probability of the linear couplers being so disposed is very remote; furthermore, not all of them will be in error on either side by the maximum of 1.5 per cent.

Perhaps experience will show that the authors have been unduly pessimistic in assuming that 1.5 per cent is a normal level of error to be experienced in economic manufacture of these devices. It is fully appreciated that similar devices are made much more accurately for use in precision testing; hence, they could be made just as accurately for bus differential protection. With such increased accuracy, however, the cost of the device would obviously be increased, and it was felt that at the present time the best compromise between high accuracy and reasonable cost would be met at the level of ±1.5 per cent error.

Mr. Camilli points out that "careful adjustments and very accurate calibrations are necessary when couplers are used to supply even very limited loads." In this case, the loading on the couplers is of minor consequence with regard to accuracy and comes into play only for the tripping operation on minimum internal fault. If the linear couplers are out of calibration by 1.5 per cent, then the minimum pickup point of the relay would be slightly affected. Actually, the variation between individual couplers as well as the fact that the relay will not be provided with an infinite number of taps for 100 per cent accurate impedance matching makes it obvious that slight errors in individual linear couplers are inconsequential with regard to the minimum pickup adjustment. A check between calculated pickup current and actual pickup current is given in the test results. What Mr. Camilli has undoubtedly overlooked is the fact that during external fault conditions when the relay must not operate, the linear couplers are performing effectively on open circuit, without loading, because their voltages are in opposition in a series circuit.

Mr. Brownlee wonders if a margin of six per cent is sufficient to take care of an expected manufacturing tolerance having a three per cent spread. The margin given is entirely adequate in that the three per cent spread made up of ±1.5 per cent errors includes all errors which may appear in the couplers, astatic or otherwise. In view of the considerations pointed out above, it is expected that the theoretical maximum error of three per cent will never occur, but even if it did, the safety factor of 2 to 1 would still obtain, which is adequate.

In Mr. Brownlee's second paragraph, our interpretation is that under certain conditions, a minimum of connected generation may exist which is lower than the usual or expected minimum. The problem is, then, to determine what is the real minimum in any application before determining the range between maximum and minimum fault currents.

With regard to the induction in the control cable, the relative disposition of the various parts of the circuit should be considered. In the linear coupler itself, a large number of turns surround an area of several square inches placed as favorably as possible with respect to a primary conductor for the induction of voltage. The control cable between linear couplers and the switchboard should consist of two wires in the same conduit or duct. This cable run will consist of only one turn with a minimum of area between the two wires permitting flux to link the circuit. As long as the two wires are run in the same duct or conduit, it is obvious that the problem is of minor consequence, particularly where the cable is in iron conduit. In one instance, for example, the pickup in the leads amounts to less than one per cent of the minimum voltage used for relaying. Where the exposure to induction is very severe, however, the solution is to use conductors in twisted pair in iron conduit.

In response to Mr. Brownlee's question about the method of adjusting the reactance, the more obvious way is to add or subtract turns until the proper reactance is obtained, and then rewind the last layer so that it is uniform around the circle. A better way more suited to production is to have a section of the winding which is uniformly wound for coarse adjustment, which can be connected in addition or opposition with smaller sections of the winding, also uniformly wound for finer adjustment with one turn taps in one of these sections for the very finest adjustment of all.

Current-Transformer Performance Based on Admittance-Vector Locus

Discussion and author's closure of paper 42-11 by A. C. Schwager, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 26-30.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): It is to be feared that the multiplicity of methods for calculating ratio and phase angle which have been presented during the past few years has served to confuse, rather than aid the application engineer, who should be the one particularly benefited. The design engineers are taking such a great interest in the problem because they hope that a single set of $F\cos\theta+M\sin\theta$ saturation curves or an admittance-vector-locus curve or a nomogram, or equivalent, will be accepted by the application engineer in lieu of a huge number of ratio and phase-angle curves. At one stroke the designer is saved a great deal of work, and the application engineer gets more fundamental information. He can get all the possible ratio and phase-angle data, if he is willing to make a few simple calculations. Yet it is doubtful that the great majority of relay application engineers have thus far been impressed.

It should help to correlate these schemes in the minds of application engineers if we sort out the points on which the designers agree, though they say different things, and discuss briefly the points on which they disagree, even though what they say sounds the same.

Those who start with Agnew all agree on fundamental principles, as Agnew and they evade the "internal reactance" or "leakage-flux" hurdle. Mr. Sinks[1] went back further than Agnew and showed conclusively that Agnew's simple vector diagram will not work if considerable leakage flux is present. However, the author of this discussion worked out a method[2] by which the Agnew formula can be applied, if one is willing to accept a definition of internal reactance in terms of a certain measured value of leakage flux. Yet at this time it is not possible to say that a single simple method for including leakage flux or internal reactance will soon be generally accepted as a standard method.

However, recognizing that leakage reactance may often be neglected, there is fundamental agreement among the designers. The obstacle to calculation is that Agnew's formula contains that pair of trigonometrical bugaboos:

$$F\cos\theta+M\sin\theta$$
$$M\cos\theta-F\sin\theta$$

and that these lead to too much arithmetic. "Simplification" of these bugaboos has been the object of Wiggins, Woods and Bottonari, Wentz, and finally Mr. Schwager. The number of these simplifications may have caused confusion, but it must be remembered that the solutions are all the same in the end; the only reason for preferring one method over the other is that one likes it better. (This is not quite true when Woods and Bottonari suggest that $F\cos\theta+M\sin\theta$ may be equal to $\sqrt{F^2+M^2}$ as this is simplification by sacrifice of accuracy.)

The two principal methods of accurate simplification are then Mr. Schwager's, which, in effect, plots $F\cos\theta+M\sin\theta$ and $M\cos\theta-F\sin\theta$ simultaneously as a vector locus, and the author's,[2] which plots them separately as a family of curves. In either type of curve volts or volts per turn can be plotted against admittance or admittance for one turn (Mr. Schwager's) or amperes or ampere turns (the author's). Both can be particular or general.

It is not possible to say that either method is better. Whatever method is easier to use is better; the decision is up to the application engineers. However, the benefits of general use of either one of the methods and accompanying data are so important to both design and application engineers that it is to be hoped that the application engineers will study both methods and endeavor to make a decision, perhaps through an appropriate AIEE committee, recommending one method and an accompanying standard form in which manufacturer's data are to be submitted.

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3. A PROPOSED METHOD FOR DETERMINATION OF CURRENT TRANSFORMER ERRORS, **G. Camilli** and **R. L. Ten Broeck.** AIEE TRANSACTIONS, volume 59, 1940, September section, pages 547–50.

P. O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. The author has presented a unique method of determining the performance of current transformers and represents a new viewpoint not previously considered for popular use. It is interesting to note that in the past several years, the diversity of problems in relaying has brought about a score of methods, all aimed toward the simplification of voluminous performance data, yet flexible enough to meet the demands of present-day relaying problems.

2. The paper very clearly illustrates the progressive steps normally considered by the designer and is of equal interest to the relaying engineer. Familiarity with the saturation characteristic of apparatus associated with magnetic devices, I believe, is universally understood by the designer and relay engineer alike. The direct values obtained from these data represent a popular yardstick for current transformers. The quantity of admittance as brought forth in the paper is also a useful measurement with perhaps greater appeal to the designing engineer.

3. The writer, however, believes that many characteristics are immediately apparent from the saturation curve based on induced voltage and excitation data. These data may easily be obtained from the completely assembled transformer and interpreted for many varying conditions such as diversity of burden, ratio, and current. Inspection of a saturation curve reveals the so-called linear range which may suffice for many ordinary relaying problems. In such cases, the marked ratio may be considered ideal over this range. A further extension of the curve beyond the so-called "knee" will provide the application engineer with sufficient data for most applications.

4. For greater accuracy, the vector conditions with relation to the transformer output and excitation components must be considered. Mr. Schwager's method has combined the effect of the exciting component by determination of "admittance-vector loci" and has the advantage that the path of this two-dimensional vector of excitation has been definitely established for a given transformer. It also reveals what proportion of ampere turns will finally be left for the output.

5. Considering the use of the saturation characteristic directly from test data, I would like to suggest here another method which is based directly on an earlier AIEE paper by Messrs. Woods and Bottonari. It consists essentially of the combination of the vector diagram and the saturation data into a functional diagram and has the advantage of being relatively a simple and direct graphical solution. Such a scheme is illustrated in Figures 1 and 2.

6. Basically, these charts represent a graphical solution of the transformer vector diagram. The variable element, for example, the excitation of the transformer is obtained from the saturation curve plotted in Figure 1. This is combined vectorially in Figure 2 with the secondary current under consideration to obtain the ratio and phase-angle error of the transformer. An example is worked out in Figure 2, and the graphical steps are indicated by the dotted arrow lines.

7. It is my feeling that this idea or a similar one could be standardized to the extent whereby the charts could be made available on standard AIEE forms. Figure 1, for instance, could be so prepared that the originator of the transformer would merely draw the two curves on the form. Figure 2 would be a standard graphical form for computing the ratio and phase-angle error directly from data obtained by use of Figure 1.

8. In conclusion, Mr. Schwager has presented an interesting method that reveals many functional details of a current transformer. He has demonstrated how a large amount of transformer information can be expressed in one chart as compared to the voluminous number of ratio and phase-angle curves normally experienced in practice today. Since this paper evidences a continuing interest in this general subject, I suggest that the relay subcommittee arrange to prepare some simplification of cur-

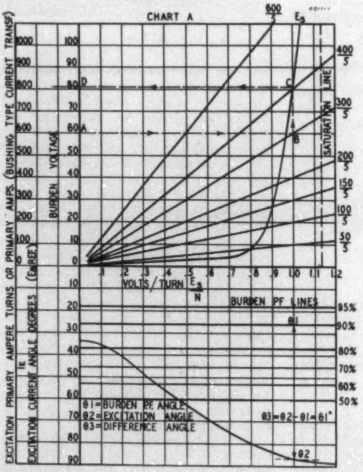


Figure 1

Figure 2. Ratio and phase-angle conversion

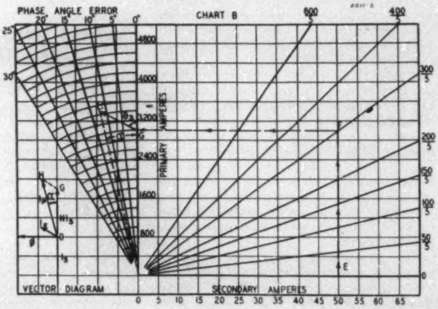
Example: Burden 1.2 ohms, 90 per cent power factor; 300 to 5 ratio; secondary amperes 50; bushing type

1. Burden volts = 50 × 1.2 = 60 volts

2. Chart A—For 60 burden volts (*A*) determine excitation current for 300 to 5 tap (*B*) reading saturation curve at *C*—810 amperes. Determine $\theta3$ for an E_S corresponding to point *B* for 90 per cent power factor *N* as indicated

3. Chart B—At 50 secondary amperes (*E*) determine ideal primary current for 300 to 5 tap (*F* and *G*). To this add excitation current (810 amperes) (*GH*) at an angle $\theta3$. Polar co-ordinate of point *H* represents the correct primary current (*OH*) and phase-angle error α. Values are: 3,480 amperes and 12-degree phase angle

4. True ratio = 3,480/50 = 69.6



rent-transformer data and have its use adopted in standard form in the near future.

Otto A. Knopp (Pacific Gas and Electric Company, Emeryville, Calif.): Through his paper, "Current-Transformer Performance Based on Admittance-Vector Locus," Mr. Schwager has performed a valuable service in having perfected a method of presenting the characteristics of a current transformer in an exceedingly simple and ingenious way. Such a method was badly needed by the manufacturer of such transformers.

The writer, who has always had a great fascination for current transformers of all types, principally for methods of testing them and testing with them, has always felt as a utility engineer that the automatic calculation by test is the simpler procedure for determining the ratio and phase angle of a current transformer, rather than the calculation of these errors from the electric constants of the iron core by the methods originally developed by P. G. Agnew.

For manufacturers the calculating method has, no doubt, great advantages for selecting the size and type of core, ampere-turn value, size of conductors, and so forth. Mr. Schwager, working for a manufacturing concern, was, therefore, interested in this calculating method, and he succeeded in developing new and useful graphical methods which not only reduced the calculation work materially, but furnished an excellent method of presenting the basic characteristics of the current transformer from which all other performance data can now be quickly determined any time they are needed, without subjecting the current transformer to new tests. This is particularly useful when the individual transformer has been sold by the manufacturer and is no longer available to him for test.

The writer, working for a public utility, is naturally more interested in using direct test methods such as described in his AIEE paper, AIEE TRANSACTIONS, May 1936,[1] for error determinations, as the transformer to be tested is in his possession and remains in his possession. Therefore, a simple test under the existing conditions, is the best approach toward getting accurate results and avoids tedious calculation and the necessity of many tests for determining the necessary constants needed for the calculation. More time would be consumed in the determination of the value of the various burdens and their power factors than in determining the ratio error or phase-angle error directly, since most utility laboratories are well equipped for testing ratio and phase-angle error of circuit transformers. However, there may even be good use for Mr. Schwager's approach in the utilities, if the manufacturer who supplies the current transformers furnished their characteristics in the form Mr. Schwager suggests. The engineering department in the utility, confronted with the problem of selecting current transformers for certain relay operations, can select a current transformer more precisely by the Schwager method than by referring to a few specific ratio and phase-angle error curves which may not apply to the particular condition.

Another application would be the use of the "admittance-vector locus" for setting up current-transformer standards, replacing the *XYZ* standards or the new standards proposed: *B*-0.1, *B*-0.2, *B*-0.5, and so forth. Another, and, in the writer's opinion, the most useful application of the "admittance-vector locus," would be by its use in the reverse. It should not be difficult, with the accurate methods and equipment available in the utility laboratories for determining ratio and phase angle of current transformers for all kinds of burdens, to determine the "admittance-vector locus." Thus, it may be possible in the utility laboratory to determine this locus as a matter of record more accurately that way than it could be determined by making measurements of the iron loss and other electrical constants of the core.

Such cores are, in most cases, rather small, requiring especially delicate instruments. Where bushing-type current transformers are involved (and I presume Mr. Schwager was mostly concerned with these), the cores are large, making the determination of the electrical constants simple and accurate. The errors of these transformers are large, making a graphical method such as Mr. Schwager's particularly well-adapted. The adaptability of the method to modern compensated current transformers, where the errors are of the order of 0.1 to 0.3 per cent and one to three minutes phase angle, is not as good. There the reverse method may be helpful.

REFERENCE

1. SOME APPLICATIONS OF INSTRUMENT TRANSFORMERS, Otto A. Knopp. AIEE TRANSACTIONS, volume 55, 1936, May section, pages 480-9.

J. A. Elzi (The Commonwealth and Southern Corporation., Jackson, Mich.): The users of current transformers have a definite need for performance data covering a wide range of conditions which cannot be adequately covered by the usual ratio and phase-angle curves. It therefore appears that the user must be provided with basic data from which any desired characteristics can be determined, and, since most of the users are not transformer designers, it is important that these data be presented in a readily usable form. In this paper the author presents a means by which this can be accomplished and has analyzed step by step the method of arriving at this curve.

Since the admittance-vector locus is a function of the characteristics of the steel used in the transformer, it would appear that any one manufacturer would need to provide only a few such curves to adequately provide data for an entire line of current transformers. It would be interesting to have the author's opinion regarding this point.

The values for the points corresponding to various ratios of E_2/N in Figures 7 and 8 of the paper are not linear, and extrapolation between points is not possible. This at first appears to limit the usefulness of the curves. However, it might be pointed out that, for a given burden and turn, ratio values of secondary current can be chosen such that ratios of E_2/N corresponding to points given on the curve will result, and performance characteristics for intermediate values for secondary current can then readily be obtained. It might also be of interest to note that since the admittance-vector locus is located with respect to the unity power-factor line, it is very easy to investigate performance at burden power factors other than those shown in the figures by simply drawing in lines for any power factor desired.

G. Camilli (General Electric Company, Pittsfield, Mass.): The author's paper is timely in presenting a novel method of determining the performance of current transformers. The new American Standards Association Standards on instrument transformers recognize the fact that characteristic data of bushing-type current transformers for relay applications can be calculated from exciting current and core-loss data. By the method developed by Mr. Schwager, these excitation data can be incorporated in his admittance-vector locus, from which the final ratio and phase-angle errors can be derived.

This graphical method should be welcomed by both the designers and by the application engineers, because, in contrast with the standard procedure, it gives a more functional picture of the performance of the transformer. It is hoped that this method, together with other graphical analyses which have been proposed, may form the basis for a future ASA Standard for the calculation of current transformers for relay applications. The proposed method may be advantageous in all those cases where either the designer or the operating engineer is interested in complete characteristics at different burdens and at different power factors. However, it is doubtful whether the scheme reduces the amount of work involved over the standard procedure when only a small amount of data is required.

A. C. Schwager: Messrs. P. O. Langguth, E. C. Wentz, and G. Camilli point out the usefulness of the admittance-vector locus for the designing engineer; the question, however, is raised as to whether the method is practical for the use of the operating engineer when only a small amount of data is required. It appears that the author has not sufficiently emphasized the importance of Figures 9 and 10 in his paper. These figures represent the ultimate simplification of the vector-locus graph. Figure 9 shows that for a given burden power factor the ratio error of a multiratio bushing-type transformer can be represented by one single curve, provided $N^2/Z>1{,}000$. Figure 3 of this discussion shows a chart applying to an actual transformer from which the ratio for any total burden power factor between 0.5 and 0.7 can be easily computed as indicated for any practical value of N^2/Z.

The author intended to convey the existence of such simple charts in Figure 9 of the paper. Chart Figure 3 applies to a 600 to 5 multiratio transformer with the ratios as indicated. The top curve applies for a total burden power factor of 0.5 and the three lower curves apply to a burden power factor of 0.7 and varying N^2/Z values. A logarithmic abscissa of two different logarithmic rates for the E_2/N values greatly increases the accuracy over the entire range.

The practical result of Figure 10, on the other hand, is a simple chart Figure 4 of this discussion from which the ratio error can be read off directly in function of the

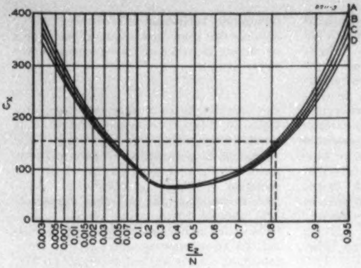


Figure 3. Ratio characteristics for 23-kv bushing current transformer

Burden power factor 50 and 70 per cent
Internal $R=0.0022\times N$ ohms
Internal X negligible
Volts per turn, $E_2/N=I_2Z/N$
True ratio $=N+C\times Z/N$

Curve	Range of N^2/Z	Power Factor (Per Cent)
A....	100–∞	50
B....	100– 400	70
C....	400–1,500	
D....	1,500–∞	

Ratio	Tap	Secondary Turns, N	N^2
50/5	B-C	10	100
100/5	A-B	20	400
150/5	A-C	30	900
200/5	D-E	40	1,600
250/5	C-D	50	2,500
300/5	B-D	60	3,600
400/5	A-D	80	6,400
450/5	C-E	90	8,100
500/5	B-E	100	10,000
600/5	A-E	120	14,400

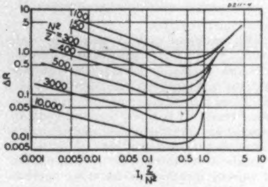


Figure 4. Ratio characteristics for 23-kv bushing current transformer

Burden power factor 50 per cent
Internal $R=0.0022\times N$ ohms
Internal X negligible
True ratio $=N(1+\Delta R)$

Ratio	Tap	Secondary Turns, N	N^2
50/5	B-C	10	100
100/5	A-B	20	400
150/5	A-C	30	900
200/5	D-E	40	1,600
250/5	C-D	50	2,500
300/5	B-D	60	3,600
400/5	A-D	80	6,400
450/5	C-E	90	8,100
500/5	B-E	100	10,000
600/5	A-E	120	14,400

primary current. N^2/Z is the parameter of the various curves; in a practical chart additional N^2/Z curves and scale subdivisions would be included. As shown in the paper, the individual curves in Figures 3 and 4 of the discussion can be readily obtained from the excitation characteristics of the steel, or from a sample of the transformer if available for test, the latter giving the more accurate data.

In view of the simplified charts Figures 3 and 4, the author considers it unnecessary to refer to Figures 7 and 8 of his paper or to the methods outlined by Mr. Wentz and by Mr. Langguth, except for design purposes or for application investigations of a more general nature. It might however be worthwhile to analyze some of the features of the above three methods.

Since we are primarily concerned with determining the ratio (complex) of the transformer, it appears that curves whose ordinates are equal to the actual error, except for constants of the transformer and burden are simpler than curves which are not proportional to the error. The admittance-vector locus represents the error, whereas if the magnetization curve is used, the error is obtained only after a division of the exciting current by a variable (I_2). In addition, the admittance-vector locus has increased reading accuracy over the magnetization curve.

In comparing Figure 5 of Mr. Wentz's paper with the author's charts, it must be realized that these charts are equivalent to the author's Figure 8 and do not cover the low-ratio bushing-type transformer represented in Figure 7, since Mr. Wentz uses a simplified equation for the ratio error.

The interesting charts A and B (Figures 1 and 2 of this discussion), which Mr. Langguth has submitted, correspond to Figure 7 of the author's paper. It appears, however, that Mr. Langguth's method requires considerable computation and drafting effort, in addition the range covered by the abscissa is only one per cent of that covered by author's Figure 7, and its practical value for multiratio transformers seems thereby seriously limited.

Since standardization of current-transformer data has been frequently mentioned in the various discussions, the author would like to summarize his suggestions; the final decisions as to the merits of the various proposed methods will, as pointed out by Mr. Wentz, have to be made by the application engineer.

It is proposed that two charts be furnished only.

1. **Admittance-vector locus as shown in Figure 5 of the discussion, or a chart showing the admittance vector components in a rectilinear co-ordinate system, with E_2/N as abscissa in logarithmic scale.**

Figure 5 of this discussion permits the calculation of the exciting current for idle transformers and if desired can be used as the basis for any detailed investigation as described in the paper.

2. **Ratio error curves as shown in Figure 3 or 4 of this discussion. Either of these curves permits the determination of the entire ratio data without complicated calculation or construction and require no knowledge of the operation of the transformer proper.**

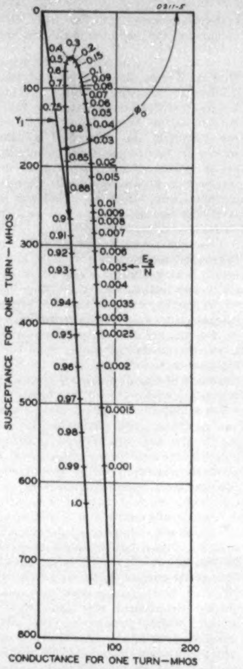


Figure 5. Admittance-vector locus for one turn for 23-kv bushing current transformer

Internal reactance negligible
Internal resistance $R=0.0022N$
$Y_N=Y_1/N^2$

Mr. Elzi points out an interesting further simplification which might have particular merit for design purposes. The admittance-vector locus can be rescaled so that $Y_{1(B)}=y_{1(B)}D/A$, where $Y_{1(B)}$ is unit admittance, A is the cross-sectional area of the core, and D is its mean diameter. Instead of a parameter of volts per turn, flux density B is used, or E_2/NA if preferred.

This basis will give values of sufficient accuracy for design purposes, even with cores of somewhat different proportions, and close values if the cores have equal outside-to-inside diameter ratios, because of similarity of flux distribution.

Mr. Elzi's concern about the difficulties encountered in interpolating E_2/N values on the admittance-vector locus might be reduced by referring to the proposed practical curves Figure 5, or by plotting the admittance-vector components on a rectilinear co-ordinate system as suggested above under standard data.

That the admittance-vector locus method is readily accepted by a utility laboratory is particularly appreciated. Mr. Knopp confirms the wide field for which the new method might be applicable. The determination of the vector locus in reverse, as suggested by Mr. Knopp, opens up a considerable field of applications. The method consists of measuring ratio error and phase angle for a known secondary burden and plotting them in a rectilinear co-ordinate system, the resulting curve being geometrially similar to the admittance-vector locus for one turn. This test curve, therefore, fully represents the transformer and permits the calculation of the various important constants, such as turn compensation and secondary leakage reactance.

Multichannel Carrier-Current Facilities for a Power Line

Discussion and authors' closure of paper 42-13 by P. N. Sandstrom and G. E. Foster, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 71–6.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The protective features of the carrier-current installation described by the authors provide an excellent example wherein the use of special, and sometimes so-called "complicated," protective measures are a principal factor in making possible important savings in major equipment (in this instance, the high-voltage oil circuit breakers). The value of such savings may be many times the cost of the additional protective equipment. The presence of continuous automatic supervision should add materially to the over-all reliability of the protective scheme.

Although the transferred tripping arrangement was provided primarily to permit clearing both power sources from a faulted transformer bank, the inherent abilty to clear both ends of the transmission line simultaneously is quite valuable. It is assumed that some form of distance relays is used for the phase protection in this connection. If so, is any special provision made to prevent operation of the distance relays during system oscillations, especially those which do not result in an actual out of step condition? Perhaps the multiple tone system described will lend itself rather well to an "out-of-step blocking" arrangement.

The protection of a three-terminal line or "Tee-tap" involves problems which sometimes cannot be solved by the simple addition of distance relays to a conventional carrier-current system. Have the authors made any study of the possible application of their transferred tripping arrangement to three-terminal lines?

P. N. Sandstrom and **G. E. Foster:** In answer to Mr. Brownlee's discussion, his assumption is correct that distance-type relays are used for protection against phase-to-phase faults. The particular type employed is the high-speed impedance type. No special provision has been made to prevent the operation of these relays during system oscillations. With the present method of operation there is very little chance for instability.

With reference to the question of protection of a three-terminal line with distance relays in conjunction with conventional carrier-current system, the authors have had contact with an installation of this type. This installation has given satisfactory service for over two years.

The authors have not made a complete study of the possible application of the transfer trip arrangement to a three-terminal line. One method of extending this transferred tripping method to a three-terminal line would be by means of a carrier loop consisting of three carrier frequencies. The first frequency connects station *A* to station *B*; the second frequency connects station *B* to station *C*, and the third frequency connects station *C* to station *A*. At each station only one transmitter and one receiver would be needed. The blocking loop would be extended over this carrier loop. Tripping would be accomplished by breaking the blocking loop at any point and starting the tripping frequency at that point. Since a loop is formed, the tripping signal would go to the other two points, giving almost simultaneous opening of all three terminals.

Transient Characteristics of Current Transformers During Faults

Discussion and authors' closure of paper 42-39 by C. Concordia, C. N. Weygandt, and H. S. Shott, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 280–5.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.) and **W. K. Sonnemann** (Westinghouse Electric and Manufacturing Company, Newark, N. J.): In the opening paragraph of this paper, the authors indicate that very little quantitative data on the transient performance of current transformers have been published. It is agreed that a complete set of transient-performance curves covering all existing designs of current transformers has never been issued. Such a set of curves would involve volumes. Apparently, this paper intends to present data which may be considered quantitative through the use of the so-called size constant of the transformer. In this respect, however, we do not feel that the paper has made any great strides forward over the Wentz-Sonnemann paper mentioned as reference 2. In the reference paper, the authors have presented formulas for the calculation of current-transformer performance in terms of design constants and secondary circuit resistance. In Mr. Harder's discussion of that paper, he reduced the design constants to terms of the saturation curve which puts the necessary design information in a form which can be obtained much more readily in many cases, and which can be used more easily.

We feel that the size constant as given in the paper is not too good a criterion of transformer excellence for the following reasons.

1. We believe that the factor, *l*, representing the length of the magnetic circuit, is unduly stressed, and tends to lead to erroneous conclusions. For example, if the factors *l* and *A* are both increased to, say, double their given value, the size constant is unchanged, but the transformer will, in practically every case, give better performance for any given application. This improvement results from the increase in area and not from an increase in length, as indicated in the author's conclusion 1. The reason for this is that the amount of flux in the core necessary to circulate a given current in the secondary circuit is independent of the length of the magnetic circuit. Once the flux reaches the saturation point, the error current begins to flow in the differential relay. If the area of the core is doubled, the time to reach saturation is increased, thus allowing an increased time interval for the d-c component of the fault current to diminish with a consequent reduction in the magnitude of the error current when it appears.

2. The factor, r_1, should and does include the resistance of the secondary burden. It inevitably follows that any one design of transformer does not have a single size constant in itself. Its size constant can only be determined after the secondary burden for the particular application has been ascertained.

The authors intimate that it is still necessary to consider each application of bus differential relays on its own merits. While this is true to a certain extent for some schemes, and where there are special considerations, it does not give a true picture of the situation as it has existed since the advent of the variable percentage differential relay. Sufficient study of the transient characteristics of current transformers and sufficient tests to verify the conclusions reached have been made so that in the usual bus-differential application, it is possible to apply the variable percentage differential relay without exhaustive engineering research. Such research is avoided through applying the relay in line with certain well-established criteria. These may be explained as follows:

1. The current-transformer ratio should be so chosen as to keep the maximum secondary fault current within reasonable limits as determined by a practical relay design.

2. On multicircuit busses, it is sometimes necessary to use two relays per phase in order to provide an adequate number of restraining elements.

3. The current transformer should not saturate on the maximum symmetrical fault current to which it will be subjected. This does not mean that it will not saturate on asymmetrical fault current. Instead, the saturation caused by the d-c component will usually be severe, giving rise to a substantial error current. However, the use of the variable percentage characteristic as well as an adequate number of restraining elements prevents the relay from false tripping for external faults.

C. Concordia, C. N. Weygandt, and **H. S. Shott:** On reading over the discussion by Messrs. Wentz and Sonnemann, we note such a very great difference in viewpoint that it will be somewhat difficult to find a common ground for discussion. Messrs. Wentz and Sonnemann seem to feel that enough had already been done on this problem so that all the significant questions are readily answered, but we cannot agree with this opinion. To our knowledge the only quantitative results previously published have been from spot tests and often, as in the case of the paper by Messrs. Wentz and Sonnemann, without any means of identifying the transformer.

Referring to the first paragraph of the discussion, it is not merely by introducing the concept of size constant that our paper intends to present quantitative data, but also in a much more exact representation of the saturation curve. It seemed desirable, in view of the complexity and importance of the subject, to attempt as rigorous a solution of the problem as practicable, rather than to continue to oversimplify it.

Referring to the second paragraph of the discussion, we infer that Messrs. Wentz and Sonnemann cannot have given our paper the attention which we feel it perhaps deserves. As stated in conclusion 1: "Of two transformers having the same size constant the one having the longer magnetic circuit will give better performance for the same value of primary current." This statement is true, and to say, as the discussers have, that it is not the longer magnetic circuit which accounts for the improvement, seems to us to be quibbling. We do not subscribe to the discussers' arguments about the area; in fact, it could just as well have been the length l and the secondary resistance r_2 which were changed, instead of length and area, and conclusion 1 would still hold. The performance curves as shown by Figures 9–12 are all related to the "amperes per inch of core length." Thus, if length and area are both doubled, the size constant will remain the same, but the same value of primary current will result in half the ampere turns per inch, and, hence, the performance will be shown by a different point on the curves. Regarding the discussion of r_2, we refer the discussers to the definition of r_2 following equation 1 of the paper. Both the size constant and performance of any transformer depend on its secondary burden.

Referring to the discussion of the variable percentage differential relay it seems desirable to point out that such a relay would have severe limitations when used with conventional bar-type current transformers even when the current is only five times normal, as indicated by Figure 9.

Progress Report of D-C Testing of Generators in the Field

Discussion and authors' closure of paper 42-1 by E. R. Davis and M. F. Leftwich, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 14–18.

Charles F. Hill and **L. J. Berberich** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Davis and Leftwich have found with others that spot readings of insulation resistance were not as effective in determining the condition of the insulation as insulation resistance-time curves. Absorption or insulation resistance-time curves periodically determined have been used by many operators with some success. This success for the ordinary low-voltage determinations (500–1,000 volts) has, however, been limited to:

1. Detection of moisture in insulation.
2. Determination of leakage over the end windings due to moisture, dirt, oil, and combinations of these.
3. Studying the curing of the insulation in a new machine which is illustrated by the data in Figure 8 of the paper.

Low-voltage d-c testing on the other hand, has not been of much value in the location of damage to the insulation due to mechanical causes.

A number of operators have also attempted to locate mechanical defects by means of other tests such as power factor and ionization detection with negative results, in general. It had been our opinion that rather high-voltage d-c testing should be more effective in detecting mechanical deterioration in such insulation, but attempts by some operating engineers did not give positive results. It is, therefore, encouraging to see that the authors have achieved some measure of success with such high-voltage d-c tests.

The EEI subject committee on generator insulation testing (AIEE TRANSACTIONS, volume 60, 1941, p. 1003) reports some tests with high-voltage d-c on several machines which were about to be rewound. They were not always successful in locating deliberately inflicted mechanical damage. They did find, however, that insulation resistance may be considerably less when measured at high-voltage than when measured at a low-voltage in a machine known to have a defective winding. We have made many measurements of insulation resistance on sections of good coils insulated for 13.8 kv of both the older and the newer types over a range of voltage from 500 to 25,000 volts. These showed that the insulation resistance was substantially unaffected by voltage. Therefore, when a voltage effect is found, some insulation defect may be indicated. This should be investigated further.

The authors have logically started at lower voltages and gradually stepped up the voltages in their testing program, until now a voltage of 14,000 is used on machines of the higher-voltage ratings. We believe, however, that still higher voltages could be used. We have made a number of tests of the a-c and d-c breakdown strengths of both the older and newer type of insulation and have found that the d-c breakdown strength corresponds very closely with the a-c peak breakdown strength. The EEI subcommittee already referred to obtained the same result on old machines. It is, therefore, indicated that d-c voltages at least as high as the permissible a-c peak-test voltage on old apparatus could be used in testing without damage to good insulation. This would correspond to voltages up to 20,000 or 25,000 for a 13.8 kv machine which is somewhat higher than the maximum voltage used by the authors.

Messrs. Davis and Leftwich have indicated that many of the failures are due to mechanical causes. They have shown that high-voltage d-c has definite value in detecting this type of insulation defect. Table II of the paper shows that many of the breakdowns on test have occurred at d-c voltages far below the a-c peak-operating voltages. The authors state that some of these breakdowns have occurred on coils near the neutral. We would like to ask if all coils which broke down on test at voltages below the a-c peak operating voltage were far removed from the line end in the phase group.

The authors have apparently kept a record of service and test failures over a long period of time. Such records are of considerable value, not only to the operator, but also to the manufacturer for improvement in design may be suggested. These records are summed up in Table IV of the paper. One would expect that the real value of a testing program should be indicated by a reduction in failure rate in service. While Table IV does not give the service failure rate before the testing program was started, it does not show any consistent improvement in failure rate in service as experience was gained in testing. We would like to ask if the authors have any explanation for the lack of a definite improvement in service failure rate since the testing program was begun.

There is a possibility that machine voltages themselves are locating defects which develop on coils nearer full-phase potential, leaving only those on coils near ground potential for the d-c tests. If this is true, then the use of much higher d-c test voltages might be very useful to anticipate or eliminate service failures.

C. T. Weller (General Electric Company, Schenectady, N. Y.): During recent years, an important activity of the Institute has been the co-ordination of insulation. This involves testing methods as well as values, so it is gratifying to have a "Progress Report" on a d-c method to supplement the usual a-c methods. The use of high direct voltage for commercial routine testing dates from about 1916, when the development of our d-c cable-testing sets was started. These were developed

1. To provide equipment of greater portability than the ponderous a-c equipment then required for testing cable lengths.
2. To determine if impending cable failures could be anticipated from the time-current curves of the insulation.

The question of test equipment portability (1) does not ordinarily arise in the case of generators, but the question of impending failure detection (2) is always pertinent. After a long investigation, a ratio of 2.4:1 between the d-c crest and a-c rms test voltage values for cables was arrived at. A corresponding test-voltage ratio for apparatus apparently has not been established, so arbitrary values are sometimes used.

The effect of direct voltage on insulation is indicated by some of our experiences with the early cable-testing sets in the laboratory. We attempted to use sphere gaps to determine approximately the drop across the rectifier but found that the polarity of the direct voltage had an effect on the arcover voltage. In view of the well-known limitations of sphere gaps from the standpoint of accuracy, we built an electrostatic voltmeter for use as a d-c–a-c transfer instrument; this was rated 100 kv (crest a-c or constant d-c value) and was of the oil-insulated, reflecting, repulsion type. The voltmeter worked beautifully with alternating voltage, but with gradually increasing direct voltage became more and more erratic, until it was impossible even to estimate what the deflection might be; the performance indicated that the leakage was both large and variable but no arc-overs occurred. In order to simulate cable lengths we built up a capacitor of glass-tinfoil units mounted in a wooden frame carefully treated with paraffin. As the direct voltage across the capacitor to ground increased, leakage through the seams in the wood finally caused smoke to appear, so additional insulators had to be provided. The direct-voltage values involved were much higher than those under discussion today, but the results were first responsible for our conclusion that direct voltage constitutes a much more severe test on insulation than alternating voltage when leakage is present. Variable leakage occasionally occurs in cable circuits and some very erratic arc-overs have resulted. However, it should be emphasized that cables, because of their uniform insulation and resulting voltage stresses, are ideal subjects for d-c testing, but such is definitely not the case for apparatus. It is evident that the authors were wise in proceeding with caution. It appears that many additional "Progress Reports" will be necessary before a proper d-c–a-c test ratio can be established for apparatus.

P. H. McAuley (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The author's results show that high-voltage d-c testing of insulation, which amounts to a combined dielectric and Megger test, is very effective. The following data may help to explain some of the results given in the author's Table III and Figure 8.

Table I of this discussion shows the results of tests at 500 volts d-c on four fractional horsepower 440-volt motors of a type which has given good performance in service. The tests were designed to determine the variations in resistance likely to be encountered on machines. The readings were at one minute after voltage was applied, but in most cases resistances were still increasing after 15 minutes. The corresponding currents may be obtained by dividing the voltage (500) by the resistance values shown. First, note the remarkable consistency in the performance of the four different motors. The first test was made at room temperature just as the motors were received from the factory. In a 48-degree-centigrade oven the resistances dropped slightly. However, with the oven at 75 degrees centigrade and later at 95 decrees centigrade, the values are practically the same as the original room temperature values. Upon cooling to room temperature the insulation resistances increased in about a 100 to 1 ratio. The last column shows the effect of standing around under summer air conditions plus an overnight cooling cycle in a refrigerator. All values, of course, are well up in the range regarded as indicative of insulation in good condition.

The apparent explanation of the behavior of the insulation is moisture absorption. When these motors were stored following the varnish bakes, a long slow process of moisture absorption followed under summer humidity conditions. Capacity and power-factor measurements also show this process rather well. At the higher temperatures moisture is being driven off and resistances do not change much. But when the motors cool again, insulation resistances in the dry condition at room temperature are very much higher. Exposure to room air plus a cooling cycle conducive to condensation soon reduces the readings to $^1/_{1,000}$ the former values. In spite of the varnish treatment, moisture influences leakage currents. Also, in these particular tests, the results were influenced in part at least by the leads which touched the frames at places in the terminal boxes. It has been found that rubber-insulated leads are no guarantee of high insulation resistance in the leads. In general, leads can have a great influence on indicated insulation resistances.

Table I. Insulation Resistance in Megohms After 1 Minute at 500 Volts D-C on 4 Small 440-Volt Motors at Controlled Oven Temperatures

Motor	28C	48C 5 Hr	75C 20 Hr	95C 19 Hr	22C 23 Hr	25C (18 Days) 5C (15 Hours) 25C (7 Hours)
1	460	125	430	415	22,000	27
2	475	119	400	445	65,000	16
3	455	99	340	360	42,000	25
4	495	106	310	340	38,000	31

These results apply, of course, to machines which stand idle for long periods and are subjected to high humidity conditions. On samples and on motors subjected regularly or continuously to high operating temperatures, insulation resistances usually increase with aging. It has been noticed that measurements at room temperature show about a 10 to 1 increase over considerable periods of time.

Individual values of insulation resistance above the range of values due to dirt or moisture can hardly be regarded as significant. However, similar machines seem to perform very consistently. If a given type is well-classified, insulation resistance readings may prove of some value in evaluating the condition of individual units. Admittedly we have a good deal to learn before this is possible.

B. Van Ness, Jr. (Pennsylvania Water and Power Company, Baltimore, Md.): In recent years there has been a growing interest in the development of a nondestructive method of checking the condition of generator insulation. The increasing demands upon standby and reserve equipment during the present emergency stress the need for a solution to the problem—which is complex and difficult—and one requiring the attention of many investigators. Therefore, the present paper is particularly timely.

Insulation troubles may be classified roughly under two headings:

1. **Temporary**—in which some outside agent temporarily reduces or impairs the insulation strength. In this class of trouble may be typed insulation affected by moisture and surface dirt.
2. **Permanent**—in which the dielectric strength of insulation is permanently reduced by mechanical breakage, electrical rupture, and so on.

The distinction between these two classes of insulation trouble is not always appreciated.

In general, the more commonly recognized nondestructive methods employed in machine insulation testing will rather definitely pick out the temporary types of machine insulation weakness introduced by moisture or dirt, but as yet they are not at all definite in the location of permanent types of insulation difficulties. Therefore, it is encouraging to see the fresh viewpoint presented in the paper by Messrs. Davis and Leftwich and the progress they are making in field testing. With the exception of destructive a-c high-potential testing, theirs is the first method reported to definitely pick out incipient insulation faults of the permanent type. In many cases of their checks it was not necessary to test to failure to detect faults, but in some cases the faulted winding actually failed during testing. This method of testing, therefore, does not as yet completely meet the desired ultimate of nondestruction, but it is a definite approach toward that goal. Winding damage is localized and the hazard of widespread trouble from service failure is eliminated. Furthermore, it is very possible that the test breakdowns occurring at the inception of the program might have been averted, had testing been under way when the insulation was first affected.

The higher test voltages offer interesting possibilities. It is hoped that the authors will find it possible to extend the method to the 13,800-volt class at comparable test voltages and with equally successful results.

E. R. Davis and **M. F. Leftwich:** The interest shown by those presenting discussions is greatly appreciated by the authors.

Dr. Hill and Dr. Berberich mention that some of the generator armature breakdowns on test occur on coils near the neutral. In checking our records, we find that many of the failures on test occurred approximately halfway between line and neutral coils in the phase group. The test voltage of failure usually exceeds the a-c peak operating voltage. However, at the beginning of the test schedule many of the incipient failures were located near the generator neutral at comparatively low test voltages so that no "standard" test voltage could be determined until these defective coils were located and removed.

Referring to the rate of failure in service as shown in Table IV, we wish to point out that the average age of generators prior to beginning of tests was 7.78 years as compared with 13.45 years to January 1, 1942. These ages take into account additional capacity added to system and machines which have been rewound. Further checking of records reveal that in the 10-year period immediately prior to test schedule, a total of 403,650 kva failed in service as compared with 360,115 kva as shown in Table IV. This difference of 43,535 kva is very encouraging, because the average age of windings during this 10-year period was increasing. It is believed with the newer test apparatus and additional testing experience this downward trend will continue and perhaps exceed the rate of decline in kilovolt-amperes failing in service as noted.

The difficulties which Mr. Weller encountered in accurately measuring high d-c potentials are easily understandable, although our problem was not so difficult, because we are using a comparatively low voltage. However, by keeping comparative records on individual generators, a slight inaccuracy of test potential as recorded by instruments would hardly be evident, providing the same test set and instruments are used.

The effect of moisture on insulation when a comparatively high d-c voltage is applied, as described in Mr. McAuley's discussion, is very interesting. We have used a d-c voltage of 5,000 on large 6,600-volt generator armatures while being dried prior to initial installation and have obtained similar results. However, a much longer interval of time was required to obtain a change in insulation resistance.

We hope that many high d-c voltage tests will be made in the near future, particularly on armatures of the 13,800-volt class. The compilation of such data may eventually lead to definite test standards. At least, as Mr. Van Ness, Jr., states, the higher test voltages offer interesting possibilities.

Measurement of Maximum Demand

Discussion and author's closure of paper 42-15 by P. M. Lincoln, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 57–62.

Perry A. Borden (The Bristol Company, Waterbury, Conn.): The soundness of the theory of measuring demand on a logarithmic basis has already been well taught by Dr. Lincoln in his earlier papers; and his original form of the instrument, even with the inherent errors which he has now pointed out, demonstrates the practical application of this theory and provides probably a truer basis for determining the limits of electrical apparatus than is found in the mechanical instruments of the block-interval class. I believe that those utilities who have adopted the thermal demand meter have found its use to be economically sound and are satisfied that such slight departures which it may possess from the theoretically correct values are insignificant as compared with the inherent departures of those values from being a true criterion of the actual limits of the apparatus. That demand measurement can never be reduced to an exact science is an admitted fact—and is probably the only fact which can account for the toleration of measuring methods which may show such vast discrepancies as have long been recognized and have been vividly brought to light in Dr. Lincoln's Ithaca tests.

In the development of the new form of thermal meter there has obviously been encountered the same problem that is met wherever the measurement of temperature is involved, namely, that of making sure that the temperature as indicated by the sensitive device is the same temperature as that in which we are interested. In this case, however, since temperature is but one link in a chain of energy transformations essential to the final measurement, the various loading and compensating devices appear to have succeeded not only in taking care of such discrepancies but in utilizing the time lag of heat transfer to simulate the temperature response characteristic of the electrical plant.

From an engineering standpoint, and even from an economic standpoint, the thermal demand meter would seem to approach as closely as possible to the ideal method of obtaining the desired measurement. The main objection to its use seems to be, now as heretofore, that of providing to the customer a satisfactory explanation of the engineering soundness and economic fairness of its readings. There has been raised against the use of thermal demand the argument that is readings are difficult to define and to explain in comprehensive terms to the nontechnical user. This objection probably has some foundation and would not in any way be removed by the adoption of the suggested modification in which, with the adoption of a longer characteristic time period, the meter is deliberately designed to depart from following a true logarithmic law. These objections, however, appear to be principally academic, and should not introduce more difficulty in the work of the rate maker than is found in the incorporation of a power-factor clause in charges for electrical service. Here, it is admitted that the possibility of several definitions might form a basis for controversy; but, as in most other instances of formulating definitions, a certain amount of compromise must be tolerated in the interests of ultimate simplicity. This attitude is well brought out in Professor Karapetoff's contribution, "A Parable About the Mean Radius of the Ellipse," published in the AIEE JOURNAL, volume 39, 1920, page 732. (I here take the opportunity to recommend that this parable be reprinted for the edification of the younger members of the Institute.)

The secondary importance of the actual value of the selected time interval in demand measurement has long been realized and was confirmed in the tests under actual load conditions as described in my 1920 paper on demand measurement (AIEE TRANSACTIONS, volume 39, page 1847). Dr. Lincoln, in his discussion of that paper, pointed out many of the features of demand measurement which have been brought to a head in his present paper. While in 1920 we were hardly so audacious as to suggest anything like the universal adoption of a 60-minute time interval, it now appears that such a period, especially if incorporated in a logarithmic meter of the suggested form, might fill a very large place in the measurement of demand.

W. J. McLachlan (General Electric Company, Schenectady, N. Y.): The condemnation of the block-interval meter in Mr. Lincoln's paper appears practically to incriminate the prevailing practice in this country. This allegation at face value is alarming. That it is not, is evidenced by the fact that the same theoretical shortcomings of the block-interval type were expounded, before the Institute, quite thoroughly by Mr. Lincoln in 1915, again in 1918, again in 1934, again in 1939, and now are repeated in 1942.

Surely, there must be some reason behind the continuing use of a device whose shortcomings have so long been so well understood. In fact, there are many reasons. Among them are:

1. Any measurement of demand constitutes not an exact, but rather, only a rough approximation of the proportion of the system investment cost incurred in supplying that demand. This has been recognized practically since the conception of demand rates.

2. Since no demand allocation method known is exact, there has been a preference for a measurement basis which is simple to understand and explain. This is a definite advantage of the arithmetic average of the block-interval type as contrasted to the logarithmic characteristic.

3. "Peak splitting" is of significance only on load peaks of short duration, which occur singly during the billing period or which repeat at an interval exactly equal to the "block interval" or a multiple thereof. Probability states that random repetition of peaks within the billing period, which is the usual case, will minimize the "peak-splitting" effect.

4. Short-duration loads, where peak splitting can possibly have any effect, and where advantage is claimed for the logarithmic characteristic are a problem unto themselves, because the ability of the system to supply such loads is limited generally by voltage drop and not the thermal ability of the system. The voltage-drop effect is instantaneous, and its import cannot be measured by any of the time-interval demand measurements now used, be they logarithmic or arithmetic.

5. While heating of system equipment follows a logarithmic characteristic, the time constants involved in the various equipments are so radically differing in themselves, and different from those of the logarithmic meters now employed, as to make any claims for advantage in this respect at least open to question. For instance, a transformer may require six to seven hours to reach 90 per cent of ultimate temperature, as contrasted to the one-hour meter characteristic. Actually, an arithmetic curve much more nearly approximates the heating of such an equipment during a one-hour interval than does a one-hour logarithmic meter curve.

6. The fact that one type of meter follows the heating curve of equipment, at one fixed value of load, more closely than another is of little significance when it is considered that the response of both meters, to differing values of load, is in accordance with the first power of the current (since the measurement is in kilowatts, kilovolt-amperes, or reactive kilovolt-amperes) whereas the heating of equipment is much more nearly proportional to the square of the current. Thus the basic measurement is not in accordance with the thermal loading of equipment.

7. Testing convenience has been a predominant advantage of the block-interval type.

The discusser holds no brief for the block-interval meter. It is the manufacturer's business to build the types of equipment which the trade chooses to desire. The purpose of this discussion is to record the fact that there are factors which should be considered in arriving at this choice which do not appear in Mr. Lincoln's paper.

P. M. Lincoln: I take positive exception from the comments submitted by Mr. W. J. McLachlan. The indication of maximum demand as registered by the thermal demand meter is completely exact and not a rough approximation as Mr. McLachlan would have us believe. Its only departure from complete exactness is due to the impossibility of causing heat to diffuse instantaneously throughout the masses of matter that are being heated or cooled during normal operation. By careful design, this departure from complete exactitude can be made negligible.

May I ask Mr. McLachlan why he advocates an admittedly inaccurate method of measuring maximum demand when an exact method is available?

I would also disagree with Mr. Perry A. Borden's statement "that demand measurement can never be reduced to an exact science is an admitted fact." This statement is quite true when applied to an arithmetic average, but almost entirely false when applied to a logarithmic average. I, for one, make no such admission. The thermal demand meter as described in my paper is absolutely exact except in so far as its indication is influenced by the impossibility of causing heat to diffuse instantaneously. Apparently, both Mr. McLachlan and Mr. Borden fail to recognize the basic difference between a continuous and discontinuous function of time. The indication of the thermal wattmeter is absolutely exact except in so far as above indicated.

My paper is, perhaps, more unique for what it omits than for what it contains. For instance, in column 4, there appears a mathematical expression for the difference in the amount of heat that enters the two elements of the thermal wattmeter during normal operation. This expression is incomplete. The factor $\cos\phi$ should be added where ϕ is the phase angle between E and I. Instead of $2EIr$ as given it should read $2EIr\cos\phi$. In other words, the thermal wattmeter is a true wattmeter independent of power factor. I did not include the factor $\cos\phi$ in my paper because of the many questions which such an inclusion might have raised. For instance, at power factors other than unity, what is the best relationship between the factors E, I, r and $\cos\phi$? At unity power factor, the best relationship is to make the influence of the factors E and I approximately equal. However, we must bear in mind that at 50 per cent power factor, the influence of the factor I is twice that at unity power factor; at 25 per cent power factor, four times; at 10 per cent power factor, ten times. This naturally raises the question what is the best relationship between the factors E, I, r and $\cos\phi$ for the usual conditions of service that are met in normal operation. If all these questions had been discussed to the extent their merits dictate, my paper might have lengthened to many times its present length, and, in my opinion, it is already long enough. In view of this undue possible lengthening, it seemed better to omit the factor $\cos\phi$ from the expression in column 4.

Freedom from power-factor error is obtained only by exceeding care in the design of the small transformer used. The capacity of this small transformer is very small. In any transformer there is a phase difference between primary and secondary. The smaller the transformer, the greater, in general, is this phase difference. By careful attention to transformer design, I have been able to keep the phase difference in this transformer down to a matter of two or three minutes of phase angle. This phase angle is small enough so that the resulting power-factor error is negligible even on very low power factors. It is worthy of note that exceeding care must be taken in this small transformer design.

In column 14 there is given a mathematical expression for the indication of the thermal wattmeter. Due to my own inadvertence, the text of paper does not sufficiently explain the full significance of this expression. This expression gives the indication of the thermal wattmeter when a given amount of energy (watt-hours or kilowatt-hours) is passed through the meter in a given time t. Also, this expression holds true only if the wattmeter is indicating zero at the time the load is applied. If the meter indication is other than zero at the time of load application, the final meter indication is vastly different than if its indication is zero. If the rate of load application is lower than the meter indication at the instant of load application, the demand wattmeter indication actually decreases. Also, the expression holds true only if heat diffusion is assumed to be instantaneous.

I wish to call particular attention to the last sentence in my paper. I am strongly of the opinion that the time interval for demand measurement should be standardized. I see no reason why John Jones should be assessed for maximum demand on the basis of a time interval that differs from that of Jim Robinson. The real object of any demand assessment is to assess the service user an amount that will reimburse the utility for its fixed charges. It is perfectly true that if John Jones has a perfectly steady load, while Jim Robinson takes his load at a rate of ten times or 100 times that of John Jones, but during only a small fraction of his time interval, then Jim Robinson should pay more for his service than John Jones. The thermal-demand wattmeter automatically recognizes the difference between John Jones' method of taking service and Jim Robinson's. The standard block-interval meter does not.

It is further my opinion that the standard block-interval demand meter must be scrapped. When it is possible for a service user to reduce his maximum demand assessment to one-half his actual maximum demand, the method of maximum demand measurement by which this becomes possible must be scrapped. This is particularly true since it is quite possible for the service user to accomplish this end without the utility even becoming aware that it is being done. In my opinion, this scrapping should have occurred back in 1917 when the thermal-demand wattmeter first appeared. During this 25 years, the public utilities of the United States have expended something of the order of $100,000,000 for demand-metering equipment. However, perhaps there has been no large loss by the public utilities of the United States in this delay of 25 years in scrapping date since much has been learned concerning the character of the thermal-demand wattmeter during this 25 year period. For instance, in 1917 when the thermal-demand wattmeter was first produced, there was no known means of adjusting its time interval. The thermal-demand wattmeter was introduced into Canada about 1919 and has become the standard demand wattmeter of Canada. In Canada, however, the time interval is approximately 15 minutes—the time interval of the thermal wattmeter as first produced. The longer time intervals have not yet been introduced into Canada. In my opinion, 15 minutes is much too short a time interval for demand measurement. As indicated in my paper, I favor adoption, as standard, the maximum time interval now being used, 60 minutes. If the thermal type of demand wattmeter had been adopted as standard in the United States in 1917, the time interval would unquestionably have been 15 minutes and we in the United States would have found ourselves in the same position as Canada now is—too short a time interval.

In adopting a standard time interval we must also give due consideration to ammeters as well as wattmeters as pointed out in my paper. Shall the one-hour thermal-demand wattmeter be the one that arrives at 90 per cent of final in one hour, or 81 per cent? Although I have never actually produced a thermal-demand wattmeter that will arrive at 81 per cent of final indication in one hour, my experimental work has clearly indicated that there will be no difficulty in producing such a meter. I personally believe that it would be preferable to keep the wattmeter's time interval at the time to arrive at 90 per cent of final and change the ammeter's to 95 per cent, but my opinion on this question is not so fixed as upon some of the other questions discussed herein.

It might further be pointed out that the thermal-demand meter may readily be so connected as to indicate vars or kilovars as well as watts or kilowatts. It may thus be used to take account of the power factor of the load under measurement. I think rate makers will generally agree that at the present time, the power factor of the load is not recognized to the extent that it deserves. By the measurement of vars or kilovars as well as watts or kilowatts, power factor can be recognized to whatever extent the rate maker may think advisable.

In my paper, as well as in this closure, I have expressed some rather pronounced, perhaps radical, opinions. If anyone commenting thereon wishes to disagree with any of those opinions, here is the place, and now is the time to express those disagreements. I have quite often found myself in disagreement on the matters discussed herein, and a few more added to those disagreeing with me will be received with complete composure. Let me urge those disagreeing with any of my expressed opinions to have no hesitation in saying so.

The Acceleration-Oscillogram Method of Motor-Torque Measurement

Discussion and authors' closure of paper 42-5 by C. R. Atkinson and E. G. Downie, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, January section, pages 7–9.

R. W. Ager (Cornell University, Ithaca, N. Y.): I wish to welcome the paper by Messrs. Atkinson and Downie to the literature on acceleration testing. Every method has its own inherent advantages. The main advantage of the method using the velocity-time curve as suggested seems to be in the use of standard equipment and in the speed with which calculations can be made. However I would like to ask the authors what accuracy can be obtained by this method. The authors have not shown how widely scattered the calculated points might be, nor do they indicate how successive calculations made from the same film might check if, perhaps, made by different calculators.

I wish to call attention to the differences in the shapes of the two curves of Figures 6 and 7 of the paper. These were taken from tests[1] made at different voltages and, therefore, some difference in shape might be expected due to saturation. While in my tests I found some differences in curves taken with a similar spread of voltage, the curve shapes were not as varied.

The authors' curves show a dip in the torque slightly above 50 per cent synchronous speed. Similar results have been found in my tests on some motors. A dip can be caused by dissymmetry in the electric circuit of the rotor as indicated by Figure 2 of my paper. This curve was for a wound-rotor induction motor operated with an unsymmetrical controller. Pronounced dips were also found in the curves taken on a 15-horsepower synchronous machine. The three large synchronous motors tested, however, showed no indication of having such dips in the torque curves, although the accuracy of the tests was such that even slight dips would have been recorded had they been present.

The torque pulsations indicated in these tests can occur due to two causes. These are:

1. The salient pole structure of the magnetic circuit.
2. Any dissymmetry of the electric circuits on the two axes.

Both of these cause pulsations of the same frequency, namely twice slip frequency. A comparison of the pulsations indicated in Figure 3 of the paper with those indicated in Figures 1, 2, and 4 will show that the latter are much more pronounced. In Figure 3 the speed curve does not show a negative slope until synchronous speed is reached. From an electrical point of view there seems little reason for this difference.

A careful examination of Figures 1, 2, and 4 will show that in these tests the frequency of the pulsations did not vary greatly. Also, the same period was indicated for the torque transient at starting (except in Figure 4 where the transient was too small to show clearly). Figure 3 indicates a different behavior. The starting transient shows only slight pulsations, and the main pulsations between half and full speed have a continually decreasing frequency which is exactly twice that of the field discharge current. The pulsations of Figure 3 check with the expected performance, the others do not. In taking the curve of Figure 3 the motor was operated without the added inertia of the second machine which was used in the other tests. With the second machine coupled to the motor, a torsion pendulum was formed which was set in vibration by the sudden application of the starting torque and also by the torque pulsations when the latter were of the proper frequency. This resonance effect probably accounts for the building up of the large swings noted in the tests. The high shaft torques corresponding to the indicated values might be present in generator sets driven by synchronous motors. That they are so well shown in these tests would indicate an application for this type of testing.

REFERENCE

1. SOME ACCELERATION TESTS ON LARGE SYNCHRONOUS PUMP MOTORS, **R. W. Ager.** AIEE TRANSACTIONS, volume 60, June section, pages 416–22.

John F. H. Douglas (Marquette University, Milwaukee, Wis.): This paper is of interest to my colleague, Mr. E. A. Halbach of Marquette University, and myself, because we have been trying to perfect a generator and oscillograph which will measure and record torque-time and torque-speed curves. We submit two oscillograms, among our best, but still with too much ripple due to defects of generator construction. The first is the torque-speed oscillogram of a five-horsepower century induction motor started from the line. The second is the torque-time oscillogram of a synchronous motor. This shows in sequence the 50 per cent speed-torque dip, the increased torque on changing to full voltage, and the hunting pulsations caused by closing the field switch. We consider our oscillograms superior to oscillogram $5B$ of the paper, but inferior to those published by Dannath and Redfearn.[1] Dannath and Redfearn took extraordinary pains to build a ripple-free tachometer generator. Most of the other writers[2] on this subject also attribute the ripples to defective generator construction. When a Prony-brake retardation test of any motor is taken, and the ripples show up prominently, it is a good indication that the device is not sufficiently developed to measure real torque ripples of smaller magnitude.

The writers have undertaken a very difficult subject. While they furnish but little data on the device, it must be remembered that it would take a 500-microfarad capacitor, with a 100-volt tachometer, and a standard oscillograph, to record the acceleration of a motor starting in one second.

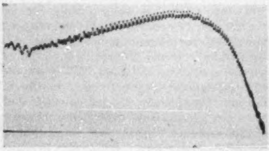


Figure 1

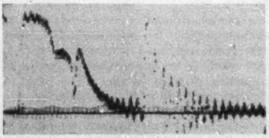


Figure 2

We believe the authors should furnish constructional features, the resistance and inductance, of the generator, the capacitance of the capacitor used, the sensitivity, resistance, and time constant of the oscillograph, if their generator is superior to Dannath's. In any case they should publish a sample Prony-brake retardation oscillogram to demonstrate that their system is free from undue ripples inherent in the tachometer generator. It is hoped that the discussion on this paper will bring out desirable specifications for an instrument or system of the type described.

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C. R. Moore. *Electric Railway Journal*, volume 35, February 5, 1910, page 227.

Ytterberg. *Elektrotechnische Zeitschrift*, volume 45 November 7, 1912, page 1158.

Knauer and Schultze. *Elektrotechnische Zeitschrift*, volume 43, October 26 and November 9, 1922, pages 1307–11, and 1357–9.

H. W. Bousman. *Michigan Technic*, January 1927, pages 20–32.

L. A. Finzi (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The determination of speed-torque characteristics of motors from acceleration oscillograms seems to deserve broader application on test floors. When average torque values are being considered, the calculation of acceleration by graphical differentiation of speed-time records may be sufficient. But for instantaneous values a direct electrical differentiation method is desirable.

Many methods including the one suggested by the authors have been used for this purpose. The authors record the charging current from a d-c tachometer to a capacitor. Errors are caused by the resistance and losses of the circuit. A more serious objection is the voltage pulsations from the tachometer collectors, which are difficult to smooth out and sometimes cause misleading ripples in the oscillographic record of the capacitor current. Other methods use mutual inductances to give secondary voltages proportional to the variations of the voltage applied to the primary.

Very good records may be obtained by applying the tachometer voltage to a D'Arsonval galvanometer, which has a second coil wound on the moving frame. The voltage induced in this second coil is recorded with the oscillograph. If α is the deflection produced in the galvanometer by a given instantaneous value of tachometer voltage, the voltage induced in the second coil by its motion in the field will be proportional to $d\alpha/dt$ and hence to the acceleration of the motor shaft. The construction of such a galvanometer (originally developed by Heiles at Karlsruhe University around 1924) requires certain care, as the inertia of the moving parts must be kept low, and quite a number of turns are required on the secondary coil. However, the adaptability of the instrument, when used according to the proper galvanometric technique, and the smoothness of the curves obtained make it very useful, especially when instantaneous peculiarities of torque curves are to be investigated.

Finally as a variation of the capacitor method, a small freely rotating d-c motor, with constant field and with the armature fed by the tachometer voltage, may be used. A change of speed of the tachometer causes a transient current which is again proportional to the acceleration or deceleration of the main motor shaft. In view of the comparatively large currents available in the measuring circuit, this method may be useful for such applications as acceleration governors. References 1 and 2 of this discussion may be of interest.

REFERENCES

1. **A. Ytterberg.** *Elektrotechnische Zeitschrift*, 1912, page 1158.

2. A METHOD FOR MEASUREMENT AND CONTROL OF ANGULAR ACCELERATION, **L. A. Finzi.** *Annals* of Naples University, 1935.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): The method proposed in this paper of using the acceleration speed characteristics to translate back into torque data is a very good and powerful one. Many things are being learned by it, but up to now the limitation has been in obtaining faithful speed indications. The authors are to be complimented on being able to produce usable records of speed through the means of their special tachometer generator. The use of such a generator will doubtless eliminate difficulties of other devices which introduce large losses of their own and thus mask the effects being sought. The direct acceleration record, through the charging current to a capacitor in parallel with this generator is especially interesting. The application of this generator to deceleration tests will extend the potentialities of that method to smaller machines. Another application which suggests itself is induction machine torque-speed tests, where torques that depart from conventional theory are only recently being recognized.

Of particular interest to this discusser are the torque curves obtained on small educational motor-generator sets, and their peculiarities discussed in appendix C of the paper. The peculiarities at about half speed which the authors found difficult to believe heretofore are altogether too common on small machines, and are particularly evidenced by the sounds which accompany this acceleration period. In the electrical engineering laboratory at Worcester Polytechnic Institute, most of the small sets exhibit some dips at half speed but usually pass by this point without difficulty. One machine, however, a 12.5-kva AHI synchronous motor, has shown particularly bad characteristics. At reduced voltage the machine would lock in at 58 per cent of synchronous speed and not accelerate further until thrown over to full voltage, even then being reluctant to pull up to full speed from that point. Incremental torque available for further acceleration was apparently zero at that speed.

Investigation of the factors giving rise to this extreme dip indicated that they relate to:

1. Applied voltage.
2. Resistance in the field circuit.
3. Temperature of windings.

The last factor only aggravated conditions on repeated tests, and so will not be discussed further. The circumstances of this machine were that it had recently been converted from use as an independent synchronous motor into a unit of a motor-generator set. The additional demands on its starting characteristics now made the problem acute where it had been only bothersome before.

Voltage taps were then changed from 65 per cent to 80 per cent of rated applied voltage with beneficial results but still with a long hesitation as it passed through the critical speed. Line-start procedure was then employed. These results indicated that voltage was not alone at fault, as the improvement was not in proportion to that expected from the voltage-square relation.

The field discharge resistor was 25 ohms originally. In desperation, it would sometimes be opened to permit the machine to accelerate. Obviously, this procedure was undesirable, but it proved the source of the trouble to be in the phase relationship between air-gap flux and rotor-circuit currents controlled by the short-circuited field. The resistance was changed successively to 50 and then to 75 ohms, the current at starting remaining substantially constant at six amperes. Improvements thus obtained indicated that the currents at standstill were being swung from nearly 90 degrees to nearer 45 degrees lagging with respect to the flux. The machine would thus accelerate easily through the dip point, which was raised to 60 per cent speed, and groans caused by torques were considerably reduced. The conclusion of this exploration would be that field circuits on synchronous motors should be not short-circuited but closed through the highest resistance that can be tolerated without endangering the insulation.

There is considerable similarity between synchronous-motor starting and induction-motor performance, and they are subject to similar methods of analysis. Torques are produced by components due both to fundamental frequency fluxes and to the harmonic fluxes which give rise to stray load loss. The resultant torques obtained by this complex pattern would seem to this discusser to permit resolving them into positive sequence (forward) torques due to the amortisseur winding and field circuit currents, and negative sequence (reverse) torques due to harmonics and other field circuit effects. Below half speed the positive torques predominate, being aided by field effects. At half speed, certain of the forward factors become reversed and aid the negative torques until, at some speed slightly above 50 per cent, the two opposing torques produce only enough resultant torque for the requirements of that critical speed, with none remaining for acceleration.

It is to be hoped that the "tools" developed and the interest aroused in this subject will lead to a clarification of the factors involved in a-c motor starting torques, as well as the many other studies which this acceleration-oscillogram technique will make possible.

R. M. Saunders (University of Minnesota, Minneapolis, Minn.): It would be interesting for the authors to point out the differences between their method of torque measurement and the acceleration method outlined in the "Proposed Test Code for Synchronous Machines" (1937), page 20, paragraphs 130 and 131.

In Figures 6 and 7 of the paper the authors show a disagreement of as high as 100 per cent in the torque measurements on the machine under test and suggest that these errors are due to heating of the armature and amortisseur windings. If this were true, the resistance of the amortisseur winding would be approximately doubled, and the temperature rise associated with this doubling of the resistance would be in excess of 250 degrees centigrade. This seems to be out of line with the usual industrial practice in regard to limitation on the temperature rise of cage windings. The writer would like to ask the authors if some other errors, such as transient effects, could not have crept into the other torque measurements.

In using the authors' form of the acceleration method, the accuracy depends upon two factors: the WR^2 and the readings of $\tan \theta$. In regard to the former, variations in the casting of spiders often make the calculation of the WR^2 subject to variations of as much as ten per cent. Extremely accurate calculations may be made and the result should be within five per cent. In regard to the latter quantity, the values taken from an oscillogram are apt to be in error due to the small physical measurements, the width of the trace, and the actual calibration of the oscillograph. In the writer's opinion these could easily amount to a ten per cent variation in the final result, even if the observers were careful. With the Prony-brake method an experienced test floor man can duplicate his results within ten per cent. Hence, the Prony-brake method should be equally accurate and somewhat more direct, which seems to make it preferred on machines up to 500-horsepower.

C. R. Atkinson and **E. G. Downie:** Interest has been shown in the exact electrical characteristics of the d-c tachometer generator used in our acceleration torque tests. The machine has a resistance of 51.7 ohms, inductance of 0.512 henry, and generates 0.155 volt per rpm. When running at 1,700 rpm, generating 263 volts d-c, it was found to have principal ripple voltages of 0.25 volt at 56 cycles or double rotational frequency, and 2.3 volts at 680 cycles commutator frequency. A ten-microfarad capacitor reduces the commutator ripple voltage to 0.04 volt (invisible on the speed trace), yet does not affect the amplitude of generated frequencies below 40 cycles. To get a reasonably smooth trace of capacitor charging or acceleration current requires 100 microfarads across the tachometer generator output, in addition to the smaller capacitor in series with the galvanometer. Response is satisfactory then, only to frequencies of 20 cycles and less. Slightly higher frequencies are amplified by series resonance, and still higher are greatly reduced.

We have been interested in the average

torque curves of synchronous motors, and have found the speed curve with slope measurement simpler to take and satisfactory in results. All calculated points fall almost exactly on the curves as drawn in Figures 6 and 7 of the paper. Torque pulsations are averaged out of the speed curve before scaling. The over-all accuracy should be ten per cent or better. We question the possibility of equal accuracy or even the practicability of full-torque-curve Prony-brake tests on motors up to 500 horsepower, as suggested by one discusser.

The oscillogram of Figure 5 of the paper was taken with 12 microfarads in series with the acceleration galvanometer, and a 100-microfarad filter capacitor. Resonance in the tachometer-capacitor circuit is calculated at 21 cycles, with maximum capacitor ripple voltage rise of 31 per cent over the generator voltage. The oscillation frequency of the acceleration trace, at its maximum amplitude, agrees with the synchronous-motor pole slip frequency, and also with the double rotational ripple frequency of the tachometer. This agreement, with proximity to circuit resonance does leave the amplitude of the acceleration oscillation questionable. We have noted with interest the methods of acceleration indication described by discussers and intend to investigate their possibilities.

We are pleased to note the agreement of discussers with our findings of the synchronous-motor torque dip at 60 per cent speed. Mr. Ager's explanation of the increased torque pulsations in Figure 2 of the paper, over those in Figure 3, appears very plausible. The homopolar generator used by Dannath and Redfearn is doubtless the most ripple-free yet devised, but the required d-c amplifier considerably complicates the system. Absolute linear response of the amplifier must be assured, or any deviation known and corrected for.

Design of Long-Scale Indicating Instruments

Discussion and author's closure of paper 42-70 by A. J. Corson, R. M. Rowell, and S. C. Hoare, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, June section, pages 318–27.

M. S. Wilson (General Electric Company, Schenectady, N. Y.): The development of the complete line of long-scale instruments represents an outstanding accomplishment in the measurement art.

The goal the instrument designer is continually striving for is to obtain the maximum accuracy in the smallest size instrument, having a readability which is suitable for the application.

The table below shows a comparison of the General Electric long-scale instrument with the conventional instruments, on the basis of area versus scale length. This shows a nearly two to one gain in scale length for the same size instrument, and a scale length nearly 50 per cent longer than the six-inch-size instrument requiring more than twice the switchboard area.

It is interesting to note that full advantage was taken of the newly developed materials in each of the different types of elements. It would have been very difficult to have developed instruments of this type having satisfactory characteristics without the use of these materials. One important point in the use of magnetic materials in the field circuits of instruments is that the mechanical structure be designed so that it is mechanically stable, and where air gaps are necessary, they be sufficiently large so that minute shifts in the structure will not adversely affect the operation or accuracy of the instrument. The writer is familiar with the designs described by the authors and considers this detail to be well taken care of.

Table I

Type	Switchboard Area Required by Instrument (Square Inches)	Scale Length (Inches)
General Electric long-scale types *AB*-10 and *DB*-10	18.1	6.8
Typical 6-inch rectangular-size instrument	44.5	5.1
Typical 4-inch rectangular-size instrument	18	3.5

It is particularly noteworthy that no sacrifices in the instrument characteristics were made in order to obtain the long-scale feature, in fact, in two important respects, (*a*) in torque to weight ratio, and (*b*) shielding, the figures are better than usually found in switchboard instruments. In only one instrument, the temperature indicator, is the torque to weight ratio less than one. In this instrument it is 0.92 as compared to 0.5 which is frequently found in this class of instrument.

With the higher currents in conductors near the switchboard, the shielding of an instrument becomes of greater importance to the user. In the instruments described by the authors the errors shown in the Table 2 under "Stray Field Influence" do not exceed one-half the permissible error given in American Standards Association Standards, and for the majority of the instruments, it is appreciably below one-half the value given by ASA.

The other characteristics such as response, temperature coefficient, overload performance, and so on, are likewise such as to entirely meet the requirements of the various applications, where a universal instrument of this type may be used.

One further point which is of considerable importance to the user, but which is not covered in any existing specifications, is the stability of the mechanical structure of the instrument element. This is very important since it determines whether or not the instrument will operate satisfactorily maintaining its calibration over long periods of time. To do this the clearances between moving and fixed parts must be correctly and permanently maintained.

This means that the element must not only be so designed that it is structurally stable but the different materials must be carefully selected from the standpoint of temperature coefficient of expansion and aging characteristics so that they will not adversely affect the operation of the instrument even when subjected to severe operating conditions.

Paul MacGahan (Westinghouse Electric and Manufacturing Company, East Orange, N. J.): The paper by Messrs. Corson, Rowell, and Hoare is of special interest as another example of the fact that progress often proceeds in spirals—advancing bit by bit, after turning and returning to almost the same point.

Thus, the writer in his paper presented in June 1912, at the annual convention described a complete and harmonious line of long-scale a-c instruments.[1] A reference to this paper might with advantage be added to the bibliography given in the present paper for the sake of the record and to indicate the progress of the art.

In the discussion of the 1912 paper, Mr. F. P. Cox of the General Electric Company stated as follows: "The long scale which is inherent in the type is a desirable feature, but we must not neglect to take into account that what we want to know is the current in the circuit, that if you can read an instrument beyond its limits of accuracy, or if you have an instrument which has an error greater than its reading capacity, you equally get into trouble. What you want is an instrument which will give you, so far as you can read it, the combination of being readable and possessing an accuracy which will give the closest approximation. . . ."

Mr. Cox was right in 1912 in pointing out that a scale length which gives a readability greater than justified by the accuracy of the movement is unwarranted. In my closure I stated that the long scales were an advantage because instruments had to be read from a distance. But, as time went on, the old-fashioned switchboard gave way to the compact control disk type with instrument panels which brought the instruments close to the operator. For this reason as well as for economic and application reasons, the long-scale induction instruments of 1912 were by 1920 superseded by more modern types having scale lengths well calculated to be normal with respect to the accuracy of the measuring element.

A little analysis of the problem of scale-length factor in over-all accuracy bears upon the fact that with the highest grade of construction a certain amount of side shake in the bearings is inevitable because of the end-play requirements in the ordinary horizontal-shaft instruments. This could be and sometimes is reduced in vertical-shaft portable instruments by reverse-pivoting, with the top pivot supporting the weight. But such structures are obviously impractical in switchboard types, if we except the older forms of horizontal edgewise instruments.

The end play and consequently the side play has approximately the same value in all ordinary sizes of practical instruments. This side play is generally of the order of 0.010 inch and allows this amount of uncertainty in the pointer position. The result may be approximately expressed as amounting to an error equal to $1/L$, where L is the scale length in inches.

This then is that portion of the total error in a well-designed instrument which can be reduced by increasing the scale length.

Compared to the usual and most common

form of rectangular switchboard instruments which are about $5^1/_2$ inches square with scales five inches long, the authors have increased the length of scale to 6.8 inches, by using the circular dial construction instead of the flatter 100-degree dial design. To do this has required the development of entirely new measuring elements, a job, the magnitude of which, is not generally appreciated by the user.

A one per cent accuracy rating amounts to over $^1/_{16}$ inch on a 6.8 inch scale. Switchboard instruments having the usual one per cent accuracy rating are also produced with scale lengths of $3^1/_2$ inches in rectangular cases of the size described by the authors. As one per cent of such scale lengths is about $^1/_{32}$ inch they are quite adequate for the rather less frequent applications for which a four-inch switchboard instrument is preferred to the $5^1/_2$-inch type, and where readings within one per cent may be taken with the instrument not over three or four feet from the observer.

REFERENCE

1. INDUCTION-TYPE INDICATING INSTRUMENTS, **Paul MacGahan.** AIEE TRANSACTIONS, volume 31, 1912, pages 1565–77; discussion 1599–1608.

A. J. Corson: Mr. MacGahan has directed attention to his early interest in long-scale indicating instruments, and specific reference is made to his paper "Induction-Type Indicating Instruments" presented in 1912. In our examination of prior art, this reference was overlooked, but it will be promptly added as he suggests.

The remainder of his discussion originates the interesting question—how shall a development of this character be evaluated? Firstly, the design methods and mechanisms disclosed in the paper are perfectly general in character, and their application to instruments of a $4^1/_4$-inch rectangular dimension is, in a sense, incidental. Opinions as to the importance of this particular size of instrument may differ. We believe it to be of considerable moment as a contribution to more compact switchgear and better instrument grouping on panels. From the designer's point of view, however, the essential point is the extension of the scale length from 90 to 240 angular degrees, resulting in an approximate two to one gain in terms of linear measure, in any instrument of given size.

The question then resolves itself into a determination of the added value of this long scale. Firstly, both general considerations and test results show that long scales are observed more accurately than short ones. This gain in visual accuracy occurs twice, once in calibration and once in the observer's reading. Secondly, errors due to side motion of the moving system are reduced, as has been pointed out. In addition, the moving system is balanced with greater precision in terms of scale length. These and similar considerations have led to the almost universal adoption of long angular scales in such nonelectrical instruments as temperature indicators, mechanical tachometers, pressure indicators, and liquid-level gauges. While the application of long-range indication to electrical measuring instruments is admittedly more difficult, this would hardly appear to constitute valid grounds for attacking the desirability of the long scale as such, particularly when it is demonstrated that the design problems can be adequately solved.

The foregoing discussion assumes long-range indication to be obtainable without loss in factors which determine accuracy. An examination of the performance data shows this assumption to be generally true. Mr. Wilson's discussion is directed principally to these performance characteristics and brings out interesting points relative to torque to weight ratios, shielding, and damping. The various instruments have been designed to meet, and do meet, existing standards of performance, an accomplishment which was made possible by adhering to the fundamental types of mechanisms for which these standards were established.

The Fundamentals of Industrial Distribution Systems

Discussion and author's closure of paper 42-21 by D. L. Beeman and R. H. Kaufmann, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, May section, pages 272–9.

H. G. Barnett (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As a part of the discussion of the basic factors involved in distributing power in an industrial plant, Mr. Beeman and Mr. Kaufmann have made a very interesting cost analysis of a particular industrial plant. This analysis is specially interesting to me because of a large number of similar studies which I have helped to make both before and after Mr. Powel and I proposed the industrial-plant network idea before the Institute at the last winter convention (AIEE TRANSACTIONS, volume 60, 1941, April section, pages 154–6).

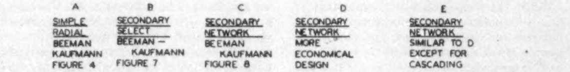
These studies covered a wide variety of plants. The load densities ranged from about 5 to 25 volt-amperes per square foot. All commonly used utilization voltages (120/208, 220, and 440 volts) were involved. Total loads ranged from 1,000 to 10,000 kva.

The results of these studies do not justify the conclusion that any one type of system can be expected to adequately meet the requirements in a large percentage of cases, although the authors have claimed that as the purpose of their analysis of a particular case. The wide variations, among actual plants, of voltage and arrangement of the incoming supply circuits, plant layout, utilization voltage, type of loads, and other factors make it impossible to use any one system in more than a minority of cases.

These studies do not confirm the wide difference of cost between the secondary network and the secondary selective and simple radial systems that is shown by Mr. Beeman and Mr. Kaufmann. Therefore, we have made a study of the plant described in the paper using the same loads, voltages, and requirements set forth in the paper. Cost estimates were based on using drawout circuit breakers in the 440-volt switchgear units. The relative costs shown by this analysis are shown in Table *A* along with the corresponding costs found by Mr. Beeman and Mr. Kaufmann. We have been able to check the authors very closely in the case of the secondary-selective system when the simple radial system is assumed to cost 100 per cent. However, our analysis shows a cost of 168 per cent for the secondary network system used by Mr. Beeman and Mr. Kaufmann which, according to their analysis, costs 175 to 200 per cent of the cost of the simple radial system. By using four transformer and switchgear units instead of six and using the same type of equipment used in the six unit network, the cost of the network is reduced to 156 per cent instead of 168 per cent of the cost of the simple radial system. By making use of cascading, which is sometimes advocated, the cost is further reduced to 150 per cent.

If it is assumed that the plant studied by the authors, because of reliability, flexibility, or other factors, requires a better system than the simple radial, the network becomes economically comparable to the other systems that can be used. For example, if the cost of the secondary-selective

Table A



	A SIMPLE RADIAL BEEMAN KAUFMANN FIGURE 4	B SECONDARY SELECT BEEMAN – KAUFMANN FIGURE 7	C SECONDARY NETWORK BEEMAN KAUFMANN FIGURE 8	D SECONDARY NETWORK MORE ECONOMICAL DESIGN	E SECONDARY NETWORK SIMILAR TO D EXCEPT FOR CASCADING		
BEEMAN KAUFMANN STUDY	100	145-150	175-200				%
		100-103	121-138				%
CHECK STUDY	100	143	168	156	150		%
		100	118	109	105	117-108	%

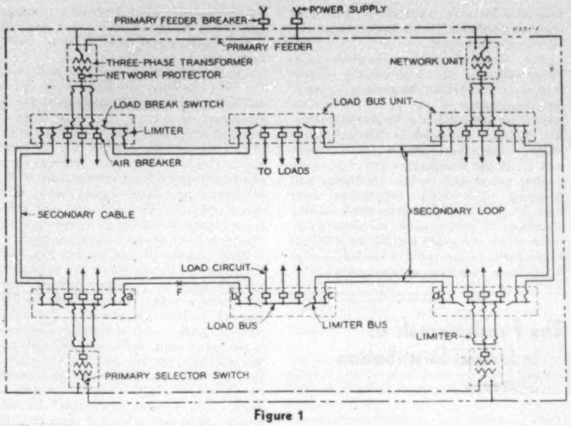


Figure 1

system is taken as the basis of comparison, the cost of the secondary network system ranges from 105 to 118 per cent as shown in Table *A*. The cost of 118 per cent is for the network system used by the authors for their comparisons. Ten other arrangements of the network system using limiters, primary selector switches, cascading, as well as full interrupting capacity breakers in various combinations, prove to be less expensive than the network chosen by the authors. The difference in cost between a network and the secondary-selective system is so small that the two can be compared, for practical purposes, on the basis of their respective operating characteristics.

All the comparative costs shown in Table *A* are based on using drawout breakers in the switchgear assemblies. If fixed breakers are used, the total cost is reduced in all cases but the relative costs are essentially the same as shown by the per cent figures shown in Table *A*.

The authors of the paper have expressed some doubt as to the limiter being a first-rate protective device. A thorough understanding of the characteristics of the limiter and its application should remove this doubt. The secondary loop in the industrial-plant network system is, in effect, a distributed bus extending throughout the load area. This extension of the bus and the consequent increase of the probability of faults on this bus make it necessary to provide means for isolating a faulted section of the loop. The device to do this should be as simple and reliable as possible, because it probably will operate only very infrequently, possibly once every several years.

Since the loop protective equipment is likely to operate very infrequently, maintenance and inspection are apt to be neglected. Lack of maintenance is no hazard to the operation of the limiter. Since the protective device operates only for a fault in the loop circuit, replacing a blown limiter does not materially increase the time required to restore the faulted circuit to operation, because the damaged conductors must be repaired at the same time. Immediate reclosure of the circuit is not even desirable. Therefore, the limiter presents no disadvantage and is actually somewhat more reliable than a breaker. The reason for using limiters in the loop is not a matter of cost but a question of basic system operation.

The correct operation of the loop protective devices depends on the use of parallel conductors in the loop as shown in Figure 1. When parallel circuits are used, a fault such as at *F* in Figure 1 causes only the limiters *a* and *b* to blow. If a single conductor per phase were used, limiters *c* or *d* or both would blow instead of *b* with the result that service from one load bus would be interrupted. If a load bus is established only at transformer locations, the load interruption would be less likely to happen. However, if a transformer should be disconnected from the loop for some reason, the same undesirable situation would exist as described in connection with Figure 1.

Three points will summarize this discussion.

1. Experience shows that no one system can be expected to adequately meet all the requirements in more than a small part of actual cases.

2. Whenever reliability, flexibility, or other operating factors require a better system than a simple radial scheme, the network frequently will be comparable in cost to other systems that can be used.

3. Limiters in the secondary loop have a basic operating advantage over the use of breakers; reduction of cost is not the fundamental reason for using limiters.

John S. Parsons (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors state that their purpose in writing this paper is to obtain the adoption of one system of distribution for industrial plants. Because of the large number of types of industrial plants and the many requirements and conditions peculiar to the various types, the use of any one type of distribution system in all of them is fundamentally unsound and impractical. We might just as well say that one type of distribution system should be used in the commercial areas and the residential areas of all cities and towns, and in all rural areas. As a result of the large amount of work we have done in connection with distributing power in industrial plants, one general conclusion can be definitely drawn. There is no one type of distribution system which should be used in all plants.

Also, as a result of our work on this subject, we have come to the conclusion that the simple radial is the cheapest system which can be used, and the secondary network is the best system, but it is by no means universally applicable. During the last year we have made distribution studies for more than 60 specific plants. As a result of these studies we recommended the secondary network system for about 60 per cent of these plants and some form of radial system for the others. Network systems are now in operation or being installed in about 25 per cent of the plants studied.

A paper dealing with the fundamentals of industrial distribution systems should cover all of the major forms of systems which are in use. The authors, however, in a fundamental paper on the subject have completely ignored the primary-selective network and spot network systems. These two forms of the secondary network system have been used more extensively in industrial plants than has the simple network system covered in the paper. The primary-selective and spot network systems offer practically all of the advantages of the simple network system, and usually at lower cost.

In comparing those forms of distribution systems covered by the paper, the authors place much emphasis on initial installed cost. We have found that for those plants where the simple radial system does not provide sufficiently reliable service, and there are very many such plants, we are in almost all cases talking about an initial cost difference of from 5 to 20 per cent between those systems which might satisfactorily be used. The cost of a plant distribution system will generally represent 10 per cent or less of the total investment in the plant. We are, therefore, talking about a difference in plant investment of approximately 0.5 to 2 per cent. Initial installed cost certainly should be considered when comparing plant distribution systems; however, it should not be over emphasized.

There are many factors which have a bearing on the installed cost of a distribution system. One such important factor is the matter of diversity. Yet diversity has not been discussed in this fundamental paper. When compared with a plant where no diversity between load centers is assumed, that is, a diversity factor of one is assumed, a plant having an appreciable diversity between load centers, such as a shipbuilding plant for example, will result in a higher installed cost for the radial systems and spot network and a lower installed cost for the simple network and primary-selective network systems. Even in those plants where the diversity is small or unknown at the time the distribution system is designed and is not considered in the design of the system, diversity will be reflected in better regulation and lower losses plus ability to carry more load if a simple or primary-selective network system is used. We have found that diversity and other factors peculiar to specific plants may cause the relative costs given in the paper to vary to the point where a secondary network system is less costly than the secondary-selective radial system.

When considering the cost of different forms of distribution systems, initial installed cost is not the whole story. The cost of meeting growing and changing load conditions over a period of years should be considered. This is of particular importance today, because the plants being built and revamped to turn out defense material will, when the war is over, require considerable modification to fit them for peacetime production. The flexibility of the simple and primary-selective network systems enables them to meet growing and changing load conditions at considerable less cost than do the radial systems and particularly the secondary-selective radial system.

The operating and maintenance difficulties with the secondary network system as discussed in the paper are, in our opinion, greatly exaggerated. This opinion is based on experience with network systems both in industrial plants and in a large number of small cities throughout the country. The great majority of operators we have talked to would rather operate a network system than a radial system. It certainly does not require a crew of supermen to operate and maintain a network system.

Our years of experience with all forms of distribution systems have taught us the danger of drawing general conclusions from one or even a number of distribution studies. Any industrial plant of appreciable size should be specifically studied and the form of distribution system which best meets its individual requirements should be used, whether it be network or radial or a combination of the two. The decision as to the proper system to use should not be made on the basis of generalizations based on the study of one or several specific plants, unless they are practically duplicates of the one under consideration.

Charles P. West (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have analyzed all the commonly used distribution systems for low-voltage power in industrial plants. Comparisons are made on the basis of cost, performance, reliability, and other factors. They recommend the secondary-selective circuit arrangement (Figure 7 in their paper) above all others and summarize their findings under five headings. The following comments are offered on their conclusions.

1. Installed costs are compared on the basis of the secondary-selective system being 150 per cent of the simple radial system base and the network system 200 per cent. Cascaded breakers are used to justify 25,000-ampere interrupting-capacity breakers for the former while 50,000-ampere breakers are specified for the latter. Cascading also can often be used here to reduce the size of the breakers. The network system is also subject to modification in a similar manner to that discussed for the secondary selective system, if parts of the areas supplied can sacrifice power for short intervals. This would tend to equalize the cost of the two systems.

2. The network system circuits shown by the authors do not show loop load-break isolating switches. These should be recommended, and when used, the loop circuits can be isolated for maintenance. A network system with these switches is as safe as the selective arrangement which is advocated, because there is no more necessity for working on live apparatus. In all cases it should be assumed that first-grade equipment will be applied, not "second-rate."

3. The authors concede "the highest order of service reliability" to the network system. It surpassed their recommended system in this respect. The fundamental network arrangement in the power supply and tie circuits unquestionably permits clearing faults with less disturbance to the continuous operation of a plant than any other system.

4. A network system engineered so that current distributes properly is no more difficult to operate than a selective circuit arrangement. This problem exists in any electrical network, and no difficulties have been experienced in the systems which are operating.

5. Voltage regulation is conceded to be good in both systems.

On the whole, the network system has the advantage in the important third point, reliability, and does not suffer by comparison in the others.

H. L. Rawlins and **P. O. Langguth** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. The authors have presented an interesting analysis of industrial distribution systems wherein four types of distributed load-center arrangements are described. As a result of this comparison, they have recommended a secondary-selective scheme of distribution.

2. To clarify this situation, we are adding a form of distributed load-center system, which consists of the secondary network with limiters. This arrangement will have costs comparable to the secondary-selective circuit arrangement with a service reliability rating of *A*. Safety requirements are completely fulfilled by the use of a knife switch or load-break switch between the cable limiters and the load bus. The particular section of faulted cable and limiters can be serviced dead with no interruption of service by simply opening the switch at each end of the involved cable section. Similar results can also be obtained by the use of disconnecting-type limiters.

3. The necessity for an indicator on limiters has not yet become apparent. There are several convenient means of determining if an open circuit exists in a cable run. Occasional check with a clip-on ammeter has been employed satisfactorily on network systems. No problem, however, is presented if an indicator should be required, because suitable indicators in various forms have been available over a period of years.

4. We note that Messrs. Beeman and Kaufmann describe the limiter as "second-rate apparatus." We might mention that a fusible element which functions directly by heat generated within itself is recognized as the most reliable of all over-current devices. This principle is fundamental and has been used over a long period of years in various forms with complete success.

5. There is a new development in this picture which has not yet been disclosed to the Institute. It consists of a new high-current interrupting device which simplifies the use of a network distribution system. This is a noteworthy development and places an entirely different complexion on the situation.

6. Briefly, this limiter is totally enclosed to prevent external demonstration of the device when clearing a fault. It has repeatedly interrupted currents in excess of 50,000 amperes at 600 volts with no perceptible noise or visible demonstration. A typical oscillogram is illustrated here of an interruption of 52,000 amperes with a restored voltage of 614 volts. The extremely short arcing time should be noted. Numerous tests in the high-power laboratory over a large range of currents have demonstrated the dependability of this limiter both in respect to time-current characteristics and its ability to interrupt any current within its rating.

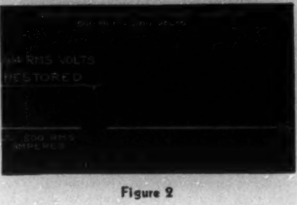


Figure 2

7. The authors of this paper have stated by implication that the secondary network systems utilizing limiters lack reliability and are unsafe. We who have worked with this new form of 600-volt totally enclosed limiter, and who have seen it work repeatedly under exhaustive tests, are confident that the secondary network system utilizing such devices will normally be favored in industrial distribution systems.

E. M. Hunter and **J. C. Page** (General Electric Company, Schenectady, N. Y.): In laying out low-voltage a-c systems, frequently the two most difficult points to be settled are:

1. The basic type of system.

2. The general class of equipment to be used in the step-down substations.

Once these two important general points have been decided upon, the remaining details can usually be worked out easily. It is with respect to these two items that this paper by Messrs. Beeman and Kaufmann should prove valuable to persons engaged in laying out low-voltage a-c systems for industrial plants.

It is interesting to note that a system cannot be judged fairly unless both the basic circuit arrangement and the general class of equipment used in the circuit are judged simultaneously. For example, a low-voltage network, with open-type bus work or wiring, circuit breakers of inadequate interrupting rating, or fused knife switches, is probably considerably less reliable than a simple radial system incorporating properly insulated wiring, metal-enclosed load-center unit substations, and with circuit breakers of adequate interrupting rating.

Attention is called to Table I of the paper. The simple radial system in the tabulation is given a triple-*A* rating, and is marked down only on "service reliability." It will be of interest to see how this service reliability rating can be improved. The basic principle of load-center distribution is to divide the entire factory load area into small areas, each with its own substation. In some of the defense plants that have been installed, as many as 30 load-center unit substations have been used. Each of these units have been made alike in kilovolt-ampere rating, and so on, so that one spare load-center unit kept in the store room provides the means of restoring service to any area and without additional investment in spare capacity in each of the units. Sometimes these spare units are made portable. From this it is apparent that the simple radial type of system need not be condemned too severely for poor service reliability if the proper precautions are taken.

The network type of system is the highest in cost and has an *A* rating which is the highest in service reliability. It is the opinion of the discussers that the network type of system can be justified only when the ultimate in service reliability is required. The high cost of a network results, at least in part, in the fact that the secondary circuit breakers must have higher interrupting capacities than those used on the other three types of systems. Cost reductions obtained by the use of devices of inadequate interrupting capacities on the secondary of the network have been proposed in some applications. These methods, of course, lower the service reliability rating of the network, and it appears that they have no place in the network system.

A. E. Anderson (General Electric Company, Philadelphia, Pa.): The items of safety to operating personnel and continuity of service have been referred to in the paper. These, with other factors, are of considerable importance, particularly under present conditions, when such great importance is being placed on the nation's manufacturing facilities.

The use of circuit breakers in the high-voltage sides of the transformers in place of disconnecting switches as shown in the various schemes, Figures 2, 4, 5, 7, and 8 of the paper, would make possible a safer switching scheme and one more desirable from an operating standpoint. Along with protective relays it would provide additional protective and operating advantages.

At one time the value of short-circuit current obtainable from a low-voltage a-c system was generally considered as limited, regardless of system capacity. Fairly recent investigations have shown that these currents can be readily calculated, and that the calculated and measured values agree within usual engineering tolerances. Thus, the procedure has changed from something rather vague and mysterious to something that is rational, straightforward, and easy to apply.

The authors have recognized the latter procedure and have provided air circuit breakers of adequate interrupting rating for the various low-voltage circuits. It is further recognized that heavy short-circuit currents are to be expected if the voltage regulation is to be held to low values. While the above consideration points to circuit breakers of higher interrupting rating, there are two major factors that tend to counteract this tendency, without a sacrifice of other desirable operating features. One is the smaller distributed load center, with its saving in I^2R losses and flexibility, as compared with a single large substation. The second factor is the use of circuit breakers in cascade, which permits the use of a breaker, of interrupting rating less than that corresponding to the current obtainable at the particular location, provided it is backed up by a properly co-ordinated breaker, whose interrupting rating is equal to or greater than the available short-circuit current. No longer is it necessary to make a haphazard selection of circuit breakers for these low-voltage systems, with the possibility of running considerable risk of damage to the breaker and associated equipment, in the event of a short circuit near the outgoing terminals of the circuit breaker. It is quite important that breakers of adequate interrupting capacity be used. This means feeder breakers capable of interrupting the maximum short-circuit currents of the circuits in which they are connected, or feeder breakers of somewhat less interrupting rating in cascade with backup breakers of full interrupting rating.

The authors have included a number of pertinent items relating to the use of limiters, or limiters and isolating switches. In many applications the introduction of limiters will raise the short-circuit requirements of the feeder breakers, since they do not permit the cascade connection to be employed. In other words, the time of operation of the limiters with which the discusser is familiar is so great in relation to that of the modern feeder breaker, that it cannot give the desired degree of backup protection. Accordingly, the interrupting rating of the feeder breakers must be increased from some smaller value, permitted by the cascade connection, to a rating at least equal to the available short-circuit current. Consequently the introduction of limiters will, for most applications, increase the size and cost of the feeder breakers.

W. J. McLachlan (General Electric Company, Schenectady, N. Y.): The one most important conclusion drawn from the Beeman-Kaufmann paper is that, while some form of load-center distribution system usually will be found superior for plant supply, no one form of system is universally applicable. The best form for a given case depends principally on *cost* versus the value of *service continuity*, and the selection of the best form requires a rational evaluation of these items. It is to be hoped that the availability of this paper will encourage such rational evaluations, evidence of which has been lacking in an unfortunately large number of cases in the immediate past.

In evaluating the relative *costs*, it is obviously desirable that the forms of systems be costed on a comparable basis. The one item of greatest discrepancy in past comparisons has been the selection of low-voltage breaker interrupting capacities. Systems of one type, with breakers selected with interrupting capacities at least equal to calculated short-circuit currents, have been compared with another with the interrupting capacities grossly below calculated short-circuit values. Investigations, which have shown that calculated currents can be obtained in low-voltage circuits, point to the desirability of selecting interrupting capacities which are not below calculated values. However, if one elects to take the risk of applying lower rated breakers in one system, all logic requires that the same degree of risk be accepted in selecting the breakers for all forms of systems, if the cost comparison is to be equitable and fair.

When evaluating *service continuity*, it is essential that the over-all continuity of the system be considered. The form of load-center system selected can affect only part of the possible service outages. The total power outages result from failures in the following portions of the system:

1. Transmission supply to the plant.
2. Plant-supply stepdown substation.
3. Plant-distribution primary feeders.
4. Load-center unit transformers.
5. Low-voltage feeders from load-center units to individual load areas.
6. Laterals to individual loads and individual load and control equipment.

In general, only those outages due to failures in the plant-distribution primary feeders and load-center unit transformers are influenced by the form of load-center system selected. If the majority of the outages are expected to be due to failures in the other parts of the system, the total outages can be influenced by but a small degree by the form selected and differences in reliability of the types of systems described are of proportionately small significance. As an example, several applications of the more expensive secondary-network system have come to attention in plants supplied by only one long high-voltage transmission circuit. In such cases, the probability of failures of the entire plant supply is several times that of failures in the plant-distribution primary feeders, making it appear that little effective benefit will be derived from the added cost and operating complication of the network system.

On the other hand, if the expected interruptions in the primary supply system are rare, which presupposes at least duplicate transmission sources, and probably dual supply substations, the service reliability of the distribution system takes on increased significance. Even then, it should be remembered that, regardless of the form of load-center system selected, the outages due to failures in the low-voltage feeders and laterals and in the individual load and control equipment will be unaffected by the selection made.

These comments are made to encourage rational evaluation in the selection of the system to be employed in specific instances. It is believed that this desirable condition will be considerably enhanced by the availability of the comparative data in the Beeman-Kaufmann paper.

R. H. Kaufmann: It is indeed gratifying to note the interest which is displayed in the subject of industrial power distribution. It is indicative of an increasing consciousness of the importance of this phase of the industrial electrical problem which is the first step leading toward a more general recognition of the important part which the distribution system plays in the performance of electrified industry.

The debatable issues which have been raised in the several interesting and valuable discussions center largely on the selection of the particular form of load-center distribution to be used, and more specifically a consideration of the secondary-selective system versus the secondary network.

The secondary-selective system has a distinct advantage in respect to electrical simplicity and safety of operation. Each main low-voltage bus section has access to two independent power-supply channels which insures reliability of service. The secondary-selective system obviously will not be used to the complete exclusion of all others. A very considerable number of present industrial systems are adopting the straight radial form. The primary-selective system will find application in some instances, particularly for load areas involving scattered independent buildings each requiring only a small amount of power which will hardly justify more than a single load-center unit. The secondary network system will likewise find fields of application, particularly for continuous process operations in which even a momentary loss of voltage would result in large consequential losses either in the form of damage to productive machinery or spoilage of large quantities of material.

Variation in System Form

It has been intimated that the form of secondary-network system illustrated in Figure 8 of the basic paper fails to realize as good an over-all economy as can be obtained with other variations.

Mr. Barnett, for instance, suggests the incorporation of the primary selective feature as illustrated in Figure 1 of his discussion. The fundamental difference lies in the use of two primary feeders instead of three and the addition of primary transfer switching equipment at each load-center unit. Relative to the straight radial system as in Table I of the basic paper, the primary selective-secondary network system shows a first cost level of 185 per cent as compared with 175 per cent for the system illustrated in Figure 8 with no improvement in other characteristics. In fact, some definite disadvantages must be accepted:

1. Both primary feeders must come in close proximity at each load center, which provides a number of vulnerable spots at which a disturbance could result in a complete power outage for the entire area.
2. Operating hazards as described in the primary-selective system would likewise be accepted.
3. This system depends for its merit on the prompt transfer of units to the healthy feeder following a feeder outage.
4. It is important that the primary-selective switches be alternately positioned under normal service conditions to avoid the connection of two or more adjacent stations to the same primary feeder. If this precaution is neglected, loss of power on that primary feeder would impose a very heavy transfer load in the tie circuit and in the next adjacent energized load-center unit.

The use of four rather than six substations, as suggested by Mr. Barnett, in either of these secondary network systems would fail to improve the over-all economy. The size of each station would be increased to 1,125 kva (6/4 of 750 kva) and short-circuit current levels on the low-voltage bus sections would exceed 50,000 amperes. A substantial increase in low-voltage switching-equipment cost would thus be incurred. The interconnecting low-voltage tie circuits would be of substantially the same total length but would require 50 per cent increased current-carrying capacity due to the greater load fed from one station.

The spot network represents a variation which closely resembles the secondary-selective system. It differs in that the spot network permanently parallels the two transformers on a common bus using network protectors in the low-voltage circuit from each transformer. The absence of interconnecting low-voltage tie circuits with remotely located stations (as in the secondary-selective system) considerably simplifies the problem for the operating department. The chief question to be considered in deciding between these two system forms is: Is there justification for the additional initial investment resulting from increased switching-equipment cost to obtain uninterrupted voltage in those few instances that power flow on one primary feeder is interrupted; or will service reliability requirements be adequately met by maintaining an alternate low-voltage power supply which can be tapped at a moment's notice merely by closing the low-voltage tie breaker between the complementary bus sections?

Cascade operation of low-voltage air circuit breakers in the secondary network system is regarded as contrary to the fundamental objective. The secondary network system distinguished itself by affording service continuity (uninterrupted service) as contrasted with service reliability (available service). A compromise in this respect would strip the secondary network of its chief advantage.

Diversity Factor

Mr. Parsons in his discussion stresses diversity factor as an important influence on over-all cost of the secondary network system. Although it may have significant influence, yet to be able to take advantage of diversity factor between load-center substations, it is important that, at the time of heavy load on one particular station, the immediately adjacent stations be operating at subnormal load. It is of little benefit to have lightly loaded stations on the opposite side of the loop as they will contribute only slight support to the remotely located, heavily loaded section. The installation of cross tie circuits to improve this situation will likely defeat its own purpose as a result of increased tie-circuit cost and switching-equipment interrupting capacity and cost. Furthermore, the small advantage obtained affects only the transformer capacity. Primary feeder circuits together with switching equipment, low-voltage feeders together with switching equipment, and interconnecting tie circuits together with switching equipment are not benefited. Inasmuch as the transformers account for approximately 10 to 15 per cent of the cost of the over-all system under investigation, based on unity diversity factor, it must certainly be concluded that no substantial reduction in system cost could be realized as a result of diversity factor.

Limiters

For all comparisons reported in the paper, standard air circuit breakers have been used in all low-voltage circuits, including feeders and tie circuits. The standard air circuit breaker constitutes a recognized power-switching device with ratings and performance thoroughly covered by industry standards. The application of standard air circuit breakers within the limits defined by ratings provides the advantages:

1. Ability to repeatedly switch the circuit at will with assured safety.
2. Ability to safely and positively interrupt the maximum fault current.
3. Ability to quickly restore service following an interruption.
4. Reliable indication of operation and position. (Remote indication of circuit-breaker position is easily possible.)
5. Availability of electrical operation.

Messrs. Barnett, Rawlins, and Langguth have commented on the possible application of limiters located in the tie circuit of the secondary network system, in combination with a suitable switching device to provide the necessary degree of circuit control. In the discussion by Messrs. Rawlins and Langguth, reference is made to the limiter as a new development not yet publicly disclosed. This, of course, implies that it is still in the developmental stage and that the ultimate stabilized product will likely incorporate any improvements that installation and use indicate are needed.

In addition to the ability to interrupt fault currents safely, positively, and consistently, there are a number of other considerations. Thermal overload protection of circuit runs within the building should be provided. The thermal effect from heat normally liberated in the fusible unit must be recognized and cared for. Provision should be made for dissipating the heat so generated without subjecting the terminal section of the cable or low-voltage bus to elevated temperatures. The use of tie-circuit switching equipment which duplicates that used in the balance of the structure would be desirable.

Operation

In respect to operating problems of the several typical system forms, there can be no question that the secondary network system is excelled by the other three forms. Beyond that, the ratings in Table I have been assigned no quantitative significance.

That the secondary network system permits growing loads to be met with greater ease and less cost is a controversial contention. In addition to installation of the new substation with associated high-voltage supply and low-voltage feeder circuits, the secondary network system involves incorporation of the new station in the existing tie-circuit system. Unless tie circuits are properly designed, short-circuit current levels on existing stations may be elevated. The primary feeder arrangement must be reviewed to insure that power supply to two or more adjacent stations will not be simultaneously interrupted as a result of a single primary feeder outage.

A New Voltage-Regulating Relay Plus Line-Drop Compensator

Discussion of paper 42-32 by H. J. Carlin, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, February section, pages 63–6.

H. L. Prescott (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The new relay described in Mr. Carlin's paper has several features differing from previous voltage-regulating relays which affect the application of the relay to a step voltage regulator and it may be well to look at it from an application viewpoint.

This relay has a damping characteristic of importance on many circuits on which there are, almost continuously, small voltage changes which are not large enough to close the voltage-regulating relay contacts. The conventional type of relay follows these changes as they occur. The relay described in this paper, due to its inherent time delay, damps out these continual changes and operates smoothly, following the mean voltage. Many circuits also are subject to relatively large momentary voltage changes due, for example, to switching surges. The conventional relay will also follow these changes while the new relay will to a large extent damp them out. This damping characteristic, therefore, reduces the mechanical duty on the relay, reduces wear on the bearings, and increases the length of life of the mechanical parts.

A second feature of importance in applying this relay is its integrating or cumulative characteristic. In general, the older types of voltage-regulating relays are used to operate a time delay relay and in order to cause an operation of the step regulator, the voltage applied to the voltage-regulating relay must hold the relays contacts closed for the full timing period of the time delay relay. With the new voltage-regulating relay, the relay response depends not upon the voltage remaining outside the range of the relay setting continuously for a definite time but upon the difference in the time it is outside the range from the time it is within the range. Therefore, if the voltage is outside the range more of the time than it is inside the range, this voltage-regulating relay will close its contacts and cause the regulator to operate to bring the mean voltage inside the relay settings. This relay will hold the voltage closer to the desired value than will the older types of voltage-regulating relays.

A third feature of the new voltage-regulating relay is its differential or inverse time characteristic. When conditions on the power system cause relatively large voltage changes, it is desirable to have the voltage regulators operate more quickly than when the voltage changes are small. Figure 1 of the paper shows that the greater the voltage variation from the neutral value from which the relay is set, the shorter will be the time until the contacts close. The new relay, then, instead of having a constant time delay regardless of the magnitude of the voltage change as is the case with separate voltage-regulating and time delay relays, has a shorter time delay when large voltage corrections are required than it has when the voltage variation from normal is small.

The characteristics just described result in reducing contact maintenance. Since momentary voltage variations do not cause the voltage regulating relay to close its contacts, this relay closes its contacts after the time delay period rather than before the time delay period, and, therefore, the contacts close only when an operation of the regulator is actually to be made. The number of contact operations, therefore, is reduced to equal the number of tap-changer operations, resulting in less burning of the contacts than on the older type voltage-regulating relays whose contacts closed to initiate operation of the time delay relay. This feature also eliminates the necessity for separate compounding coils.

The line-drop compensator which is built into the relay described in this paper is different in both construction and principle of operation from the line-drop compensators which have previously been built into voltage-regulating relays. In the older relays, the line-drop compensator operation was based only on the magnitude of the current. In the new relay, the compensator operation is based not only on the magnitude of the current, but also upon its power factor and the impedance angle of the line. The new device, therefore, gives a closer approximation to the theoretically correct value than the older type. It will, therefore, be applicable in some cases where in the past separate voltage-regulating relay and line-drop compensator were used.

Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less

B. W. JONES
ASSOCIATE AIEE

THE protection of a motor against an overload or the protection of a motor and its branch circuit against a short circuit are basically the same problem. The entire circuit is heated at a rate which is a function of the amount of current flowing, and this current must be interrupted before the winding or any part of the circuit exceeds a prescribed temperature. The means used to protect against these two magnitudes of power may be the same, or it may be entirely different as will be brought out later. It is the purpose of this article to show what the heating characteristics are of the motors, the control, and the branch circuit wiring, and what the characteristics of the protective devices should be in order that the power will be disconnected from the main feeders under all conditions of excess current before excess temperatures are attained.

The points which this article will discuss and some of the conclusions reached are:

1. That the short-time heating characteristic of three-phase induction motors stators can be shown in a simple graphical method when we know the current density in the stator windings at rated current. These data should then be used as the basis for determining the tripping characteristic of overload and short-circuit protective devices.

2. If the cross section of the reduced section of a zinc fuse link is not greater than any part of the copper conductors nor greater than 45 per cent of any Nichrome conductor (the heaters of overload relays), then the fuse will give good short-circuit protection to the copper and to the Nichrome conductors.

3. Small horsepower starters, which are within the range of from 1 to 50 horsepower at 440 or 550 volts, or of 1 to 25 horsepower at 220 volts (classified as National Electrical Manufacturers' Association size 3 or smaller), require a short-circuit protective device that will interrupt the circuit during a heavy short circuit in less time than one-half cycle. Tests have shown that a suitable size nonrenewable cartridge-type fuse of good design is capable of doing this.

4. Because of this fast interrupting ability, this type of fuse is current-limiting (see Figures 6 and 7). Therefore, it gives two-way protection, in that it both limits the amount of current which reaches the starters, and it is exceptionally fast when the currents are high.

5. Because of the inductance in a d-c generator, its current will build up at a slower rate during a short circuit than will the current in an a-c circuit, and therefore a fuse will be more current-limiting on a d-c system when supplied from a d-c generator than on an a-c system. Unlike the a-c power, the peak voltage and the average voltage is the same on d-c systems. Because of these two factors it is easier for a fuse to interrupt a short circuit on a d-c system than on an a-c system of the same rated voltage since the entire interrupting period takes place in a fraction of a half cycle.

6. A NEMA size 3 starter (50 horsepower at 440 volts or 550 volts, three phase) or smaller can be fully protected against any major damage from short circuits on a system having available currents up to 100,000 amperes, provided a correct current and voltage nonrenewable cartridge-type fuse of good design is placed in each branch circuit line, and provided the fuses are positively held in their clips so as to prevent magnetic forces from throwing them out.

7. A circuit breaker provided with a fast instantaneous magnetic trip, as used on our tests, demonstrated its ability to protect NEMA size 4 and larger starters, when on a system having an available current equal to or less than the interrupting rating of the breaker.

8. The maximum size fuse that should be used is $3^{1}/_{2}$ times the current rating of the overload relay, or four times the current rating of the motor, and if this does not result into the same current, then the smaller of the two should be selected. See Figure 3.

Permissible Temperatures

Let us first decide what are excess temperatures. Much has been written on this subject,[1] but it is quite generally agreed that 90 to 100 degrees centigrade is a satisfactory temperature (approximately 50 degrees rise), where it is maintained for long intervals, but where the temperatures are transient and last for only a few seconds or minutes and occur infrequently, then this 50 degrees rise can be materially increased. For conditions like a stalled squirrel-cage induction motor which lasts for only a few seconds the 50 degrees rise can be doubled. A rise of 100 degrees is satisfactory for a stalled motor because it will seldom be called upon to withstand this condition and then for only a few seconds.

Current-Time Heating Characteristics

Let us now ascertain the heating characteristic of a three-phase squirrel-cage type of induction motor. When a representative type of induction motor is running at rated load, approximately one third of its total heat loss will be produced by the iron, and the remaining two thirds will be produced by the copper. But if the motor is stalled and taking five times rated current, then the iron loss will re-

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B. W. JONES is consulting engineer, industrial control department, General Electric Company, Schenectady, N. Y.

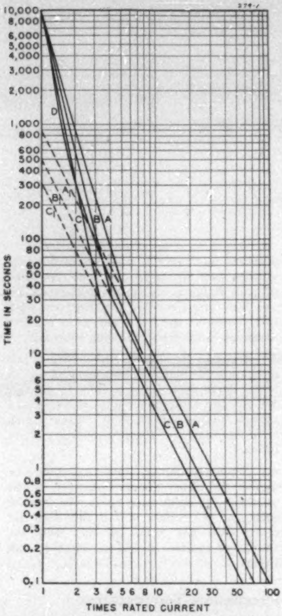

Figure 1. Heating characteristics of three-phase induction motors. Also tripping characteristics of a representative temperature overload relay

Curve *A*—Current-time curve for copper, showing rate of heating where current density is 3,000 amperes per square inch at rated current, and maximum temperature reached is 100 degrees centigrade rise

Curve *B*—Same as *A* except current density is 4,000 amperes per square inch

Curve *C*—Same as *A* except current density is 5,000 amperes per square inch

Curves A_1, B_1, C_1—Extensions of *A*, *B*, and *C* to show the rate of rise if there were no heat loss

Curve *D*—Current-time tripping curve of a high-grade temperature overload relay

Because of heat conduction into the insulation and into the iron of the motor, and because of convection from the motor, the temperature rise for times of 30 seconds and longer will range between 50 degrees centigrade minimum and something under 100 degrees centigrade maximum

main the same, but the copper loss will be 25 times larger. Under either a running or a stalled condition, the copper will lead the iron in temperature, but in the stalled condition will be leading so far that we can assume the iron temperature will be stationary in temperature, while the copper is going its limit. The length of time required for the copper to go its limit of 100 degrees centigrade rise will depend upon the current density in the copper. Let us assume that the current densities in the stator copper of the standard lines of three-phase induction motors fall within 3,000 to 5,000 amperes per square inch when carrying their rated currents, then curves *A*, *B*, and *C* of Figure 1 show the time required for these three copper densities to reach 100 degrees rise for any current from 1 to 100 times their ratings. This is based on the assumption that all the heat remains in the copper, and none is conducted out into the insulation or iron, or carried off by convection or by radiation. For short circuits where the considered time is one second or less, this assumption is sufficiently correct, but for stalled conditions of the motor, or for heavy running overloads where the no-loss time required to reach 100 degrees rise is 30 seconds or more, there will be sufficient loss from the copper due to heat transfer that recognition should be made. Referring to Figure 1, it will be noted that at the 30-second position of the *A*, *B*, and *C* curves they have changed their current-time rate in order that this heat transfer can be approximately indicated in a simple diagrammatic manner. At the 30-second position, where the two curves diverge, we know that if no heat had escaped from the copper, it would have a 100 degrees rise, but we know that some heat has escaped during this 30 seconds, so the temperature will be below the 100 degrees rise (perhaps 85 or 90 degrees rise or less). Likewise, at the 10,000-second position, where the motor is carrying its rated load, the heat transfer has reached an equilibrium whereby the temperature rise on the copper is 50 degrees—the rated rise of the motor—therefore, along various positions of the *A*, *B*, and *C* curves between 30 and 10,000 seconds, the temperature rise gradually increases as the load increases, reaching something less than 100 degrees rise at a stalled condition.

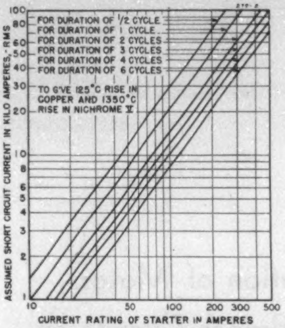

Figure 2. Required interrupting speeds fully to protect starters having heater-type overload relays against specified short-circuit currents

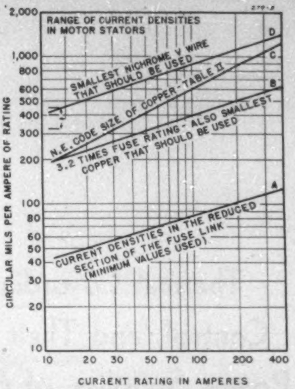

Figure 3. Current densities in copper and in Nichrome V wires, in stator windings of squirrel-cage three-phase induction motors, and in the restricted section of General Electric low-voltage cartridge-type fuses when carrying their respective rated currents

Thus, by the use of log-log-paper a very simple graphical method can be used, wherein the well-known thermal capacity of copper and the assumed current density used in the motor design can be used as the basic factors in this graphical method. The resultant temperatures obtained in this simple manner will be sufficiently close for all practical purposes, and the temperatures will be on the conservative side. Therefore, the three straight lines in Figure 1 between 30 and 10,000 seconds, which are labeled *A*, *B*, and *C* can be used to closely represent the transient heating time-current characteristics of the stator of the three designs of motors which have the 3,000-, 4,000-, and 5,000-amperes per square inch densities in the copper when carrying their rated currents.

Required Temperature-Overload-Relay Characteristics

Thus, if these three time-current characteristic curves are used as the basis for designing three temperature overload relays such that the relay-tripping times never exceed those shown on these curves and yet keep fairly close to them, then these relays should be capable of protect-

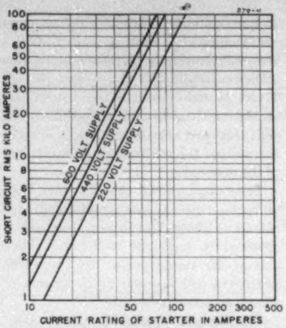

Figure 4. Magnitude of short-circuit currents for a circuit in which 20 volt-amperes per phase is assumed for the total impedance of the circuit when carrying rated current

ing the three designs of motors against all magnitudes of overloads and yet permit them to do their full quota of work. However, the heater type of temperature overload relay, which develops its source of heat in one element—its heater—and transfers this heat to the bimetal or temperature sensitive element, tends to slow down on the high-current end such that the relay-tripping characteristic curve would normally cross the motor-heating curve at the high-current end. This type of relay must, therefore, be sufficiently fast at the stalled current value—approximately six times current—to give the required protection, and then at the lower current values of overload it will overprotect the motor. But since this is the characteristic of the relay most commonly built for the smaller motors, it follows that if this design is so made that it is correct for the stalled condition of the motor, it will be somewhat faster than necessary for all the lower degrees of overloads. This degree of overprotection is not generally sufficient to interfere with the motor's doing its required work.

The total thermal capacity of a motor is a function of its copper, its insulation, and its iron, but under a stalled condition the *effective* thermal capacity of this same motor is primarily a function of only its copper. Thus the effective thermal capacity of a motor under all degrees of overloads is a variable, having about a four-to-one range. Therefore, if a relay is designed which has a duplicate heating characteristic to that of the motor, then it must also have an effective thermal capacity which is a variable. This of course complicates the problem and results in a larger and a more complicated relay. Such a design is available and has

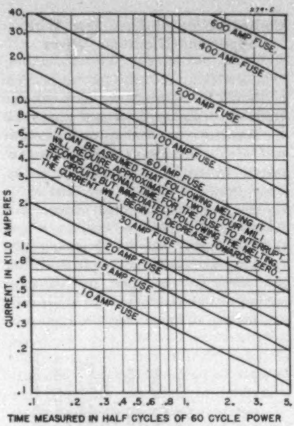

Figure 5. Time required for General Electric cartridge-type low-voltage fuse links (zinc) to melt

been supplied since 1924, but it is not a large production unit because of its size and cost. Unless this desired characteristic can be designed into a relay which is both inexpensive and small, I doubt whether the trade will need this variable thermal-capacity characteristic sufficiently to demand it. Large motors will always require the best designs available, but the control for small motors will be determined largely by cost. Therefore, the small and inexpensive heater type of thermal overload relays will very likely be continued for some time.

The above discussion regarding how we can protect the insulation of a motor by protecting it against excess temperatures should not lead us to conclude that, if we do a good job in this respect, no insulation troubles will occur. These troubles may develop as a result of transient voltages, faulty work, mechanical injury, or from many other causes. Therefore means should be provided which will give adequate fault protection to the branch circuit, the control devices, the motor, and to the operating personnel.

Short-Circuit Protection

The devices which provide the degree of overload protection just discussed will not, in general, provide the necessary protection to the branch circuit and to the control against short circuits. In our discussion we have assumed that ten times rated current is the dividing line between overload and short-circuit conditions. Above ten times current the effective thermal capacity will be proportional to the copper in the stator winding of the motor and in the branch circuit and the control. Also we have assumed that 100 degrees centigrade rise will be the upper limit of temperature for the stator copper under overload conditions, but for short-circuit conditions 100 degrees rise is somewhat low. We will, therefore, assume that the temperature rise may be 125 degrees—a very, very conservative value—and that the Figure 2 curves will show the necessary speed of interruption required to keep the copper within these limits. The 125 degrees rise (165 or 175 degrees total) is not the limit to which it is permissible to carry copper with its insulation, but, when wire is selected for its continuous current-carrying ability, its resultant temperature under short-circuit condition will be in accordance with the above assumptions.

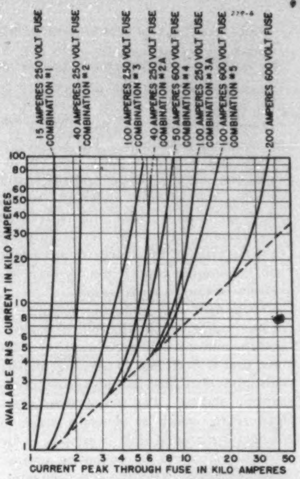

Figure 6. Current-limiting characteristics of one standard nonrenewable cartridge fuse per phase on a three-phase power supply

Combinations 1, 2, 2*A*, 3, and 3*A* were tested on 440 volts, three-phase power. Combinations 4 and 5 were tested on 600 volts, three-phase power

Starter Combination	Ohms Impedance
1	0.252
2	0.157
2*A*	0.029
3	0.065
3*A*	0.015
4	0.048
5	0.018

Since the path of the current which reaches the motor must travel by way of the conductor cable and the devices on the control panel, it will be necessary to also consider the heating characteristic of these current-carrying parts during this short-circuit condition. The conductor cables and contactor parts, we will assume, are made of copper and the heater of the temperature overload relays, which are in most motor circuits, is made of Nichrome. Therefore, we can assume that the path of the short-circuit current will be through copper and through Nichrome of known cross sections.

To protect the conductors adequately in the branch circuit, the control devices, and the motor winding, we must use a device that is both fast in opening the circuit and also has the necessary interrupting ability. The temperature overload relay, which is generally used to protect a motor against overloads, is far too slow to assist in protecting against short circuits. Also the line contactor, connecting and disconnecting the motor to and from the line, does not generally have the necessary interrupting capacity, even if the temperature overload relay could assist it in opening sufficiently fast—as it cannot do—and so we must look for other means which can function sufficiently fast and have adequate interrupting ability.

The most common form of short-circuit protective means used on low-voltage lines is cartridge-type nonrenewable fuses and air-break circuit breakers. If we consider first the fuse then a third material, namely zinc of the fuse link, will be added in the path of the short circuit. We must, therefore, consider the correct relationship between the cross sections of the zinc fuse link, the copper, and the Nichrome heater so that neither the copper nor the Nichrome will exceed certain prescribed temperature rises. To do this we will assume that when the fuse melts, the copper will have reached 70 degrees centigrade rise, the Nichrome 800 degrees rise, this fuse-melting period being one unit of time. Following the melting of the fuse, the current will be reduced to zero in an additional time which we will assume to be two units of time (this has been shown by tests to be a conservative assumption), and the rms current value, during this fuse-arcing period, we will assume to be 58 per cent of the value existing at the instant of melting. During the fuse-arcing period the copper is assumed to have an additional rise of 55 degrees and the Nichrome a rise of 550 degrees, making a total rise of 125 degrees for copper and 1,350 degrees for Nichrome.

Co-ordination of Fuse Size With Copper and Nichrome Wires

In setting up the basis for our problem as shown in Figure 3, we have selected the largest cross sections on the fuses and the smallest cross sections on the copper wire sizes, which means that there is some margin in our basic figures. Let us assume that a copper, a zinc, and a Nichrome wire are connected in series and that a given value of current is put through them. Also let us assume that the cross sections of the copper and of the zinc wires are the same, but the cross section of the Nichrome wire is larger by 2.2 times. The average resistance of zinc per circular mil foot between 20 and 419 degrees centigrade (melting) is 69 ohms, while the average resistance of copper per circular mil foot between 20 and 70 degrees is 12 ohms. This makes the average resistance of zinc 5.75 times larger than copper. Therefore, when the copper reaches 70 degrees rise the zinc should be $70 \times 5.75 = 400$ degrees centigrade rise assuming no loss of heat. Since the Nichrome wire, which is 2.2 times larger in cross section, is carrying the same current, and since its average resistance is 670 ohms per circular mil foot, the Nichrome wire should have a rise of $70 \times 670/12 \times (1/2.2)^2 = 800$ degrees centigrade.

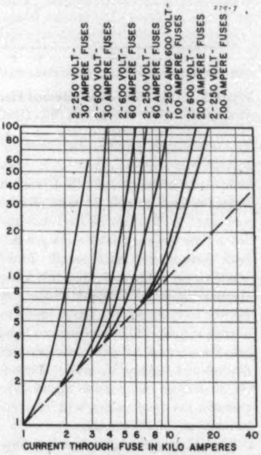

Figure 7. Current-limiting characteristics of standard nonrenewable cartridge fuse on 250-volt d-c power

Now, the above time is just sufficient to melt the fuse material; to raise the copper to 70 degrees centigrade rise; and the Nichrome to 800 degrees rise. At this time the current is a maximum, and a sufficient additional time must be allowed to reduce this current from this maximum value to zero. We will assume that this current reduces along a straight line so that the rms current is 58 per cent of the maximum.

Tests have shown that for very high currents, twice as much additional time should be allowed for interruption as was required to melt the zinc fuse. Then during this arcing period the rms value of current will be equal to 58 per cent of the value which melted it.

Therefore, if we initially allow a sufficient current to flow through the copper to cause it to reach a 70 degrees centigrade rise (from 20 degrees base) in a unit of time and then allow 58 per cent of this current to continue to flow for two units of time, the copper will have a 125 degrees total rise, because if a given current value flowing for one unit of time results in a 70 degrees rise, then 58 per cent of this current for two units of time will give $70 \times (0.58)^2 \times 2 \times 1.17 = 55$ degrees centigrade rise. This rise of $55 + 70 = 125$ degrees centigrade. (The 1.17 represents the average increase of the copper resistance due to the higher temperature.)

And if this same 58 per cent value also went through the 220 per cent cross section of Nichrome, it would have an additional rise over its 800 degrees centigrade value of $(125 - 70)680/14 \times (1/2.2)^2 = 550$ degrees centigrade or a total of $800 + 550 = 1,350$ degrees centigrade. (The 680/14 also represents the increased resistance due to the higher temperature.)

Therefore, from this line of reasoning, which has been checked by test, we can expect satisfactory results if we have a given cross section of the zinc fuse link—the reduced part of the fuse link—and then see that no part of the copper cross section is smaller than this fuse cross section, and also see that the Nichrome cross section is not smaller than 2.2 times the fuse cross section.

To obtain the above cross-sectional relationships, let us call the current rating of the motor as 100 per cent; then the current rating of the overload relay will be 115 per cent; the current rating of the branch circuit wiring as given in the 1940 National Electrical Code, Table II, chapter 10 will be 125 per cent; and the current rating of the fuse will be 400 per cent. Figure 3 shows graphically the current densities in these three materials. For example a 25-ampere control should have copper cross sections of $250 \times 25 = 6,250$ circular mils and Nichrome cross sections of 550×25=13,750 circular mils as minimum cross sections while the maximum size fuse that should be used is 25×3.2=80 amperes, which should have the same cross section as the copper—6,250 circular mils. If the motor has a service factor of 1.15, then the motor rating would be 25 divided by 1.15=21.7 amperes.

Estimated Magnitude of Short-Circuit Current

It is frequently desirable to estimate the magnitude of short-circuit current that will pass through a given size starter, and then determine how long this current can be permitted to flow without exceeding the temperatures prescribed above. Figure 4 was developed on the basis of 20 volt-amperes per phase for the total impedance of the circuit when carrying rated current (this is an approximate minimum impedance generally found). This will hold fairly well for the smaller sizes of control (25 horsepower and less) where the impedance of the supply circuit is a small part, but for larger sizes of starters this method will show too large a value of current. But if an expected value of current is determined by this or other means, then Figure 2 will show how long this value of current can be permitted to flow and still keep the temperatures within the bounds set above, namely 125 degrees centigrade rise on the copper and 1,350 degrees rise on the Nichrome. For example, Figure 4 shows that for a 25-ampere starter at 600 volts the expected short-circuit current would be 10,000 amperes, while Figure 2 shows that a 25-ampere starter can withstand only 4,700 amperes for one one-half cycle or 3,300 amperes for one cycle of time duration. Therefore, since the heating varies as the square of the current this size of starter could carry 10,000 amperes for only $(0.47)^2$ (half cycle) or 0.00184 second. This is obviously faster speed than that of a circuit-breaker operation—which is in the order of 0.008 to 0.015 second—but as shown in Figure 3, a correctly selected fuse can do the job for any current because its time of operation will vary with the magnitude for the current.

Interrupting Speed of Fuses

Another pair of questions that are frequently asked is how fast will a fuse interrupt the circuit, and how much available energy can it interrupt? The first question must be divided into two parts in order that we might segregate the important factors, namely the length of time required to melt the link, and then the time required to interrupt the circuit. The time required to melt the link of General Electric nonrenewable fuses is shown in Figure 5, which gives the time in half cycles (60 cycles assumed). For a fuse to be current-limiting, it must melt in the early stages of a half cycle—such as one fourth of the half cycle—Figure 5 shows that a 100-ampere fuse of this type will melt in this time when approximately 10,000 amperes are put through, and Figure 6, which is a test record, shows that the available current may be around 10,000 amperes value. This means that the fuse stops the current from materially exceeding the value reached at melting. And of course, this effect becomes more pronounced on the smaller size fuses.

It is, therefore, obvious from Figures 5 and 6 that the degree of current-limiting becomes less and less as the size of fuses increases, and that for ordinary powered circuits the fuses larger than 200 amperes size do not give much current-limiting effects. It is also obvious that the 200-ampere fuse and smaller interrupt in one half cycle or less when subjected to currents of 10,000 to 20,000 amperes. This means that fuses do an exceptionally fast interrupting job in protecting motors and controllers of 100 horsepower (size 4 starters) and less.

Interrupting Ability of Fuses

The second half of the question dealing with the amount of available energy which these fuses can interrupt was determined by providing a 250-volt d-c source which was capable of delivering 96,000 amperes, and also a three-phase source which was capable of delivering an rms symmetrical current of 84,000 amperes at 440 volts and 114,000 amperes at 600 volts. In the d-c circuit was placed two standard nonrenewable General Electric 250-volt fuses in series to simulate the usual two fuses that are in a circuit. Two 600-volt fuses were also tested in this circuit, wherein the available currents were varied from 10,000 to 96,000 amperes in several steps, and the results were perfect. The build-up of the current in this circuit was much slower than in an a-c circuit so that the current-limiting effects were much more pronounced as shown in Figure 7.

In the three-phase 440-volt a-c circuit one 250-volt fuse of the above type was connected in each supply line, and the available currents ranged from 18,000 to 84,000 symmetrical rms amperes. The resulting currents are shown in Figure 6, and the performance was perfect. When the supply power was 600 volts, then one 600-volt fuse was connected in each supply line, and the available current ranged from 24,000 to 114,000 amperes. The results, which were perfect in operation, are also shown in Figure 6. It can therefore be said that with this arrangement of connections, these fuses are capable of interrupting a circuit, either a-c or d-c, which has an available short-circuit current of 100,000 amperes.

Conclusions

The following recommendations are made:

1. Correctly designed overload relays should be used to protect all sizes of motors against overloads. To do this, relays having three degrees of tripping speeds should be available—fast, medium, and slow tripping.

2. Every motor should be listed in one of three groups to show whether it is a fast-, medium-, or slow-heating unit. The correct overload relay could then be selected for each motor.

3. Full short-circuit protection for NEMA size 3 and smaller starters can be obtained by the use of correct-size nonrenewable cartridge-type fuses of a good design. Interrupting devices which are slower than these fuses may not give full protection.

4. NEMA size 4 and larger starters will be protected by fast-tripping air circuit breakers which have an instantaneous type of tripping device.

5. The copper cross section of the branch circuit, the control, and the motor should be equal to or larger than the reduced cross section of the zinc fuse link, and the cross section of the Nichrome of the overload relays should be 2.2 times larger than the zinc fuse link.

6. Since safety first laws are becoming more stringent, since available short-circuit energy is rapidly increasing, and since starters are frequently mounted near workmen, it is quite essential that adequate short-circuit protection be provided for all branch-line circuits. Since it has been demonstrated that this protection can be provided for standard existing starters by means of standard fuses, even on large power systems, it was considered of sufficient importance to bring it to your attention at this time so that this needed protection can be obtained and especially during this critical war period.

Reference

1. AIEE Standards Pamphlet No. 1 A, September 1941. (Proposed supplement to AIEE Standards Pamphlet No. 1.)

Selenium Rectifiers and Their Design

JOHN E. YARMACK
ASSOCIATE AIEE

Synopsis: Selenium rectifiers are dry-plate rectifiers of the electronic type. The history of dry rectifiers began some 30 years ago with the advent of the copper-sulphide rectifier. In the early 20's, the copper-oxide type came into common use, and during the past three years, selenium rectifiers have found wide application in American industry. Fundamentally, this third and latest type of rectifier is a continental development and has been manufactured in Europe within the International Telephone and Telegraph group of companies during most of the 1930's. Sufficient experience was gained in the technique of manufacture, as well as in methods of design and in selection of applications, to warrant engineering statements as to their general usefulness and importance to industry.

The purpose of this paper is to outline general principles of design of stacks, one or more of which make a rectifying unit. Starting with the availability of selenium plates or discs of various types, the engineer is confronted with the problem of

1. Selecting the proper size of plate to provide the required d-c output.
2. Computing the required a-c voltage to be impressed on the rectifier stacks to give the necessary d-c output.
3. Analyzing various factors such as type of load, nature of service, and cost of selected rectifier.

The paper includes dynamic and other characteristics of the principal sizes of selenium rectifier plates as applied to the design of single-phase, three-phase, full-wave, and half-wave rectifier units, and all variety of loading. Typical circuits are analyzed and novel representative computations of rectifier units are included.

Selenium Plates

THE rectification of alternating currents by means of the selenium rectifier takes place wholly within the constituent plate or plates, seven sizes of which are shown in Figure 1. The rectifying medium of this electronic device consists of selenium; the principle of operation is similar to that of other dry plate rectifiers, that is, low resistance in the forward direction, and high resistance in the reverse direction. A metal plate serves as back electrode; over it a layer of selenium is deposited, thin enough to give minimum internal losses, but sufficiently thick to withstand high inverse voltage. A soft metal of low-melting temperature is then applied over the selenium layer to form a front electrode. Finally, by means of controlled processes, a barrier layer is formed between the selenium and the front electrode.

Selenium Rectifier Stacks

Rectifier stacks are produced by assembling selenium rectifier plates (40 plates are usually maximum) on a center stud with contact discs or washers interspersed between plates. Series and parallel arrangement can readily be provided as required, by inserting insulators between plates and introducing terminal lugs into the stack.

A stack consisting of 40 plates may be connected in several different ways: for example, as a bridge-circuit unit having ten plates in a series and one plate in parallel (4-10-1), or one plate in series and ten in parallel (4-1-10). The same stack may also be connected as a half-wave rectifier having all 40 plates in series to take care of voltage and one plate in parallel to take care of current (1-40-1). Further, the 40-plate stack may also be assembled as a doubler (2-10-2), two of which make a bridge-circuit rectifier with ten series and two parallel plates in each arm of the bridge.

For the three-phase half-wave circuit the total number of plates must be a multiple of three to allow a total connection such as 3-6-2 for a 36-plate single-stack unit. Similarly, this latter stack, if connected as 6-6-1, becomes either a bridge or center-tap three-phase rectifier.

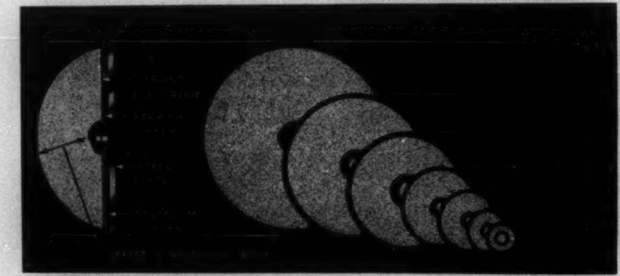

Figure 1 (Below left). Cross section of a selenium rectifier plate showing the sequence of various layers
(Right) Seven basic sizes of selenium rectifier plates

Rating of Plates

Current ratings of selenium plates are a function of their effective rectifying areas and heat-dissipating capacities. Table I lists seven basic plates, ranging from $^3/_4$ to $4^3/_8$ inches in diameter; it also shows ratings of the basic plates in various rectifier circuits. The ampere capacities shown in this table are based on an ambient temperature of 35 degrees centigrade and were determined experimentally. The requirements of a selenium rectifier in respect to current are always met by selecting the proper size of plates and their number in parallel, if the total rating of the rectifier exceeds the rating of an individual plate.

The ratings of the plates shown in Table I can be increased by providing additional cooling. Doubling the spacing between plates assembled in a single stack increases the heat-dissipating ability of the stack; the rating of plates is some 25 to 50 per cent higher than those shown in Table I. Table II lists six selenium plates having the same effective rectifying areas as those listed in Table I, but their current ratings are higher because of wider spacing.

By providing cooling fins, the forward current-carrying capacity of the plates can be raised still further. Table III contains eight additional plates with fins, the current ratings of which are from 2 to $2^1/_2$ times greater than the plates of the same rectifying areas listed in Table I. With extended ratings of the plates, whether by means of wide spacings or cooling fins, the internal losses of selenium rectifiers are greater; consequently, their efficiency is slightly reduced, and the voltage regulation is adversely affected.

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JOHN E. YARMACK is commercial engineer with International Telephone and Radio Manufacturing Corporation, New York, N. Y.

Tables I, II, and III also give the maximum permissible reverse rms voltages per plate. The voltages thus indicated have ample safety margin. If, however, the reverse voltage be increased beyond safe limits, the rectifying layer between the selenium and the front electrode alloy breaks down. The maximum specified voltage must not, therefore, be exceeded. Tables I, II, and III include ratings of selenium plates in d-c circuits.

Rectifier stacks with narrow or wide spacing, as well as those with fins, are ordinarily cooled by convection. Large-size selenium rectifiers, however, often utilize forced draft ventilation in order to save space and to economize in rectifying elements. The normal plate rating can thereby be increased twofold and sometimes threefold. If, however, extended loading of plates with the aid of forced ventilation is not desired, substantially greater current output, as compared with free air conditions, can be obtained by mounting the stacks in a chimneylike enclosure (Figure 2). As will be noted from the figure, the least favorable condition is that of mounting the stacks in a cabinet provided with perforated covers.

The current rating of selenium rectifiers is limited only by the final plate temperature resulting from heating. The stacks can be heavily overloaded, provided the maximum safe operating temperature of 75 degrees centigrade is not exceeded. When this temperature is reached, either the load must be reduced to normal, or provision must be made for cutting the rectifying elements out of service as in the two half-wave rectifiers illustrated in Figure 3, widely used in business machines. Each element in these machines is equipped with a pair of bimetal strips connected by means of an adjustable screw. If, by chance, the key punch or the duplicator happens to be jammed and causes the temperature of the stacks to reach a range of 70 or 80 degrees centigrade, the bimetal

Table I. Current and Voltage Ratings of Seven Basic Selenium Plates When Used in Narrow Spacing Assemblies and With Resistive and Inductive Loads

For Battery-Charging and Capacitive Loads, These Ratings Are Reduced by 20 Per Cent. Conditions: Continuous Duty; 35 Degrees Centigrade Ambient Temperature

Plate Type No.	Diameter of Plates (Inches)	Maximum Number of Plates Per Stack	Maximum RMS Reverse Voltage Per Plate (Volts)	Single-Phase Rectifiers: Half-Wave	Single-Phase Rectifiers: Bridge	Single-Phase Rectifiers: Center-Tap	Three-Phase Rectifiers: Half-Wave	Three-Phase Rectifiers: Bridge	Three-Phase Rectifiers: Center-Tap	35 C Ambient Rating of Plates Used as D-C Valves: Amperes	35 C Ambient Rating of Plates Used as D-C Valves: Volts
				D-C Amperes							
1	$^{3}/_{4}$	36	18	0.04	0.075	0.075	0.10	0.11	0.13	0.06	15
2	1	36	18	0.075	0.15	0.15	0.20	0.225	0.27	0.12	15
3	$1^{3}/_{8}$	36	18	0.15	0.30	0.30	0.40	0.45	0.55	0.23	15
4	$1^{3}/_{4}$	40	18	0.30	0.60	0.60	0.80	0.90	1.1	0.45	15
5	$2^{5}/_{8}$	40	18	0.60	1.2	1.2	1.6	1.8	2.2	0.90	15
6	$3^{3}/_{8}$	40	16	1.2	2.4	2.4	3.2	3.6	4.5	1.8	12
7	$4^{1}/_{8}$	40	14	2.0	4.0	4.0	5.3	6.0	7.5	3.1	12

Table II. Current and Voltage Ratings of Six Selenium Plates (Similar to Table I, Except Number 1 Plate Omitted) When Used in Wide Spacing Assemblies and for Resistive and Inductive Loads

For Battery-Charging and Capacitive Loads, These Ratings Are Reduced by 20 Per Cent. Conditions: Continuous Duty; 35 Degrees Centigrade Ambient Temperature

Plate Type No.	Diameter of Plates (Inches)	Maximum Number of Plates Per Stack	Selenium Plate No. Used (See Table I)	Maximum RMS Reverse Voltage Per Plate (Volts)	Single-Phase Rectifiers: Half-Wave	Single-Phase Rectifiers: Bridge	Single-Phase Rectifiers: Center-Tap	Three-Phase Rectifiers: Half-Wave	Three-Phase Rectifiers: Bridge	Three-Phase Rectifiers: Center-Tap	35 C Ambient Rating of Plates Used as D-C Valves: Amperes	35 C Ambient Rating of Plates Used as D-C Valves: Volts
					D-C Amperes							
20	1	28	2	18	0.11	0.22	0.22	0.29	0.33	0.4	0.17	15
21	$1^{3}/_{8}$	28	3	18	0.23	0.45	0.45	0.6	0.67	0.82	0.34	15
10	$1^{3}/_{4}$	28	4	18	0.39	0.78	0.78	1.0	1.1	1.4	0.58	15
11	$2^{5}/_{8}$	28	5	18	0.78	1.6	1.6	2.1	2.3	2.8	1.2	15
14	$3^{3}/_{8}$	28	6	16	1.5	3.1	3.1	4.1	4.6	5.8	2.4	12
18	$4^{3}/_{8}$	28	7	14	2.6	5.2	5.2	6.9	7.8	9.7	4.0	12

Table III. Current and Voltage Ratings of Eight Selenium Plates (Numbers 4, 5, 6, and 7) Equipped With Cooling Fins of Different Sizes, and Used for Resistive and Inductive Loads

For Battery-Charging and Capacitive Loads, These Ratings Are Reduced by 20 Per Cent. Conditions: Continuous Duty; 35 Degrees Centigrade Ambient Temperature

Plate Type No.	Size of Cooling Fins (Inches)	Maximum Number of Plates Per Stack	Selenium Plate No. Used (See Table I)	Maximum RMS Reverse Voltage Per Plate (Volts)	Single-Phase Rectifiers: Half-Wave	Single-Phase Rectifiers: Bridge	Single-Phase Rectifiers: Center-Tap	Three-Phase Rectifiers: Half-Wave	Three-Phase Rectifiers: Bridge	Three-Phase Rectifiers: Center-Tap	35 C Ambient Rating of Plates Used as D-C Valves: Amperes	35 C Ambient Rating of Plates Used as D-C Valves: Volts
					D-C Amperes							
9	$2^{5}/_{8}$ Diameter	28	4	18	0.58	1.1	1.1	1.5	1.7	2.1	0.87	15
12	$3^{3}/_{8}$ Diameter	28	5	18	0.90	1.8	1.8	2.4	2.7	3.3	1.4	15
13	$4^{3}/_{8}$ Diameter	28	5	18	1.1	2.2	2.2	2.9	3.3	4.0	1.7	15
15	$4^{7}/_{8}$ Diameter	28	6	16	1.8	3.5	3.5	4.6	5.2	6.5	2.7	12
16	$4^{3}/_{8}$ Diameter	24	6	16	1.9	3.8	3.8	5.0	5.6	7.0	2.9	12
17	6 x 6	28	6	16	2.7	5.4	5.4	7.2	8.1	10.0	4.1	12
19	6 x 6	28	7	14	3.7	7.4	7.4	9.8	11.1	13.3	5.7	12
8	8 x 8	28	7	14	5.0	10.0	10.0	13.0	15.0	18.0	7.5	12

strips separate, thus breaking the circuit. The plates then cool off and the bimetal strips again close (Figure 4).

Intermittent Service

Considerable gain in the current capacity of selenium rectifiers can be obtained when they are used in intermittent service, in which case, the duty cycles must be definitely established. The variety of these intermittent applications is great, and their complete discussion here would be too lengthy. One formula, however, is frequently used for periodic loadings:

$$I_m = I_{\max}\sqrt{\frac{A}{A+P}} \tag{1}$$

where I_m is the continuous current rating, $I_{\max}$ the maximum current drawn periodically, A the operating period, and P the inoperative interval; both A and P should be in the same units. Experience has shown that this formula can be used only if A is less than the selenium plate time constant T, which may be defined by:

$$t_1 = t_2(1-e^{\frac{A}{T}}) \tag{2}$$

and which varies from five to eight minutes, depending on the plate size (t_1 and t_2 are instantaneous and final plate temperatures, respectively). When operating periods are separated by inoperative intervals of such length that the rectifier again cools practically to normal ambient temperature, much greater plate overloads are practicable.

Design of Stacks by Direct Values

After choosing the size of plate with proper current-carrying capacity, the internal voltage drop of the plate is considered. Figure 5 illustrates the average experimentally determined internal voltage drop for seven plates in either bridge or center-tap single-phase circuits. The voltage drop per plate, designated as dv, is one half of the difference between the rms values of voltages read on the input and output side of the rectifier. These quantities are plotted as ordinates against the arithmetical values of output current in amperes plotted as abscissa.

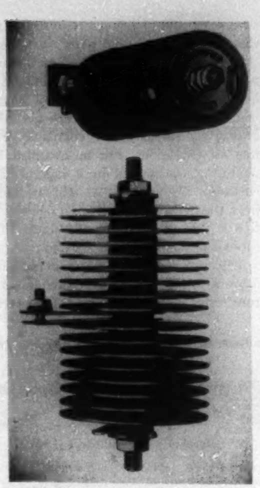

Figure 3. Two half-wave selenium rectifiers for operation of magnets in business machines

Both stacks are equipped with bimetal strips to open the circuit when the plate temperature exceeds a specified value

The output voltage of a selenium rectifier is determined by the input voltage V_{ac} less the total voltage drop within the rectifier. The computation of the necessary a-c voltage to be impressed on the selenium rectifier involves consideration of the voltage drop per plate and the number of plates through which the current flows.

Table IV. Selenium-Rectifier Design Constants: k_1 = Form Factor; n = Number of Plates in Series; k_2 = Circuit Factor; V_p = Maximum Voltage Per Plate; V_{ac} = Phase Voltage, Except Three-Phase Bridge Where It Is Line Voltage

Number of Phases	Circuit Type	k_1	n	k_2
1	Half-wave	2.3	$\frac{V_{ac}}{V_p}$	1
1	Bridge	1.15	$\frac{V_{ac}}{V_p}$	2
1	Center-tap	1.15	$\frac{2V_{ac}}{V_p}$	1
3	Half-wave	0.855	$\frac{\sqrt{3}V_{ac}}{V_p}$	1
3	Bridge	0.74	$\frac{V_{ac}}{V_p}$	2
3	Center-tap	0.74	$\frac{2V_{ac}}{V_p}$	1

Using the data given in Tables I, II, and III, and the internal voltage drop per plate illustrated in Figure 5, any single-phase selenium rectifier can be designed and the necessary a-c voltage computed by the following formula:

$$V_{ac} = k_1 V_{dc} + k_2 n \text{ dv} \tag{3}$$

where V_{ac} is the input voltage, k_1 the form factor to convert the arithmetical value to the rms voltage value (Table IV), V_{dc} the required d-c output voltage, k_2 the number of arms through which the current must pass in the circuit for each half-cycle, n the number of plates in series per arm, and dv the voltage drop per plate for the circuit employed. The constants k_1 and k_2 vary depending upon whether a bridge or center-tap connection, a single or three-phase circuit is employed.

As an example, let us design a bridge-type unit required to deliver four amperes at 16 volts continuously under the maximum ambient temperature of 35 degrees centigrade in a single-phase circuit. Selenium plate 7, $4^3/_8$ inches in diameter

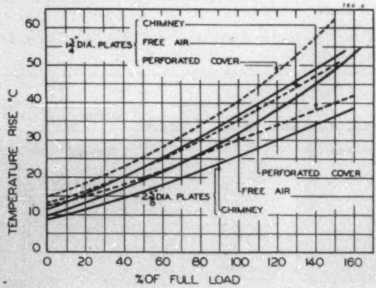

Figure 2. Temperature characteristics of two selenium rectifiers under three different cooling conditions

1. In a cabinet with perforated top and bottom covers
2. In free air
3. In a chimney-like enclosure fully open at the top and bottom

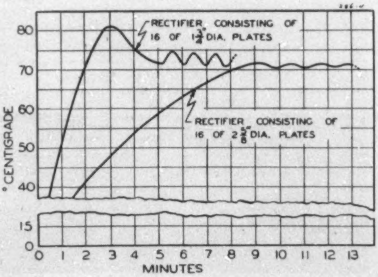

Figure 4. Time-temperature characteristics illustrating the performance of selenium rectifiers equipped with bimetal strips (Figure 3)

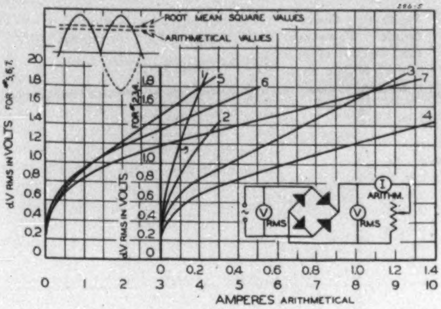

Figure 5. Rectification characteristics of seven basic plates ($^3/_4$, 1, $1^3/_8$, $1^3/_4$, $2^5/_8$, $3^3/_8$, and $4^3/_8$ inches in diameter) used in the design of single-phase bridge and center-tap rectifiers for inductive and resistive loads

(listed in Table I and rated at four amperes) will serve the purpose. Using equation 3 and corresponding constants from Table IV:

$$V_{ac} = 1.15 \times 16 + 2n \text{ dv}$$

The number of plates in series, that is, quantity n, necessary at this stage, can be computed by the following formula:

$$n = \frac{k_1 V_{dc}}{V_p - 2 \text{ dv}} \qquad (4)$$

V_p for plate 7 is 14, and dv, as read off characteristic 7 in Figure 5, is 1.29; hence

$$n = \frac{1.15 \times 16}{14 - 2 \times 1.29} = 1.6 \text{ or 2 of number 7 plates in series}$$

Figure 7. Characteristic illustrating the relation of F_v and N for single-phase half-wave, center-tap, and bridge rectifier circuits for battery-charging application or capacitive loads

V_{ac}, therefore, at the start of service, is equal to 23.6 volts. With the aging of the selenium rectifier, the forward resistance increases, and the inverse resistance also slightly increases. Based on lengthy experience in the design and application of these rectifiers, it can be stated that the only variable quantity of equation 3 is dv, which may increase as much as 50 per cent under most adverse conditions. An additional tap, therefore, should be provided in the transformer winding to give the V_{ac} required for the aged condition:

$$V_{ac} = 1.15 \times 16 + 2 \times 2 \times 1.29 \times 1.5 = 26 \text{ volts}$$

Designs by Relative Values

The foregoing example and reference to Figure 5 characteristic apply only to the single-phase bridge and center-tap designs with either inductive or resistive loads. With capacitive and battery-charging loads, even in the above-mentioned circuits, intermittent loading on the selenium plates occurs with periodic values of the forward current greater than the periodic values for the same d-c output feeding resistive or inductive load; voltage drops per plate are, therefore, greater in the former case. Applications occur, however, where the voltage drop per plate is smaller than the values shown in Figure 5. An example is the three-phase circuit where the rectified current is practically at the peak value of the applied alternating current. The output current density per plate in these circuits is considerably higher than in the case of a single-phase bridge circuit; furthermore, the type of load, whether it be resistive, inductive, capacitive, or battery charging, has practically no effect on the voltage drop per plate.

Inasmuch as a wide variety of circuits and types of loading is encountered, a

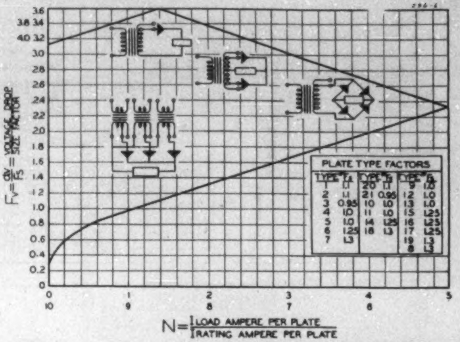

PLATE TYPE FACTORS

TYPE #	F_s	TYPE #	F_s	TYPE #	F_s
1	1.1	20	1.1	9	1.0
2	1.1	21	0.95	12	1.0
3	0.95	10	1.0	13	1.0
4	1.0	11	1.0	15	1.25
5	1.0	14	1.25	16	1.25
6	1.25	18	1.3	17	1.25
7	1.3			19	1.3
				8	1.3

Figure 6. Characteristic illustrating the relation of F_v and N for single-phase half-wave, center-tap, and bridge rectifier circuits with resistive or inductive load; also for three-phase half-wave rectifier circuit with all types of load

Figure 8. Characteristic illustrating the relation of F_v and N for three-phase center-tap and bridge rectifier circuits for all types of loads

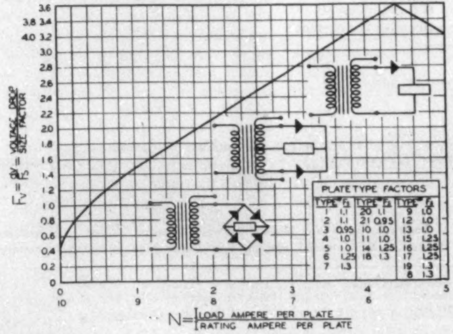

PLATE TYPE FACTORS

TYPE #	F_s	TYPE #	F_s	TYPE #	F_s
1	1.1	20	1.1	9	1.0
2	1.1	21	0.95	12	1.0
3	0.95	10	1.0	13	1.0
4	1.0	11	1.0	15	1.25
5	1.0	14	1.25	16	1.25
6	1.25	18	1.3	17	1.25
7	1.3			19	1.3
				8	1.3

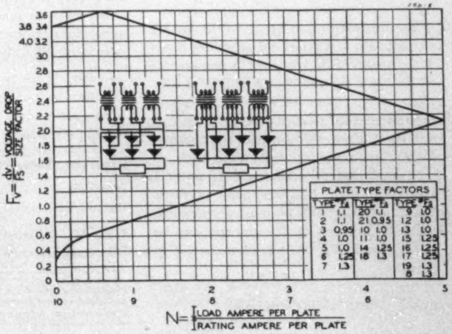

PLATE TYPE FACTORS

TYPE #	F_s	TYPE #	F_s	TYPE #	F_s
1	1.1	20	1.1	9	1.0
2	1.1	21	0.95	12	1.0
3	0.95	10	1.0	13	1.0
4	1.0	11	1.0	15	1.25
5	1.0	14	1.25	16	1.25
6	1.25	18	1.3	17	1.25
7	1.3			19	1.3
				8	1.3

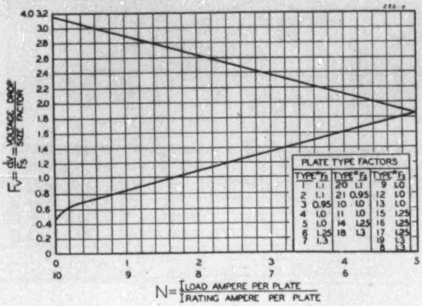

Figure 9. Characteristic illustrating the relation of F_v and N for d-c and blocking circuits

method for rating the 21 rectifier types according to relative values of output current and voltage drop per plate has been developed. Figures 6, 7, 8, and 9 illustrate the relationships N and F_v for various types of circuits and loads.

The use of these characteristics involves first, determination of the value N, which is obtained by dividing the actual ampere load per plate by the ampere rating of the basic plate employed. With this value determined, the value of F_v is read from one of the characteristics and then multiplied by the plate size factor F_s to obtain the actual voltage drop per plate, dv for the plate selected.

Figure 6 illustrates the relationship of F_v to the value N for half-wave, center-tap, and bridge single-phase circuits when loaded with either resistive or inductive load; also for half-wave three-phase circuits for all types of loads. Figure 7 gives a similar relationship for F_v to the value N for half-wave, center-tap, and bridge circuits, all single-phase for capacitive or battery-charging applications. Figure 8 shows the relationship of F_v to N for three-phase bridge and center-tap circuits for all types of loads. Finally, Figure 9 shows the relationship of F_v to N for d-c and blocking circuit applications.

To illustrate this method of design, let us compute a three-phase selenium rectifier capable of delivering 325 amperes at 13 volts for the filament supply of a television transmitter tube. An additional requirement is that the rectifier deliver not more than 488 amperes at approximately two volts into tube when cold.

Several plates in Tables I, II, and III should be tried; however, plate 13, rated at 3.3 amperes, will be most economical; the total current of 325 amperes can be safely handled by 100 plates connected in parallel.

$$I_{dc} = \frac{325}{100} = 3.25 \text{ amperes per plate}$$

This plate loading with 1.8 ampere rating for basic plate 5 used in type 13 will give

$$N = \frac{3.25}{1.8} = 1.8$$

From Figure 8, $F_v = 1.07$ and $\text{dv} = 1.07 \times 1 = 1.07$.

In aging, the dv value may increase 50 per cent, and thus becomes 1.6. Substituting the known quantity in formula 4:

$$n = \frac{0.74 \times 13}{18 - 2 \times 1.6} = 0.65; \text{ or 1 plate in series}$$

The foregoing proves that the entire rectifier should consist of a total connection of (6-1-100), where the first number (6) designates the number of arms of the three-phase circuit, the second number (1) indicates the number of plates in series, and the third number (100) gives the number of plates in parallel. The total of 600 plates of type 13 may be conveniently assembled into 24 stacks, each having 25 plates in parallel.

Using formula 3 and the design constants of Table IV:

$$V_{ac} = 0.74 \times 13 + 2 \times 1 \times 1.07 = 11.8 \text{ volts}$$

when rectifying elements are new;

$$V_{ac} = 0.74 \times 13 + 2 \times 1 \times 1.6 = 12.8 \text{ volts}$$

when they are fully aged.

In order to meet the requirements of approximately two volts with the current output of 488 amperes:

$$I_{dc} = \frac{488}{100} = 4.88 \text{ amperes per plate}$$

$$N = \frac{4.88}{1.8} = 2.7$$

From the characteristic of Figure 8

$$F_v = 1.37$$

For the new stacks, therefore, $\text{dv} = 1.37 \times 1 = 1.37$, and for fully aged stacks

$$\text{dv} = 1.37 \times 1.5 = 2.06$$

Using formula 3 and these two values of dv, the new and aged V_{ac} will be found to be 4.2 and 5.6 volts, respectively. The 50 per cent limit for the current requirement is actually met by the external limiting reactances in each phase of the three-phase circuit. The complete assembly of this rectifier is shown in Figure 10.

The ordinary single-phase bridge-type rectifier for resistive loading can also be designed through the use of relative values. Figure 11 illustrates a two-ampere, 120-volt telautograph unit.

With two number 9 plates in parallel, or one ampere per plate:

$$N = \frac{1}{0.6} = 1.67$$

From the characteristic of Figure 6

$$F_v = 1.2 \qquad \text{dv} = 1.2 \times 1.0 = 1.2$$

Hence

$$n = \frac{1.15 \times 120}{18 - 2 \times 1.2 \times 1.5} = 9.6, \text{ or 10 plates in series}$$

The total connection of the rectifier is then 4-10-2. Practical considerations suggest four stacks, two of which make one bridge with ten plates in each arm of the bridge. Again, from formula 3, corresponding constants of Table IV and the above dv value, V_{ac} is found to be 162 volts when new and 174 volts when aged.

Ratings (Tables I, II, and III) of selenium rectifier plates, functioning as blocking valves in d-c circuits, are higher in current and lower in voltage value than they are in half-wave alternating circuits. The higher current rating is acceptable, inasmuch as the forward resistance ordinarily decreases when only forward current passes through the selenium rectifier plates. The reverse current of the blocking unit, on the other hand, is higher, and the safe voltage limit is, therefore, more conservatively established than for a-c circuits. As an example, a 30-volt, 4.5-ampere blocking unit consists of a total connection of 1-2-5 number 5 plates. The value N for this unit is equal to one, and dv is 0.84 (Figure 9).

Experience has shown that for constant-current battery-charging and capacitive loading, the current rating should be only

Table V. Relation of Factor k_3 by Which Normal Rating of Number 7 (That Is, $4^3/_8$-Inch-Diameter) Selenium Plates Can Be Increased, and the Speed and Amount of Air Necessary

Multiplying factor for normal plate ratings, k_3	1	1.5	2	2.5	3	3.5	4	4.5
Air speed in feet per minute	0	60	90	120	160	200	310	400
Cubic feet per minute per plate	0	0.5	0.8	1.1	1.4	1.7	2.8	3.7

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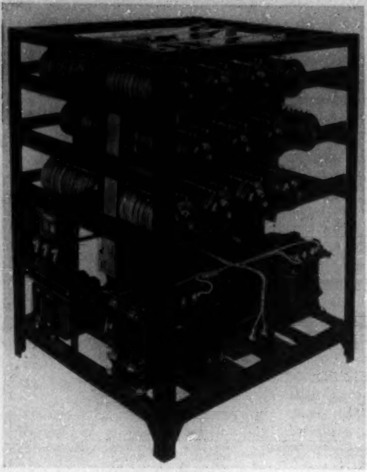

Figure 10 (left). Rectifier unit of 325-ampere 13-volt rating with reactances limiting output current to 488 amperes at two volts

A-c input: 220 volts, three phases, 60 cycles. Rectifying element consists of 600 plates arranged in 24 stacks, each stack consisting of 25 plates in parallel

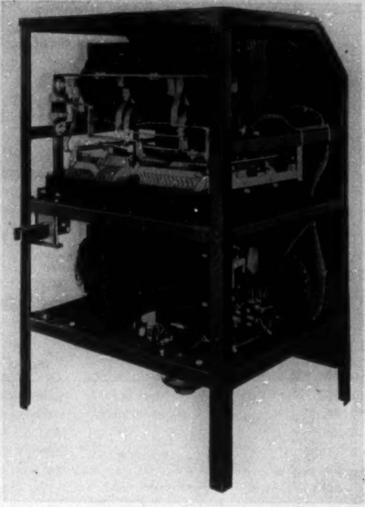

Figure 12 (right). Three-phase selenium rectifier unit employing forced-draft ventilation for output of 600 amperes at six volts

Over-all efficiency: 74 per cent; power factor: 94 per cent

80 per cent of the values tabulated in Tables I, II, and III. In the design of the rectifier for charging a 60-cell battery at the rate of 0.4 ampere, with 2.4 volts per cell, plate 4 may be selected. The value of N is 0.67. The new dv, as read off the characteristic of Figure 7, is 1.25 and, after aging, it becomes 1.87. The number of plates in series is determined either by

$$n=\frac{V_b}{V_p} \tag{5}$$

$$\text{or } n=\frac{V_b/\sqrt{2}}{V_p-2\text{ dv}} \tag{6}$$

depending on which is greater.

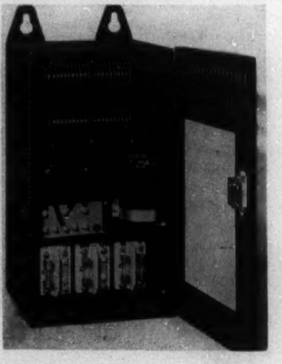

Figure 11. Selenium rectifier with output of two amperes at 120 volts for powering telautograph equipment

$$V_{ac}=\frac{V_b}{\sqrt{2}}+k_2 n\text{ dv} \tag{7}$$

$$=\frac{60\times 2.4}{\sqrt{2}}+2\times 8\times 1.25=122\text{ volts}$$

An additional tap to give 132 volts for the fully aged condition of the stacks should be provided.

Design of Forced-Draft-Ventilation Unit

The extended current rating of selenium rectifier plates with forced draft or fan cooling and the speed of air constituting the forced ventilation require further consideration. A rather conservative relationship of current rating to air velocity has been established and successfully used for the $4^3/_8$-inch.-diameter number 7 plate (Table V).

As an example, let us design a three-phase full-wave rectifier supplying a d-c output of 600 amperes at six volts. Because of the low required output voltage, as compared to the full rms voltage permissible for the number 7 plate, the center-tap circuit is most economical and gives greatest efficiency. With the fan delivering air at a speed of 120 feet per minute, the 7.5-ampere (Table I) loading of this plate can be increased 2.5 times (Table V), thus making it 18.7 amperes per plate. Practical consideration of possible 10 per cent overload suggests that this unit should have 36 plates in parallel for the total current output of the unit. This makes the value N (Figure 8) equal to 2.22, and the initial and aged dv equal to 1.6 and 2.4 respectively. The total connection of the rectifier is, therefore, 6-1-36. The new V_{ac} for half of the transformer secondary voltage is 6.1 volts, and for the fully aged condition 6.9 volts. A view of this equipment is shown in Figure 12.

Voltage Regulation

The inherent voltage regulation of the selenium rectifier is in the neighborhood of 10 to 20 per cent. In computing the regulation, one must determine the no-load

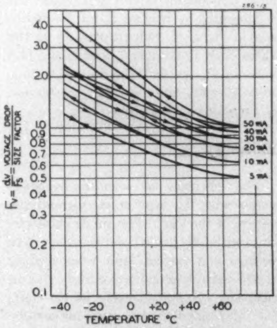

Figure 13. Relation of F_v at varying current densities to the ambient temperatures throughout a heating and cooling cycle

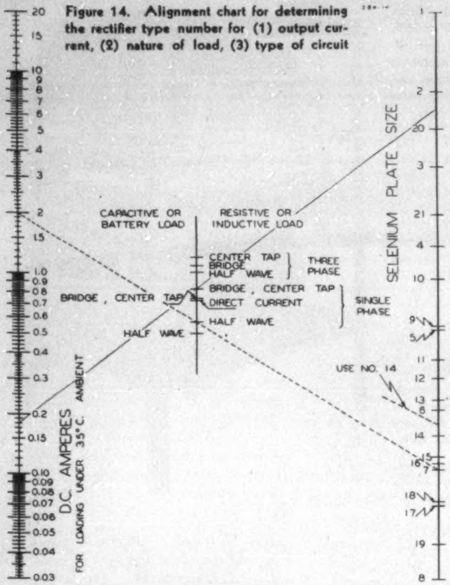

Figure 14. Alignment chart for determining the rectifier type number for (1) output current, (2) nature of load, (3) type of circuit

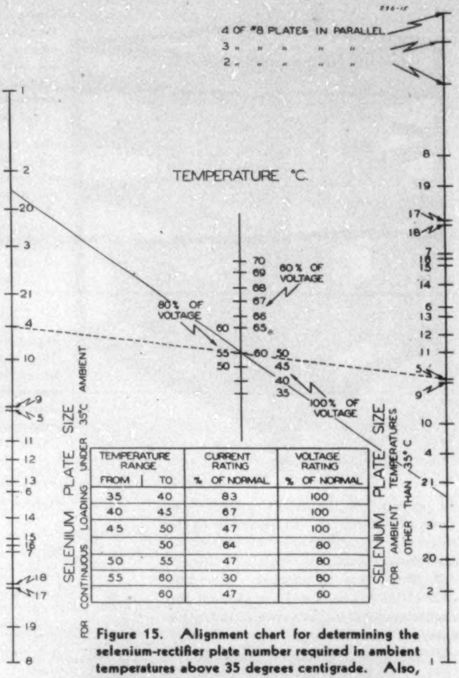

Figure 15. Alignment chart for determining the selenium-rectifier plate number required in ambient temperatures above 35 degrees centigrade. Also, table of percentage of current and voltage ratings at ambient temperatures above 35 degrees centigrade

TEMPERATURE RANGE FROM	TEMPERATURE RANGE TO	CURRENT RATING % OF NORMAL	VOLTAGE RATING % OF NORMAL
35	40	83	100
40	45	67	100
45	50	47	100
	50	64	80
50	55	47	80
55	60	30	80
	60	47	60

value of dv and then the no-load output voltage. In the case of the first example of a four-ampere, 16-volt rectifier, the dv value from Figure 5 is 0.4. The d-c output voltage, therefore, at no load is

$$V_{dc} = \frac{23.6 - 2 \times 2 \times 0.4}{1.15} = 19.1$$

$$\text{Regulation} = \frac{19.1 - 16}{16} \times 100 = 19.4 \text{ per cent}$$

Similarly, the regulation of the three-phase 325-ampere 13-volt unit (Figure 10) is computed by reading F_r for N equal to zero in Figure 8. Substituting 0.3 for the value of dv in formula 3, and taking 11.8 volts for V_{ac}, one finds the no-load voltage to be 15 volts, and the voltage regulation is, therefore, 15.4 per cent.

Efficiency

The efficiency of selenium rectifiers varies with the type of circuit and the nature of the load. The single-phase circuits with fully loaded plates in respect to voltage and current, and when feeding either resistive or inductive loads, give an efficiency of approximately 64 per cent; the same circuits, when used for battery charging give an efficiency some 14 per cent higher due to the greater value of the rectified voltage. The three-phase circuits give efficiency values in the neighborhood of 83 per cent and, for all practical purposes, remain the same irrespective of the type of load. For all circuits and loads, however, the efficiency of selenium rectifiers increases with decrease of load down to approximately 25 per cent of full value, and thereafter falls off rapidly.

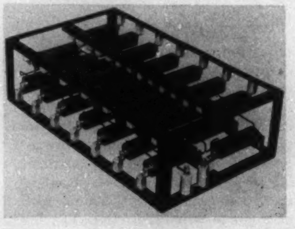

Figure 16. Single-phase bridge-connected rectifier

Output: 180 milliamperes at 2,300 volts under ambient temperatures from −40 to +60 degrees centigrade

The efficiency itself depends on the combined losses in the selenium rectifier from forward and reverse currents, and, in formula form, it is:

$$\frac{V_{dc}I_{dc}}{(V_{dc}I_{dc}) + W_f + W_r} \times 100 = \% \text{ efficiency} \quad (8)$$

where W_f are losses due to forward current, and W_r are losses due to the reverse current.

The computation of exact efficiency of all sizes of plates and various loads and circuits is rather involved and constitutes an extensive subject in itself.

In order to illustrate the simplified method of efficiency computation and the effect of dv changes on its value, let us compute the efficiency of the 325-ampere 13-volt three-phase unit illustrated in Figure 10.

Forward losses per plate in the three-phase bridge circuit are

$$w_f = \frac{\sqrt{2} I_{dc} \, \mathrm{dv}}{3}$$

where $\sqrt{2}$ is a conversion factor for approximating the peak value of the a-c wave in terms of effective value of dv. Divisor 3 results from the fact that in three-phase bridge circuit each plate is utilized $2\times{}^1/_6$ times in each cycle.

$$w_f=\frac{325/100\times\sqrt{2}\times1.07}{3}=1.64 \text{ watts per plate}$$

or $W_f=6\times100\times1.64=984$ watts for all 100 plates in six arms.

The reverse losses are approximately one third of forward losses computed for the normal rating of the plate used in this unit

$$W_r=\frac{1}{3}\times\frac{6\times100\times0.8\times\sqrt{2}\times1.8}{3}=136 \text{ watts}$$

The new input wattage W, therefore, is

$$\begin{aligned}W&=(I_{dc}\times V_{dc})+W_f+W_r\\&=(325\times13)+984+136=5{,}345 \text{ watts}\end{aligned}$$

$$\text{Efficiency}=\frac{4{,}225}{5{,}345}\times100=79.2 \text{ per cent}$$

Similarly, assuming a possible 50 per cent change in dv, the efficiency of this rectifier with stacks fully aged is computed as 72 per cent.

The foregoing designs illustrate the importance of the quantity dv, which is dependent on the forward resistance and current density of the plates, as well as the ambient temperature under operating conditions. Its changing values under varying conditions greatly influence the efficiency, regulation, and aging of selenium rectifiers. The ambient temperature and current density relationships affecting the value of dv are illustrated in Figure 13. The arrows on the curves indicate that the resistance of the plates decreases with increase of temperature. As the plates cool off, the resistance again increases, and dv is greater at the new lower temperatures than during the rising-temperature phase of the heating cycle. This phenomenon diminishes with lower current densities to a point where the resistance is the same at corresponding temperatures of the heating and cooling portions of the cycle.

Selection of Plate Type for 35 Degrees Centigrade and Higher Ambient Temperature

Almost invariably more than one selenium plate type appears suitable for given output requirements. The type of circuit or nature of loading, as well as the cost, however, restricts the choice.

The alignment chart (Figure 14) has been found useful in selecting plate types for specified output currents. If a straight edge is laid connecting the required d-c output with the type of circuit and the nature of load, the intersection of the straight edge on the plate size scale gives the type number of the required rectifier plate. If the intersection falls between two plate type numbers, the plate having the higher current rating should be chosen.

For ambient temperatures higher than 35 degrees centigrade, the plate type number for a 35 degrees centigrade ambient should first be found. Referring to Figure 15, a straight edge connecting the 35 degrees centigrade ambient temperature plate type number with the desired higher ambient value of the temperature scale intersects the right-hand scale, indicating the required higher ambient plate type number. Again, if the intersection is between two plate type numbers, the plate type number with the higher current rating should be used. It will be noted that, by decreasing the voltage rating, a small increase in current rating is allowable.

As an example, let us design a high-voltage low-current rectifier of the type illustrated in Figure 16 for either resistive or inductive load. Referring to Figure 14, the line drawn through the 0.18 reading on the left-hand scale, and the point marked "single-phase bridge" in the middle of the chart, intersects the right-hand scale between "2" and "20." Thus, plate 20 (Table II) would be used if the rectifier is to operate under maximum ambient temperature of 35 degrees centigrade. In order to derate plate 20 for a 60 degrees centigrade ambient, reference is made of Figure 15. The line drawn on this chart through the same point (between "2" and "20") of the left-hand scale (as in Figure 14) and the point marked "60 per cent of voltage" indicates that the number 21 selenium plate (Table II) should be used for the required assembly. Further computations of quantities N, dv, and n result in the design of a rectifier with total connections of 4-224-1, arranged in 28 stacks, each consisting of 32 of $1^3/_8$-inch plates in series.

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Rectifier Terminology and Circuit Analysis

C. H. WILLIS
FELLOW AIEE

C. C. HERSKIND
MEMBER AIEE

ANYONE reading several articles on the subject of rectifiers must be impressed by the confusion caused by a lack of exact terminology. This paper presents definitions of a few terms representing the most important concepts arising in the rectifier field. The definitions are presented with the hope that they will be found sufficiently useful to be widely adopted. Constructive criticism of the terms and definitions is invited.

These terms have been defined to conform with the general practice in the power-rectifier field. It is to be hoped, however, that the concepts will be sufficiently broad and accurate to be generally useful in all fields where rectifiers are employed.

In many of the definitions the term "circuit element" is employed. This has not been defined, because it is in general use in electrical literature and has been defined elsewhere.[1] The definitions suggested are given below.

Rectifier Terms

1. *Rectifying Element.* A rectifying element is that circuit element which has the property of conducting current effectively in only one direction.

In arc rectifying devices, the portion of the circuit including an anode, its cathode, and the arc space between the anode and cathode, is the rectifying element.

When a group of rectifying units is connected, in either parallel or series arrangement, to operate as one circuit element, the group of rectifying units should be considered as a rectifying element.

2. *Double-Way Rectifier* (*Double-Direction Rectifier*). A double-way rectifier is a rectifier in which all parts of the alternating-voltage circuit elements, conductively connected to the rectifying elements, conduct current in both directions (that is, in alternate half-cycles).

3. *Single-Way Rectifier* (*Single-Direction Rectifier*). A single-way rectifier is one in which no portion of the alternating-voltage circuit elements, conductively connected to the rectifying elements, conducts current in both directions (that is, in alternate half-cycles) because of the action of the rectifying elements.

4. *Simple Rectifier.* A simple rectifier is a rectifier in which the total direct current at any instant, exclusive of the commutating time, flows through a single rectifying element for a single-way rectifier, or flows through two rectifying elements in series (one on either side of the a-c circuit element) for a double-way rectifier.

5. *The Number of Phases in a Simple Rectifier.* The number of phases in a *simple single-way* rectifier is equal to the number of rectifying elements.

The number of phases in a *simple double-way* rectifier is equal to half the number of rectifying elements.

A double-way rectifier may have a primary-current wave shape and a d-c ripple voltage corresponding to a single-way rectifier having twice the phases of the double-way rectifier.

In describing a *simple rectifier*, three items of information are usually necessary and sufficient:

(*a*). **The number of phases of the rectifier.**

(*b*). **Classification as either single way or double way.**

(*c*). **The transformer-circuit description if transformers are used.**

Example: A three-phase single-way rectifier using delta zigzag wye transformers.

6. *Multiple Rectifier.* A multiple rectifier is one in which two or more similar simple rectifiers are connected in such a way that their direct currents add, but their d-c voltage ripples do not coincide. Interphase transformers are usually required between the component simple rectifiers in a multiple rectifier.

When two or more groups of rectifying elements operate so that their d-c currents add and their d-c voltage ripples coincide, the groups of rectifying elements are in parallel.

In describing a multiple rectifier, four items of information are usually given, and a fifth, the classification as single-way, is usually assumed. The information expressly stated is:

(*a*). **The primary wave shape, or the number of phases in a simple single-way rectifier to give the same primary wave shape.**

(*b*). **The number of component simple rectifiers in the multiple rectifier.**

(*c*). **The number of phases in a component simple rectifier.**

(*d*). **The transformer-circuit description if transformers are used.**

Example: a 12-phase quadruple wye (single-way) rectifier, using delta zigzag wye transformers.

7. *Cascade Rectifier.* A cascade rectifier is a rectifier in which two or more similar simple rectifiers are connected in such a way that their d-c voltages add.

8. *Commutating Reactance.* The commutating reactance is the reactance which effectively opposes normal currens transfer between rectifying elements which conduct consecutively.

In the actual rectifier circuit this reactance is usually distributed in various parts of the circuit; for example, the anode leads, the transformer windings, and the a-c supply system. In many cases these reactances may be considered as lumped in the transformer secondary.

9. *Reactance Factor* (*Commutating-Reactance Factor*). The commutating-reactance factor is the per-unit commutating reactance calculated on the arbitrary base of the rms value of the secondary line-to-neutral voltage divided by the direct current commutated.

Particular attention is called to the terms "double-way" rectifier and "single-way" rectifier. These terms are suggested to replace the terms "half-wave" and "full-wave" as frequently employed. There are two arguments against the old terms. In the first place, they have been used by two groups in different ways. For example, the circuit shown in Figure 1 is sometimes described as a "full-wave" circuit. It is also described as a "half-wave" circuit by a substantial group.

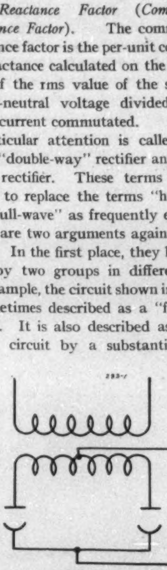

Figure 1. Single-way rectifier circuit

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C. H. Willis is professor of electrical engineering, Princeton University, Princeton, N. J., and C. C. Herskind is electrical engineer, power rectifier department, General Electric Company, Schenectady, N. Y.

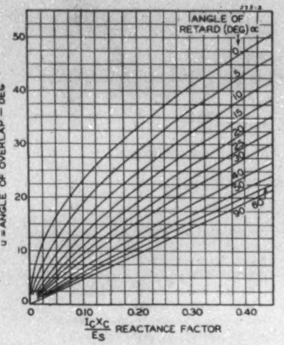

Figure 2. Angle of overlap—characteristic curves. $P = 3$

$$\cos(u+\alpha) = \cos\alpha - \frac{I_C X_C}{\sqrt{2}E_S \sin\frac{\pi}{P}}$$

when $\alpha = 0$

$$\cos u = 1 - \frac{I_C X_C}{\sqrt{2}E_S \sin\frac{\pi}{P}}$$

With this background of confusion a new term seems the only solution. A further objection to the terms "full-wave" and "half-wave" is the fact that they are accurately descriptive only in the case of 180-degree conduction, which is not widely used in power rectifiers. The terms "double-way" and "single-way" describe accurately the physical difference which separates the two classes of rectifiers and results in different general relations for several current and voltage ratios.

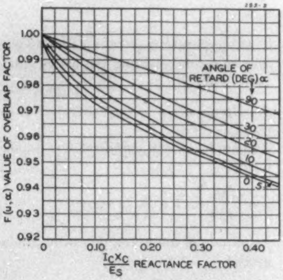

Figure 3. Overlap factor—characteristic curves. $C = 3$

$$F(u,\alpha) = \sqrt{1-CK(u,\alpha)}$$

when $\alpha = 0$

$$F(u) = \sqrt{1-CK(u)}$$

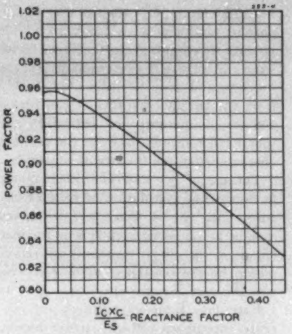

Figure 4. Power factor—characteristic curve for delta double-wye rectifier. $P = 3$, $C = 3$

$$PF = \frac{3}{\pi}\cdot\frac{1}{\sqrt{1-3K(u)}}\cdot\left(1-\frac{E_x}{E_{do}}\right)$$

The "simple rectifier" is an essential step in the development of the more complicated rectifier circuits, and it seems necessary to employ this term.

The definition of the number of phases as given is required if a general relation for the d-c voltage is to be employed. This definition is logical and consistent. Unfortunately the biphase rectifier as here defined has frequently been classed as a single-phase rectifier.

The "multiple rectifier" is here restricted to the case of several simple rectifiers operating in parallel in such a way that their d-c voltage ripples do not coincide. This is a unique arrangement in rectifier practice which improves the operation on both the a-c and d-c sides. Some name for this type of rectifier is needed.

The effect of commutating reactance in causing d-c voltage regulation is a source of much confusion. It is to be hoped that an exact definition of commutating reactance may help to dispel some of this confusion.

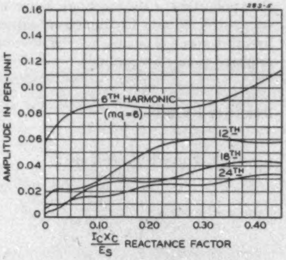

Figure 5. Amplitude of harmonic voltages in d-c voltage wave—characteristic curves. $q = 6$

$\alpha = 0$
$m = 1, 2, 3,$ and 4
Per-unit base is E_{do}

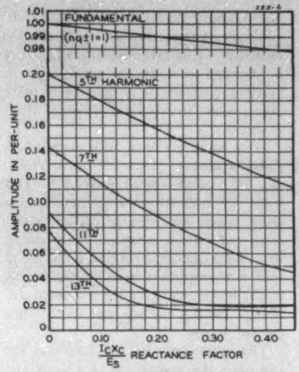

Figure 6. Amplitude of harmonic currents in a-c line current—characteristic curves. $q = 6$

$\alpha = 0$
$n = 0, 1, 2$
Per-unit base is amplitude of fundamental with no overlap

Reactance Factor

The methods employed in analyzing rectifier circuits and the formulas for rectifier-circuit characteristics have been quite thoroughly covered in the literature and are now well understood. However, no standard procedure has been devised for making the circuit calculations or presenting the calculated data, and as a result

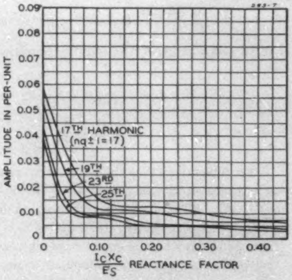

Figure 7. Amplitude of harmonic currents in a-c line current—characteristic curves. $q = 6$

$\alpha = 0$
$n = 3$ and 4
Per-unit base amplitude of fundamental with no overlap

the routine calculation of rectifier characteristics has been more laborious than necessary. Furthermore, with the lack of a standard method for presenting the calculated results, it is difficult to make comparisons between the different types of circuits.

Some attempts have been made to express the rectifier characteristics as a function of a single-circuit factor.[2-5] This work has indicated the usefulness of a factor which combines the current, reactance, and voltage constants of the circuit in a single quantity, although the use of such a quantity has been limited largely to the determination of the harmonic content of the d-c voltage and the a-c line current waves.

This paper proposes the definition and advocates the general use of a single factor of this kind for the purpose of simplifying the description and calculation of the operating conditions of any rectifier circuit. It is further proposed that this factor be known as the "reactance factor" and that it be the product of the direct current commutated I_C and the line-to-neutral commutating reactance X_C divided by the rms value of the transformer secondary line-to-neutral voltage E_S, that is, I_CX_C/E_S.

Circuit Analysis

The rectifier-circuit characteristics depend to a large degree upon the effect of reactance in causing an overlapping of the anode current waves The amount of overlapping is given by the well-known equation for the angle of overlap, which follows:

$$\cos u=1-\frac{I_CX_C}{\sqrt{2}E_S\sin\frac{\pi}{P}}$$

With phase control, the overlap equation is

$$\cos(u+\alpha)=\cos\alpha-\frac{I_CX_C}{\sqrt{2}E_S\sin\frac{\pi}{P}}$$

It will be noted that the angle of overlap is a function of the "reactance factor," I_CX_C/E_S. Therefore, for any given value of the reactance factor, the angle of overlap will always be the same, regardless of the particular current, voltage, or reactance constants of the circuit. From this, it follows that if the magnitudes of the current and voltage waves in the rectifier circuit are expressed in per-unit quantities upon a suitable base, the configuration of the current and voltage waves will be the same for any given value of the reactance factor, regardless of the voltage, current, and reactance constants of the circuit. Inasmuch as the reactance factor enters directly in the determination of the overlap, and may be calculated readily from the circuit constants, it provides a convenient quantity for indicating the functional relations between the rectifier characteristics and the circuit constants.

The use of per-unit quantities greatly facilitates the calculation of rectifier circuits. One set of characteristic curves may be prepared for each type of rectifier circuit, if the circuit constants are represented by the reactance factor I_CX_C/E_S and the rectifier characteristics, such as voltage drop, harmonic voltages, and currents, interphase voltages, power factor, and so forth are expressed in per unit. Using such a procedure, it is possible to reduce rectifier calculation to a minimum, calculating the characteristic curves only once. For example, to obtain information regarding any specific circuit, its reactance factor is first calculated from the circuit constants. Reference is then made to the characteristic curves, and the corresponding per-unit value of the desired characteristic determined. The actual numerical value may be calculated by multiplying this per-unit value by the base value. The theoretical d-c voltage E_{do} and the rms value of the transformer secondary line-to-neutral voltage E_S are convenient bases for calculation of per-unit voltages, while the direct current commutated I_C and the theoretical a-c line current I_L are convenient bases for per-unit currents.

The list of rectifier formulas and the accompanying curves show a few of the more commonly used rectifier characteristics, with their equations reduced to their simplest form in terms of reactance factor and the calculated values given on a per-unit basis. These curves illustrate the application of the reactance factor to one type of circuit. Characteristic curves for other types of circuits may readily be prepared from the equations.

Rectifier Formulas

1. Angle of overlap u (Figure 2)

$$\cos(u+\alpha)=\cos\alpha-\frac{1}{\left(\sqrt{2}\sin\frac{\pi}{P}\right)}\frac{I_CX_C}{E_S}$$

(with no phase control)

$$\cos u=1-\frac{1}{\left(\sqrt{2}\sin\frac{\pi}{P}\right)}\cdot\frac{I_CX_C}{E_S}$$

2. Overlap factor $\sqrt{1-cf(u,\alpha)}$ (Figure 3)

where

$$f(u,\alpha)=\frac{1}{2\pi}\left[\frac{\sin u(2+\cos(u+2\alpha))-u(1+2\cos\alpha\cos(u+\alpha))}{(\cos\alpha-\cos(u+\alpha))^2}\right]$$

3. D-c voltage drop E_X

$$E_X=\frac{I_CX_CP}{2\pi}$$

(in per unit on base E_{do})

$$\frac{E_X}{E_{do}}=\frac{1}{2\sqrt{2}\sin\frac{\pi}{P}}\cdot\frac{I_CX_C}{E_S}$$

4. Power factor PF (Figure 4)

$$PF=K\cdot\frac{\left(\cos\alpha-\frac{E_X}{E_{do}}\right)}{\sqrt{1-cf(u,\alpha)}}$$

5. Harmonics in d-c voltage (Figure 5)
Amplitude of sine components

$$a_m=\frac{E_{do}}{2}\cos m\pi\times\left[\frac{\sin(mq+1)(u+\alpha)+\sin(mq+1)\alpha}{mq+1}\quad\frac{\sin(mq-1)(u+\alpha)+\sin(mq-1)\alpha}{mq-1}\right]$$

Amplitude of cosine components

$$b_m=\frac{E_{do}}{2}\cos m\pi\times\left[\frac{\cos(mq+1)(u+\alpha)+\cos(mq+1)\alpha}{mq+1}-\frac{\cos(mq-1)(u+\alpha)+\cos(mq-1)\alpha}{mq-1}\right]$$

Amplitude of resultant

$$h_m=\sqrt{a_m^2+b_m^2}$$

All amplitudes may be expressed in per-unit values by dividing the above equations by E_{do}.

6. Harmonics in a-c line current (Figures 6 and 7)
Amplitude of sine component of fundamental

$$a_1=\frac{\sqrt{3}I}{\pi}[\cos\alpha+\cos(u+\alpha)]$$

Amplitude of cosine component of fundamental

$$b_1=\frac{\sqrt{3}I}{\pi}\left[\frac{{}^1\!/_2\sin(2u+2\alpha)-{}^1\!/_2\sin 2\alpha-u}{\cos\alpha-\cos(u+\alpha)}\right]$$

Amplitude of the resultant of the fundamental components

$$h_1=\sqrt{a_1^2+b_1^2}$$

Temperature-Aging Tests on Class-A-Insulated Fractional-Horsepower Motor Stators

J. A. SCOTT
MEMBER AIEE

B. H. THOMPSON
NONMEMBER AIEE

Synopsis: Temperature-aging data are presented on class-*A*-insulated fractional-horsepower motor stators, determined at 200, 160, and 135 degrees centigrade. Of particular interest is the relative aging time to failure at the different temperatures and the resultant temperature-aging curve. This curve shows a slope of 10 to 15 degrees centigrade increase in temperature to halve the aging time to failure in the temperature range covered, 135 to 200 degrees centigrade.

THE question of the rate at which motor insulation ages due to the temperature to which it is exposed was brought actively to the fore several years ago[1-7] by discussion concerning methods of rating machines whose normal load cycle was intermittent. Many such are encountered in air conditioning, welding, and other applications governed by automatic controls. In such intermittent operation the temperature of the motor varies, depending upon the load cycle to which it is subjected. It is desired that the effect of such temperature variation be not greater, as regards aging of the insulation, than the continuous temperature for which the motor was designed. In order to know this, it is necessary to have a knowledge of the rate of aging at different temperatures. This is really a demand for a knowledge of the aging temperature characteristic of insulation such as is shown in Figure 6.

To obtain such a temperature-aging characteristic, we must carry out our tests at more elevated temperatures, starting perhaps much higher than those in which we are interested, and, through simultaneously started tests at a number of temperatures between this and our operating range, gradually accumulate the data from which our life-temperature curve may be drawn as far down toward the operating range as the results lead us to continue.

Method of Test

The question of type of test must next be answered. The actual operation of motors of even moderate size, under load in numbers sufficient to yield statistically reliable results, becomes costly and subject to a number of causes of motor failure, such as bearings, starting switches, and so forth.

If we wish to study the influence of temperature on the insulation alone, it is desirable that the tests be conducted so that they are not complicated by these additional sources of failure which arise in an actual running test.

In the past[1] this has been attempted by constructing samples of a variety of kinds, aging them at a number of selected test temperatures, and periodically making a number of tests and measurements of properties, usually destructive. The study of these results enabled one to plot the deterioration of these arbitrarily selected properties in terms of time and temperature, and, with some approximation, to arrive at a temperature-aging relation. One of the present authors gave the results of such a study several years ago.[1] Such tests involve doing things to the sample of insulation such as breaking it, bending it, or otherwise maltreating it. Since service does not involve these factors in similar kind or degree, one usually finished his labors with a curiosity as to

Schedule of one-hour runs and number of days to failure

○—Run one hour without failing

●—Failed during run

$N=10$

Average life $=6.4$ days

$\sigma=0.7$ day

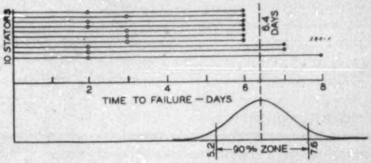

Figure 1. 200 degrees centigrade aging tests on class-*A*-insulated stators, type *N*

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J. A. Scott and B. H. Thompson are with the general engineering laboratory, General Electric Company, Schenectady, N. Y.

Amplitude of sine component of higher harmonics

$$a_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)(\cos\alpha-\cos(u+\alpha))}\times$$
$$[\cos\alpha\cos m\alpha-\cos(u+\alpha)\cos m(u+\alpha)+ m\sin\alpha\sin m\alpha-m\sin(u+\alpha)\sin m(u+\alpha)]$$

Amplitude of cosine component of higher harmonics

$$b_m=\frac{4I}{\pi}\cdot\frac{\sin(m\pi/3)}{m(m^2-1)(\cos\alpha-\cos(u+\alpha))}\times$$
$$[\cos\alpha\sin m\alpha-\cos(u+\alpha)\sin m(u+\alpha)- m\sin\alpha\cos m\alpha+m\sin(u+\alpha)\cos m(u+\alpha)]$$

Amplitude of the resultant for each of the higher harmonics

$$h_m=\sqrt{a_m^2+b_m^2}$$

The amplitude of the fundamental of the theoretical line current wave, that is, with no phase control ($\alpha=0$) and no overlap ($u=0$) is

$$a_1=\frac{2\sqrt{3}I}{\pi}\qquad b_1=0$$

All amplitudes may be expressed in per-unit values using the amplitude of the fundamental with no phase control and no overlap as a base by dividing the above equations by $\frac{2\sqrt{3}I}{\pi}$.

Refer to references 2, 3, and 5 for symbols not defined in text.

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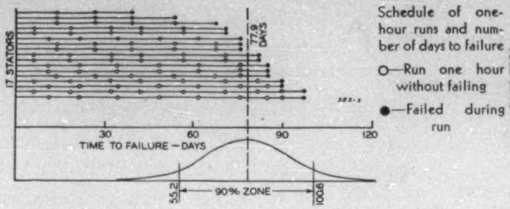


Figure 2. 160 degrees centigrade aging tests on class-*A*-insulated stators, type *N*

N=17 Average life=77.9 days
σ=13.8 days

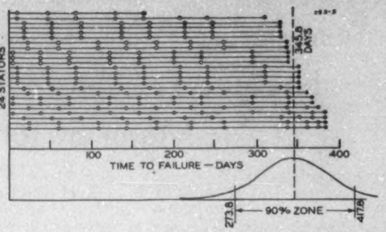


Figure 3. 135 degrees centigrade aging tests on class-*A*-insulated stators, type *N*

N=24 Average life=345.8 days
σ=43.7

what the actual service life at the test temperature might have been.

The present tests followed a course intermediate between the two alternatives outlined above, with the full recognition that the results could not be directly used to predict motor life in service, but hopeful that some light might be thrown on the temperature-aging relationship, even though only in a proportional manner. Recognizing that the completely wound and insulated stators of fractional-horsepower induction motors are both low cost and convenient specimens, and ones which represent a very widely used kind of class-*A* insulation, it was decided to use these for the test specimens. Exploratory tests and previous experience led us to choose aging temperatures of 200, 160, 135, and 115 degrees centigrade. To determine when these stators had temperature-aged to a nonserviceable condition, selected groups were removed from the aging ovens periodically and in turn assembled with the other parts of the motor (squirrel-cage rotor, end shields, bearings, and suitable switch arrangement), and subjected to an actual and severe operating test. This test consisted of a one-hour period of operation at no load, and approximately full-load heating, with a sudden and complete reversal in rotation from full speed every two minutes. On successful completion of this operation test the stators were restored to the aging oven for a continuation of the aging test. When the aging had proceeded to the point at which failures began to be experienced, tests were made more often. Proceeding in this way, an aging time was determined which reduced the insulation to such a state of embrittlement and crumbliness that physical separation was no longer afforded the conductors, and short circuits, or grounds, resulted. This aging time we call the time to failure of the insulation at that temperature for the specific conditions of the test.

Results

Figures 1, 2, and 3 show the results which have been obtained to date at 200, 160, and 135 degrees centigrade for one type of class-*A*-insulated stator, which we will, for purposes of identification, call *N*. Figures 4 and 5 show the results at 200 and 160 degrees centigrade on another type of class-*A*-insulated stator, which we will designate *M*1. Figures 8 and 9 show the results at 200 and 160 degrees centigrade on another type of class-*A*-insulated stator, designated *M*2. Stators *M*1 and *M*2 are similar except for winding differences.

These figures are similarly drawn and show:

1. The time for each stator at which test runs were made.
2. The time of the test run at which failure occurred; this is called the "time to failure" at that stator.
3. The average time to failure of the group of stators tested at each temperature.
4. The root-mean-square deviation, σ, of the time to failure of the individual stators from that of the group average. This is the customary statistical measure of the dispersion of test results.
5. A normal distribution curve calculated from the root-mean-square deviation, σ.
6. The width of the 90 per cent zone or the time interval during which approximately 90 per cent of all the motors fail. This total width is 3.29 σ.

We have purposely placed emphasis in this presentation on the normal variation in the life span, here at least 2 to 1. Life is inherently a variable thing, and engineers must accept this in their motors as they do in their own lives.

Having determined the "time to failure" of these three types of stators at several temperatures, the data can now be plotted in the form of the temperature-aging characteristic listed at the start as the object of our investigation. The average times to failure at the several test temperatures are plotted opposite each temperature. Figure 6 is such a curve for the type *N* stators from 200 down to 135 degrees centigrade (with another point at 115 degrees centigrade due to come at some time in the future). Figure 7 is for the type *M*1 stators, covering only the two temperatures—200 and 160 degrees

Figure 4. 200 degrees centigrade aging tests on class-*A*-insulated stators, type *M*1

N=10 Average life=10.8 days
σ=2.1 days

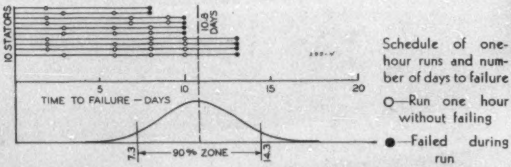
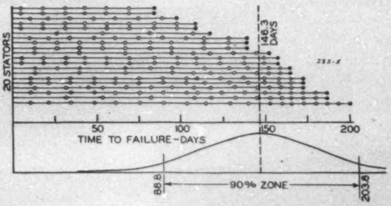


Figure 5. 160 degrees centigrade aging tests on class-*A*-insulated stators, type *M*1

N=20 Average life=146.3 days
σ=35.0 days



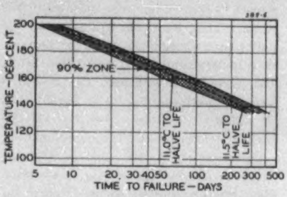


Figure 6. Temperature-aging characteristic, class-*A*-insulated stators, Type *N*

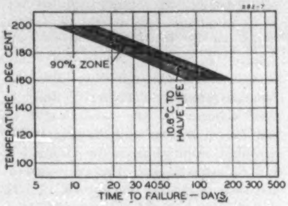


Figure 7. Temperature-aging characteristic, class-*A*-insulated stators, type *M*1

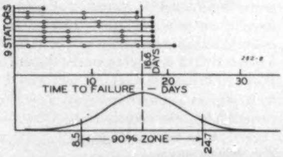


Figure 8. 200 degrees centigrade aging tests on class-*A*-insulated stators, type *M*2

Schedule of one-hour runs and number of days to failure

○—Run one hour without failing

●—Failed during run

N = 9 Average life = 16.6 days

σ = 4.9 days

centigrade, and similarly Figure 10 for the type *M*2 stator.

At the present time after 660 days elapsed time since starting, the tests on the *M*1 stators at 135 degrees centigrade have yielded five failures in 30 stators, 17 per cent. The 135 degrees centigrade point may thus be seen as lining up fairly well with the trend outlined by the two higher temperatures.

Conclusions

A method of studying the temperature-aging characteristics of class-*A*-insulated fractional-horsepower motor stators has been presented. The results available to date cover the tests made at 200 and 160 degrees centigrade and some results at 135 degrees centigrade. The tests are being continued at lower temperatures so that this paper is in the nature of a progress report. The following general statements can be made as to the comparative effects of various temperatures.

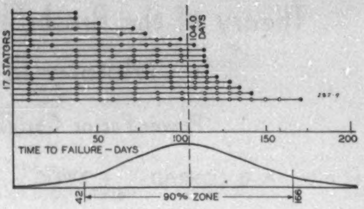


Figure 9. 160 degrees centigrade aging tests on class-*A*-insulated stators, type *M*2

Schedule of one-hour runs and number of days to failure

○—Run one hour without failing

●—Failed during run

N = 17

Average life = 104.0 days

σ = 37.5 days

1. Below 200 degrees centigrade there is no temperature which, if exceeded, causes an abrupt failure of the insulation; that is, the temperature-aging curve of the insulation of these motors is uniform and continuous.

2. Operation at higher temperatures than normal results in a shortening of the time to failure of the insulation. Figures 6 and 7 agree that a temperature increment of 10 or 11 degrees centigrade is required to cut the time to failure of the stators in half. This agrees nicely (without the implications of accuracy) with the eight-degree value widely used for transformer insulations.[8,9] Figure 8 gives a corresponding temperature increment of 15 degrees centigrade. The value has not yet been established below the 135 degrees centigrade range in these tests.

3. The width of the "90 per cent zone" (or zone containing 90 per cent of the values) is shown to emphasize the normal spread of results. Its width is obtained from Figures 1 through 5.

4. The normal time to failure of the insulation of duplicate specimens has a spread of 2 to 1.

It will be noted that no conclusion as to the probable actual operating life of the insulation for any given temperature has been drawn from these tests. This is for the reason that, as pointed out previously, the tests have been made in such a manner as to single out the effects due to temperature and to exclude other effects which would be met with in actual operation of the motor in service. This might include the effect of moisture, the effect of dirt, oil, or various gases in the atmosphere, the effect of vibration which might be encountered with various types of loads, changes in ambient temperature, intermittent loading, and other factors, some of which may tend to increase and others to decrease the life in actual operation.

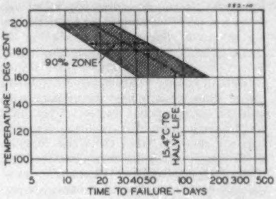


Figure 10. Temperature-aging characteristic, class-*A*-insulated stators, type *M*2

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Theory of the Brush-Shifting A-C Motor—III

Power-Factor Correction

A. G. CONRAD
MEMBER AIEE

F. ZWEIG
ENROLLED STUDENT AIEE

J. G. CLARKE
ASSOCIATE AIEE

Synopsis: In the preceding papers[1] of this series a method was described for determining the circle diagram and predicting the characteristics for the brush-shifting motor as it is used when the brushes are shifted to control speed. This paper extends the earlier work to show how the motor can be used to correct power factor. The range and limitations for this condition of operation are revealed from the circle diagram of the motor which has been developed and verified experimentally. Characteristics of the motor when used in this manner are presented.

Brush Settings for Power-Factor Correction

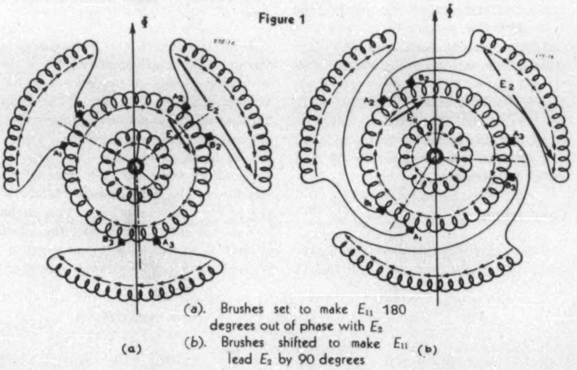
UNDER normal operation of the brush-shifting a-c motor, the two sets of brushes which carry the secondary current of the stator coils are coupled mechanically so that one set of brushes cannot be moved without a corresponding motion of the other in the opposite direction. Thus, the voltage introduced in the secondary circuit from the commutator, while it may vary in magnitude with speed adjustment, does not vary in phase relative to the voltage induced in the stator winding. Figure 1a is a schematic diagram of a two-pole, three-phase brush-shifting motor. The horizontal projections of the vectors represent the instantaneous voltages induced in the adjacent coils. The flux, ϕ, rotates at slip speed, say clockwise, relative to the stator. Brushes A_1, A_2, and A_3 are mounted rigidly on an adjustable frame so that a motion of the frame will move them all through the same angle; brushes B_1, B_2, and B_3 are mounted similarly and also move in unison. These two frames are coupled mechanically so that when making speed adjustments, the brushes are always equal distances from the center lines drawn between them. The points of maximum and minimum potential about the commutator rotate with the field relative to the stator so that the voltages at the brush pairs are at slip frequency. If the center lines drawn midway between the brushes A_1 and B_1, A_2 and B_2, and so on, remain fixed relative to the stator, the voltages collected from the commutator are fixed in phase relative to the induced voltage in the stator. The magnitude of this voltage, E_{11}, will vary with the degree of separation of the brushes but is approximately independent of speed for a given primary voltage. Interchanging the positions of the brushes A_1 to B_1, B_1 to A_1, and so on, will change the phase by 180 degrees, that is, reverse the polarity of the commutator voltage, E_{11}.

If the coupling between the frames supporting each set of brushes is removed, either set can be moved in either direction independently of the other. The various possible brush settings provide the motor with an extremely wide range of characteristics. If the flux, ϕ, is rotating clockwise relative to the stator, and the two frames are moved opposite to the direction of rotation of the flux with respect to the stator by 90 degrees from the position shown in Figure 1a (see Figure 1b), the voltage, E_{11}, will be advanced by 90 degrees inphase with respect to the voltage, E_2, induced in the stator. This procedure will advance the center lines designating the brush positions by 90 degrees relative to the stator. The phase of the voltage, E_{11}, collected from the commutator can be varied only by altering the mean position of the brushes as indicated by these center lines.

Figure 2a shows the voltage, E_2, induced in one phase of the secondary when brushes are set as in Figure 1a, that is, when E_2 is in opposition to E_{11}. Figure 2b shows what occurs when both sets of brushes are rotated in a direction so as to advance E_{11} by 90 degrees as illustrated in Figure 1b. Since both voltages, E_2 and E_{11}, are in series, and force currents through the same impedance, I_2 and I_{11} of Figure 2b must lag their voltages by the same angle. I_2 and I_{11} are the currents flowing in the secondary resulting from the voltages E_2 and E_{11} respectively. The two currents and their circle loci are shown. If the center lines are allowed to remain in their new positions of Figure 1b, and the two sets of brushes are moved across the center lines, that is, brushes A and B interchange positions, the voltage, E_{11}, from the adjusting winding will be shifted in phase by 180 degrees from the direction shown in Figure 2b, and E_{11} will lead E_2 by 90 degrees as shown in Figure 2c. The current I_2 lags the voltage E_2 by the same angle that the current I_{11} lags the voltage E_{11}.

If the motor is designed so that the effective number of series turns in the adjusting winding is half the effective number of stator turns, the largest value of the voltage, E_{11}, is half the stator voltage, E_2 at standstill. If in Figures 2a, 2b, and 2c, the brushes of each phase are spaced 180 electrical degrees apart on the commutator, E_{11} will have its largest

Figure 1



(a). Brushes set to make E_{11} 180 degrees out of phase with E_2
(b). Brushes shifted to make E_{11} lead E_2 by 90 degrees

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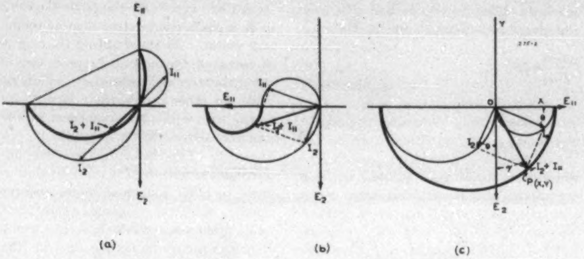
A. G. CONRAD is associate professor of electrical engineering, and F. ZWEIG and J. G. CLARKE are members of the faculty, school of engineering, all of Yale University, New Haven, Conn.



(*a*). E_{11} 180 degrees out of phase with E_2 (*b*). E_{11} lagging E_2 by 90 degrees (*c*). E_{11} leading E_2 by 90 degrees

Figure 2. Vector diagrams of secondary voltages and currents when brushes are set to make

value. The largest value of I_{11} (its value at synchronous speed) and the largest value of I_2 are as determined in part I of this series. The resultant secondary current is the vector sum of I_2 and I_{11}, and, as is shown in Figures 2b and 2c, this current is never zero whenever E_{11} has a quadrature component relative to E_2. The locus of the extremity of the vector representing the sum of I_2 and I_{11} is derived in appendix A, and is found to be another circle shown in wide lines in Figures 2b and 2c.

In Figure 2b it is evident that the total current flowing in the secondary lags the voltage, E_2, induced in the secondary by a relatively large angle at all times. In Figure 2c the total secondary current leads the voltage, E_2, over a considerable portion of the circle. These features enable the machine to generate power at a lagging power factor when used as a generator excited from a synchronous source. Furthermore it can supply these lagging loads over a wide range of speeds. This paper is primarily concerned with power-factor correction, and, therefore, will deal only with conditions illustrated in Figure 2c, rather than those of Figure 2b.

Primary Currents With Power-Factor Correction

The phase current in the primary of this motor can be determined from a knowledge of the magnetomotive forces in the adjusting winding and the stator winding for different values of slip. For each current I_2 and I_{11}, shown in Figure 2c, there is a corresponding magnetomotive force produced in each of these windings. The magnetomotive force produced by the current I_{11}, of Figure 2c, in the stator is represented by the vector oa of Figure 3. Likewise, the magnetomotive force produced by the current I_2 of Figure 2c in the stator is represented by the vector ob of Figure 3. The total magnetomotive force produced in the stator by the currents I_2 and I_{11} is therefore the vector oc of Figure 3, which is the sum of oa and ob. The locus of this stator magnetomotive force, oc, is defined by the circle dce. With this particular brush setting on a three-phase motor, there is a magnetomotive force produced by these same currents, I_2 and I_{11}, flowing in the adjusting windings. This magnetomotive force is 90 degrees behind the magnetomotive force that these currents produce by flowing in the stator. The vector oc' therefore represents the adjusting winding magnetomotive force when the stator magnetomotive force is oc. The locus of the vector oc' is the circle $d'c'e'$. The proportions of this circle are such that

$$\frac{oc'}{oc}=\frac{od'}{od}=\frac{oe'}{oe}=\frac{N_{AW}}{N_2}$$

With the brushes set as described here, motor action results only at speeds below synchronism. For such speeds, magnetomotive forces in the stator and adjusting windings are cancelled by equal and opposite magnetomotive forces in the primary. In Figure 3, the stator magnetomotive force co is counteracted by the component primary magnetomotive force oc'', and the adjusting winding magnetomotive force oc' is counteracted by the component primary magnetomotive force oc'''. The total magnetomotive force of the primary that is necessary to counteract the magnetomotive forces of the adjusting winding and the stator is the sum of oc'' and oc''', which is oc''''. The locus of the extremity of this vector oc'''' is defined by the circle $d''''c''''e''''$. The primary magnetomotive force must be produced by a current which is proportional to and in phase with oc''''. In Figure 3 this current is shown as the vector I_{1L}. The locus of I_{1L} is another circle having a center coincident with the center of the circle $d''''c''''e''''$. The locus of the primary current can be obtained by adding to this current I_{1L}, the current po. The current po is the no-load current taken by the motor when the brushes are set to make

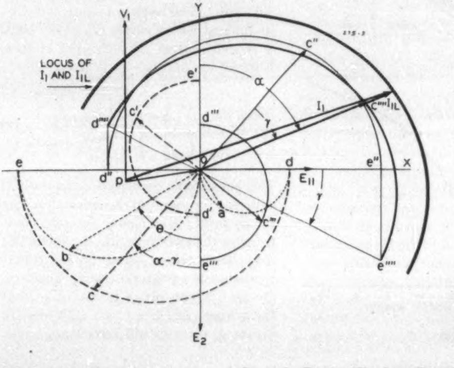


Figure 3. Vector diagram of motor when used to correct power factor

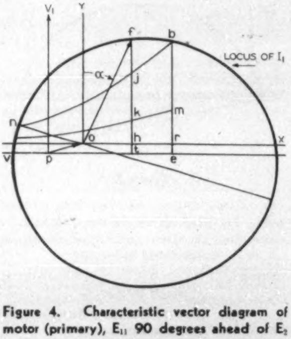


Figure 4. Characteristic vector diagram of motor (primary), E_{11} 90 degrees ahead of E_2

E_{11} zero. The total current taken by the primary, shown on this diagram, is the vector I_1, and the locus of its extremity with respect to the point p is defined by the same circle that defines I_{1L}. This circle has a diameter passing through the point o at an angle γ, the tangent of which is equal to the ratio of the effective number of turns in the adjusting winding between brushes to the effective number of stator turns, N_{AW}/N_2.

A change in the brush setting will change the total resistance and reactance of the total secondary circuit and, consequently, change the diameters of both the circle loci of I_{11} and I_2. With the brushes set to obtain the characteristics illustrated in Figure 3, the motor will take a leading current, I_1, with respect to the impressed voltage, V_1 over a considerable range of loads. For such conditions of load, it can be used to correct the power factor of the line supplying the motor.

Determination of Characteristics When Motor Is Used to Correct Power Factor

On the basis of the theory and assumptions of part I of this series, it was shown that the currents I_2 and I_{11} could be expressed thus:

$$I_2=\frac{{}_{ss}E_2S}{\sqrt{(R_2+R_{AW})^2+S^2({}_{ss}X_2+{}_{ss}X_{AW})^2}} \qquad (1)$$

$$I_{11}=\frac{E_{11}}{\sqrt{(R_2+R_{AW})^2+S^2({}_{ss}X_2+{}_{ss}X_{AW})^2}} \qquad (2)$$

Therefore

$$\frac{I_2}{I_{11}}=\frac{{}_{ss}E_2S}{E_{11}} \qquad (3)$$

If the brushes supplying each stator phase are set so that E_{11} equals half ${}_{ss}E_2$, as is illustrated in Figures 2b and 2c, then

$$\frac{{}_{ss}E_2S}{E_{11}}=2S \qquad (4)$$

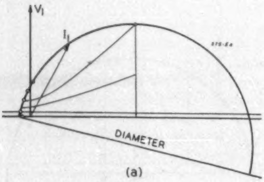
At standstill S equals one, and

$$\frac{I_2}{I_{11}}=2 \qquad (5)$$

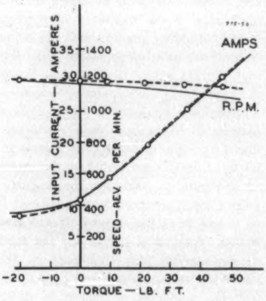
Since the ratio of $I_{2\max}$ to $I_{11\max}$ is, from equations 1 and 2,

$$\frac{I_{2\max}}{I_{11\max}}=\frac{{}_{ss}E_2}{E_{11}}\left(\frac{R_2+R_{AW}}{{}_{ss}X_2+{}_{ss}X_{AW}}\right) \qquad (6)$$

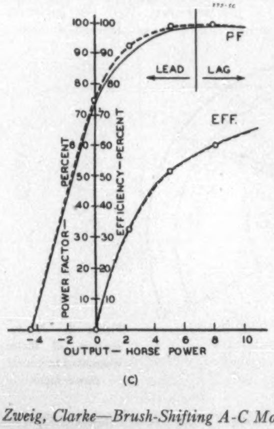
a knowledge of the ratio of R_2+R_{AW} to ${}_{ss}X_2+{}_{ss}X_{AW}$ is all that is required to determine the diameters of the two component circles, I_2 and I_{11}, and from them the resultant circle defining the extremity of the primary current vector as described above. At synchronous speed I_2 is zero and I_{11} is maximum. If the primary currents for standstill and for some other speed are known, it is possible to determine the characteristic copper loss curves for the circle diagrams as it is done for the two-element induction motor. At standstill all the energy in excess of the no-load losses is lost in the primary and secondary windings, while for other speeds these losses vary as the square of that component of the primary current which is labelled I_{1L} in Figure 3. This involves the same approximation with regard to losses that is made in plain induction motor theory; namely, that the primary copper loss varies as the square of the load component of primary current rather than as the total primary current. The error introduced by this approximation in the two-element induction motor is negligible. The error due to this approximation is somewhat larger in this motor than in the two-element motor, but not so large as to invalidate the method.



(a)



(b)



(c)

Figure 5

(*a*). Circle diagram obtained from no-load tests. Encircled points indicate primary currents taken by motor under load conditions (determined by loading)

(*b*) and (*c*). o---o---o Characteristics obtained by loading

———— Characteristics predicted from the circle diagram

The characteristic copper loss curve, *bjn*, is shown in Figure 4. The point, *m*, determined by measurement of primary resistance divides *rb* into components proportional to the primary and secondary losses. The current *pb* produces a primary loss which is represented by *mr*. The loss for any primary current, *pf*, can be represented by *kh* which in turn can be determined by the relationship

$$kh=mr\left(\frac{pf}{pb}\right)^2$$

The secondary copper loss represented by *jk* can be found in similar fashion.

The slip is a function of the angle $(\alpha-\gamma)$ by which I_2+I_{11} lags E_2. This is also the angle between the vector *oc* in Figure 3, representing the magnetomotive force due to I_2+I_{11} in the stator windings, and the vector labelled E_2, the voltage induced in the stator by the airgap flux. The angle α is the phase angle between the voltage V_1 and the primary-current component I_{1L}, or the magnetomotive force oc''''. This angle varies with slip. The angle γ is determined by its tangent which equals N_{AW}/N_2; thus, γ is independent of slip. The relation between slip and $(\alpha-\gamma)$ is given by

$$\tan(\alpha-\gamma)=\frac{1-\left(\frac{1}{\tan\gamma}\right)^2\left(\frac{I_{11\max}}{I_{2\max}}\right)S^2}{\left(\frac{1}{\tan\gamma}\right)\left(\frac{I_{11\max}}{I_{2\max}}+1\right)S} \qquad (7)$$

This equation is developed in appendix B and may be used to determine the slip at any point f (see Figure 4) on the circle defining the extremity of I_1. The speed may be obtained, since speed equals synchronous speed times the quantity, $(1-S)$. If the motor is loaded so that the primary draws a phase current *pf* in Figure 4, the slip will have some value,

S, corresponding to the point, f, and can be determined from measurements of α and equation 7.

If the impressed voltage per phase is represented by V_1, then for the input current, pf:

(1) Input $= (tf)\,V_1$ watts per phase
(2) Output $= (jf)\,V_1$ watts per phase
(3) Iron loss, friction, and windage $= (th)\,V_1$ watts per phase
(4) Primary copper loss $= (hk)\,V_1$ watts per phase
(5) Copper loss of stator and adjusting winding $= (kj)\,V_1$ watts per phase
(6) Efficiency $= \frac{jf}{tf}$ times 100 per cent
(7) The slip is determined by the angle between of and V_1 (or between of and oy) using the equation (7)
(8) Speed $= (1 - \text{slip})$ (synchronous speed in rpm) in rpm
(9) Torque $=$

$$\frac{(jf)\,V_1\ (\text{number of phases})\ 33{,}000}{2\pi\ (\text{speed in rpm})\ 746}$$
in pound-feet

The output torque is zero at the point n in Figure 4. For slips less than that at n, power must be supplied to the machine through the shaft. At the point v, slightly above synchronous speed, all the power into the machine comes by way of the shaft.

Results of Test With Leading Commutator Voltage

In order to check the validity of the foregoing theory, a test was made on a General Electric BTA, 550–1,650-rpm, 4.17–12.5-horsepower, six-pole, 60-cycle motor. With the brushes set so that the commutator voltage was ahead of the induced voltage of the stator by 90 degrees, the gap between brushes was fixed so that the commutator voltage was 7.8 per cent of the stator voltage at standstill. The results of the load test and the blocked rotor test are indicated by the encircled points on the circle diagram of Figure 5a. The theoretical primary-current circle locus was determined from three no-load readings as described in parts I and II of this series. It is observed that the diameter of the circle lies beneath the horizontal chord by an angle of approximately ten degrees. Only about five degrees of this shift is accounted for by γ (the shift caused by the magnetomotive force of the adjusting winding). The remainder can be attributed directly to the leakage reactance of the primary circuit which, with high current values, causes the induced voltage, E_2, in the secondary to lag the primary voltage. Figure 5 shows the results predicted theoretically in comparison with the experimentally determined results.

Conclusions

The advantages of power-factor correction on a line supplying a motor are well-known. Advantages of power-factor correction to the motor itself result only when these corrections increase the efficiency of the motor or its horsepower capacity. The effect of power-factor correction on the efficiency of this motor is illustrated in Figure 6. Each of the efficiency curves shown here was obtained by a load test with brush settings that provided synchronous speed at no load.

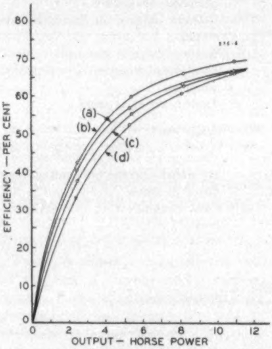


Figure 6. Efficiency as affected by power-factor correction

Brushes separated
(*a*). 0 electrical degrees
(*b*). 6 electrical degrees
(*c*). 12 electrical degrees
(*d*). 18 electrical degrees

It is evident that the addition of a small value of E_{11} (two per cent of its maximum value, that is, a brush separation of approximately five electrical degrees) to improve power factor will increase the efficiency only slightly. Further increases in E_{11} will cause excessive secondary currents and reduce the efficiency. It is evident from the theory developed here that if the primary exciting current is to be reduced the secondary must carry an additional exciting current to compensate for the reduction in the primary. Maximum efficiency for a given load will occur when the voltage E_{11} is adjusted so as to make the total copper losses in the motor a minimum. Any exciting current flowing in the secondary must be supplied through the commutator. For some conditions of load this additional current will cause poor commutation. Thus there is little to be gained from standpoint of the motor by setting its brushes to improve its power factor beyond the values obtained when E_{11} is made opposite to E_2.

From information revealed in Figure 5 it is quite evident that the motor can be adjusted to draw various amounts of leading current. This feature provides the possibility of improving the regulation and efficiency of the line supplying the motor. These curves of Figure 5 also show the relative high power factor obtainable on the brush-shifting motor as compared with the ordinary well-known two-element induction motor.

Appendix A

It can be shown that the locus of the sum of I_2 and I_{11} is a circle as follows:

The line OP of Figure 2c indicates the current vector representing the sum of I_2 and I_{11}. Let x equal the horizontal component of OP, and y equal the vertical component of OP. Since I_2 and I_{11} are always at right angles

$$OP^2 = I_2^2 + I_{11}^2$$

$$\text{or } OP^2 = I_2^2 + I_{11}^2 I_{2\text{max}} I_{11\text{max}} - I_{2\text{max}} I_{11\text{max}} (\sin^2\theta + \cos^2\theta)$$

$$\text{But}\quad I_2 = I_{2\text{max}} \sin\theta$$

$$I_{11} = I_{11\text{max}} \cos\theta$$

$$OP^2 = I_2 I_{2\text{max}} \sin\theta + I_{11} I_{11\text{max}} \cos\theta + I_{2\text{max}} I_{11\text{max}} - I_{2\text{max}} I_{11} \cos\theta - I_{11\text{max}} I_2 \sin\theta$$

$$OP^2 = I_{2\text{max}}(I_2 \sin\theta - I_{11}\cos\theta) - I_{11\text{max}}(I_2 \sin\theta - I_{11}\cos\theta) + I_{2\text{max}} I_{11\text{max}}$$

$$OP^2 = (I_{11\text{max}} - I_{2\text{max}})(I_{11}\cos\theta - I_2 \sin\theta) + I_{2\text{max}} I_{11\text{max}}$$

$$\text{but } x = I_{11}\cos\theta - I_2\sin\theta$$

$$\text{and } OP^2 = x^2 + y^2$$

$$x^2 + y^2 = (I_{11\text{max}} - I_{2\text{max}})x + I_{2\text{max}} I_{11\text{max}}$$

$$y^2 + x^2 + x(I_{2\text{max}} - I_{11\text{max}}) + \frac{(I_{2\text{max}} - I_{11\text{max}})^2}{4} = I_{2\text{max}} I_{11\text{max}} + \frac{(I_{2\text{max}} - I_{11\text{max}})^2}{4}$$

$$y^2 + \left(x + \frac{I_{2\text{max}} - I_{11\text{max}}}{2}\right)^2 = \left(\frac{I_{2\text{max}} + I_{11\text{max}}}{2}\right)^2$$

This is the equation of a circle of radius $(I_{2\text{max}} + I_{11\text{max}}/2)$ with its center at $x = -{}^1/_2(I_{2\text{max}} - I_{11\text{max}})$ and $y = 0$. A similar method may be used to show that the locus of P in Figure 2b is also a circle.

Appendix B

In order to establish equation 7, it is necessary to refer to Figure 3. In this figure $(\alpha - \gamma)$ is the angle by which oc lags E_2. The vector oc represents the magnetomotive force produced in the stator by $I_2 + I_{11}$. The magnetomotive forces produced in the stator by I_2 and I_{11} separately are ob and oa respectively. The currents I_{11}, I_2, and

I_2+I_{11} with proper change in scale could be represented by *oa*, *ob*, and *oc*. If this is done, and if the point *c* is determined by the rectangular co-ordinates *x* and *y*, then

$$\tan(\alpha-\gamma)=-\frac{x}{y}$$

Since *oe* now represents $I_{2\max}$ and *od* represents $I_{11\max}$, if θ is the power factor angle of the secondary circuit:

$$I_2=I_{2\max}\sin\theta$$
$$I_{11}=I_{11\max}\cos\theta$$

In Figure 3

$$x=I_2\sin\theta-I_{11}\cos\theta$$
$$y=I_2\cos\theta+I_{11}\sin\theta$$

so

$$\tan(\alpha-\gamma)=-\frac{x}{y}=\frac{I_{11}\cos\theta-I_2\sin\theta}{I_{11}\sin\theta+I_2\cos\theta}$$

or

$$\tan(\alpha-\gamma)=\frac{1-\dfrac{I_2}{I_{11}}\tan\theta}{\tan\theta+\dfrac{I_2}{I_{11}}}$$

From above

$$\frac{I_2}{I_{11}}=\frac{I_{2\max}}{I_{11\max}}\tan\theta$$

and from equation 3,

$$\frac{I_2}{I_{11}}=\frac{{}_{ss}E_2S}{E_{11}}$$

therefore

$$\tan\theta=\frac{I_{11\max}}{I_{2\max}}\left(\frac{{}_{ss}E_2S}{E_{11}}\right)$$

Since $\dfrac{E_{11}}{{}_{ss}E_2}=\dfrac{N_{AW}}{N_2}=\tan\gamma$, it follows that

$$\tan\theta=\frac{I_{11\max}}{I_{2\max}}\left(\frac{S}{\tan\gamma}\right)$$

$$\tan(\alpha-\gamma)=\frac{1-\left(\dfrac{1}{\tan\gamma}\right)^2\left(\dfrac{I_{11\max}}{I_{2\max}}\right)S^2}{\left(\dfrac{1}{\tan\gamma}\right)\left(\dfrac{I_{11\max}}{I_{2\max}}+1\right)S}$$

Appendix C. List of Symbols

E_{11}—Voltage generated in adjusting winding between brushes supplying one phase of stator
E_2—Voltage induced in one phase of stator
${}_{ss}E_2$—Stator voltage at standstill
ϕ—Total flux produced by primary windings
α—Angle between V_1 and I_{1L}
γ—Angle $\tan^{-1}\dfrac{N_{AW}}{N_2}$
$(\alpha-\gamma)$—Phase angle between total secondary phase current and voltage induced in stator
I_{1L}—Load component of primary current
I_1—Total primary phase current
I_{11}—Current flowing in stator and adjusting winding resulting from the voltage E_{11}
I_2—Current flowing in stator and adjusting winding resulting from the voltage E_2
$I_{11\max}$—Maximum value of I_{11} and thus diameter of I_{11} circle
$I_{2\max}$—Maximum value of I_2 and thus diameter of I_2 circle
N_2—Effective number of stator turns per phase
N_{AW}—Effective number of adjusting-winding turns per phase
R_{AW}—Resistance of adjusting winding per phase
R_2—Resistance of stator per phase
S—Per cent slip
θ—Phase angle between I_{11} and E_{11}, or I_2 and E_2
V_1—Voltage impressed upon one phase of primary
${}_{ss}X_{AW}$—Adjusting-winding reactance at standstill
${}_{ss}X_2$—Stator reactance at standstill

Reference

1. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—I, II, A. G. Conrad, F. Zweig, and J. G. Clarke. AIEE TRANSACTIONS, volume 60, 1941, August section, pages 829–36.

Theory of the Brush-Shifting A-C Motor—IV

Speed Control With Power-Factor Correction

A. G. CONRAD
MEMBER AIEE

F. ZWEIG
ENROLLED STUDENT AIEE

J. G. CLARKE
ASSOCIATE AIEE

Synopsis: The preceding papers of this series presented an analysis of the brush-shifting a-c motor. Parts I and II dealt with the case when the voltage introduced into the stator coils from the commutator was collinear with the voltage induced in the stator coils by slip. Part III dealt with the case when the voltage introduced into the stator coils from the commutator was in quadrature with the induced voltage.

This paper extends the earlier analysis to include all the possible phase positions of the voltage introduced into the stator coils from the commutator. Under these conditions power factor and speed are controlled simultaneously. Methods for constructing the circle diagram for these conditions are given, and the characteristics of the motor predicted from the circle diagram are compared with the characteristics obtained by laboratory tests.

IT has been demonstrated[1] that the locus of the extremity of the current vector of the brush-shifting motor is a circle when the brushes are set for speed adjustment, that is, when the voltage, E_{11}, collected from the commutator is collinear with the induced voltage, E_2, in the stator. Further analysis[3] has shown that when the brushes are set to make E_{11} perpendicular to E_2 for the purpose of power-factor correction, that the locus of the extremity of the current vector is also a circle. An analysis of these circles has provided[1,3] a means of explaining the operation of the motor for these special brush settings and of predicting all of its characteristics from no-load measurements.

The developments described above have been made from an analysis of:

(*a*). The secondary currents I_{11} and I_2 produced by the voltages E_{11} and E_2 respectively.

(*b*). The magnetomotive forces produced by these currents flowing in the stator and the adjusting winding.

(*c*). The resultant magnetomotive forces produced in the primary winding as a result of the secondary magnetomotive forces.

(*d*). The primary currents associated with the required primary magnetomotive forces.

This paper, employing the same methods of analysis, extends the theory of the brush-shifting motor and explains its operation when the brushes are shifted to control speed at the same time that the motor is used to correct the power factor of its supply. Specifically, it deals with the operation of the motor when the brushes are set to make E_{11} out of phase with the standstill value of E_2 by any angle β. An understanding of the developments presented here presupposes a knowledge of the material presented in the preceding papers of this series.[1-3]

Figure 1a shows a representative vector diagram of the secondary currents I_{11} and I_2, and their loci when they are produced by the voltages E_{11} and E_2 which are no longer collinear. Appendix A demonstrates that the sum of I_{11} and I_2 for this condition is a vector the locus of which is defined by another circle. This circle, representing the locus of the sum of I_2 and I_{11}, is shown in Figure 1a. For the condition shown, the machine will run above synchronous speed at no load, and the power factor will be leading. The current vectors will follow the circle loci shown when the speed is varied from synchronous speed through higher speeds to infinite speed—infinite negative slip. Since the diameter of the I_{11} circle coincides with E_{11}, swinging E_{11} to various phase positions moves the I_{11} circle along with it. For values of β ranging from 0 to 180 degrees, a current is reflected into the primary which has a leading component over the operating range of the motor. The no-load speed of the machine is above synchronous speed if β is between 0 and 90 degrees, as in Figure 1, and below synchronous speed if β is between 90 and 180 degrees.

When the brushes are set to make E_{11} in the same direction as $_sE_2$, ($\beta=0$), the center lines of corresponding stator and adjusting winding coils are collinear, and their magnetomotive forces aid. With this reference position of E_{11}, β is also the angle by which the magnetomotive force of the adjusting winding lags the magnetomotive force of the stator coils. This is the angle in electrical degrees through which it is necessary to rotate the brushes about the commutator in order to shift E_{11} by β degrees (see Figure 2). Only the phase position of E_{11} is changed if the brushes are all rotated about the commutator by the same angle, keeping their relative spacing unchanged. Positive values of β are obtained by moving the brushes in the direction of rotation from the position where $\beta=0$. Values of magnetomotive force and of E_{11} identical to those at any given setting of the brushes can be obtained by rotating all brushes 360 electrical degrees around the commutator, or by interchanging the positions of all brush pairs, and moving them all 180 electrical degrees around the commutator.

Determination of the Primary Currents

The primary current resulting from the secondary currents shown in Figure 1a can be obtained from a consideration of the various magnetomotive forces involved. If the resultant flux crossing the air gap is to remain unchanged (so that the generated voltage will remain approximately equal and opposite to the applied voltage), the magnetomotive forces produced by the secondary currents flowing in the stator and the adjusting winding coils must be cancelled by component magnetomotive forces produced by component currents flowing in the primary coils.

While the polyphase current I_2+I_{11}, which flows in the stator coils also flows in the adjusting winding coils, the magnetomotive forces produced by these two sets of coils in series are not, in general, in the same time phase with respect to the primary. This is because the adjusting winding coils are mechanically displaced from the stator coils by β electrical degrees, as shown in Figure 2. If the brushes have been shifted to retard E_{11} by β degrees (which advances the primary current); the magnetomotive force of the adjusting winding lags the magnetomotive force of the stator by β degrees, so that the primary current cancelling the magnetomotive force of the adjusting winding lags the primary current which cancels the magnetomotive force of the stator by β degrees.

Figure 1b shows a vector diagram of the components of the primary current necessary to cancel the magnetomotive

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A. G. CONRAD is associate professor, and J. G. CLARKE and F. ZWEIG are instructors in the school of engineering, Yale University, New Haven, Conn.

force produced by the secondary currents of Figure 1a flowing in the *stator*. Figure 2 shows that this component of primary current is obtained by a *mirror reflection* of the secondary current while the motor is running above synchronous speed, and by a *180-degree reflection* of the secondary current when the machine is running below synchronous speed. Figure 1c shows a vector diagram of the components of primary currents which cancel the magnetomotive forces of the secondary currents flowing in the *adjusting winding*. Figure 1d shows the magnetizing component of the primary current. Figure 1e shows the sum of these component currents, or I_1, the total primary current. At a given load, the currents reflected from the adjusting winding and the stator winding are separated by the angle β. Their sum is separated from the component reflected from the stator by the angle

$$\gamma=\tan^{-1}\frac{N_{AW}\sin\beta}{N_2+N_{AW}\cos\beta}$$

The currents reflected into the primary from the stator and the adjusting winding bear the ratio N_2 to N_{AW}.

By adding the reflected currents vectorially, the load component of the primary current is obtained. The ratio of transformation between the secondary and the primary can be obtained from the equation

$$N_1I_{1L}=\sqrt{(N_2+N_{AW}\cos\beta)^2+(N_{AW}\sin\beta)^2}\,(I_2+I_{11})$$

Obtaining the Circle Diagram

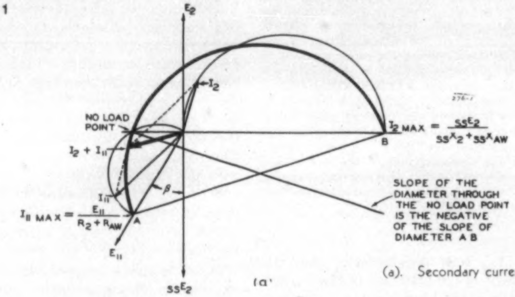
For specific cases, two general methods have been considered for obtaining the circle locus of the primary current without loading the machine. One method makes use of the fact that a circle is uniquely determined if two points and the slope of the diameter through one of the points is known. This method is commonly used to obtain the circle diagram for the two-element induction motor. It is also applicable for the brush-shifting a-c motor for any value of E_{11}. The two points commonly used are the extremities of the no-load and blocked-rotor current vectors. In the two-element induction motor, the diameter through the no-load point is 90 degrees behind the impressed voltage, so the circle can be constructed from these two currents. However, the presence of E_{11} in the brush-shifting a-c motor causes a shift of the diameter of the secondary current locus through the no-load point so that it is no longer perpendicular to E_2 (or the applied voltage). As is seen in Figure 1a, the tangent of the angle of slope of this diameter is

$$\frac{\dfrac{E_{11}}{R}\cos\beta}{\dfrac{{}_{ss}E_2}{X}+\dfrac{E_{11}}{R}\sin\beta}=\frac{\dfrac{E_{11}}{{}_{ss}E_2}\cos\beta}{\dfrac{R}{X}+\dfrac{E_{11}}{{}_{ss}E_2}\sin\beta}$$

where R and X are the total resistance and reactance at standstill referred to the secondary. The voltages and β in this equation may be determined by opening the brush leads and measuring E_{11}, ${}_{ss}E_2$, and $E_{11}+{}_{ss}E_2$. β is determined by forming a triangle of these three voltages. The quantities can also be determined directly if the brush positions and turns ratio are known. The remaining term, R/X, can be evaluated from the blocked rotor current. At standstill, the resultant secondary current, $I_{11}+I_2$, lags the resultant secondary voltage, $E_{11}+E_2$, by an angle the tangent of which is X/R. This is the same angle that I_{1L} lags V_1 at standstill. Hence, X/R is the tangent of the angle between I_{1L} and V_1 at blocked rotor. The corresponding diameter of the primary current locus lags this diameter of the secondary current locus by the angle γ, which was defined earlier.

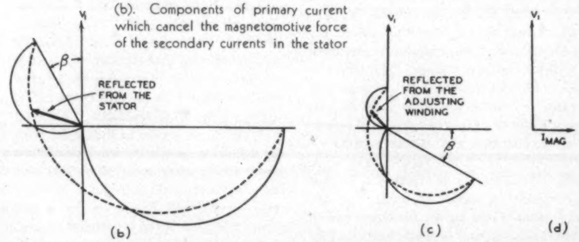
Erecting a perpendicular bisector to the chord joining the no-load and the blocked rotor current extremities gives another diameter, and the center of the

Figure 1


E2
I2
NO LOAD POINT
I2 + I11
I11
β
I11 MAX = E11 / (R2 + RAW)
A
E11
ssE2
(a)
I2 MAX = ssE2 / (ssX2 + ssXAW)
B
SLOPE OF THE DIAMETER THROUGH THE NO LOAD POINT IS THE NEGATIVE OF THE SLOPE OF DIAMETER A B

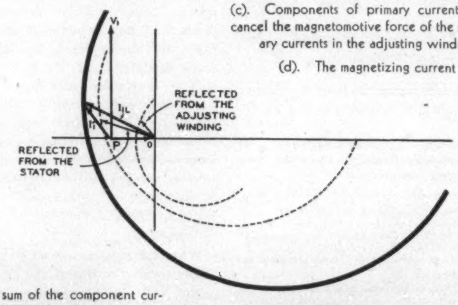
(a). Secondary currents

(b). Components of primary current which cancel the magnetomotive force of the secondary currents in the stator


V1
β
REFLECTED FROM THE STATOR
(b)
V1
REFLECTED FROM THE ADJUSTING WINDING
β
(c)
V1
IMAG
(d)

(c). Components of primary current which cancel the magnetomotive force of the secondary currents in the adjusting winding

(d). The magnetizing current


V1
I1L
I1
P
0
REFLECTED FROM THE ADJUSTING WINDING
REFLECTED FROM THE STATOR
(e)

(e). The sum of the component currents in the primary

primary-current circle locus lies at the intersection of these two diameters.

This method for determining the locus of the primary current, from the two points and the direction of the diameter through the no-load current extremity, involves the same approximation that is made in ordinary induction-motor theory —that this diameter of the circle passes through the no-load point. Actually, when the machine is running with no output torque, it is loaded with rotational losses. The circle should be constructed with the diameter drawn through the true no-load point, which can be found by supplying these losses mechanically. This refinement produces almost no change in the circle diagram.

The circle can also be located by obtaining three points, rather than by two points and a diameter. This method, which was discussed for specific cases in the earlier papers,[1,2] is valid for all brush positions, since the current locus is always a circle, and three points determine a circle. The necessary data are:

(*a*). The no-load input current and watts at normal voltage.

(*b*). The standstill input current and watts at reduced voltage.

(*c*). The no-load input current and watts at reduced voltage, with the machine running at some speed between the no-load speed and standstill.

This method eliminates the inaccuracies introduced by primary leakage reactance and rotational losses.

Characteristics From the Circle Diagram

When the locus of the extremity of the primary-current vector has been established by the methods discussed above, the characteristics of the motor can be predicted, using methods similar to those used in parts I, II, and III of this series.

In the ordinary induction-motor theory, it is assumed that the total copper loss of the motor is the loss produced by the no-load current plus the loss produced by the load component of current that is reflected from the secondary. The portion of the primary copper loss produced by the no-load current is grouped with the other no-load losses, and the sum of these is assumed to be constant. These assumptions involve two approximations, neither of which seriously affects the accuracy of the method for the two-element induction motor. First, it assumes that the current flowing in the primary is directly proportional to the current in the secondary, so that the division of copper loss between the primary and secondary is the same for all loads. Second, it assumes that the loss for two component currents flowing in a conductor simultaneously is equal to the sum of the losses produced when each component flows separately.

$$(I_{\text{mag}}^2+I_{1L}^2)R_1=(I_{\text{mag}}+I_{1L})^2R_1$$

This is true only when the component current vectors are in quadrature. Over the operating range of the ordinary induction motor, the magnetizing current is nearly at right angles to the reflected component, I_{1L}, so that the error due to this approximation is small. However, in the brush-shifting motor, the voltage E_{11} may be introduced into the secondary in such a phase position that the phase angle of the reflected current may vary widely with respect to the magnetizing current, introducing quite appreciable errors. The loss curves for the machine can be located without these approximations. Referring to Figure 3, for some general running condition when the input current is *pf*, the current flowing in the secondary is proportional to *of*, where *po* is the magnetizing current. *po* can be determined by running the machine at no load with the brushes set to make E_{11} zero. The primary copper loss is proportional to $(pf)^2$, and the secondary copper loss is proportional to $(of)^2$. When the rotor is blocked, the total input to the motor is used in supplying losses. Assuming that the sum of the iron, friction, and windage losses remains constant from no load to blocked rotor, the total copper loss can be determined for blocked rotor. From the resistances of the two windings, the division of this loss between the primary and the secondary can be determined. Thus, if the point *m* is located so that $rm\times V_1$ is the primary copper loss per phase for a primary current *pb*, and $mb\times V_1$ is the secondary copper loss per phase for a reflected current *ob*, then for a primary current *pf*, the primary copper loss per phase is $hk\times V_1$ where

$$hk=rm\frac{pf^2}{pb^2}$$

and the corresponding secondary loss is $kj\times V_1$ where

$$kj=mb\frac{of^2}{ob^2}$$

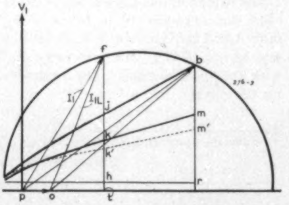


Figure 3. Locus of the primary current, and curves dividing the power component into the parts allocated to the output and the various losses

This method can be used to determine the copper losses at no load. By subtracting these copper losses from the no-load input, the friction, windage, and iron loss is determined.

Using these relations, curves can be constructed that divide the power component of the input current into the portions that are allocated to the output and the various losses. Thus, in Figure 3, for an input current *pf:*

Input $=tf\times V_1$ watts per phase

Output $=jf\times V_1$ watts per phase

Friction, windage, and iron loss $=th\times V_1$ watts per phase

Primary copper loss $=hk\times V_1$ watts per phase

Secondary copper loss $=kj\times V_1$ watts per phase

(a). Schematic diagram of the motor, showing the brushes set to retard E_{11} by β degrees. The magnetomotive force of the adjusting winding is β degrees behind that of the stator

(b). Effective currents, representing the magnetomotive forces of the stator, adjusting winding, and primary windings

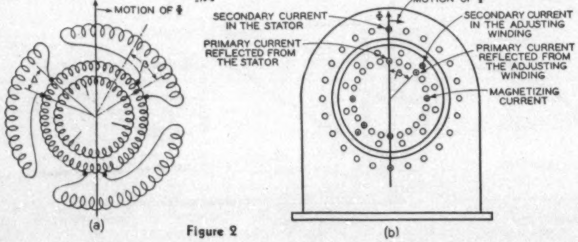

Φ
MOTION OF Φ
β
Δ
(a)
276-8
MOTION OF Φ
SECONDARY CURRENT IN THE STATOR
SECONDARY CURRENT IN THE ADJUSTING WINDING
PRIMARY CURRENT REFLECTED FROM THE STATOR
PRIMARY CURRENT REFLECTED FROM THE ADJUSTING WINDING
MAGNETIZING CURRENT
Φ
β
(b)

Figure 2

$$\text{Efficiency} = \frac{if}{tf}$$

$$\text{Power-factor} = \frac{tf}{pf}$$

When E_{11} has no quadrature component with respect to ${}_{ss}E_2$, ($E_{11} \sin \beta = 0$), the slip is equal to the secondary loss divided by the total power across the gap, or

$$S = \frac{{}_{sec}(I^2R)}{\text{developed power} + {}_{sec}(I^2R)} \times 100 \text{ per cent of no-load speed}$$

where I is the total secondary current ($I_2 + I_{11}$). Under this condition, all of the I^2R loss in the secondary is supplied by the *speed* voltage, IZ, which varies directly with slip. Appendix B demonstrates that when E_{11} has any phase position β, the voltage supplying the secondary I^2R loss is a combination of a *speed* voltage $(IZ)_s$, which varies directly with slip, and a *transformer* voltage $(IZ)_t = E_{11} \sin \beta$, which is independent of slip. Under this condition, the slip can be evaluated (see appendix B) in terms of the output and the component of the secondary loss, ${}_{sec}(I^2R)_s$, which is associated with the speed voltage. The expression for the slip is

$$S = \frac{{}_{sec}(I^2R)_s}{\text{developed power} + {}_{sec}(I^2R)_s} \times 100 \text{ per cent of } N_0$$

This slip is expressed in per cent of N_0, the no-load speed at which the motor would operate if the brushes were shifted to eliminate the quadrature component ($E_{11} \sin \beta$) without altering the inphase component ($E_{11} \cos \beta$). Similarly, the torque can be shown to be proportional to the component of the total secondary power which is independent of the transformer voltage.

$$\text{Developed power} + {}_{sec}(I^2R)_s = \frac{2\pi N_0 T_{dev}}{33{,}000} \times 746$$

or

$$T_{dev} = \frac{33{,}000}{2\pi N_0 746} (\text{developed power} + {}_{sec}(I^2R)_s)$$

The quantities ${}_{sec}(I^2R)_s$ and (developed power $+ {}_{sec}(I^2R)_s$) may be evaluated by projecting the components of I_{1L} on V_1 as outlined in appendix B. Also, it can be shown that these quantities may be evaluated from the product of the projections of I_{1L} and V_1 on an axis lagging V_1 by γ degrees, and that ${}_{sec}(I^2R)_t$, the component of the loss associated with the transformer voltage, can be evaluated from the product of the projections of I_{1L} and V_1 on an axis perpendicular to this axis. Regardless of the method used to evaluate these quantities, it is convenient to draw the dotted line $m'k'$, and so forth, on the circle diagram of Figure 3, so that

${}_{sec}(I^2R)_t = k'k \times V_1$ watts per phase
${}_{sec}(I^2R)_s = k'j \times V_1$ watts per phase
Output $+ {}_{sec}(I^2R)_s = k'f \times V_1$ watts per phase

From this, the slip and torque can be evaluated in a manner which is almost identical with that used for the ordinary induction motor circle diagram.

$$\text{Slip} = \frac{k'j}{k'f} \times 100 \text{ per cent of } N_0$$

$$\frac{2\pi N_0 T}{33{,}000} \times 746 = k'f \times V_1 \text{ watts per phase}$$

This involves the same approximation that is made with the ordinary induction motor—that the effect of rotational losses on the slip is negligible.

Experimental Check on the Circle-Diagram Theory

To check the theory that has been presented here, tests were made on the General Electric *BTA* 550–1,650-rpm 4.17–12.5 horsepower 60-cycle brush-shifting motor that was used in the earlier tests.

The brushes were set for two tests so that β was approximately 45 degrees and 135 degrees. The power factor was improved with each of these settings, and the machine ran above synchronous speed when $\beta = 45$ degrees, and below synchronous speed when $\beta = 135$ degrees.

The magnitude and phase position of E_{11} were determined accurately by the voltage measurements described earlier, and from this, from the no-load measurements and from the blocked-rotor measurements, the circles shown in Figure 4a were constructed. It is seen that these predicted circles check the observed points quite closely. Curves of speed and current against torque, and power factor and efficiency against output were constructed from the predicted circle. Figures 4b, c, d, and e show these predicted characteristics compared with those actually observed.

Conclusions

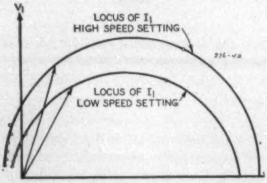
1. Theory and experiments described here have shown that it is possible to adjust the brushes of the brush-shifting a-c motor to correct the power factor of the current supplying it, regardless of the speed to which it is adjusted.

2. The locus of the extremity of the primary current with such adjustments is a circle. The magnitude and location of this circle with respect to the primary-voltage vector can be determined from no-load measurements taken on the motor.

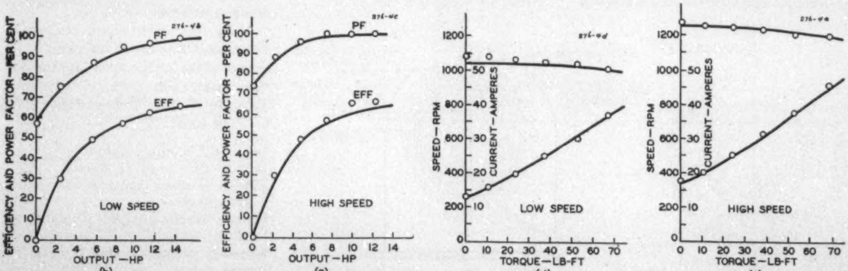
3. Power-factor correction is accomplished

Figure 4. Comparison of observed and predicted characteristics

Points are observed. Lines are predicted



(a)



(b) (c) (d) (e)

by causing exciting current of the motor to flow in the secondary elements instead of the primary. This causes extra heating in the secondary and reduces the permissible load current that the secondary can carry. In the particular machine used in this investigation, values of the quadrature component of E_{11}, ($E_{11}\sin\beta$) in excess of 10 per cent of the standstill value of the induced secondary voltage E_2 cause excessive secondary currents.

4. While the range of $E_{11}\sin\beta$ (power-factor adjustment) is limited, $E_{11}\cos\beta$ (speed adjustment) is not limited except by the design of the motor. The voltage $E_{11}\cos\beta$ is opposed in normal operation by a speed voltage, E_2, which limits the flow of current produced by it. The quadrature component, $E_{11}\sin\beta$, causes a secondary current which is opposed only by the motor impedance and not by the speed voltage. Consequently, a small angle β can cause considerable change in power factor in a low impedance motor.

5. A method of determining the characteristics of the motor when used to perform the double function of speed adjustment and power-factor correction has been presented. This method employs the theory and use of the circle diagram. The accuracy of the predicted characteristics indicates that the theory and description of the operation of the motor as presented here are essentially correct.

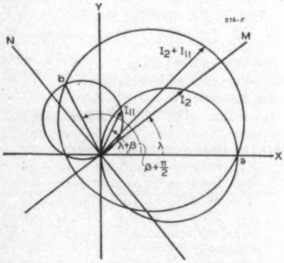


Figure 5. Diagram for appendix A

Appendix A. Proof of the Circle Locus of (I_2+I_{11})

The two small circles of Figure 5 with diameters a and b represent the loci of the currents I_2 and I_{11} respectively. Establish the rectangular co-ordinates, x and y, with the x axis along the diameter a. If angles are measured counterclockwise from the x axis, the angle between b and a is $\beta+\pi/2$. A current I_2 at angle λ from the x axis, when added to the corresponding current I_{11} at angle $\lambda+\beta$ from the x axis, results in (I_2+I_{11}). The locus of the extremity of (I_2+I_{11}) is to be expressed in terms of the x, y coordinates. To do this it is convenient to establish another set of rectangular coordinates m, n rotated counterclockwise by the angle λ from the x, y axis. It is seen that

$$m=I_2+I_{11}\cos\beta=a\cos\lambda+b\sin\lambda\cos\beta$$
$$n=I_{11}\sin\beta=b\sin\lambda\sin\beta$$

By the Pythagorean theorem

$$(I_2+I_{11})^2=(I_2+I_{11}\cos\beta)^2+(I_{11}\sin\beta)^2$$
$$(I_2+I_{11})^2=I_2^2+I_{11}^2+2I_2I_{11}\cos\beta-ab\times\sin\beta\,(\sin^2\lambda+\cos^2\lambda)+ab\sin\beta$$

Since $I_2=a\cos\lambda$ and $I_{11}=b\sin\lambda$

$$\begin{aligned}(I_2+I_{11})^2&=I_2^2+I_{11}^2+2I_2I_{11}\cos\beta-a\sin\beta\times\sin\lambda I_{11}-b\sin\beta\cos\lambda I_2+ab\sin\beta\\&=I_2^2+I_{11}^2+I_2I_{11}\cos\beta-I_2b\cos\lambda\times\sin\beta+I_2I_{11}\cos\beta-a\sin\beta\times\sin\lambda I_{11}+ab\sin\beta\\&=I_2^2+I_{11}^2+I_2b\sin\lambda\cos\beta-I_2b\times\cos\lambda\sin\beta+I_{11}a\cos\lambda\cos\beta-I_{11}a\sin\lambda\sin\beta+ab\sin\beta\\&=I_2^2+I_{11}^2+I_2b\sin(\lambda-\beta)+I_{11}a\times\cos(\lambda+\beta)+ab\sin\beta\\&=I_2[I_2+b\sin(\lambda-\beta)]+I_{11}[I_{11}+a\times\cos(\lambda+\beta)]+ab\sin\beta\\&=I_2[a\cos\lambda+b\sin(\lambda-\beta)]+I_{11}\times\{b\sin[(\lambda-\beta)+\beta]+a\cos(\lambda+\beta)\}+ab\sin\beta\\&=I_2[a\cos\lambda+b\sin(\lambda-\beta)]+aI_{11}\times\cos\lambda\cos\beta-aI_{11}\sin\lambda\sin\beta+I_{11}b\sin(\lambda-\beta)\cos\beta+I_{11}b\times\cos(\lambda-\beta)\sin\beta+ab\sin\beta\end{aligned}$$
$$(I_2+I_{11})^2=[a\cos\lambda+b\sin(\lambda-\beta)](I_2+I_{11}\times\cos\beta)+I_{11}\sin\beta[b\cos(\lambda-\beta)-a\sin\lambda]+ab\sin\beta$$

Since $m=I_2+I_{11}\cos\beta$ and $n=I_{11}\sin\beta$

$$\begin{aligned}(I_2+I_{11})^2&=[a\cos\lambda+b\sin(\lambda-\beta)]m+n\times[b\cos(\lambda-\beta)-a\sin\lambda]+ab\sin\beta\\&=m[\cos\lambda(a-b\sin\beta)+b\sin\lambda\times\cos\beta]+n[-\sin\lambda(a-b\sin\beta)+b\cos\lambda\cos\beta]+ab\sin\beta\end{aligned}$$
$$(I_2+I_{11})^2=(a-b\sin\beta)(m\cos\lambda-n\sin\lambda)+b\cos\beta(m\sin\lambda+n\cos\lambda)+ab\sin\beta$$

Since the co-ordinates x, y are expressed in the m, n co-ordinate system by the equations

$$x=m\cos\lambda-n\sin\lambda$$
$$y=m\sin\lambda+n\cos\lambda$$
$$(I_2+I_{11})^2=(a-b\sin\beta)x+(b\cos\beta)y+ab\times\sin\beta$$

But

$$(I_2+I_{11})^2=x^2+y^2$$

so

$$x^2-x(a-b\sin\beta)+y^2-yb\cos\beta=ab\sin\beta$$

Completing squares

$$\left[x-\left(\frac{a}{2}-\frac{b}{2}\sin\beta\right)\right]^2+\left[y-\frac{b}{2}\cos\beta\right]^2=ab\times\sin\beta+\frac{a^2}{4}-\frac{ab}{2}\sin\beta+\frac{(b\sin\beta)^2}{4}+\frac{(b\cos\beta)^2}{4}$$

$$\left[x-\left(\frac{a}{2}-\frac{b}{2}\sin\beta\right)\right]^2+\left[y-\frac{b}{2}\cos\beta\right]^2=\left[\frac{a}{2}+\frac{b}{2}\sin\beta\right]^2+\left[\frac{b\cos\beta}{2}\right]^2$$

This is the equation of a circle in x, y coordinates with center at

$$x=\frac{a}{2}-\frac{b}{2}\sin\beta$$

$$y=\frac{b}{2}\cos\beta$$

and radius

$$r=\left[\left(\frac{a}{2}+\frac{b}{2}\sin\beta\right)^2+\left(\frac{b\cos\beta}{2}\right)^2\right]^{1/2}$$

Appendix B. Determination of Slip and Torque

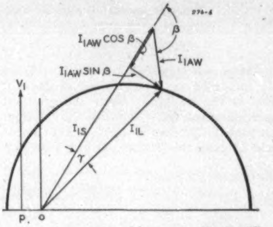
As is seen from Figure 1, I_{1L} is made up of two components, one of which, I_{1s}, cancels the magnetomotive force of (I_2+I_{11}) flowing in the stator coils, and the other, I_{1AW}, cancels the magnetomotive force of (I_2+I_{11}) flowing in the adjusting winding. These component currents bear the ratio

$$\frac{I_{1s}}{I_{1AW}}=\frac{N_2}{N_{AW}}=\frac{{}_{ss}E_2}{E_{11}}$$

and are added together at the angle β to form I_{1L} as shown in Figure 6a. Thus, if I_{1L}, β, and ${}_{ss}E_2/E_{11}$ are known, all the component currents are determined.

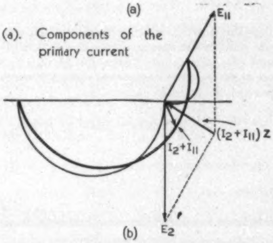
By projecting the various current vectors in this diagram on the voltage vector, V_1, powers are obtained which can be used in evaluating the speed and torque.

If the vector notation $(I_{1L})\cdot(V_1)$ is used to mean the scalar product of V_1 and the projection of I_{1L} on V_1, and if the primary resistance is assumed to be transferred into the



(a)

(a). Components of the primary current



(b)

(b). Secondary voltages and currents

Figure 6. Diagrams for appendix B

secondary, the following relations may be written

$$(I_{1L})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2+E_{11}) \qquad (1)$$

=net power transferred from the primary to the stator and the adjusting windings

$$(I_{1s})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2) \qquad (2)$$

=power transferred from the primary to the stator

$$(I_{1AW})\cdot(V_1)=(I_2+I_{11})\cdot(E_{11}) \qquad (3)$$

=power transferred from the primary to the adjusting winding

$$(I_{1AW}\cos\beta)\cdot(V_1)=(I_2+I_{11})\cdot(E_{11}\cos\beta) \qquad (4)$$

=power associated with the component of I_{1AW} which is collinear with I_{1s}

$$(I_{1AW}\sin\beta)\cdot(V_1)=(I_2+I_{11})\cdot(E_{11}\sin\beta) \qquad (5)$$

=power associated with the component of I_{1AW} which is in quadrature with I_{1s}

The power described by equation 5 can be identified as a component of the I^2R loss in the machine. Since the resultant secondary voltage is equal to the IZ drop; and the dot product of the resultant current and IZ gives the I^2R loss, we may write

$$E_2+E_{11}=-(I_2+I_{11})Z$$

and

$$(I_2+I_{11})\cdot(E_2+E_{11})=-(I_2+I_{11})^2R$$

(See Figure 6b.) If (E_2+E_{11}) is divided into the quadrature components $(E_2+E_{11}\cos\beta)$ and $(E_{11}\sin\beta)$, the projection of (I_2+I_{11}) on each of these components gives a component of the total copper loss.

$$[(I_2+I_{11})^2R]_s=-(I_2+I_{11})\cdot(E_2+E_{11}\cos\beta) \qquad (6)$$

$$[(I_2+I_{11})^2R]_t=-(I_2+I_{11})\cdot(E_{11}\sin\beta) \qquad (7)$$

where

$$[(I_2+I_{11})^2R]_s+[(I_2+I_{11})^2R]_t=(I_2+I_{11})^2R$$

But from equation 5, $[(I_2+I_{11})^2R]_t$ is seen to be the product of V_1 and the projection of $(I_{1AW}\sin\beta)$ on V_1, so that $[(I_2+I_{11})^2R]_t$ can be evaluated, and since $[(I_2+I_{11})^2R]$ is known, the remaining component $[(I_2+I_{11})^2R]_s$ can be evaluated.

Obtaining the Torque

The power which is transferred directly from the primary to the stator is, from equation 2

$$(I_{1s})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2)$$

Since this power is transferred by a flux which reacts at synchronous speed with the primary

$$(I_{1s})\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2)=\frac{2\pi N_{\text{sync}}T_{\text{dev}}}{33{,}000}\times 746$$

which can be solved for torque. This assumes the more conventional form if it is expressed in terms of N_o rather than N_{sync}, where N_o is the no-load speed when $E_{11}\times\sin\beta$ is made zero, leaving $E_{11}\cos\beta$ unchanged.

$$(I_{1s}+I_{1AW}\cos\beta)\cdot(V_1)=(I_2+I_{11})\cdot({}_{ss}E_2+E_{11}\cos\beta)=\frac{2\pi N_oT_{\text{dev}}}{33{,}000}\times 746 \qquad (8)$$

This is true because

$$\frac{N_o}{N_{\text{sync}}}=\frac{{}_{ss}E_2+E_{11}\cos\beta}{{}_{ss}E_2}=\frac{I_{1s}+I_{1AW}\cos\beta}{I_{1s}}$$

From equations 1 and 5, it is seen that equation 8 must represent the developed power plus one component of the copper loss

$$\frac{2\pi N_oT_{\text{dev}}}{33{,}000}\times 746=P_{\text{dev}}+[(I_2+I_{11})^2R]_s$$

which can be solved for torque.

Obtaining the Speed

Since the slip from synchronous speed, in per cent of synchronous speed is $E_2/{}_{ss}E_2$, the speed can be predicted from E_2. As is seen from Figure 6b

$$E_2=E_{11}\cos\beta+[(I_2+I_{11})Z]_s$$

where $[(I_2+I_{11})Z_s]$ is the component of the impedance drop which is in phase with E_2, so that

$$\frac{E_2}{{}_{ss}E_2}=\frac{E_{11}\cos\beta}{{}_{ss}E_2}+\frac{[(I_2+I_{11})Z]_s}{{}_{ss}E_2}=S_1+S_2$$

=slip in per cent of synchronous speed

where S_1 is the slip caused by $E_{11}\cos\beta$ and S_2 is the slip caused by loading. The first term on the right hand side of this equation is constant for a given brush setting, and the second term varies with torque. This expression can be evaluated more simply if the slip is measured from N_o and expressed in per cent of N_o. This slip is

$$S=\frac{S_2N_{\text{sync}}}{N_o}=\frac{[(I_2+I_{11})Z]_s}{{}_{ss}E_2+E_{11}\cos\beta}\text{ per cent }N_o$$

Here the slip is expressed in terms of the ratio of two voltages. Taking the dot product of these voltages with the secondary currents puts this expression for slip into the conventional form:

$$S=\frac{[(I_2+I_{11})Z]_s\cdot(I_2+I_{11})}{({}_{ss}E_2+E_{11}\cos\beta)\cdot(I_2+I_{11})}=\frac{[(I_2+I_{11})^2R]_s}{P_{\text{dev}}+[(I_2+I_{11})^2R]_s}\text{ per cent }N_o$$

Assumption Regarding Primary Resistance

In deriving the expressions for speed and torque, it was assumed that the primary resistance was replaced by an equivalent resistance in the secondary. Actually, the power consumed in the primary resistance does not cross the air gap. For this reason, the term $[(I_2+I_{11})^2R]_s$ in these expressions must be replaced by the term ${}_{\text{sec}}[(I_2+I_{11})^2R]_s$. This term, which is part of the copper loss in the secondary, was obtained in the manner commonly used for the ordinary induction motor. The division of the loss at blocked rotor was determined by resistance measurements, then it was assumed that the same division of losses occurred at all loads.

List of Symbols

E_{11}—The voltage inserted into the secondary element from the adjusting winding
E_2—The voltage induced in the secondary element by slip
I_{11}—The current flowing in the stator and adjusting winding, resulting from the voltage E_{11}
I_2—The current flowing in the stator and adjusting winding, resulting from the voltage E_2
I—The resultant secondary current I_2+I_{11}
${}_{ss}E_2$—The value of E_2 when the rotor is blocked
I_{mag}—The flux-producing component of the primary current
I_{1L}—The component of the primary current which cancels the magnetomotive forces of the stator and the adjusting windings
I_{1s}—The component of the primary current which cancels the magnetomotive force of the stator winding
I_{1AW}—The component of the primary current which cancels the magnetomotive force of the adjusting winding
I_1—The resultant primary current, or $I_{\text{mag}}+I_{1L}$
V_1—The applied voltage
N_1—The effective primary turns
N_2—The effective stator turns
N_{AW}—The effective adjusting winding turns
β—The angle in electrical degrees
  (*a*). Between E_{11} and ${}_{ss}E_2$
  (*b*). Between the magnetomotive forces of the stator and the adjusting winding
  (*c*). That the brushes must be rotated about the commutator to retard E_{11} by β degrees
γ—The angle in electrical degrees
  (*a*). Between I_{1s} and I_{1L}
  (*b*). Between the magnetomotive force of the stator and the resultant magnetomotive force of the stator and the adjusting winding (*c*). Between ${}_{ss}E_2$ and $({}_{ss}E_2+E_{11})$
R—The resistance of the primary, secondary, and adjusting winding, reflected to the secondary
X—The standstill reactance of the primary, secondary, and adjusting winding, reflected to the secondary
Z—The secondary and adjusting winding impedance
T_{dev}—The developed torque
P_{dev}—The developed power
$(IZ)_s$—The component of the total secondary and adjusting winding IZ voltage which is in phase with E_2
$(IZ)_t$—The component of the total secondary and adjusting winding IZ voltage which is in quadrature with E_2: $(IZ)_t=-E_{11}\sin\beta$
${}_{\text{sec}}(I^2R)_s$—The component of the secondary I^2R loss which is given by $I\cdot(IZ)_s$
${}_{\text{sec}}(I^2R)_t$—The component of the secondary I^2R loss which is given by $I\cdot(IZ)_t$
N_o—The no-load speed at which the motor

Reactance and Skin Effect of Concentric Tubular Conductors

HERBERT B. DWIGHT
FELLOW AIEE

THE concentric arrangement of tubular conductors to carry heavy alternating currents gives compactness, low reactance drop, and reduced loss from skin effect or crowding of the current to the surface of the conductors. This arrangement is being used to a considerable extent.[12] In this paper formulas and curves are given to enable the reactance and the skin-effect resistance ratio of such conductors to be determined for three-phase and single-phase circuits.

Reactive Drop

In practical cases the reactance of concentric tubular conductors is very little affected by the variable current density which causes skin effect. When uniform current density is assumed, the "geometric mean distance" method of Clerk Maxwell may be used. Using values given in paragraph 692 of his "Electricity and Magnetism," for geometric mean distances of a tubular section from itself and from other sections, (references 1, 3, and equations 28 and 30 of reference 11) formulas for the reactance drop in each tube of a concentric, three-phase circuit are obtained.

The general formula for reactive drop in conductor 1 of a group of long, parallel conductors is (see reference 11, equation 55, page 39)

$$-j\omega 2\times 10^{-9}[I_1 \operatorname{logn} D_{s1}+I_2 \operatorname{logn} D_{12}+ I_3 \operatorname{logn} D_{13}+\ldots] \text{ volts per centimeter} \quad (1)$$

where

$\omega =$ 2π×frequency,
logn denotes natural logarithm,
$D_{s1} =$ self geometric mean distance of conductor 1,
$D_{12} =$ geometric mean distance of the cross section of conductor 1 to that of conductor 2, and so forth.

The currents are in amperes. A conductor may have any shape of cross section.

The logarithm of the geometric mean distance between two cross sections is the average of the logarithms of all possible distances between points on one section and points on the other. The logarithm of the self geometric mean distance of the section of a conductor is the average of the logarithms of all possible distances between any two points of the section.

Formula 1 is subject to the condition that

$$I_1+I_2+I_3+I_4+\ldots=0$$

which is a relation usually obtained in any steady-state problem where a system consists of long, parallel conductors, and all the conductors are taken into account. In other words there is as much return current as there is going current in such a system. A system of this kind may be made up of any number of phases and any number of conductors in parallel.

Let the inside and outside diameters of the tubes, from the smallest to the largest, be d_1, d_2, d_3, d_4, d_5, and d_6. Let there be no neutral current.

The reactive drop in the inner tube, which carries I_a amperes, is, in volts per centimeter,

$$\frac{j\omega 2I_a}{10^9}\left[\frac{d_1^4}{(d_2^2-d_1^2)^2}\operatorname{logn}\frac{d_2}{d_1}-\frac{3d_1^2-d_2^2}{4(d_2^2-d_1^2)}\right]+ \frac{j\omega 2I_b}{10^9}\left[\frac{1}{2}+\frac{d_3^2}{d_4^2-d_3^2}\operatorname{logn}\frac{d_3}{d_2}-\frac{d_4^2}{d_4^2-d_3^2}\operatorname{logn}\frac{d_4}{d_2}\right]+ \frac{j\omega 2I_c}{10^9}\left[\frac{1}{2}+\frac{d_5^2}{d_6^2-d_5^2}\operatorname{logn}\frac{d_5}{d_2}- \frac{d_6^2}{d_6^2-d_5^2}\operatorname{logn}\frac{d_6}{d_2}\right] \quad (2)$$

The reactive drop in the intermediate tube, which carries I_b amperes, is

$$\frac{j\omega 2I_a}{10^9}\left[\frac{1}{2}-\frac{d_3^2}{d_4^2-d_3^2}\operatorname{logn}\frac{d_4}{d_3}\right]+ \frac{j\omega 2I_b}{10^9}\left[\frac{d_3^4}{(d_4^2-d_3^2)^2}\operatorname{logn}\frac{d_4}{d_3}-\frac{3d_3^2-d_4^2}{4(d_4^2-d_3^2)}\right]+ \frac{j\omega 2I_c}{10^9}\left[\frac{1}{2}+\frac{d_5^2}{d_6^2-d_5^2}\operatorname{logn}\frac{d_5}{d_4}-\frac{d_6^2}{d_6^2-d_5^2}\times \operatorname{logn}\frac{d_6}{d_4}\right] \text{ volts per centimeter} \quad (3)$$

Reactive drop in the outer tube, which carries I_c amperes, is

$$\frac{j\omega 2(I_a+I_b)}{10^9}\left[\frac{1}{2}-\frac{d_5^2}{d_6^2-d_5^2}\operatorname{logn}\frac{d_6}{d_5}\right]+ \frac{j\omega 2I_c}{10^9}\left[\frac{d_5^4}{(d_6^2-d_5^2)^2}\operatorname{logn}\frac{d_6}{d_5}-\frac{3d_5^2-d_6^2}{4(d_6^2-d_5^2)}\right] \text{ volts per centimeter} \quad (4)$$

The phase currents I_a, I_b, and I_c may be unbalanced. Therefore, by putting one of the currents equal to zero, the single-phase case is included.

For deriving the first expression, note that

$$I_a \operatorname{logn} d_2=-I_b \operatorname{logn} d_2-I_c \operatorname{logn} d_2$$

since there is no neutral current, and similarly for d_4 in the second expression and for d_6 in the third.

If the thickness of the above tubes is considered negligible, regarding the effect on the reactive drop, and if, as before, there is no neutral current, the reactive drop in the inner tube is

$$-\frac{j\omega 2}{10^9}\left[I_b \operatorname{logn}\frac{d_4}{d_2}+I_c \operatorname{logn}\frac{d_6}{d_2}\right] \text{ volts per centimeter} \quad (5)$$

Reactive drop in the intermediate tube is

$$-\frac{j\omega 2}{10^9}I_c \operatorname{logn}\frac{d_6}{d_4} \text{ volts per centimeter} \quad (6)$$

Reactive drop in the outer tube = 0. (7)

It does not matter in what units the diameters are given, in equations 2 to 6, so long as they are all in the same units, since only ratios of diameters occur.

For convenience in computation, it may be noted that there are

$$2.540\times 12{,}000=3.05\times 10^4 \text{ centimeters in 1,000 feet} \quad (8)$$

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HERBERT B. DWIGHT is professor of electrical machinery, department of electrical engineering, Massachusetts Institute of Technology, Cambridge, Mass.

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would operate if the brushes were shifted to eliminate $E_{11}\sin\beta$, leaving $E_{11}\cos\beta$ unchanged

References

1. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—I, A. G. Conrad, F. Zweig, J. G. Clarke. AIEE TRANSACTIONS, volume 60, 1941, August section, pages 829–33.

2. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—II, A. G. Conrad, F. Zweig, J. G. Clarke. AIEE TRANSACTIONS, volume 60, 1941, August section, pages 834–6.

3. THEORY OF THE BRUSH-SHIFTING A-C MOTOR—III, A. G. Conrad, F. Zweig, J. G. Clarke. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 502–06.

Also,

$$\text{logn } u = 2.3026 \log_{10} u \tag{9}$$

The resistance drop in each tube is added vectorially to the reactance drop to give the impedance drop in that tube.

Example 1. Find the reactance drop in each tube of the following concentric, three-phase circuit. The inside and outside diameters of the inner tube are 1.2 and 3.0 inches; those of the intermediate tube are 4.0 and 4.5 inches, and those of the outer tube are 5.6 and 6.0 inches. The current of the inner tube is one ampere; that of the intermediate tube is

$$\cos 120° + j \sin 120° = -0.5 + j0.866$$

and that of the outer tube is

$$\cos 240° + j \sin 240° = -0.5 - j0.866$$

The reactive drop in the inner tube is

$$\frac{j\omega 2}{10^9}\,[0.188 - 0.349(-0.5 + j0.866) - 0.660 \times (-0.5 - j0.866)]$$

$$= \frac{j\omega 2}{10^9}(0.692 + j0.270) \text{ volts per centimeter}$$

to compare with $j\omega 2(0.55 + j\,0.25)/10^9$ by the approximate formula 5.

The reactive drop in the intermediate tube is

$$\frac{j\omega 2}{10^9}\,(0.164 + j0.253)$$

to compare with $j\omega 2(0.14 + j0.25)/10^9$ by the approximate formula 6.

The reactive drop in the outer tube is

$$\frac{j\omega 2}{10^9}\,(0.028 + j0.048)$$

to compare with 0 by the approximate formula 7.

For the resistance drop in each tube see example 2.

Skin Effect at Low Frequency

Let there be a tubular conductor of inner radius q and outer radius r, as in Figure 1. Let there be uniform current density a_o abamperes per square centimeter in the tube and let there be a current I_p in a wire at the center of the tube.

Following the procedure of reference 5, beginning at equation 1 of that paper

$$\text{Flux density at } dx = (\text{current inside } dx)\frac{2}{q+x}$$

$$= 2\pi a_o q\left(\frac{2x}{q} - \frac{x^2}{q^2} + \frac{x^3}{q^3} - \frac{x^4}{q^4} + \ldots\right) + \frac{2I_p}{q}\left(1 + \frac{x}{q}\right)^{-1} \tag{10}$$

Integrate from x to t and multiply by $j\omega$ where $\omega = 2\pi \times$ frequency.

Put

$$\frac{4\pi\omega}{\rho} = m^2 \tag{11}$$

where ρ = resistivity of the metal in abohms per centimeter cube. Reactive drop at dx caused by flux inside radius r due to a_o and I_p

$$= \frac{jm^2t^2}{2!}a_o\rho\left[1 - \frac{1}{3}\frac{t}{q} + \frac{1}{4}\frac{t^2}{q^2} - \ldots - \frac{x^2}{t^2} + \frac{1}{3}\frac{x^3}{t^2q} - \frac{1}{4}\frac{x^4}{t^2q^2} + \ldots\right] + \frac{j\omega 2I_pt}{q}\left[1 - \frac{1}{2}\frac{t}{q} + \frac{1}{3}\frac{t^2}{q^2} - \ldots - \frac{x}{t} + \frac{1}{2}\frac{x^2}{tq} - \frac{1}{3}\frac{x^3}{tq^2} + \ldots\right] \tag{12}$$

Let a current density $a_1 + a_1'$ flow, such that its resistance drop will be equal and opposite to the terms in x of equation 12, in order to keep the voltage drop from all causes uniform over the section, as it is in actual fact.

$$a_1 = \frac{jm^2t^2}{2!}a_o\left(\frac{x^2}{t^2} - \frac{1}{3}\frac{x^3}{t^2q} + \frac{1}{4}\frac{x^4}{t^2q^2} - \ldots\right) \tag{13}$$

$$a_1' = \frac{j\omega 2I_p}{q\rho}\left(x - \frac{1}{2}\frac{x^2}{q} + \frac{1}{3}\frac{x^3}{q^2} - \frac{1}{4}\frac{x^4}{q^3} + \ldots\right) \tag{14}$$

These currents will in turn produce flux in the metal and from the terms in x of the resulting voltage drops there are obtained values of current densities a_2 and a_2' and so on. The resulting values are

$$a_2 = \frac{(jm^2t^2)^2}{4!}a_o\left[\frac{x^4}{t_4} - \frac{2}{5}\frac{x^5}{t^4q} + \frac{3}{10}\frac{x^6}{t^4q^2}\ldots\right]$$

$$a_3 = \frac{(jm^2t^2)^3}{6!}a_o\left[\frac{x^6}{t^6} - \frac{3}{7}\frac{x^7}{t^6q} + \frac{9}{28}\frac{x^8}{t^6q^2}\ldots\right]$$

$$a_4 = \frac{(jm^2t^2)^4}{8!}a_o\left[\frac{x^8}{t^8} - \frac{4}{9}\frac{x^9}{t^8q} + \frac{1}{3}\frac{x^{10}}{t^8q^2}\ldots\right]$$

$$a_2' = \frac{jm^2t^2}{3!}\frac{j\omega 2I_p}{\rho}\left[\frac{x^3}{t^2q} - \frac{1}{2}\frac{x^4}{t^2q^2} + \frac{7}{20}\frac{x^5}{t^2q^3} - \frac{11}{40}\frac{x^6}{t^2q^4}\ldots\right]$$

$$a_3' = \frac{(jm^2t^2)}{5!}\frac{j\omega 2I_p}{\rho}\left[\frac{x^5}{t^4q} - \frac{1}{2}\frac{x^6}{t^4q^2} + \frac{5}{14}\frac{x^7}{t^4q^3} - \frac{2}{7}\frac{x^8}{t^4q^4}\ldots\right]$$

$$a_4' = \frac{(jm^2t^2)^3}{7!}\frac{j\omega 2I_p}{\rho}\left[\frac{x^7}{t^6q} - \frac{1}{2}\frac{x^8}{t^6q^2} + \frac{13}{36}\frac{x^9}{t^6q^3} - \frac{7}{24}\frac{x^{10}}{t^6q^4}\ldots\right]$$

Let the total current in the tube be I. By integrating the complete expression for current density over the cross section of the tube

$$I = \pi a_o(2qt + t^2)\left[1 + c_1\frac{jm^2t^2}{3!} + c_2\frac{(jm^2t^2)^2}{5!} + \ldots\right] + I_p\left[d_1\frac{jm^2t^2}{2!} + d_2\frac{(jm^2t^2)^2}{4!} + d_3\frac{(jm^2t^2)^3}{6!} + \ldots\right] \tag{15}$$

where

$$c_1 = 1 - \frac{1}{20}\frac{t^2}{q^2} + \frac{1}{20}\frac{t^3}{q^3} - \frac{11}{280}\frac{t^4}{q^4} + \ldots$$

$$c_2 = 1 - \frac{1}{14}\frac{t^2}{q^2} + \frac{1}{14}\frac{t^3}{q^3} - \ldots$$

$$c_3 = 1 - \frac{1}{12}\frac{t^2}{q^2} + \ldots$$

$$c_4 = 1 - \frac{1}{11}\frac{t^2}{q^2} + \ldots$$

as in reference 5, and where

$$d_1 = 1 + \frac{1}{3}\frac{t}{q} - \frac{1}{12}\frac{t^2}{q^2} + \frac{1}{30}\frac{t^3}{q^3} - \ldots$$

$$d_2 = 1 + \frac{2}{5}\frac{t}{q} - \frac{1}{10}\frac{t^2}{q^2} + \frac{3}{70}\frac{t^3}{q^3} - \ldots$$

$$d_3 = 1 + \frac{3}{7}\frac{t}{q} - \frac{3}{28}\frac{t^2}{q^2} + \frac{1}{21}\frac{t^3}{q^3} - \ldots$$

$$d_4 = 1 + \frac{4}{9}\frac{t}{q} - \frac{1}{9}\frac{t^2}{q^2} + \ldots$$

This gives the value of a_o in terms of I and I_p.

If the tube were not present, the center conductor carrying I_p would supply its resistance loss including its own eddy-current loss. Then, as stated by Waldo V. Lyon[6] at the top of page 1377, AIEE TRANSACTIONS, 1921, in his paper on eddy-current losses in armature conductors, if the outer tube and its current I be introduced, it does not change the eddy-current loss in the inner conductor, and the total additional power supplied by the circuits to the two conductors is equal to the resistance loss of the tube. There is a transfer of power from the inner conductor to the tube. These amounts of power can be computed.

In the work so far, magnetic flux outside the tube has not been considered as it would not change any of the current densities. It will be included now in computing the transfer of power, and, as would be expected, it will be shown that it has no effect on the expression for eddy-current loss.

The voltage drop in all elements of the tube is the same as that at its outer surface and is

$$j\omega 2(I + I_p)\,\text{logn}\,\frac{s}{r} + \rho i_{(t)} \tag{16}$$

where $i_{(t)}$ is the current density at the outer surface and where s is a certain large distance to which flux is counted. Since the expression in s will finally cancel out, the result is the same no matter how large a value of s is chosen.

Let conj I be the conjugate of I, that is, the same complex quantity except with j changed to $-j$. Multiply equation 16 by conj I and take the real part, to obtain the power delivered to the tube from its own circuit, per centimeter of the tube. This is

$$\text{Re conj } I\, j\omega 2(I+I_p) \operatorname{logn}\frac{s}{r}+$$
$$\text{Re conj } I\, a_0\rho\left[1+b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\right.$$
$$\left.\ldots\right]+\text{Re conj } I\, I_p\frac{j\omega 4t}{2q+t}\left[e_1+e_2\frac{jm^2t^2}{3!}+\right.$$
$$\left.e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right] \quad (17)$$

where Re denotes "real part of." Note that in this paper conj applies only to the single letter which follows it, but Re applies to the complete expression which follows.

$$b_1=1-\frac{1}{3}\frac{t}{q}+\frac{1}{4}\frac{t^2}{q^2}-\ldots$$
$$=\frac{1}{2}+\frac{q}{t}-\frac{q^2}{t^2}\operatorname{logn}\left(1+\frac{t}{q}\right)$$
$$b_2=1-\frac{2}{5}\frac{t}{q}+\frac{3}{10}\frac{t^2}{q^2}-\frac{17}{70}\frac{t^3}{q^3}\ldots$$
$$b_3=1-\frac{3}{7}\frac{t}{q}+\frac{9}{28}\frac{t^2}{q^2}-\frac{11}{42}\frac{t^3}{q^3}\ldots$$
$$b_4=1-\frac{4}{9}\frac{t}{q}+\frac{1}{3}\frac{t^2}{q^2}\ldots$$
$$b_5=1-\frac{5}{11}\frac{t}{q}+\frac{15}{44}\frac{t^2}{q^2}\ldots$$

as in reference 5.

$$e_1=1+\frac{1}{12}\frac{t^2}{q^2}-\frac{1}{12}\frac{t^3}{q^3}+\frac{3}{40}\frac{t^4}{q^4}-\frac{1}{15}\frac{t^5}{q^5}\ldots$$
$$=\left(\frac{1}{2}+\frac{q}{t}\right)\operatorname{logn}\left(1+\frac{t}{q}\right)$$
$$e_2=1+\frac{1}{10}\frac{t^2}{q^2}-\frac{1}{10}\frac{t^3}{q^3}+\frac{51}{560}\frac{t^4}{q^4}\ldots$$

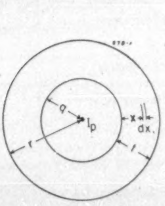


Figure 1. Tubular conductor and central wire

$$e_3=1+\frac{3}{28}\frac{t^2}{q^2}-\frac{3}{28}\frac{t^3}{q^3}\ldots$$
$$e_4=1+\frac{1}{9}\frac{t^2}{q^2}-\frac{1}{9}\frac{t^3}{q^3}\ldots$$

The total flux caused by the current in the tube, as far as radius s, is

$$2I\operatorname{logn}\frac{s}{r}+4\pi a_0t^2\left[\frac{b_1}{2!}+b_2\frac{jm^2t^2}{4!}+\right.$$
$$\left.b_3\frac{(jm^2t^2)^2}{6!}+\ldots\right]+\frac{4I_pt}{2q+t}\left[e_2\frac{jm^2t^2}{3!}+\right.$$
$$\left.e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right] \quad (18)$$

Multiply by $j\omega$ to obtain the voltage induced in the central conductor. Then multiply by conj I_p and take the real part to obtain the additional power supplied by the circuit of the central conductor. This power is transferred to the tube and helps supply the resistance loss in the tube. By adding equation 17, the total resistance loss in the tube is found to be

$$\text{Re conj } I(I)\, j\omega 2\operatorname{logn}\frac{s}{r}+\text{Re conj } I\, a_0\rho\times$$
$$\left[1+b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+$$
$$\text{Re conj } I\, I_p\frac{j\omega 4t}{2q+t}\times$$
$$\left[e_1+e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right]+$$
$$\text{Re conj } I_p(I)j\omega 2\operatorname{logn}\frac{s}{r}+$$
$$\text{Re conj } I_pa_0\rho\left[b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+$$
$$\text{Re conj } I_p\, I_p\frac{j\omega 4t}{2q+t}\left[e_2\frac{jm^2t^2}{3!}+\right.$$
$$\left.e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right] \quad (19)$$

Now conj $I\,I$ is a real quantity, $|I|^2$, since

$$(a-jb)(a+jb)=a^2+b^2+j0$$

conj $I\,I_p+I$ conj I_p is also a real quantity, since

$$(a-jb)(c+jd)+(a+jb)(c-jd)=2ac+2bd+j0$$

It is the sum of a complex quantity and its conjugate. Therefore, the terms in logn s/r have no real part and disappear.

The resistance loss in the tube, then, is

$$\text{Re conj } Ia_0\rho+\text{Re}(\text{conj } I+\text{conj } I_p)a_0\rho\times$$
$$\left[b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right]+$$
$$\text{Re}\frac{j\omega 4t}{2q+t}\left[\text{conj } I\, I_pe_1+I_p\,(\text{conj } I+\right.$$
$$\left.\text{conj } I_p)\left\{e_2\frac{jm^2t^2}{3!}+e_3\frac{(jm^2t^2)^2}{5!}+\ldots\right\}\right] \quad (20)$$

Note that the real parts of the entire expressions are taken.

Equations 15 and 20 can be applied to the three tubes in succession of a three-phase concentric circuit, thus obtaining the skin-effect resistance ratio of each tube, that is, the ratio of the resistance loss in the tube compared to the loss with direct current of the same amperage.

The power lost in a tube with direct current of amperage I is

$$\frac{|I|^2\rho}{\pi(2qt+t^2)} \quad (21)$$

The skin-effect resistance ratio of the inner tube is the same as that of an isolated tube, since the surrounding tubes do not affect its current density. In this case, $I_p=0$. The formulas and curves were published in reference 5.

Dividing equation 20 by equation 21 and substituting the value of a_0 given by equation 15.

$$\frac{R_{ac}}{R_{dc}}=\text{Re}\,\frac{1+b_1\frac{jm^2t^2}{2!}+b_2\frac{(jm^2t^2)^2}{4!}+\ldots}{1+c_1\frac{jm^2t^2}{3!}+c_2\frac{jm^2t^2}{5!}+\ldots} \quad (22)$$

for the inner tube.

For the intermediate tube

$$\frac{R_{ac}}{R_{dc}}=\text{Re}\frac{LM}{Q}+\text{Re}S \quad (23)$$

where

$$L=1+\left(1+\frac{\text{conj } I_p}{\text{conj } I}\right)\left\{b_1\frac{jm^2t^2}{2!}+\right.$$
$$\left.b_2\frac{(jm^2t^2)^2}{4!}+\ldots\right\} \quad (24)$$
$$M=1-\frac{I_p}{I}\left\{d_1\frac{jm^2t^2}{2!}+d_2\frac{(jm^2t^2)^2}{4!}+\ldots\right\} \quad (25)$$
$$Q=1+c_1\frac{jm^2t^2}{3!}+c_2\frac{(jm^2t^2)^2}{5!}+\ldots \quad (26)$$

(Same as equation 16, reference 5, or denominator of 22)

$$S=\frac{I_p}{I}e_1\frac{jm^2t^2}{1!}+\left(\frac{I_p}{I}+\left|\frac{I_p}{I}\right|^2\right)\times$$
$$\left\{e_2\frac{(jm^2t^2)^2}{3!}+e_3\frac{(jm^2t^2)^3}{5!}+\ldots\right\} \quad (27)$$

In balanced three-phase circuits, for the two opposite phase rotations

$$\frac{I_p}{I}=-\frac{1}{2}+j\frac{\sqrt{3}}{2} \text{ or } -\frac{1}{2}-j\frac{\sqrt{3}}{2} \quad (28)$$
$$1+\frac{\text{conj } I_p}{\text{conj } I}=\frac{1}{2}-j\frac{\sqrt{3}}{2} \text{ or } \frac{1}{2}+j\frac{\sqrt{3}}{2}$$
$$\frac{I_p}{I}+\left|\frac{I_p}{I}\right|^2=\frac{1}{2}+j\frac{\sqrt{3}}{2} \text{ or } \frac{1}{2}-j\frac{\sqrt{3}}{2}$$

The two opposite phase rotations produce slightly different amounts of eddy-current loss, that is, different values of R_{ac}, depending on whether the greater current density is produced in the inner or the outer surface of the intermediate tube. See Figure 3.

For the outer tube

$I_p = -I$, and

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\frac{1+d_1\dfrac{jm^2t^2}{2!}+d_2\dfrac{(jm^2t^2)^2}{4!}+\dots}{1+c_1\dfrac{jm^2t^2}{3!}+c_2\dfrac{(jm^2t^2)^2}{5!}+\dots} \tag{29}$$

Equations 22, 23, and 29 give the skin-effect resistance ratio of the three tubes of a three-phase concentric tubular circuit. They are applicable up to about $mt = 4$ and $t/d = 0.2(t/q = {}^2/_3)$, as in reference 5.

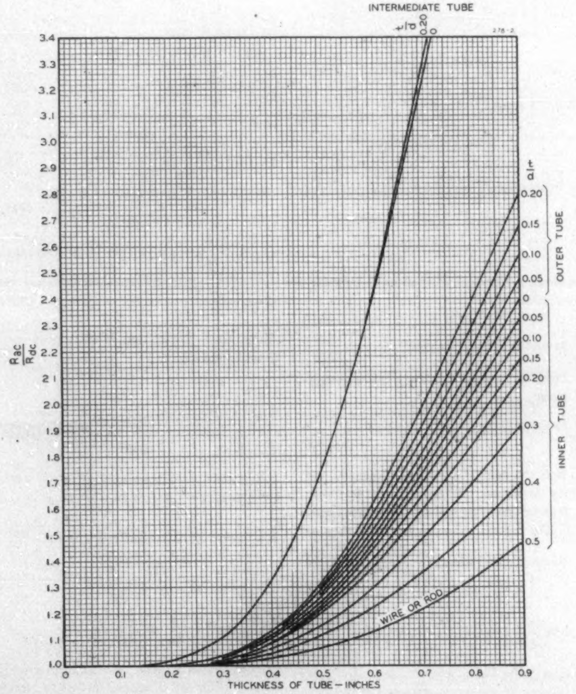
The values of the ratios are plotted in Figure 2 on a base of thickness of copper tube, for standard frequency and temperature. This can save the work of computing by the formulas, in cases where the curves apply. For a frequency f such as 25 or 50 cycles, multiply the tube thickness by $\sqrt{f/60}$ before reading from the curves.

In order to show that there is a larger skin-effect ratio for the intermediate tube than for the others, the current density in the three phases is plotted in Figure 3, as computed by equation 13 and following formulas. The case chosen is that of three flat straps, which is the same as that of three concentric tubes with extremely large radii.

The skin-effect resistance ratio of the outer tube of a single-phase concentric circuit is the same as the ratio for the outer tube of a three-phase concentric circuit as given by Figure 2 or equation 29, since the total current inside the tube in either case is $-I$. The resistance ratio of the inner tube of a single-phase concentric circuit, and also the ratio of an isolated tube, are the same as that of the inner tube of a three-phase circuit, as given by Figure 2 or equation 22. See also references 5 and 7.

Figure 2. Skin-effect ratios for solid copper tubes, 75 degrees centigrade, 60 cycles, three phase

Thickness = t and outside diameter = d, of the tube being considered



Example 2. Find the effective resistance at 60 cycles and 75 degrees centigrade of the three copper tubes described in example 1.

The inner tube, whose inside and outside diameters are 1.2 and 3.0 inches, has a resistance to direct current of 5.47×10^{-8} ohm per centimeter. From Figure 2, taking $t = 0.9$ inch and $t/d = 0.3$, the skin-effect resistance ratio is 1.92. Then R_{ac} for 60 cycles is

$$5.47 \times 10^{-8} \times 1.92 = 1.050 \times 10^{-7} \text{ ohm per centimeter}$$

This is to be used for resistance drop and copper loss in the inner tube.

For the intermediate tube, with inside and outside diameters 4.0 and 4.5 inches, and thickness 0.25 inch, the resistance to direct current is 9.74×10^{-8}. From Figure 2, $R_{ac}/R_{dc} = 1.059$ and therefore $R_{ac} = 1.030 \times 10^{-7}$ ohm per centimeter.

For the outer tube, the inside and outside diameters are 5.6 and 6.0 inches. The resistance ratio, from Figure 2, is 1.008 and

$$R_{ac} = 8.92 \times 10^{-8} \times 1.008 = 8.99 \times 10^{-8} \text{ ohm per centimeter}$$

Bessel-Function Formulas for Single-Phase Concentric Circuits and Isolated Tubes

The well-known solution for skin effect in an isolated tube in terms of Bessel functions is as follows.[2,7]

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\frac{\alpha t(q+r)}{2r} \times \frac{I_o(\alpha r)K_o'(\alpha q) - K_o(\alpha r)I_o'(\alpha q)}{I_o'(\alpha r)K_o'(\alpha q) - K_o'(\alpha r)I_o'(\alpha q)} \tag{30}$$

where I_o and K_o are modified Bessel functions of the first and second kinds, of order zero, Re denotes real part of

r = outside radius
q = inside radius
t = thickness of tube $= r - q$
$a^2 = \dfrac{j4\pi\omega}{\rho} = jm^2$
$\alpha = me^{\frac{i\pi}{4}}$
$j = \sqrt{-1}$
$\omega = 2\pi \times$ frequency
ρ = resistivity of the metal, in abohms per centimeter cube

Note that

$$I_o(\alpha r) = \text{ber } mr + j \text{ bei } mr \tag{31}$$

$$K_o(\alpha r) = \text{ker } mr + j \text{ kei } mr \tag{32}$$

$$I_o'(\alpha r) = e^{-i\pi/4}(\text{ber}' \ mr + j \text{ bei}' \ mr) \tag{33}$$

$$K_o'(\alpha r)=e^{-j\pi/4}\,(\text{ker}'\,mr+j\,\text{kei}'\,mr) \qquad (34)$$

Equation 30 is applicable to the inner tube of a concentric circuit, and to a nonconcentric circuit in which the conductors are separated by more than a few diameters and proximity effect is considered negligibly small.

For the outer tube of a concentric circuit, where all the return conductors are within the outer tube[2,4]

$$\frac{R_{ac}}{R_{dc}}=\text{Re}\,\frac{-\alpha t(q+r)}{2q}\times \frac{I_o(\alpha q)K_o'(\alpha r)-K_o(\alpha q)I_o'(\alpha r)}{I_o'(\alpha q)K_o'(\alpha r)-K_o'(\alpha q)I_o'(\alpha r)} \qquad (35)$$

This is the same as formula 30 for an isolated tube, except interchange q and r and multiply by -1. Both formulas 30 and 35 may be derived by following the general method used in references 7 and 4

Formulas 30 and 35 may be used for direct computation of numerical problems, using tabulated values from references 7, 8, 9, or 10.

For very small or very large values of the argument, or for tubes whose thickness is small compared to their radius, series formulas, as given in this paper, may be shorter to use.

High-Frequency Formulas

For frequencies higher than 60 cycles, formulas derived from the asymptotic expansions of Bessel functions are often convenient. Except that they do not give any effect of radiation, and that they apply only to the dominant mode of transmission in a coaxial line, they are applicable to very high frequency since they are series with powers of the frequency in the denominators of the terms. As is usual with power series, their applicability depends on the rapidity with which the terms of the series become smaller and smaller.

Formulas of this type for the inner and outer conductors are given here. They are applicable to single-phase, concentric circuits and the formula for the inner conductor is applicable also to all the tubular conductors of circuits which are not concentric and in which the conductors are separated by more than a few diameters. With high frequency, such circuits are more likely to occur than three-phase concentric circuits.

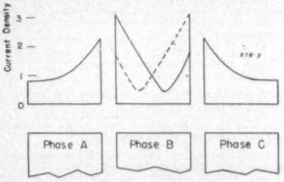


Figure 3. Current density in three-phase strap conductors, closely adjacent

Current density for same amperage of direct current = 1. $mt=\sqrt{6}$. The dotted line is for opposite phase rotation

To express equation 30 as a power series using asymptotic expansions (see reference 10, items 810.6, 810.7, 816.1, and 816.2) we have

$$-\frac{I_o'(\alpha q)}{K_o'(\alpha q)}=\frac{I_1(\alpha q)}{K_1(\alpha q)}$$

$$=\frac{e^{2\alpha q}}{\pi}\,\frac{1-\dfrac{3}{1!8\alpha q}-\dfrac{3\times 5}{2!(8\alpha q)^2}-\dfrac{3^2\times 5\times 7}{3!(8\alpha q)^3}\cdots}{1+\dfrac{3}{1!8\alpha q}-\dfrac{3\times 5}{2!(8\alpha q)^2}+\dfrac{3^2\times 5\times 7}{3!(8\alpha q)^3}\cdots}$$

$$=\frac{e^{2\alpha q}}{\pi}\left[1-\frac{6}{8\alpha q}+\frac{18}{(8\alpha q)^2}-\frac{204}{(8\alpha q)^3}\cdots\right]$$

$$\frac{Z_{ac}}{R_{dc}}=\frac{\alpha t(q+r)}{2r}\,\frac{N}{D}$$

where

$$N=e^{\alpha r}\left\{1+\frac{1}{8\alpha r}+\frac{9}{2(8\alpha r)^2}+\frac{75}{2(8\alpha r)^3}\cdots\right\}+ e^{2\alpha q-\alpha r}\left\{1-\frac{1}{8\alpha r}+\frac{9}{2(8\alpha r)^2}-\frac{75}{2(8\alpha r)^3}\cdots\right\}\times \left\{1-\frac{6}{8\alpha q}+\frac{18}{(8\alpha q)^2}-\frac{204}{(8\alpha q)^3}\cdots\right\}$$

and

$$D=e^{\alpha r}\left\{1-\frac{3}{8\alpha r}-\frac{15}{2(8\alpha r)^2}-\frac{105}{2(8\alpha r)^3}\cdots\right\}- e^{2\alpha q-\alpha r}\left\{1+\frac{3}{8\alpha r}-\frac{15}{2(8\alpha r)^2}+\frac{105}{2(8\alpha r)^3}\cdots\right\}\times \left\{1-\frac{6}{8\alpha q}+\frac{18}{(8\alpha q)^2}-\frac{204}{(8\alpha q)^3}\cdots\right\}$$

Then

$$\frac{Z_{ac}}{R_{dc}}=\frac{\alpha t(q+r)}{2r}\left[1+\frac{4}{8\alpha r}+\frac{24}{(8\alpha r)^2}+\frac{192}{(8\alpha r)^3}\cdots+ 2e^{-2\alpha t}\left\{1+\frac{6}{8\alpha r}+\frac{42}{8\alpha r)^2}+\frac{348}{(8\alpha r)^3}\cdots\right\}\times \left\{1-\frac{6}{8\alpha q}+\frac{18}{(8\alpha q)^2}-\frac{204}{(8\alpha q)^3}\cdots\right\}+ \text{terms in } e^{-4\alpha t}\cdots\right]=\frac{t(q+r)}{r^2}\times$$

$$\left[\frac{\alpha r}{2}+\frac{1}{4}+\frac{3}{16\alpha r}+\frac{3}{16\alpha^2 r^2}\cdots+e^{-2\alpha t}\times \left\{\alpha r+\frac{3}{4}+\frac{21}{32\alpha r}\cdots\right\}\left\{1-\frac{3}{4\alpha q}+ \frac{9}{32\alpha^2 q^2}\cdots\right\}+\text{terms in } e^{-4\alpha t}\cdots\right]$$

omitting the final terms of the last two brackets. Now

$$\alpha=me^{j\pi/4}=m\left(\frac{1}{\sqrt{2}}+\frac{j}{\sqrt{2}}\right)$$

$$\frac{1}{\alpha}=\frac{1}{m}e^{-j\pi/4}=\frac{1}{m}\left(\frac{1}{\sqrt{2}}-\frac{j}{\sqrt{2}}\right)$$

$$\frac{1}{\alpha^2}=\frac{1}{m^2}e^{-j\pi/2}=\frac{1}{m^2}(0-j)$$

$$e^{-2\alpha t}=e^{-mt\sqrt{2}}(\cos mt\sqrt{2}-j\sin mt\sqrt{2})$$

(See reference 10, item 408.05) Taking the real part, the resistance ratio for an isolated tube is

$$\frac{R_{ac}}{R_{dc}}=\frac{t(q+r)}{r^2}\left[\frac{mr}{2\sqrt{2}}+\frac{1}{4}+\frac{3}{16mr\sqrt{2}}+\frac{0}{r^2}\cdots+ e^{-mt\sqrt{2}}(\cos mt\sqrt{2})\left\{\frac{mr}{\sqrt{2}}-\frac{3}{4}\frac{t}{q}+ \frac{3}{32mr\sqrt{2}}\left(7-6\frac{r}{q}+3\frac{r^2}{q^2}\right)\cdots\right\}+ e^{-mt\sqrt{2}}(\sin mt\sqrt{2})\left\{\frac{mr}{\sqrt{2}}-\frac{3}{32mr\sqrt{2}}\times \left(7-6\frac{r}{q}+3\frac{r^2}{q^2}\right)\cdots\right\}+\text{terms in } e^{-2mt\sqrt{2}}\cdots\right] \qquad (36)$$

The first line is seen to be the series that is applicable to solid wire. Formula 36 was given a number of years ago in his Master of Science thesis at Massachusetts Institute of Technology by J. M. Roberts, now professor of electrical engineering at the University of Louisville, Louisville, Kentucky.

The expansion of formula 35 for large values of m is not obtained by interchanging q and r in formula 36, for $e^{-mt\sqrt{2}}$ would become $e^{mt\sqrt{2}}$. The expansion is made by taking $K_o(\alpha q)/K_o'(\alpha q)$ as the initial part. The result is:

High-frequency formula for resistance ratio of outer tube:

$$\frac{R_{ac}}{R_{dc}}=\frac{t(q+r)}{q^2}\left[\frac{mq}{2\sqrt{2}}-\frac{1}{4}+\frac{3}{16mq\sqrt{2}}+\frac{0}{q^2}\cdots+ e^{-mt\sqrt{2}}(\cos mt\sqrt{2})\left\{\frac{mq}{\sqrt{2}}-\frac{3}{4}\frac{t}{r}+ \frac{3}{32mq\sqrt{2}}\left(7-6\frac{q}{r}+3\frac{q^2}{r^2}\right)\cdots\right\}+ e^{-mt\sqrt{2}}(\sin mt\sqrt{2})\left\{\frac{mq}{\sqrt{2}}-\frac{3}{32mq\sqrt{2}}\times \left(7-6\frac{q}{r}+3\frac{q^2}{r^2}\right)\cdots\right\}+\text{terms in } e^{-2mt\sqrt{2}}\cdots\right] \qquad (37)$$

The Penetration Formula

The "penetration formula" is a well-known and useful method for computing the a-c resistance of conductors at moderately high frequencies. However, special precautions should be taken in using it for tubes, and these will now be described.

A convenient statement of the penetration formula, arranged so as to give a close approximation to the Bessel function solution at high frequencies, is that the alternating current is taken to penetrate, at uniform current density, to a depth δ_1 and the a-c resistance is taken to be equal to the d-c resistance of a tube consisting of the surface layer of metal of thickness δ_1. The formula for the thickness is

$$\delta_1 = \sqrt{2}/m \qquad \text{centimeters} \tag{38}$$

where

$$m = \sqrt{(4\pi\omega/\rho)}$$

and where ρ is in abohms per centimeter cube (see equation 11).

For an isolated round wire or tube of outside radius r, the effective resistance, then, is that of a tube of cross section

$$\pi\{r^2-(r-\delta_1)^2\} = 2\pi r\delta_1\left(1-\frac{\delta_1}{2r}\right)$$
$$= \frac{2\pi r\sqrt{2}}{m}\left(1-\frac{1}{mr\sqrt{2}}\right) \tag{39}$$

from equation 38.

The resistance ratio by the penetration formula is the ratio of the two cross sections, which is

$$\frac{\pi(r^2-q^2)m}{2\pi r\sqrt{2}}\left(1-\frac{1}{mr\sqrt{2}}\right)^{-1} = \frac{t(q+r)}{r^2}\times$$
$$\left(\frac{mr}{2\sqrt{2}}+\frac{1}{4}+\frac{1}{4mr\sqrt{2}}+\ldots\right) \tag{40}$$

by the binomial expansion of $(1-x)^{-1}$.

The first two terms of the series in equation 40 are the same as in formula 36. These two terms give the equation of the straight line which is the asymptote of the curve of R_{ac}/R_{dc} plotted on mt, as in Figure 3, reference 5.

In order for equation 40 to agree with the corresponding portion of formula 36 within what might be called slide-rule accuracy, or about 0.5 per cent, it is necessary for $1/8m^2r^2$ to be less than 0.005. That is

$$\frac{\delta_1}{r} < \frac{1}{4} \tag{41}$$

This is often expressed by stating that the penetration depth δ_1 should be a small fraction of the radius of curvature, and under such a condition it can apply to conductors that are not round.

In the case of round tubes, another requirement is found from equation 36, namely, that the penetration depth δ_1 should be a small part of the thickness t. Equation 40 plainly does not take care of the terms in $e^{-mt\sqrt{2}}$ of formula 36, and so, if the penetration formula is to be a good approximation, these terms should be proportionately small. This leads to the condition that

$$\frac{\delta_1}{t} < 0.3 \text{ approximately} \tag{42}$$

In most cases, if equation 42 is complied with, then the requirement of equation 41 also is met.

The penetration formula can be applied to the inner surface of the outer tube of a concentric circuit.

Example 3. Find the skin-effect resistance ratio for an outer tube in which $mt=3.2$, $mq=4.8$, and $mr=8$.

$$\frac{q+r}{2q} = \frac{4}{3},\quad \alpha t = mte^{j\pi/4}$$

$$\begin{aligned} I_0(\alpha q) &= \text{ber } mq + j \text{ bei } mq \\ &= \text{ber } 4.8 + j \text{ bei } 4.8 \\ &= -5.45 + j0.884 \end{aligned}$$

$$\begin{aligned} K_0'(\alpha r) &= e^{-j\pi/4}(\text{ker}'\ 8.0 + j \text{ kei}'\ 8.0) \\ &= e^{-j\pi/4}(-0.000880 - j0.001336) \end{aligned}$$

By equation 35,

$$\frac{R_{ac}}{R_{dc}} = \text{Re}\left\{-3.2(0+j)\times\frac{4}{3}\right\}\frac{-0.447+j0.376}{0.629}$$
$$= 2.55 \text{ by equation 35}$$

From equation 37

$$\frac{t(q+r)}{q^2} = 1.777$$

First line of equation 37

$$= 1.697 - 0.25 + 0.0276$$
$$= 1.475$$

$$mt\sqrt{2} = 4.525 \qquad e^{-4.525} = 0.01083$$

$$\cos 4.525 = -0.1858$$

Second line of equation 37

$$= 0.01083\times(-0.1858)(3.39-0.30+0.062\ldots)$$
$$= -0.00634$$

Third line of equation 37

$$= 0.01083\times(-0.983)(3.39-0.062)$$
$$= -0.0354$$

$$\frac{R_{ac}}{R_{dc}} = 1.777(1.475-0.0063-0.0354)$$
$$= 2.55$$

Thickness of tube in inches, for copper at 75 degrees centigrade and 60 cycles

$$m^2 = \frac{4\pi\omega}{\rho} = \frac{4\pi\times 120\pi}{1{,}724}\times\frac{234+20}{234+75}$$
$$= 2.26$$

$$m = 1.504$$

$$\text{thickness} = \frac{3.2}{1.504}\times\frac{1}{2.54} = 0.838 \text{ inch}$$

$$t/d = \frac{3.2}{2\times 8} = 0.2$$

From Figure 2

$$R_{ac}/R_{dc} = 2.54$$

By the penetration formula, the section of the equivalent tube of thickness δ_1 is

$$\pi\{(q+\delta_1)^2-q^2\} = 2\pi q\delta_1\left(1+\frac{\delta_1}{2q}\right)$$
$$= \frac{2\pi q\sqrt{2}}{m}\left(1+\frac{1}{mq\sqrt{2}}\right)$$

by equation 38

$$\frac{R_{ac}}{R_{dc}} = \frac{\pi t(q+r)m}{2\pi q\sqrt{2}\left(1+\frac{1}{mq\sqrt{2}}\right)}$$
$$= \frac{3.2\times 12.8}{2(6.78+1)} = 2.63$$

which is three per cent too large.

$$\delta_1 = \frac{\sqrt{2}}{1.504} = 0.94 \text{ centimeter or 0.37 inch}$$

$$\frac{\delta_1}{t} = \frac{0.37}{0.838} = 0.44$$

which is somewhat too large for the employment of the penetration formula.

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Standardized Load-Center Unit Substations for Low-Voltage A-C Systems

E. M. HUNTER
MEMBER AIEE

J. C. PAGE
ASSOCIATE AIEE

THE tremendous expansion of production facilities necessitated by American defense and war efforts has given the manufacturer of electrical equipment a serious challenge. Entire large defense projects, such as manufacturing plants, navy yards, drydocks, air depots, and office buildings, have been built in incredibly short times. Any one of these projects will not be complete without an electrical distribution system. One of the many tasks before the electrical manufacturer is to produce the necessary apparatus for the electrical distribution systems, and have it available by the time the project is ready to receive it.

In most of the recent projects power has been distributed to ultimate loads by means of the load-center power distribution systems. This type, although only recently applied to low-voltage systems, has been used successfully for years on high-voltage systems in the central station field. The essentials of load-center distribution for low-voltage a-c systems are

1. A large load area is divided into small load areas, each ranging from 600 to 1,000 kva at 480 volts for example.
2. Power is distributed at medium voltage (2.4 to 13.2 kv) to substations, located near the electrical load center in each of the small areas where the voltage is transformed to utilization voltage to service the area loads.

It has been found that the load-center distribution system when properly applied is superior to other systems from a performance standpoint, and less costly. One of the most important contributions of the electrical manufacturer to the field of low-voltage a-c systems in recent years has been the development and standardization of the load-center unit substation to service the small load areas described above. To the purchaser of electrical apparatus, the development and standardization of the load-center unit substation means that a complete, co-ordinated, pre-engineered step-down substation for a low-voltage a-c system may be ordered in the same manner as a standard motor or standard motor-control equipment.

It is the purpose of this paper to present

1. A definition of a load-center unit substation.
2. The general requirements which have been met by the standardized line with which the writers are most familiar.
3. A description of units with illustrations.
4. Tables for the ready selection of load-center unit substations.

For clarity the term "load-center unit substation" is defined here as follows:

A completely metal-enclosed integrated substation equipment incorporating

1. The required high-voltage switching, control, and incoming circuit termination facilities.
2. Transformer section or sections to transform a-c power from the medium-voltage range (2,400 to 15,000 volts) to the low-voltage range of 600 volts and below.
3. The required low-voltage switching, control, and outgoing feeder circuit termination facilities.

An installation view of a load-center unit substation is shown on Figure 1.

General Considerations

The load-center distribution system is generally thought of in connection with servicing a fairly large area with power. Thus the ultimate areas to be served usually consist of large buildings, mines, yards, docks, piers, camps, and so forth.

Safety considerations usually limit the highest voltage at which power may be carried within the load area to 13.2 kv. It is usually uneconomical to carry power to load-center locations at less than 2,400 volts because of the large cable required to carry the higher currents. Thus, for a given application any one of the medium voltages may be selected for this service, depending on local conditions and the requirements to be met. Consequently, from the electrical manufacturer's point of view, equipment must be available for the medium voltages, such as 2,400, 4,160, 4,800, 7,200, 12,000, and 13,200 volts.

When the circuit voltage of the available power supply is above 13.2 kv, it is generally advisable to step this down to a voltage in the medium-voltage range for distribution to the load area. This can be conveniently accomplished by means of a multicircuit unit substation, such as is shown on Figure 2. Where many such load areas are to be served, and service reliability is at a premium, two sources of supply may be brought in, or the master substations can be networked to form a primary network. A mobile substation can be used to serve as a spare, similar to the one shown on Figure 3.

From the standpoint of the load-center distribution, there are four types of basic

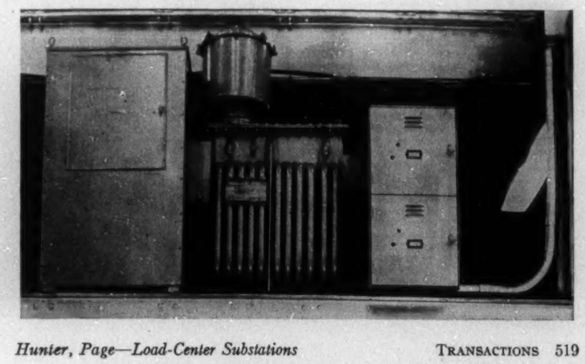

Figure 1. Indoor installation view of load-center unit substation, Pyranol-filled, rated three-phase, 60 cycles, 100 kva, 2,400 to 208/120 volts

Unit has two sets of primary cutouts for alternate supply and two low-voltage air circuit breakers for feeder protection. Located under crane rails, and supplies power to fluorescent lamps

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E. M. HUNTER is application engineer, protective devices, central station engineering department, and J. C. PAGE also is with central station engineering department, both of General Electric Company, Schenectady, N. Y.

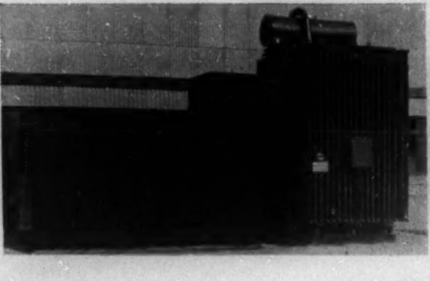


Figure 2. Outdoor unit substation rated three-phase, 60 cycles, 4,500 kva self-cooled, 6,000 kva forced air-cooled, 13,800 to 2,400 volts

Unit has four outgoing feeder circuit breakers and is located next to building it serves

circuit arrangements used.[1] These may be described as

1. The straight radial system.
2. The primary-selective system which comprises two primary lines brought into each load-center unit substation, usually with provision for connection to only one line at a time.
3. The radial-primary secondary-selective system, which involves bringing in only one source of primary power, while utilizing normally open secondary tie circuits to other substations to obtain power for emergency operation.
4. The low-voltage network scheme.

Table I. General Requirements of Load-Center Unit Substations

Requirement Item	Rating or Description of Substation
Frequency	Practically all 60 cycles
Phases	Three
Kva ratings	100, 200, 300, 450, 500, 600, 750, 1,000, 1,200, 1,500, 2,000
Primary voltage	2,400, 4,160, 4,800, 7,200, 12,000, 13,200
Secondary voltage	600, 480, 240, 208/120
Transformer coolant	Pyranol, oil, air
Incoming primary feeders	One or two (switches or circuit breakers)
Outgoing secondary feeders	Any number (usually less than ten air circuit breakers)
Enclosures	Indoor: general purpose outdoor: weatherproof
System type	
1. Straight radial	One incoming primary circuit to substation, radial secondary feeders
2. Primary selective	Preferred and emergency incoming primary circuits to substation, radial secondary feeders
3. Secondary selective	One incoming primary circuit to substation, radial secondary feeders with secondary emergency tie circuits to other substations
4. Secondary network	One incoming primary circuit to substation, secondary-network protector, radial secondary feeders, secondary tie circuits

Any one or several of these basic systems may be used in a given application, depending upon the requirements to be met.

The low-voltage ratings of load-center unit substations are fixed by the American Standards Association standard three-phase transformer voltages which are 600, 480, 240, and 208/120 *Y* volts. It has been found that the ASA standard kilovolt-ampere ratings, from 100 through 2,000 kva inclusive, cover nearly 100 per cent of the application requirements for this type of equipment.

The requirements as to location are such that it is essential to have available units for both indoor and outdoor service. These two types allow the distribution engineer to locate the substation in electrical load-center locations, often in space that would otherwise be worthless. An installation view of an outdoor unit is shown on Figure 4.

Thus, the general requirements to be met by such equipment may be tabulated as in Table I.

Description of Equipment

The transformer section of the load-center unit substation consists of a three-phase transformer with leads from the high- and low-voltage windings brought out of the tank through side-wall bushings to metal-enclosed bus connections known as throats. The throats are built integral with the transformer tanks, with the low-voltage throat located on one short-dimension side of the tank and the high-voltage throat located on the opposite side of the tank. The high-voltage throat is arranged so that any one of a number of high-voltage arrangements of switching equipment and cable-termination apparatus may be connected to it, while the low-voltage throat is arranged for connection to metal-enclosed low-voltage switchgear.

The high-voltage equipment may consist of metal-enclosed switchgear. Circuit breakers are available with interrupting ratings from 25,000 to 150,000 kva in 5-kv metal-clad equipments, and 250,000

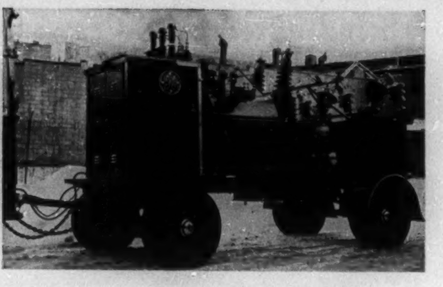


Figure 3. Side view of 1,000-kva mobile substation

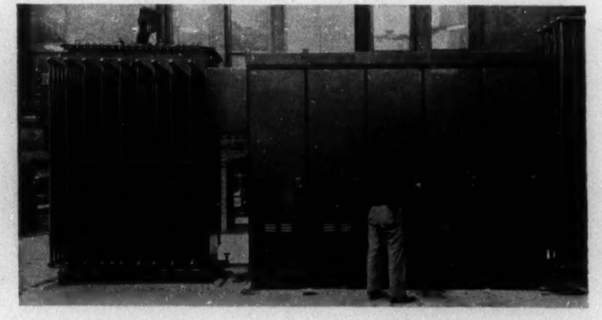


Figure 4. Outdoor installation view of load-center unit substation, Pyranol-filled, rated three-phase, 60 cycles, 1,500 kva, 13,800 to 575 volts

Unit has primary liquid-filled disconnecting switch and ten low-voltage air circuit breakers for protection of feeders supplying office building. Transformer at right is spare

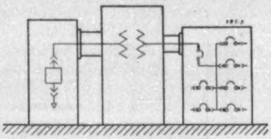


Figure 5. Load-center unit substation with one primary circuit breaker

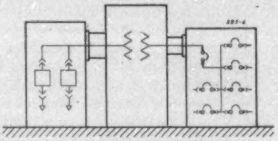


Figure 6. Load-center unit substation with two primary circuit breakers

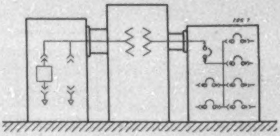


Figure 7. Load-center unit substation with two primary circuit-breaker positions and one circuit breaker

Load-Center Unit-Substation Application Table II. 120/208 Y Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating										
	100	150	200	300	450	500	600	750	1,000	1,200	1,500
	Normal Current (Amperes)										
	278	417	556	834	1,250	1,388	1,665	2,080	2,780	3,330	4,164
I.	For Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting and Current Rating)										
25,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
50,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
75,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
100,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
150,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
250,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
500,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	100-AL2
Unlimited	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2	
II.	For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)										
25,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	100-AL2	100-AL2
250,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	100-AL2	100-AL2
500,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
Unlimited	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	
III.	For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)										
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	
IV.	Total Low-Voltage Short-Circuit Currents—Amperes RMS (Impedances as Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 50 Per Cent of Transformer Rating)										
25,000	8,580	12,300	13,300	18,800	26,100	28,300	32,300	35,800	43,500	48,600	55,700
50,000	8,970	13,200	14,200	20,700	29,600	32,400	37,800	42,300	53,300	61,000	71,600
75,000	9,110	13,400	14,600	21,300	31,000	34,100	40,100	45,200	57,800	64,900	79,800
100,000	9,170	13,500	14,800	21,700	31,800	35,000	41,300	46,800	60,400	70,500	84,800
150,000	9,240	13,700	14,900	22,100	32,600	36,000	42,700	48,500	63,300	74,400	90,600
250,000	9,280	13,900	15,000	22,400	33,300	36,800	43,900	50,000	65,900	77,900	95,700
500,000	9,350	14,000	15,100	22,600	33,800	37,500	44,800	51,300	67,900	81,000	100,000
Unlimited	9,380	14,100	15,200	22,900	34,400	38,100	45,900	52,500	70,200	84,100	105,000

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage
2. Transformer contributes approximately 91 per cent of total short-circuit current for 50 per cent connected motors

V.	Transformer Impedance Per Cent										
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5

VI. **Magnetic Air Circuit Breakers Available**

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200, 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

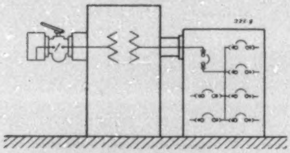


Figure 8. Load-center unit substation with primary flange-mounted liquid-filled cutouts

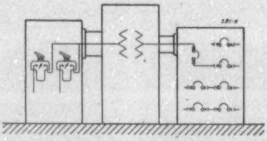


Figure 9. Load-center unit substation with two sets of primary liquid-filled cutouts in free standing enclosure

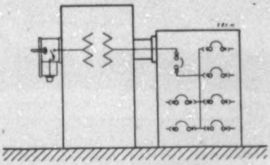


Figure 10. Load-center unit substation with primary liquid-filled flange-mounted switch

Load-Center Unit-Substation Application Table III. 240 Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations Only

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating										
	100	150	200	300	450	500	600	750	1,000	1,200	1,500
	Normal Current (Amperes)										
	241	361	481	722	1,083	1,203	1,443	1,804	2,406	2,886	3,609
I. For Transformer Main Low-Voltage Breakers (Selection Based on Adequate Interrupting and Current Rating)											
25,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
50,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
75,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
100,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
150,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
250,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2	100-AL2
500,000	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
Unlimited	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
II. For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interruping Rating Only. Check Current Ratings From Table V)											
25,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	75-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	100-AL2	100-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2	100-AL2	100-AL2
III. For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
IV. Total Low-Voltage Short-Circuit Current—RMS Amperes (Impedances As Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 100 Per Cent of Transformer Rating)											
25,000	8,040	11,600	12,800	18,200	25,200	27,500	31,600	35,600	43,700	52,500	57,300
50,000	8,370	12,300	13,500	19,720	28,300	31,100	36,300	41,300	52,200	57,000	71,200
75,000	8,470	12,600	13,800	20,300	29,600	32,500	38,300	43,700	56,100	62,700	78,300
100,000	8,550	12,700	14,000	20,600	30,200	33,300	39,400	45,100	58,300	66,200	82,500
150,000	8,610	12,800	14,100	21,000	30,900	34,200	40,600	46,600	60,800	70,000	87,500
250,000	8,650	12,900	14,200	21,200	31,500	34,900	41,700	48,000	63,000	73,700	92,000
500,000	8,700	13,000	14,300	21,500	32,000	35,500	42,400	49,000	64,800	76,800	95,900
Unlimited	8,730	13,100	14,400	21,700	32,500	36,100	43,300	50,100	66,700	80,200	100,000

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage

2. Transformer contributes approximately 84 per cent of total short-circuit current for 100 per cent connected motors

V.	Transformer Impedance Per Cent										
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5

VI. Magnetic Air Circuit Breakers Available

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

to 500,000 kva in 15-kv metal-clad equipments. The vertical-lift removable type of breaker is standard in these equipments. Instruments, meters, and relays, mounted on steel panels, may be part of the equipment.

The metal-clad switchgear may have only one breaker as shown in Figure 5. Again there may be two breakers to meet the requirements of the primary-selective scheme as shown in Figure 6. A modification of the primary-selective scheme comprises two breaker positions and only one breaker, as shown in Figure 7. One removable breaker is arranged so that it can be inserted in either of the two breaker positions. This, of course, results in a lower installed cost and has proved attractive for some applications.

In place of the high-voltage metal-enclosed switchgear a set of three flange-mounted gang-operated oil-filled cutout switches may be used, as shown in Figure 8. This type of switch is rated five kilovolts, 250 amperes continuous. Most applications of the switch have utilized the switch as a disconnect only, with copper disconnecting blades in place of fuse links. The cutouts may be fused in the smaller ratings, when co-ordination with the low-voltage circuit breakers can be obtained such that the low-voltage circuit breakers will clear secondary faults before primary fuses are damaged. Cutouts may be fused up to 100 amperes at 5 kv and 200 amperes at 2.5 kv, with inter-

Load-Center Unit-Substation Application Table IV. 480 Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations Only

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating											
	100	150	200	300	450	500	600	750	1,000	1,200	1,500	2,000
	Normal Current (Amperes)											
	120	181	241	361	542	601	722	902	1,203	1,443	1,804	2,406
I.	For Transformer Main Low-Voltage Breakers (Selection Based on Adequate Interrupting and Current Rating)											
25,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
50,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
75,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
100,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
150,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
250,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
500,000	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2	75-AL2
II.	For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	75-AL2	75-AL2
III.	For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2
IV.	Total Low-Voltage Short-Circuit Current—RMS Amperes (Impedance As Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 100 Per Cent of Transformer Rating)											
25,000	4,020	5,810	6,390	9,080	12,700	13,800	15,800	17,800	21,800	24,700	28,600	34,400
50,000	4,180	6,150	6,760	9,860	14,200	15,500	18,200	20,600	26,100	30,600	35,600	43,800
75,000	4,250	6,270	6,910	10,200	14,800	16,300	19,200	21,900	28,100	32,600	39,100	49,000
100,000	4,280	6,340	6,980	10,300	15,100	16,700	19,700	22,500	29,200	34,200	41,300	52,200
150,000	4,310	6,400	7,050	10,500	15,500	17,100	20,300	23,300	30,400	35,900	43,800	56,200
250,000	4,330	6,460	7,120	10,600	15,800	17,500	20,800	24,000	31,500	37,400	46,000	59,900
500,000	4,350	6,500	7,160	10,700	16,000	17,800	21,200	24,500	32,400	38,700	47,900	63,000
Unlimited	4,370	6,540	7,200	10,800	16,300	18,100	21,700	25,000	33,400	40,000	50,100	66,800

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage
2. Transformer contributes approximately 84 per cent of total short-circuit current for 100 per cent connected motors

V.	Transformer Impedance Per Cent											
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5	5.5

VI. Magnetic Air Circuit Breakers Available

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200, 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

rupting ratings of 5,000 amperes rms at 5 kv, and 10,000 amperes rms at 2.5 kv.

For a five-kilovolt primary-selective system two sets of cutouts are available in a free-standing metal house. All of the cutouts have gang-operating mechanisms, usually interlocked so that only one set can be closed at a time. This arrangement is shown in Figure 9.

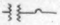
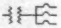
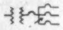
A liquid-filled 15-kv disconnecting switch arrangement is shown in Figure 10. This switch is the one developed for network transformers. It is suitable for circuits with normal currents up to 200 amperes. The switch may be filled with either oil or Pyranol, depending upon the application.

In addition to these high-voltage arrangements, the load-center unit substation may be equipped with a cable terminal and junction box without any disconnecting means. These may be oil- or Pyranol-filled for either 5-kv or 15-kv circuits, or air-filled for 5-kv circuits. This arrangement is shown in Figure 11.

The transformer section itself is available in all standard three-phase kilovolt-ampere ratings from 100 to 2,000 kva. Transformers rated above 250 kva are equipped with four $2^1/_2$ per cent taps spaced two above and two below normal high voltage. Automatic voltage-regulating equipment is not included in the transformer section, because there has been no demand for this equipment.

Load-Center Unit-Substation Application Table V. 600 Volts, Three-Phase

For Use in Connection With Load-Center Unit Substations Only

Available Primary 3-Phase Short-Circuit Kva	Transformer Kilovolt-Ampere Rating											
	100	150	200	300	450	500	600	750	1,000	1,200	1,500	2,000
	Normal Current (Amperes)											
	96.2	144	192	289	433	481	577	722	962	1,154	1,444	1,925
I. 	**For Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting and Current Rating)**											
25,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2	75-AL2
II. 	**For Transformer Multiple Low-Voltage Breakers (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)**											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	50-AL2
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2	50-AL2	50-AL2	75-AL2
III. 	**For Branch Circuit Breakers Co-ordinated With Transformer Main Low-Voltage Breaker (Selection Based on Adequate Interrupting Rating Only. Check Current Ratings From Table V)**											
25,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A
50,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B
75,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
100,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
150,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
250,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B
500,000	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2
Unlimited	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	15-AE1A	25-AE1B	25-AE1B	50-AL2
IV.	**Total Low-Voltage Short-Circuit Current—RMS Amperes (Impedances As Given in Table V, Total Connected Motor Kilovolt-Amperes Equals 100 Per Cent of Transformer Rating)**											
25,000	3,210	4,640	5,100	7,260	10,100	11,000	12,600	14,200	17,500	19,800	22,900	27,500
50,000	3,350	4,920	5,400	7,870	11,300	12,400	14,500	16,500	20,900	24,100	28,500	35,100
75,000	3,390	5,010	5,520	8,110	11,800	13,000	15,300	17,500	22,500	26,100	31,300	39,200
100,000	3,420	5,060	5,580	8,250	12,100	13,400	15,800	18,100	23,300	27,400	33,100	41,800
150,000	3,450	5,120	5,640	8,380	12,400	13,700	16,300	18,700	24,300	28,700	35,100	45,000
250,000	3,460	5,170	5,680	8,490	12,600	14,000	16,700	19,200	25,300	29,900	36,900	47,900
500,000	3,480	5,200	5,730	8,570	12,800	14,200	17,000	19,600	25,900	31,000	38,400	50,400
Unlimited	3,490	5,220	5,760	8,660	13,000	14,400	17,300	20,000	26,700	32,300	40,100	53,500

1. For different voltage base, multiply values in Table IV by the ratio: Standard voltage/New voltage

2. Transformer contributes approximately 84 per cent of total short-circuit current for 100 per cent connected motors

V.	Transformer Impedance Per Cent											
	4.0	4.0	5.0	5.0	5.0	5.0	5.0	5.5	5.5	5.5	5.5	5.5

VI. Magnetic Air Circuit Breakers Available

Type	Interrupting Rating (RMS Amperes)	Standard Continuous-Current Trip Ratings (Amperes)
15-AE1A	15,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225
25-AE1B	25,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600
50-AL-2	50,000	15, 20, 25, 35, 50, 70, 90, 100, 125, 150, 175, 200, 225, 250, 275, 300, 325, 350, 400, 450, 500, 550, 600, 800, 1,000, 1,200, 1,600
75-AL-2	75,000	2,000, 2,500, 3,000
100-AL-2	100,000	4,000, 5,000, 6,000

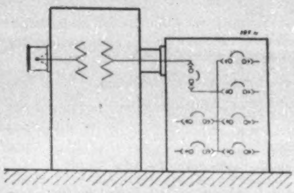


Figure 11. Load-center unit substation with primary junction box only

The transformer may be liquid-filled with the medium of cooling either oil or Pyranol. The Pyranol coolant is generally preferred for both indoor and outdoor load-center applications, but oil-filled transformers are often used outdoors. The Pyranol transformer has been a big factor in the development of the load-center distribution idea. Pyranol will not burn, and thus the Pyranol load-center unit substation can be located indoors near production centers without an expensive fireproof vault.

The dry type of transformer, cooled by the natural circulation of air through the windings, has also been developed for load-center unit substations.

Metal enclosed low-voltage switchgear is used in load-center unit substations. Usually, a number of branch circuits radiate from a single load-center unit substation. These branch circuits are controlled by means of air circuit breakers, mounted in compartments in the low-voltage switchgear. Air circuit breakers, with current interrupting ratings of 15,000, 25,000, 50,000, 75,000, and 100,000 amperes rms, are available in these equipments. The breakers may be either manually or electrically operated. The majority of the applications have required the use of manually operated breakers only. All circuit breakers operate with a time delay on overload currents, but trip instantaneously on short circuit. The breakers may be of either the stationary or the draw-out type. The draw-out type has been used extensively because of the interchangeability of draw-out breakers, which is an important factor in avoiding production delays during routine inspection. The breakers are available in stacks of one, two, three, and four high, depending on the size of the breaker. Two or more stacks can be located side by side to give the required number of breakers. These equipments are used with radial types of systems. For network applications, the standard network protector replaces two of the air circuit breakers in one of the stacks.

Load-Center Unit-Substation Application Tables

The interrupting rating of a circuit breaker is the maximum current which it is able to interrupt adequately and safely. It is important to use circuit breakers of adequate interrupting rating on load-center unit-substation circuits, as well as on any other circuits in which circuit breakers are used, in order to assure reduced arcing time with minimum damage to equipment when faults do occur. Contrary to a former rather widespread belief that low-voltage circuits will not produce short-circuit currents in excess of 20,000 amperes, tests have shown that short-circuit currents of several times this value may occur.[2] These values can be predicted by calculation with reasonable accuracy. Tentative rules for the calculation of these currents have been established by the AIEE.[3]

The load-center unit-substation application tables given here are based on the occurrence of a three-phase secondary fault near the terminals of the unit substation. The only source of power to the secondary, except for secondary motors, is assumed to be through the load-center unit substation. It is assumed that the total connected motor kilovolt-amperes will not exceed 50 per cent of the transformer rating on 208/120-volt systems and 100 per cent of the transformer kilovolt-ampere rating on 240-, 480-, and 600-volt systems. See Tables II, III, IV, and V.

The calculations for the short-circuit currents are based on the following:

1. Systems with voltages of 208/120, 240, 480, and 600 volts, because these are ASA standard voltages given in tentative standards *C*-57.1, dated March 1940.

2. Transformer reactances have been selected to give the most economical balance between transformer reactance and circuit-breaker interrupting rating consistent with reasonable voltage regulation. The actual values of reactance are indicated in the tables.

3. Calculations are based on proposed AIEE standard rules, that is: the symmetrical three-phase rms short-circuit current is increased by a factor of 1.25 to allow for an average d-c component. For the motor contribution, 50 per cent connected motor kilovolt-amperes is assumed to be capable of supplying a short-circuit current of 2.5 times the normal full load current of the transformer, and 100 per cent connected motor kilovolt-amperes is assumed to be capable of supplying a short-circuit current of 5.0 times the normal current of the transformer.

Standard load-center unit-substation breaker arrangements are shown on Figures 12, 13, and 14. The basis of selection of the magnetic air circuit breaker used in the tables for these three arrangements are as follows:

Any air circuit breaker directly connected to the power source (*A'* or *A*) can be used up to 100 per cent of its interrupting rating only.

Any magnetic circuit breaker (*B*) which is backed up by a properly co-ordinated main breaker of adequate interrupting rating (*A'*) can be used up to 200 per cent of its interrupting rating.

Breakers arranged as in Figure 14 are said to be in "cascade."[4] The back-up breaker (*A'*) must be properly co-ordinated with the cascaded breaker (*B*), so that the breaker (*A'*) will trip instantaneously on short-circuit currents in excess of the interrupting rating of the breaker (*B*).

The cascade arrangement of air circuit breakers shown in Figure 14 often results in a lower-cost substation than is obtained with the arrangement shown in Figure 13. However, the cascade arrangement has the limitation that a heavy short circuit on any of the branch breakers may open the main air circuit breaker and result in an interruption of service to all of the branch feeders. Thus, when lower cost can be obtained with the cascade arrangement, careful consideration should always be given to the value of continuity of service.

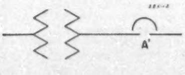


Figure 12. For main breaker A'

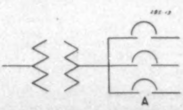


Figure 13. For branch breakers A

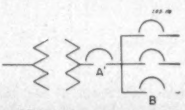


Figure 14. For cascaded branch breakers B

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High-Frequency Coaxial-Line Calculations

H. H. RACE
FELLOW AIEE

C. V. LARRICK
ASSOCIATE AIEE

Synopsis: Coaxial cables insulated with solid dielectric are used for transmitting electric energy over wide ranges of frequency. During the development of such cables it was important to be able to predict the characteristics of finished cable by calculations based upon the known dimensions and physical properties of the conductors and the insulation. Many comparatively simple formulas were known to be valid at high frequency, but their applicability at lower frequencies was questioned.

In the first part of this paper we have presented:

1. The general relations which hold under all conditions of operation.
2. The conditions under which "skin-effect" tables offer a convenient method for obtaining values of resistance and inductance, as well as the conditions under which relatively simple equations for these quantities are sufficiently accurate.
3. The conditions under which the complete expression for attenuation may be considerably simplified to the form generally used in high frequency practice.

In the second part of the paper we have presented formulas and graphs for the ratio of terminal voltages as a function of the phase angle of the line for several types of load for both dissipationless and dissipative lines. These relations are helpful in understanding observed voltage variations with frequency caused by reflections, particularly on short lengths of line.

In the third part of the paper a derivation is indicated leading to a general equation for the efficiency of transmission. Simplifications are made which are often applicable in practice, and curves are plotted showing the effect of mismatch in reducing efficiency.

A. Transmission-Line Relations

THE electrical characteristics of a concentric transmission line with distributed constants are given by the relations

$$\text{Propagation coefficient} = \gamma = \alpha + j\beta = \sqrt{ZY} \quad (1)$$

$$\text{Characteristic impedance} = Z_0 = \sqrt{Z/Y} \quad (2)$$

where

α = attenuation per centimeter length of line
β = phase shift per centimeter length of line
$Z = R + j\omega L = z\angle\theta_z$ = impedance per centimeter length
$Y = G + j\omega C = y\angle\theta_y$ = admittance per centimeter length
R = series resistance per centimeter length
L = series inductance per centimeter length
G = parallel conductance per centimeter length
C = parallel capacitance per centimeter length

Separating real and imaginary terms in equation 1 we have[1]

$$\alpha = \sqrt{zy}\cdot\cos {}^1/_2(\theta_z+\theta_y) = A\cos(\theta/2) \quad (3)$$

$$\beta = \sqrt{zy}\cdot\sin {}^1/_2(\theta_z+\theta_y) = A\sin(\theta/2) \quad (4)$$

or in terms of the distributed properties of the line

$$\alpha = \sqrt[4]{(RG-\omega^2LC)^2+(G\omega L+R\omega C)^2}\cdot \cos {}^1/_2\left[\tan^{-1}\left(\frac{G\omega L+R\omega C}{RG-\omega^2LC}\right)\right] \quad (5)$$

$$\beta = \sqrt[4]{(RG-\omega^2LC)^2+(G\omega L+R\omega C)^2}\cdot \sin {}^1/_2\left[\tan^{-1}\left(\frac{G\omega L+R\omega C}{RG-\omega^2LC}\right)\right] \quad (6)$$

Also since it is the magnitude of characteristic impedance which is of interest in cable design we have from equation 2

$$Z_0^2 = (z/y) = \sqrt{(RG+\omega^2LC)^2+(G\omega L-R\omega C)^2}/(G^2+\omega^2C^2) \quad (7)$$

Equations 1 through 7 are general for any frequency and for any transmission line to which a sinusoidal voltage is applied, so long as the proper coefficients R, L, G, and C are used. We shall therefore consider next the relations by which these four quantities can be calculated.

1. Admittance Per Centimeter Length

Our past experience[2] has shown the convenience of expressing the admittance of a capacitor of any shape as follows:

$$Y = G + j\omega C = \omega C_v(\epsilon'' + j\epsilon')$$

from which we have the following definitions of unit quantities characterizing the dielectric

$$\epsilon' = C/C_v = \text{dielectric constant} \quad (8)$$

$$\epsilon'' = G/\omega C_v = \text{loss factor} \quad (9)$$

where C_v = capacitance of the same size and shape of electrodes in vacuum. For coaxial cylinders of radii r_1 and r_2

$$C_v = 0.241\times10^{-12}/\log_{10}(r_2/r_1) \text{ farads/cm} \quad (10)$$

$$\therefore C = 0.241\times10^{-12}\epsilon'/\log_{10}(r_2/r_1) \text{ farads/cm} \quad (11)$$

and

$$G = 1.515\times10^{-12}f\epsilon''/\log_{10}(r_2/r_1) \text{ mhos/cm} \quad (12)$$

Both coefficients ϵ' and ϵ'' may vary with frequency and must be known or assumed for the insulating material for the frequency range being considered.

2. Impedance Per Centimeter Length

The resistance and the inductance of both the inner and outer conductors will vary with frequency because of "skin effect." Therefore there are no simple general relations which can be used at all frequencies.

If the inner conductor is a round wire, the skin effect at low frequencies can be obtained from Table I, which has been worked out from complicated exact formulas.[3-5] This table gives the ratios R/R_0 and L/L_0 (where R_0 and L_0 represent the resistance and inductance at zero frequency) as functions of the parameter

$$x = 2\pi r_1\sqrt{2\mu f/\rho} \quad (13)$$

For copper $\mu = 1$, $\rho = 1{,}724$ abohmcentimeters and

$$x = 0.214 r_1\sqrt{f} \quad (14)$$

The depth of penetration y is defined as the thickness of shell to give the same d-c resistance and is given by the relation

$$y = \sqrt{\rho}/2\pi\sqrt{\mu f} \quad (15)$$

or

$$y = 6.62/\sqrt{f} \text{ centimeter (for copper)} \quad (16)$$

If the outer conductor is a hollow cylinder of thickness t (where $r_2 > 5t$), the corresponding values for the ratio R/R_0 were worked out by J. R. Whinnery and are given in Figure 1 as a function of t/y where y is the depth of penetration[6] (equation 16).

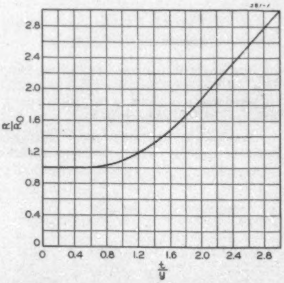

Figure 1. Ratio of a-c to d-c resistance R/R_0 as a function of the ratio of thickness to depth of penetration t/y for a cylindrical outer conductor in a coaxial line

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H. H. Race is research engineer in the research laboratory, and C. V. Larrick is in the general engineering laboratory, both with General Electric Company, Schenectady, N. Y.

The resistances of the inner and outer conductors are added together to give the total resistance per unit length

$$R_t=0.548\times10^{-6}\left[\underset{\text{inner}}{\left(\frac{R/R_0}{r_1^2}\right)}+\underset{\text{outer}}{\left(\frac{R/R_0}{2tr_2}\right)}\right]$$

$$\text{ohm per centimeter length} \quad (17)$$

where (R/R_0) for the inner conductor is taken from Table I and (R/R_0) for the outer conductor is taken from Figure 1.

If the parameter x is greater than 20, a condition which is normally satisfied at high frequency, then the depth of penetration is so small that it is not necessary to use Table I. Likewise if t/y for the outer conductor is greater than 3, then the inner and outer resistances vary inversely as the radii of the conductors as follows:[3]

$$R=\sqrt{\rho\mu f}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)\times10^{-9} \text{ ohm per centimeter length} \quad (18)$$

For copper conductors this reduces to

$$R=4.15\times10^{-8}\sqrt{f}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)$$

$$\text{ohm per centimeter length} \quad (19)$$

If the inner conductor is stranded and the outer conductor braided, the above calculations will be optimistic, since they do not take into account contact resistance between strands, which may be very important, especially if the copper is dirty or becomes corroded or oxidized. After considerably more empirical evidence is obtained, perhaps multiplying factors can be assigned to indicate a proportionate increase of resistance above the values given by equation 17 caused by stranded inner conductors and braided or wrapped outer conductors. For example, we have found by experience that in coaxial cables built with 0.2-centimeter-diameter inner conductors the attenuation is about one decibel per 100 feet greater at 315 megacycles with a stranded inner conductor than it is with a solid inner conductor.

The expression for inductance[3] at zero frequency is

$$L_0=\left\{4.6\log_{10}(r_2/r_1)+0.5+\left[\frac{4.6r_3^4}{(r_3^2-r_2^2)^2}\log_{10}(r_3/r_2)-\frac{3r_3^2-r_2^2}{2(r_3^2-r_2^2)}\right]\right\}\times$$

$$10^{-9} \text{ henry per centimeter} \quad (20)$$

where r_3 = outer radius of outer conductor in centimeters. The first term gives the inductance between the conductors; the second and third terms give the inductance within the inner and outer conductors respectively.

To show the relative magnitudes of the several terms in equation 20, assume the physical dimensions for cable 1 for which the complete calculations are shown in Table II (next section). Substituting in equation 20 we obtain:

$$L_0=[2.62+0.5+(8.9092-8.8752)]\times$$

$$10^{-9} \text{ henry per centimeter}$$

Both terms of the inductance within the outer conductor were obtained using five-place logarithms, and the numbers have been recorded in order to indicate that the calculation involves a small difference between two nearly equal quantities. However, this also demonstrates that the inductance within the outer conductor may be neglected in comparison with the other terms of equation 20. Since the inductance within the conductors decreases as frequency increases, we have

$$L=[4.6\log_{10}(r_2/r_1)+0.5(L/L_0)]\times$$

$$10^{-9} \text{ henry per centimeter} \quad (21)$$

where L/L_0 for the inner conductor is obtained from Table I. At high frequencies the inductance within the inner conductor also becomes negligible so that we may use the simple relation

$$L=4.6\log_{10}(r_2/r_1)\times10^{-9} \text{ henry per centimeter} \quad (22)$$

3. Approximations Leading to Simplified Formulas

In order to show the conditions under which the general equations 1–7 can be reduced to the commonly used simplified forms, we have shown in Table II the important terms involved for two cables at two frequencies.

Table II shows that at 315 megacycles items 10, 12, and 13 are negligible compared to item 11, that is:

$$\omega^2LC\gg RG \quad (23)$$

and

$$(\omega^2LC)^2\gg(G\omega L+R\omega C)^2 \quad (24)$$

If equation 5 is simplified assuming the above inequalities

$$\alpha\cong\sqrt{\omega^2LC}\cdot\cos\frac{\theta}{2}\cong\sqrt{\omega^2LC}\sin\left(\frac{\delta}{2}\right)\cong$$

$$\sqrt{\omega^2LC}\left(\frac{\delta}{2}\right) \quad (25)$$

where

$$\theta=180^\circ-\delta \quad (26)$$

and

$$\tan\delta\cong\left(\frac{G\omega L+R\omega C}{\omega^2LC}\right)\cong\sin\delta\cong\delta \quad (27)$$

or

$$\alpha\cong(G\omega L+R\omega C)/2\sqrt{\omega^2LC} \quad (28)$$

or

$$\alpha\cong\alpha_d+\alpha_c\cong{}^1/_2G\sqrt{L/C}+{}^1/_2R\sqrt{C/L} \quad (29)$$

where α_d = contribution to attenuation by dielectric loss only

and α_c = contribution to attenuation by copper loss only

α_d may also be obtained directly from equation 5 by assuming $R=0$, and α_c may be obtained by assuming $G=0$ in this same equation. Therefore, we have shown that under the conditions for which equations 23 and 24 are true, the contributions to attenuation of the copper losses and the dielectric losses may be calculated separately and considered to be additive.

Substituting in equation 25 the rela-

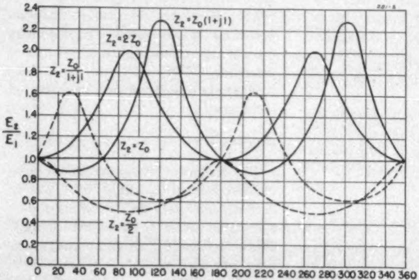
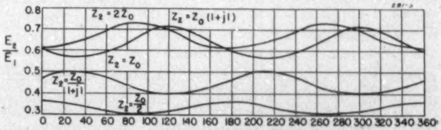


Figure 2. Ratio of receiving to sending voltage E_2/E_1 as a function of line angle βl for several values of terminating impedance Z_2 in a dissipationless line

Figure 3. Ratio of receiving to sending voltage E_2/E_1 as a function of line angle βl for several values of terminating impedance Z_2 for a line having a total attenuation of six decibels



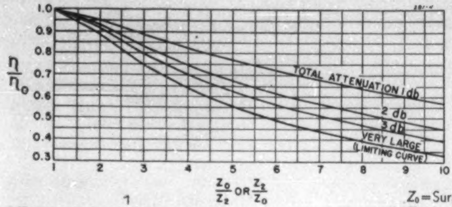


Figure 4. Efficiency of transmission for several values of total attenuation for a range of values of mismatch

$\alpha l = 0.115 \times$ total attenuation of line in decibels

$$\eta = \frac{1}{\cosh 2\alpha l + \frac{1}{2}\left(\frac{Z_0}{Z_2} + \frac{Z_2}{Z_0}\right)\sinh 2\alpha l}$$

Z_0 = Surge impedance (resistive)
Z_2 = Load impedance (resistive)
$\eta_0 = e^{-2\alpha l}$ when $Z_0 = Z_2$

Table I. Skin-Effect Resistance and Inductance Ratios for Solid Round Wires

x	R/R₀	L/L₀	x	R/R₀	L/L₀
0.0	1.00000	1.00000	2.5	1.17538	0.91347
0.1	1.00000	1.00000	2.6	1.20056	0.90126
0.2	1.00001	1.00000	2.7	1.22753	0.88825
0.3	1.00004	0.99998	2.8	1.25820	0.87451
0.4	1.00013	0.99993	2.9	1.28644	0.86012
0.5	1.00032	0.99984	3.0	1.31809	0.84517
0.6	1.00067	0.99966	3.5	1.49202	0.76550
0.7	1.00124	0.99937	4.0	1.67787	0.66632
0.8	1.00212	0.99894	4.5	1.36275	0.61563
0.9	1.00340	0.99830	5.0	2.04372	0.55597
1.0	1.00519	0.99741	6.0	2.39359	0.46521
1.1	1.00758	0.99821	7.0	2.74319	0.40021
1.2	1.01071	0.99465	8.0	3.09445	0.55107
1.3	1.01470	0.99266	9.0	3.44638	0.31257
1.4	1.01988	0.99017	10.0	3.79857	0.28162
1.5	1.02582	0.98711	11.0	4.15100	0.25622
1.6	1.03323	0.98342	12.0	4.50358	0.23501
1.7	1.04203	0.97904	13.0	4.85831	0.21703
1.8	1.05240	0.97390	14.0	5.20915	0.20160
1.9	1.06440	0.96795	15.0	5.56205	0.18822
2.0	1.07818	0.96113	20.0	7.32767	0.14128
2.1	1.09375	0.95343	25.0	9.09412	0.11307
2.2	1.11126	0.94482	30.0	10.88101	0.09424
2.3	1.13069	0.93527	40.0	14.39545	0.07069
2.4	1.15207	0.92482	50.0	17.93032	0.05656
			60.0	21.46541	0.04713
			80.0	28.53593	0.03535
			100.0	35.60666	0.02828
					0.00000

$$\lim_{x \to \infty}\left(\frac{R}{R_0}\right) = 0.354x$$

Table II. Important Items in Equation 5

	Cable 1		Cable 2	
Cable specifications				
r_1	0.1028 cm		0.325 cm	
r_2	0.381 cm		1.27 cm	
t	0.025 cm		0.025 cm	
ϵ'	2.04		2.65	
ϵ''	0.001		0.0089	
Frequency (cycles per second)	2.5×10^{4}	3.15×10^{8}	2.5×10^{4}	3.15×10^{8}
1. C (eq 11)	1.017×10^{-12}	1.017×10^{-12}	1.08×10^{-12}	1.08×10^{-12}
2. G (eq 12)	6.65×10^{-11}	8.39×10^{-7}	1.85×10^{-10}	2.34×10^{-6}
3. x (eq 14)	3.48	(590)	11.0	(1,230)
4. R/R_0	1.485	(209)	4.15	(435)
5. R (eq 17)	1.06×10^{-4}		0.303×10^{-4}	
6. R (eq 19)	(0.81×10^{-4})	91.0×10^{-4}	(0.254×10^{-4})	28.5×10^{-4}
7. L/L_0	0.769		0.256	
8. L (eq 20)	3.00×10^{-9}		2.85×10^{-9}	
9. L (eq 22)	(2.62×10^{-9})	2.62×10^{-9}	(2.72×10^{-9})	2.72×10^{-9}
10. RG	7.05×10^{-15}	7.63×10^{-9}	5.60×10^{-15}	6.67×10^{-9}
11. ω^2LC	7.52×10^{-11}	1.045×10^{-2}	7.58×10^{-11}	1.15×10^{-2}
12. $G\omega L$	3.13×10^{-14}	4.35×10^{-6}	8.27×10^{-14}	1.26×10^{-5}
13. $R\omega C$	1.69×10^{-11}	1.83×10^{-6}	5.14×10^{-12}	6.09×10^{-6}
14. A (eqs 3 and 5)	8.8×10^{-6}		8.7×10^{-6}	
15. ℓ (eqs 3 and 5)	167.3		176.1	
16. $\cos(\theta/2)$	0.11		0.034	
17. α (eq 5)	9.69×10^{-7}		2.96×10^{-7}	
18. α_c (eq 30)	(8.0×10^{-7})	9.0×10^{-5}	(2.52×10^{-7})	2.83×10^{-5}
19. α_d (eq 32)	(1.7×10^{-9})	2.14×10^{-5}	(4.67×10^{-9})	5.9×10^{-5}
20. α (eq 29)	(8.02×10^{-7})	11.1×10^{-5}	(2.57×10^{-7})	8.7×10^{-5}
21. N_{db}/100 ft	2.56×10^{-2}	2.94	7.35×10^{-3}	2.31
Error using approximate equation	−17%		−13%	

tions for R, L, C, and G given in equations 19, 22, 11, and 12 respectively, we have

$$\alpha_c \cong 1.50\times10^{-10}\sqrt{f\epsilon'}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)/\log_{10}\times(r_2/r_1) \text{ neper per centimeter} \quad (30)$$

or

$$N_c \cong 3.98\times10^{-6}\sqrt{f\epsilon'}\left(\frac{1}{r_1}+\frac{1}{r_2}\right)/\log_{10}\times(r_2/r_1) \text{ decibel per 100 feet} \quad (31)$$

and

$$\alpha_d \cong 1.05\times10^{-10} f\epsilon''/\sqrt{\epsilon'} \text{ neper per centimeter} \quad (32)$$

or

$$N_d \cong 2.78\times10^{-6} f\epsilon''/\sqrt{\epsilon'} \text{ decibel per 100 feet} \quad (33)$$

where

$$N_{\text{total}} \cong N_c(\text{copper loss only}) + N_d \text{ (dielectric loss only)} \quad (34)$$

If equation 7 is simplified assuming the inequalities, equations 23 and 24, we have

$$Z_0 = \sqrt{L/C} = 138 \log_{10}(r_2/r_1)/\sqrt{\epsilon'} \quad (35)$$

which is the expression usually used for characteristic impedance at high frequency.

Likewise assuming the inequalities, equations 23 and 24, equation 6 reduces to

$$\beta \cong \sqrt{\omega^2 LC} \cong \omega C Z_0 \quad (36)$$

The trigonometric term in equation 5 is important, because it is zero if the losses are zero. Thus an approximate evaluation at high frequencies for this term (although it is small) leads to the useful relation given in equation 25.

On the other hand, the trigonometric term in equation 6 is unimportant, since it is unity for zero losses, and losses at high frequencies simply decrease this term slightly below unity. The recognized effect of high losses in increasing the phase constant must appear, because some of the factors in the first term of equation 6 are not negligible compared to $\omega^2 LC$. However, when the inequalities, equations 23 and 24, are true, the losses do not appreciably increase the phase constant.

Similarly equation 29 can be expressed in terms of Z_0

$$\alpha \cong (GZ_0/2) + (R/2Z_0) \quad (37)$$

The frequency above which the simplified relations, equations 25–37, can be used depends upon the error which can be tolerated. Table II shows that appreciable errors result if the approximate relations are used at 25 kilocycles. However these relations should be sufficiently accurate for frequencies in the broadcast band or above.

B. Voltage Relations

In a transmission line of length l, having uniformly distributed constants, the complex expression for the ratio of the voltage at the sending end E_1 to the voltage at the receiving end E_2 is given by the relation

$$\frac{E_1}{E_2}=\cosh \gamma l+\frac{Z_0}{Z_2}\sinh \gamma l \qquad (38)$$

which upon expansion becomes

$$\frac{E_1}{E_2}=\left(\cosh \alpha l+\frac{Z_0}{Z}\sinh \alpha l\right)\cos \beta l+ j\left(\sinh \alpha l+\frac{Z_0}{Z_2}\cosh \alpha l\right)\sin \beta l \qquad (39)$$

where Z_0 = surge impedance of line
and Z_2 = impedance across receiving end of line

From this general relation several special cases lead to usable simplifications.

1. DISSIPATIONLESS CASE

$$\left(\begin{matrix}G=R=\sinh \alpha l=0\\ \cosh \alpha l=1,\ Z_0=\text{real}\end{matrix}\right)$$

(*a*). Assuming pure resistance termination, (Z_2=real), the absolute value of the voltage ratio given by equation 39 can be reduced to

$$\left|\frac{E_1}{E_2}\right|=\sqrt{\frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right]+\frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right]\cos 2\beta l} \qquad (40)$$

(*b*). Assuming complex termination ($Z_2=\sqrt{R_2^2+x_2^2}$) equation 39 becomes

$$\left|\frac{E_1}{E_2}\right|=\sqrt{\frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right]+\frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right]\cos 2\beta l+\frac{X_2Z_0}{Z_2^2}\sin 2\beta l} \qquad (41)$$

The last two equations have been used to calculate the data given in Figure 2 which show the ratio of receiving to sending voltage E_2/E_1 for the dissipationless line for several types of load impedance.

2. DISSIPATIVE LINE

For a line having high losses, particularly at low frequency, the surge impedance (Z_0) may have an appreciable reactive component. But in lines for which equations 23 and 24 apply, the reactive component of Z_0 is negligible as indicated by equation 35.

(*a*). Assuming pure resistance termination (Z_2=real) equation 39 reduces to

$$\left|\frac{E_1}{E_2}\right|=\sqrt{\frac{Z_0}{Z_2}\sinh 2\alpha l+\frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right]\cosh 2\alpha l+\frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right]\cos 2\beta l} \qquad (42)$$

(*b*). Assuming complex termination, ($Z_2=\sqrt{R_2^2+x_2^2}$) equation 39 becomes

$$\left|\frac{E_1}{E_2}\right|=\sqrt{\frac{RZ_0}{Z_2^2}\sinh 2\alpha l+\frac{1}{2}\left[1+\left(\frac{Z_0}{Z_2}\right)^2\right]\cosh 2\alpha l+\frac{1}{2}\left[1-\left(\frac{Z_0}{Z_2}\right)^2\right]\cos 2\beta l+\frac{X_2Z_0}{Z_2^2}\sin 2\beta l} \qquad (43)$$

Figure 3 shows plots of the last two relations for a total attenuation of six decibels for the same terminating impedances as were used in Figure 2.

3. HIGH-LOSS LINES

As the total attenuation is increased, the variation in the ratio of E_2/E_1 with line angle decreases. For a line having an attenuation of 20 decibels, $\alpha l=2.3$, and there is only about two per cent error in the relation

$$\cosh \alpha l=\sinh \alpha l={}^1/_2\epsilon^{\alpha l} \qquad (44)$$

Under these conditions equation 39 can be further simplified.

(*a*). Assuming pure resistance termination (Z_2 = real)

$$\left|\frac{E_1}{E_2}\right|=\frac{\epsilon^{\alpha l}}{2}\left(1+\frac{Z_0}{Z_2}\right) \qquad (45)$$

(*b*). Assuming complex termination ($Z_2=\sqrt{R_2^2+x_2^2}$)

$$\left|\frac{E_1}{E_2}\right|=\frac{\epsilon^{\alpha l}\sqrt{Z_2^2+2R_2Z_0+Z_0^2}}{2Z_2} \qquad (46)$$

The simplest relation for the ratio E_1/E_2 is obtained when the line is terminated in a load which is exactly equal to its surge impedance ($Z_2=Z_0$) in which case

$$\left|\frac{E_1}{E_2}\right|=\epsilon^{\alpha l} \qquad (47)$$

Equations 38–47 were derived in order to interpret voltage data taken during measurements of attenuation in commercial high-frequency solid dielectric coaxial cables.[7]

C. Efficiency of Transmission

The efficiency of transmission is defined as the ratio of output to input power; however, the mathematical expression is simpler if the reciprocal relations are used. The most general case is that for which both the characteristic and load impedances of the line are complex. This case has been solved by E. W. Hamlin (unpublished report, General Electric Company). The essential steps and final relation are:

$$\text{Characteristic impedance}=Z_0=R_0+jX_0 \qquad (48)$$

$$\text{Load impedance}=Z_2=R_2+jX_2 \qquad (49)$$

then the input impedance is given by[8]

$$Z_1=Z_0\left[\frac{Z_2\cosh \gamma l+Z_0\sinh \gamma l}{Z_0\cosh \gamma l+Z_2\sinh \gamma l}\right] \qquad (50)$$

and the input current as a function of the load current is given by

$$I_1=\frac{I_2}{Z_0}\left(Z_0\cosh \gamma l+Z_2\sinh \gamma l\right) \qquad (51)$$

$$\text{The input power}=|I_1|^2\times\text{real component of } Z_1 \qquad (52)$$

$$\text{and the output power}=|I_2|^2R_2 \qquad (53)$$

Hamlin takes the ratio of these last two relations and upon expansion and simplification obtains the general equation:

$$\frac{W_1}{W_2}=\frac{R_0}{R_2|Z_0|^2}\left[(R_2R_0+X_2X_0)\cosh 2\alpha l+{}^1/_2(|Z_2|^2+|Z_0|^2)\sinh 2\alpha l\right]+\frac{X_0}{R_2|Z_0|^2}\left[(X_0R_2-X_2R_0)\cos 2\beta l+{}^1/_2(|Z_2|^2-|Z_0|^2)\sin 2\beta l\right] \qquad (54)$$

As a simplification of equation 54, let $X_0=0$, that is, assume that the reactive component of the characteristic impedance is negligible, as is usually the case in high-frequency transmission. Then equation 54 reduces to

$$\frac{W_1}{W_2}=\cosh 2\alpha l+{}^1/_2\left(\frac{R_2}{R_0}+\frac{R_0}{R_2}+\frac{X_2^2}{R_2R_0}\right)\sinh 2\alpha l \qquad (55)$$

When the reactive component of the load impedance is also negligible, the last term in the parenthesis drops out.

The last equation has been used to calculate the efficiency of transmission for several values of total attenuation for a range of values of mismatch. (Z_0/Z_2 or Z_2/Z_0 from 1 to 10) as shown in Figure 4. An important observation is that for a mismatch as large as 2/1, the efficiency is 90 per cent or more of the efficiency which could be obtained with perfect matching of characteristic and load impedances.

The curves of Figure 4 express only the increase in power losses in the line caused by standing waves on the line resulting from reflections at the load end. Unless the attenuation of the line is high, the

Calorimetric Method for Determining Efficiencies of Electric Machines

VICTOR SIEGFRIED
MEMBER AIEE

CHARLES W. THULIN
ASSOCIATE AIEE

AMONG the methods available for determining losses and efficiencies of electric machines, many have been developed to their utmost capabilities. Some measure individual losses separately and with good accuracy,[1–10] but many have inherent drawbacks. There still remains the desirability of a satisfactory over-all test which will rate the performance of all sizes of machinery under actual conditions of loading and yet yield highly accurate results. This aim is largely met by the calorimetric method and is justified by its basic character. A simplification of the classical testing technique can be introduced in which the important measurements are all electrical. By this means, accurate measurements of heat values are obviated, and the calorimeter becomes in reality only a comparator for a substitution process. Test results of calorimetric measurements on small induction and d-c motors show excellent agreement with other tests on the same machines. Analysis of the method indicates that it is equally applicable to all classes of machines of any size and that it can be used more generally for over-all loss and efficiency measurements under loaded conditions.

Calorimetric Method

The classical form of the calorimetric method makes use of the basic principle that all losses are converted into heat. Measurements of thermal values from specific heat of the coolant, its volume, and temperature rise, have involved uncertainties and difficulties which have tended to discredit the method. However, these can be avoided by a simplification possible with electric machinery, in which the volume of the cooling medium is kept constant, and the temperature rise is measured under load and again with calibrating power input, thus giving a direct indication of the total losses within the system. The method is applicable to many types of machines, motor or generator, d-c or a-c, with only slight revisions in detail for each. Since total losses may be determined by it, and all other losses can be measured separately, stray load loss can be determined by this type of test. The American Standards Association Standards[8] and the test codes recognize the use of the calorimetric method, although reports of its use are rather scarce in the American literature. European tests reported to the International Electrotechnical Commission meetings at Prague in 1934 and Scheveningen in 1935 by the late Edouard Roth[2,11] show the development of simplifications in the calorimetric method. The procedure was to enclose the machine under test in a trunking or duct, through which a constant quantity of air was blown. Temperature rises from inlet to outlet were determined by the resistance of grids in the air stream, and losses to produce equal rises were introduced by means of heaters. Elaborate interpolation of data was required, because of difficulty in adjusting to exact equality.

Further extension of these simplifications is made in the tests being reported in this paper. Where possible, the calibrating losses are introduced directly into the machine under test so as to simulate actual losses exactly. A succession of points is taken so that a graphical determination of equal temperature-rise conditions may be made between the loaded and calibrating runs, thus minimizing the work of calibration. The use of differential thermocouples makes possible direct readings on a microammeter without the necessity of delicate potentiometer readings.

Equipment and Procedure

The test work was done on two machines, one an induction motor and the other a d-c motor of comparable size. The equipment consists principally of:

A double-walled calorimetric box of heat-insulating material to enclose the machine under test, in this case reversible so as to be usable on either end of the set.

A constant-speed blower to force air through the box.

Thermocouples differentially connected between inlet and outlet ports.

Power-measuring instruments in the leads to the test box.

Heaters inside the box near the test machine.

Figures 1 and 2 show the arrangement of parts.

The pair of machines is mounted on a wooden base with an iron subbase, and bolts are staggered so as to prevent any direct path of high thermal conductivity to the outside. A layer of insulating board is fitted around the machine feet, and the box is sealed to it. A slot on one side makes provision for a close-fitting air seal around the shaft, the coupling being outside of the enclosure. Dead air spaces

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VICTOR SIEGFRIED is assistant professor of electrical engineering, Worcester Polytechnic Institute, Worcester, Mass., and CHARLES W. THULIN, now with Bell Telephone Laboratories, Inc., New York, N. Y., was instructor in electrical engineering at Worcester Polytechnic Institute when this paper was written.

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mismatch at the load end will affect the input impedance of the line, and in any practical case the coupling between the line and the generator will have to be adjusted to give the best power transfer.

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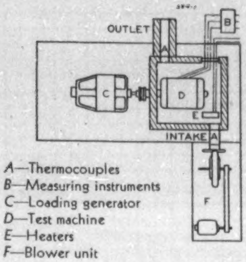

A—Thermocouples
B—Measuring instruments
C—Loading generator
D—Test machine
E—Heaters
F—Blower unit

Figure 1. Arrangement of calorimeter and machines

are filled with rock wool. A terminal plate provides electrical connections to the inside. Thermocouples are connected differentially in series between the blower pipe at the inlet and an insulated outlet at the diagonally opposite corner. Small ports are used to insure thorough mixing of the air, thus keeping all parts at a uniform temperature. This was verified by test and by observing the air stream through a window at the top of the box when smoke was injected.

As indicated in the introductory discussion, this method uses the calorimetric quantities for a comparison of the power values which will produce the same temperature conditions:

1. The machine is operated under load, and electric power input and thermocouple measurements are made. All losses of the machine, whether in electrical or mechanical form, are dissipated within this box and eventually contribute to the heating of the air. No measurement of shaft output is made unless desired as a check.
2. The coupling between machines is broken, and a set of measurements is made with the machine running idle to find the power input that is required to duplicate previous thermal conditions.

Finding the loss input which gives exactly the previous (loaded) temperature difference is unnecessarily tedious. For satisfactory results and minimum time used on a test, the better procedure is to

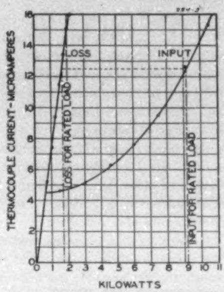

Figure 3. Temperature differences under load and calibrating conditions

Value of loss for corresponding input indicated by equal temperature differences

take two curves of temperature difference (microamperes) for several values of heating, the air volume being held constant. One curve is taken as a function of total input under loaded conditions, and another is taken as a function of total losses. Such a set of curves is shown in Figure 3, with both curves plotted to the same scale for illustrative purposes. The total losses for any input are then determined from the curve of calibrating loss at the corresponding temperature difference. The machine output is found by subtraction and efficiency is computed in the usual manner.

As may be expected, time must be allowed for the thermocouple currents to become stabilized. Figure 4 shows typical curves for one run (air rate different from Figure 3). In the case of this thermal system, about one hour was required per reading, although this time could be shortened by changing the air rate during the transition period, gradually returning to the standard value. The air rate must be selected so that a total difference between inlet and outlet temperatures does not exceed about 20 degrees centigrade to approximate nominal ambient conditions. Since actual thermal values are not required, blower speed need be kept constant only during any one set of measurements. Air density and humidity should remain constant within the same period or not change appreciably. Inlet air temperature may vary slightly without error, but care should be taken to keep loading and control rheostats reasonably distant from the inlet. Because the method requires only repeatable conditions, even slight air leaks are not serious so long as they stay constant. Speed of the machine under test may vary slightly without serious error, due to changing the ventilation pattern within the calorimeter.

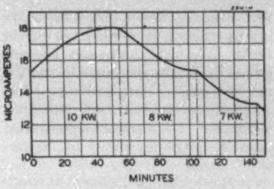

Figure 4. Typical temperature-difference curves for three values of total input to box with machine loaded

Readings taken at end of intervals

Up to this point, the procedure is perfectly general for any type of apparatus. The loaded temperature determination is likewise general, but consideration must now be given to methods of introducing the no-load or calibrating power into the calorimeter. The test machine is disconnected from the load, and it is allowed to run at its nominal speed. Additional heat may be introduced by heaters so placed that the internal ventilation will circulate the heated air. The calibrating power is thus the sum of the no-load input to the machine and the power to the heaters. In the case of induction machines, the entire power may be introduced into the machine by raising the applied voltage so that increased running-light losses will duplicate those at full load. This has the advantage of placing all the loss within the machine, although current

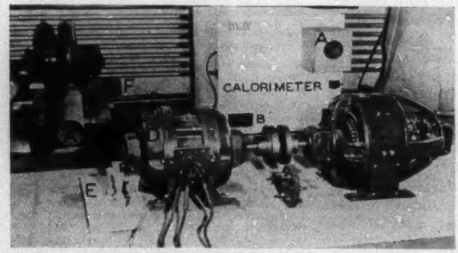

Figure 2. Calorimeter and machines

A—Thermocouples
B—Terminals for input and instruments
C—Loading generator
D—Test machine
E—Heaters
F—Blower unit

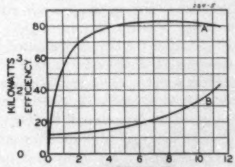

Figure 5. Total losses and efficiency at different loads derived from Figure 3

A—Per cent efficiency *B*—Total losses limitations may require partial use of the heaters. Synchronous machines permit similar abnormal adjustments by field control of reactive currents, but it must not be overlooked that field copper loss contributes to the total heat within the calorimeter. D-c machines are the least amenable to this treatment, and heaters have to suffice.

Test on Induction Motor

An induction motor which had been thoroughly calibrated by many tests was available for test by the calorimetric method. This was fortunate, because the induction motor illustrates the good features of this test to the utmost, and illustrates the *method* well. In the calibration of the box, all of the necessary no-load losses can be confined to the machine itself by raising the applied voltage and exciting current until copper and rotational losses are of the order of total loaded losses, provided the stator current is not dangerously large. This permits the nearest approach to the ideal of having all losses identical in the two tests. The squirrel-cage machine used is rated 10 horsepower, 220/440 volts, 26.6/13.3 amperes, 60 cycles, 1,710 rpm squirrel-cage motor, and was operated at 220 volts.

Complete data were obtained from which separation of losses could be made, the calorimetric tests providing only the total loss value. Slip, stator resistance, and rotation losses were measured in addition to line current, voltage, and power usually measured in the efficiency test. Thermocouple current is the indication of temperature difference and is plotted in Figure 3 for the steady-state deflections given by a certain total input in the load run, and by a certain total loss input in the unloaded run. Since corresponding deflection currents indicate corresponding power values, the total loss for a given input is found by following across the proper ordinate to pick off the loss abscissa. The dotted lines indicate test data for rated load on the machine. It may be seen that deflection is proportional to loss in the box for constant blower speed. This is to be expected so long as the heat is dissipated in the same manner at all loads. As the box gets hotter however, other factors appear to permit heat to escape by conduction, with a resultant flattening of the curve. These make an actual calibration for each test very essential. An additional test using heaters for no-load input was made with identical results.

The final curves of total losses and efficiency derived from the data of Figure 3 are shown in Figure 5, with output as the abscissa. The full-load efficiency of this machine is 81.5 per cent as compared with 81.7 per cent from other test methods using conventional losses plus stray load loss measured separately.

As a demonstration of the accuracy possible with the calorimetric method, a set of values of stray load loss found by this test is plotted in Figure 6. The experimental points are obtained by two groups of observers. The solid curve is the stray load loss as determined by other methods,[4,5] several curves not widely different being averaged together. These results are quite consistent, especially when it is appreciated that stray load loss here is found as an indirect measurement by subtracting all other known losses from the total determined by the calorimetric method.

Test on D-C Machine

The calorimetric set used was reversible; so the box was turned around on the base, and measurements were made with the d-c machine as the test machine. The induction motor acting as a generator was loaded by operating it from a separate alternator. In this test, the calibrating losses were supplied by heaters in addition to the running-light input. The full-load efficiency by this method was 83.1 per cent, whereas the efficiency by conventional methods plus stray load loss[10] was 83.4 per cent.

Conclusions

The calorimetric method described in this paper consists of two fundamental steps only:

1. Determination of a temperature rise of the cooling medium under loaded conditions.
2. Substitution of an equivalent measurable loss which will produce the same temperature rise under no-load conditions.

This is a comparison or substitution method which determines with high accuracy the total losses of a machine under conditions of actual loading. The method permits good comparative data to be taken even under poor calorimetric conditions, although refinements improve the ease of obtaining good results.

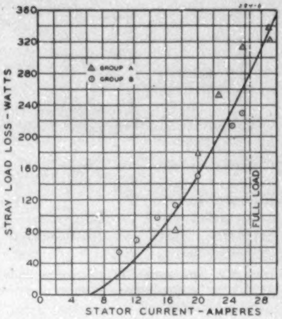

Figure 6. Stray load loss of induction machine tested

Solid curve average of tests by other methods.[5] Experimental points determined by calorimetric method by two groups of observers

Losses measured by it compare directly and favorably with those of other methods of high accuracy and show a final stray load-loss value very close to those from other tests. Although tests of only two machines are reported, the method has been demonstrated to be workable and fundamentally sound. Since it depends on a basic principle, the accuracy which may be expected will be determined by the care and techniques employed. Further attention to the calorimetric method will doubtless bring forth other advances in its use.

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High-Voltage Fusing of Transformer Banks

H. H. MARSH, JR.
MEMBER AIEE

G. B. DODDS
ASSOCIATE AIEE

Synopsis: The paper summarizes some 15 years' experience with the high-side fusing of 11-, 22-, and some 66-kv transformer banks. The original purpose of fusing has through this experience been expanded to cover functions that are deemed desirable, and the authors feel that their approach to the specific protection field served by fuses has resulted in satisfactory protection at minimum cost.

The results of some faults occurring in fused and unfused banks are described briefly, and the various factors taken into consideration in selecting the proper fuse to use are discussed. The conclusions drawn are that fuses, when applicable, are a satisfactory economical means of obtaining reasonable coverage for faults in and beyond transformer banks.

THE experience of the Duquesne Light Company with what we shall call modern high-voltage power fuses began in 1926 when Schweitzer and Conrad liquid fuses were applied to the high-voltage side of several 22-kv banks in stations serving customers from a radial transmission line. The previous outages to this particular line were the determining factor in these fuse applications as an effort to improve service to the remainder of the line in cases of station trouble.

Early Experience

For the first few years our experience with these fuse installations was anything but satisfactory. These fuses deteriorated, causing uncalled for operations, and many justified blowings resulted in failures to clear, flashover of inadequate spacings, and flashover of inadequate insulators. These fuse operations were carefully followed, and such conditions of installation as contributed to unsatisfactory performance were eliminated. Mountings were replaced with higher insulation, spacing and clearances generally increased, and 34.5-kv fuses standardized upon for our 22-kv service. These changes, together with the manufacturers' changes of liquid, cap design, fuse element changes, and other advances, had made it possible to place considerable confidence in high-voltage fuses such that by 1937 our experience justified an extension of our fusing applications and the development of a general policy of fusing where applicable.

Experience up to 1937—Fused Versus Unfused Stations

By 1937 our number of fused banks had increased in proportion to our growing confidence in high-voltage fuses, and at that time we had 32 power applications in both customer and company distribution stations. An analysis of performance of fused and unfused stations for the years 1930 to 1937, as disclosed by fault records, brings out a striking comparison, and the records are tabulated in Table I for parallel consideration.

In the fused group we had had no fires, no explosions, no serious damage and, in many cases, immediate return to service of the faulted equipment, since prompt clearing had prevented permanent faults. Contrasted with this in the unfused group we had had three fires, one explosion, and had lost nineteen transformers such that they were either scrapped or completely rewound.

Origin of High-Voltage Fusing Policy

The original purpose of the high-side fusing of transformer banks was to minimize outages to multicustomer circuits where one customer fault would otherwise involve a total line outage.

An analysis of the above performance record disclosed that the fuses, in performing this original function, had provided additional benefits by preventing fires, explosions, or serious damage.

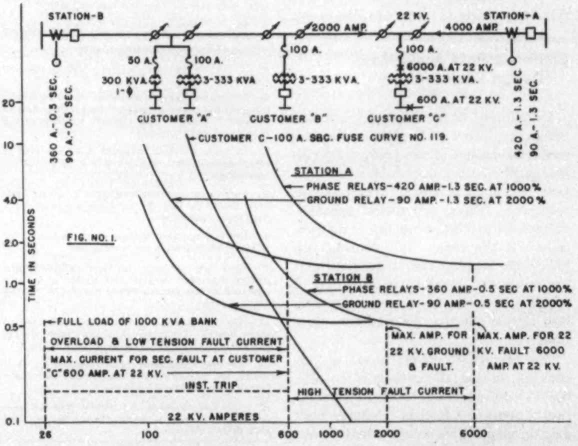
Based on this analysis, a policy has been established to provide adequate protection for all new bank installations and, over a period of years, to cover existing stations using fuses wherever applicable.

Experience 1937 to the Present

Our fuse installations now number well over 126, cover banks ranging in size from 50 kva at 22 kv, up to 10,000 kva at 66 kv, and involve the use of Schweitzer and Conrad types *D*, *C*, and *SM*, and Westinghouse type *BA*.

From 1937 to the present we have had 59 faults involving 87 fuse operations all functionally correct, and several of these,

Figure 1. Selection curves of transmission-line relays, transformer-bank fuses, and low-voltage protection



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H. H. MARSH, JR. is assistant general superintendent, substations and shops, and G. B. DODDS is relay protection engineer, Duquesne Light Company, Pittsburgh, Pa.

we feel, have avoided possible explosions and fires, and in addition, have prevented interruptions to other customers served from the lines involved. Many of these fuse operations have cleared low-voltage faults, where customer protection has been inadequate or inoperative, and where such faults could not have drawn sufficient current to operate the high-voltage protective relays until after winding breakdown. Such fuse operations have avoided transformer-winding burnout.

In one station fuses were used on each of four 10,000-kva, 66/11-kv transformer banks to avoid the installation of a high-voltage breaker and relaying, and, in final analysis, to give the customer better service. On two occasions these fuses have performed as intended, once by blowing to clear a fault not covered by the customer's low-voltage protection, and once by not blowing on a similar type of fault which was cleared by such protection. In the first instance high-voltage breaker protection would have given a complete interruption until performance of switching to segregate the defective low-voltage equipment, while the fuse application caused no interruption to three fourths of the service and segregated the fault promptly and properly.

The success we have had with our fuse installations has not been due alone to the improvement in the fuses themselves, nor to better installation conditions, but has been the result of these factors together with a judicious selection of the proper fuse for the existing conditions, and to a rigid inspection policy, and the practice of removing such fuses that give any indication of suspicion as to their condition.

Control of Fuse Applications (Design Stage)

In deciding all protective layouts, our design engineers seek the advice and recommendations of the protection engineer, and whether the problem involves that of the more complicated types of reactance relays, pilot-wire schemes, differential protection, or fuses, the procedure is the same. In following this policy, the simplest fuse installation gets the necessary careful consideration it requires, and all of the factors entering into proper selection get uniform attention.

Our protection policy, based on experience, is that all portions of an electrical system should be covered by protection of some sort to clear whatever fault may develop. This policy cannot always be met on an economically justified basis by relays and breakers. Where this condition is encountered, high-voltage fuses frequently provide a reasonably satisfactory solution.

Selection of Fuses for Transformer-Bank Protection

In selecting fuses for transformer-bank protection, investigation is made of the fault current obtained for faults, both on the high-voltage and low-voltage side of the transformer bank, and consideration is given to the protection that is provided on the low-voltage side of the transformer bank. A fuse is then selected with a time-current curve and a current-carrying capacity that will provide the desired time of operation for low-voltage faults.

In many cases, transformers that are fused supply industrial customers, and a variety of types of protection are used by the customer on his own breakers. Settings of these protective devices are determined, and high-voltage bank fuses are chosen to select with the low-voltage equipment. In some cases where customers' equipment is involved, it has been found that the protective equipment has been set unnecessarily high in current and time, and these settings have been lowered to the mutual advantage of the utility and the customer.

The fuse-current rating in relationship to full-load current of the protected transformers is not selected as any arbitrary value in relationship to the size of the transformer bank, but is selected as required with due consideration of the time-current characteristics of the different fuses available.

Since the application of these fuses is for the clearing of faults in and beyond the transformers, no attempt is made to protect the transformers against overloads. In this connection the current rating of fuses on our system varies from $1^1/_2$ to 4 times the bank full-load rating. The time delay and current-carrying capacity of the fuses are inseparable and are varied in order to obtain the necessary time delay for clearing of low-voltage faults. In practice the time delay provided for low-side faults varies from a minimum of 0.3 second on some lighting transformers to a maximum of 1.5 seconds on some power banks.

Time Selection of Fuses With High-Voltage Relays

While fuses are installed primarily to obtain clearing of faults in transformer-

Table I. Comparison of Performance of Fused and Unfused Substations

Fused Stations	Unfused Stations
1. Two fuses operated to clear a defective transformer caused by water entering the winding through a leaky low-voltage bushing.	1. Entire station, consisting of three 2/1 transformers and housing, destroyed by fire as result of transformer failure. Evidence of original failure lost.
2. All fuses operated to clear a defective bank of three 667-kva units during flood.	2. Three 333-kva transformers destroyed, fortunately without fire, when a customer low-voltage fault held on, the customer's breaker found inoperative.
3. One fuse operated to clear a failure of customer static capacitor on 2,300-volt side of bank.	3. Three 100-kva units destroyed, necessitating complete replacement as result of low-voltage winding failure.
4. On two different occasions one fuse operated to clear a defective nonautomatic breaker bushing which had flashed over during storms.	4. Three transformers destroyed as result of low-voltage customer-equipment failure. Fault held on until breakdown of high-voltage winding, when 22-kv protective relays operated. Fortunately no fire.
5. Three fuses operated to clear a high-voltage bus flashover.	5. Failure of one transformer resulted in oil fire which destroyed the entire station on customer premises.
6. Two fuses operated on initial cut-in to clear a low-voltage potential transformer which had been connected reversed with 115-volt winding on the customer's 2,300-volt bus.	6. Transformer failed and exploded at the station, resulting in fire which involved three other units and loss of all electrical equipment. Several intermittent grounds had been indicated at the supply station.
7. On three different occasions fuses operated to clear trouble on customer-owned high-voltage transformers.	7. Low-voltage disconnects pulled under load, fault held on until winding breakdown. Three 500-kva units complete winding loss.
8. Two fuses operated to clear a 2,300-volt bus fault caused by boys having thrown wire across a bare low-voltage bus.	8. On two occasions customer low-voltage cables caused failure of low-voltage bushings, due to fault hanging on and ultimately communicating to the high-voltage side of the bank.
9. One fuse operated to clear an internal flashover of high-voltage lead in one transformer.	
10. One fuse operated to clear defective low-voltage customer breaker and again three fuses operated to clear a flooded three-phase unit, leaving line in service.	
11. On four different occasions fuses operated to clear internal flashovers in transformers during storms.	
12. On three occasions fuses operated to clear faults on four-kilovolt circuit fed from bank. Low-voltage breaker inoperative.	
13. One fuse operated to promptly clear an accidental contact with a ground chain being handled with rubber gloves.	

bank windings or on secondary busses that would not otherwise be cleared properly, other benefits are sometimes obtained. These are:

(*a*). To prevent or decrease outages to other customers.

(*b*). To reduce damage to high-voltage bushings of transformers due to quick clearing.

In connection with item *a*, selection is not always obtained between the transformer fuses and the protective relays on the transmission system. In many cases, however, selection is obtained, particularly for short-circuit faults, and there are many instances where long outages to other tap or nonautomatic loop stations have been prevented by fuses clearing transformer faults before relays operated.

In some instances, the fuse and transmission-line relays may operate together, in which case the transmission line can be returned to service, as the fuses will have cleared the fault.

On many faults, however, the transmission-line ground relays do not select with fuses during line-to-ground faults, if the fault currents are of a low value. This nonselection is recognized and accepted.

With regard to item *b*, many transformer-bank high-voltage bushing flashovers have been cleared so quickly that service could be restored immediately.

Illustration of Specific Application

In Figure 1 is shown one example of application of fuses to three relatively small customers, supplied with tap service from a 22-kv loop circuit. In this particular case the distance between automatic stations is only about $1^1/_2$ miles and is close to a large 66/22-kv substation; as a result the current for 22-kv faults is relatively high.

Time-current curves of phase and ground relays at stations *A* and *B* and the melting time of fuses at customer *C* are shown. Maximum current of 6,000 amperes at 22 kv for a fault on the high-voltage side of transformer at customer *C* is indicated and a current of 600 amperes at 22 kv for fault on the low-voltage side of the same transformer.

These curves show that for a fault on the low-voltage side of the transformer at customer *C*, the melting time of a certain 100-ampere fuse is 0.45 second. This is adequate time to permit the low-voltage bank oil circuit breaker, with instantaneous trip coils, to operate. The curves also show that the phase relays at stations *A* and *B* select with this fuse at all current values. It is shown that the ground relays at the two stations do not select with the fuses at all currents. However, as the fault current is supplied from both ends of this circuit, the fuse obtains more current than either of the ground relays and may select. Also for any ground fault current above 1,000 amperes, the fuse will properly select with the ground relays. As it is possible to obtain 2,000 amperes ground current at this location, the probability of the ground relays selecting with the fuses is very good.

From the above it is seen that in this case the fuses not only serve to protect the transformer banks, but also to protect service to the adjacent customers.

The above illustration is only one of numerous similar conditions on our system.

Miscellaneous Factors to Be Considered in Applying Fuses

In applying fuses for the protection of transformers that supply various types of loads, different factors must be considered, depending on the type of load.

For example, fuses on the high-voltage side of a furnace transformer should have a continuous carrying capacity above the value of current that can be obtained for the condition of shorted electrodes in the furnace, as this is a condition that may exist for a considerable period of time during normal operation of the furnace. In this case, the fuses do not adequately protect the transformer bank in event of low-side failures, but do serve to disconnect the equipment from the system in event of severe faults in the furnace transformer.

In applying fuses for the protection of a transformer bank that supplies distribution circuits that are protected with oil circuit breakers, care must be taken to obtain sufficient time selection between the breaker opening and the blowing of fuse, so that the fuse will not operate incorrectly on normal reclosure of the oil circuit breaker on a faulted distribution circuit. Our present practice is to allow from 0.7 to 1.0 second selection between low-voltage reclosing feeder relaying and high-side bank fuses and while we have experienced no difficulty with this difference in selection, we realize it is open to question and to reconsideration as experience requires.

Conclusion

It is not intended to convey the impression that high-voltage fuses should be used for the protection of transformer banks in preference to relays and oil circuit breakers. Where this equipment is available or can be justified, it should be used. In many cases, the cost of oil circuit breakers cannot be justified, and in such cases, fuses, if applicable, can be used.

In general, it is felt that, in the voltage range of our experience, the modern high-voltage fuse, properly applied and not forgotten, is a reliable protective device that should be added to the other protective schemes, and that it has a definite place in meeting requirements at moderate cost that cannot otherwise be economically justified.

A 600-Volt Enclosed Limiter for Network Use

P. O. LANGGUTH
MEMBER AIEE

H. L. RAWLINS
MEMBER AIEE

J. M. WALLACE
ASSOCIATE AIEE

Synopsis: The extension of the conventional network system to industrial plants has established requirements for a higher-voltage limiter.

The network system has been adopted as a standard by many utilities for 120–208-volt distribution, whereas industrial-plant distribution is usually at higher voltages up to 600 volts due to the predominance of power load. The requirements of a limiter for this service are more severe, due to the proximity of operating personnel and increased interrupting duty associated with higher voltages and shorter cable runs.

A new totally enclosed limiter has been developed for this application. This limiter embodies new principles of operation and will interrupt fault currents in excess of 50,000 amperes at 600 volts without perceptible noise or visible demonstration.

NETWORK systems as originally conceived and installed did not make use of limiters, for the systems were based on the premise that cable-insulation failures encountered on 120–208-volt circuits would be self-clearing. Experience has proved this premise to be generally correct.[1] Obviously, however, there can be solid metal-to-metal faults that are not self-clearing. Furthermore, faults, although self-clearing, could persist until heating of the cable resulted in permanent damage to the cable insulation. Usually, such persistent faults would spread to adjacent cables, materially increasing the resultant damage. The large amount of power liberated in these extensive faults often produced combustible gases in sufficient quantities to cause manhole explosions.[2] These factors led to the development of the limiter which consists of a fusible section located at the ends of cable runs to guarantee the clearing of any fault on the cable before its insulation is damaged or the fault spreads to other cables. The correct analysis and solution of this problem is due largely to the work of C. P. Xenis of the Consolidated Edison Company of New York.[3]

Limiters have been in service on 120–208-volt network systems for several years and have been found ideal for the application.[4] Factors contributing to the rapid adoption of limiters on network systems are as follows:

1. The limiter requires no maintenance. It has no contacts or adjustments and consists of a simple conductor having swedged or bolted connections. It can be connected into the cable and forgotten. The rapid adoption of the limiter has been due principally to this one factor of simplicity and reliability.

2. It eliminates damage to unfaulted portions of the cable. Being a thermal device, it is ideally suited to the protection of cable insulation from heating due to fault currents.[5]

3. The limiter insures the rapid clearing of heavy fault currents, thereby minimizing system disturbances and localizing all faults.

4. In networks having two or more cables per phase, proper selectivity between limiters is automatic. Co-ordination with network protector fuses can be easily obtained by proper planning.[6,7]

5. A quick reclosing device in place of the limiter would be of no particular benefit, for considerable maintenance must be performed on the cable after a fault.

6. The limiter is relatively inexpensive and renders a service out of all proportion to its cost. It may be used at both ends of all cables. Consequently, a fault will disable only a short section of cable.

Previous Limiters

Until the advent of industrial networks the application of limiters was confined almost exclusively to 120–208-volt networks for city distribution. The requirements of limiters connected in these systems were not too severe. The low system voltage combined with short-circuit currents which seldom reached 30,000 amperes represented moderate interruption duty. Since the limiters were usually placed in underground vaults, there were no strict requirements on noise or demonstration. In addition, the limiters were not the only line of defense, for most faults at this low voltage are self-clearing.

The limiter developed for this service consists of a reduced metallic or fusible section incorporated in a connector lug

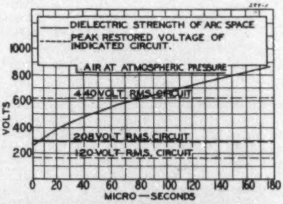

Figure 1. Recovery of dielectric strength of arc space of short arcs with reference to the peak restored voltage of standard systems

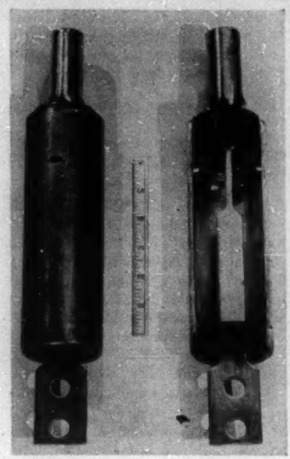

Figure 2. Assembled and sectional view of new limiter

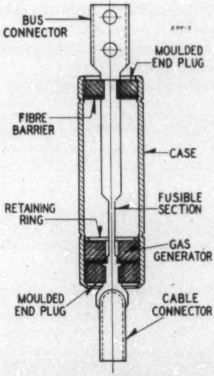

Figure 3. Cross section of new limiter

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P. O. LANGGUTH (now in military service) is assistant manager, circuit breaker and protective devices division, H. L. RAWLINS is section engineer, and J. M. WALLACE is design engineer, switchgear engineering department, all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

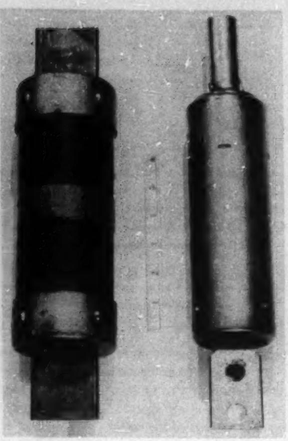

Figure 4 (left). Comparison of new limiter with cartridge fuse

or other connecting device. The fusible section is surrounded with heat-resistant insulation, and the whole assembly is placed in a rubber sleeve and securely taped to prevent the escape of hot gases or flame during interruption. These limiters, when operating within their current and voltage limits, have given successful interruptions without demonstration. They have been found admirably suited to the service for which they were designed.

Requirements for a New Limiter

The entrance of the network distribution system into the industrial field created several new problems. Systems up to 600 volts were encountered, cable runs were considerably shorter, and power concentrations were greater. All of this led to greatly increased short-circuit currents which had to be interrupted at these higher voltages. Under these conditions, faults are seldom self-clearing.[8] Referring to Figure 1, it is seen that an arc space recovers a dielectric strength of about 250 volts instantly at current zero. This is sufficient to cause interruption of arcs on a 120–208-volt circuit. Considerable time is required for the arc space to recover sufficient dielectric strength to cause circuit interruption at higher voltages; hence, faults on a 440-volt system must be cleared by an interrupting device. Limiters, therefore, were necessary to guarantee the interruption of the circuit. Strict requirements concerning noise and demonstration were imposed on the limiters, because they were required to operate in close proximity to factory personnel. Furthermore, the limiters for industrial application could not present any fire hazard whatsoever.

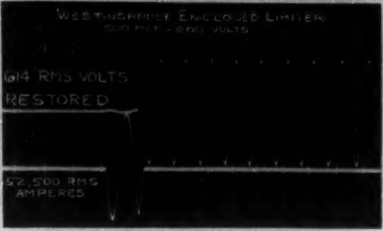

Figure 6 (right). Interruption of current above rating

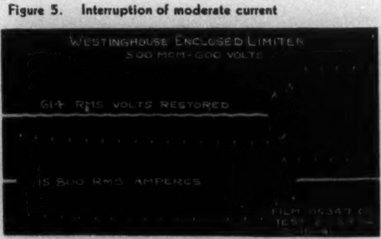

Figure 5. Interruption of moderate current

In accordance with these requirements, it was decided that a limiter for this new service should have the following characteristics:

1. It must be totally enclosed and have minimum space requirements.

2. It must have an interrupting rating of 50,000 amperes at 600 volts, 60 cycles.

3. It must have a low temperature rise to permit bunching of limiters in confined spaces.

4. It must have a time-current characteristic to permit co-ordination with other limiters, co-ordination with its cable insulation, and co-ordination with the network protector fuses.

5. It must have suitable terminals to permit coupling with the proper cable.

Design of a 600-Volt Limiter

The design of a totally enclosed limiter of the required small size presented the dilemma usually associated with engineering problems. On one hand the device must have five times the interrupting ability of a cartridge fuse of comparable current rating, and on the other hand, not only must it be made smaller, but the demonstration attendant to circuit interruption must be eliminated.

The design was accomplished by solving two problems:

1. The metallic vapors and gases from the heavy fusible element had to be accommodated. Tests revealed that a copper sheath or cover around the fusible element would supply a condensing surface on which the fused metal would be deposited, thus eliminating high internal pressures.

2. Totally enclosed interrupting means had to be provided to insure positive interruption of all currents up to the interrupting rating. Further development evolved a concentric fiber gas generator, which would supply the necessary turbulence to cause deionization of the arc.

Figures 2 and 3 show the final design of the limiter. The conducting element is made from copper tubing which is

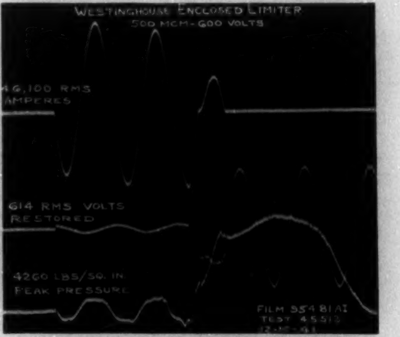

Figure 7 (right). Pressure record during interruption by new limiter of fault current near rating

The pressure variations preceding the melting of the fusible section are induced but are not present after the current is interrupted

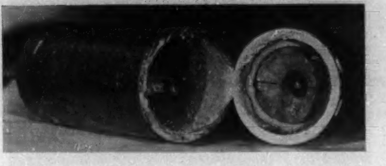

Figure 8. View of limiter cut open after interrupting moderate current

Note deposition of copper on inside surface of case

flattened and sized to produce the proper fusible element and terminal connectors. The closure plugs are moulded on the conducting element in an extrusion-type mould with the retaining ring in place. Split fiber washers are used as barriers to prevent arc flame from burning the moulded material and to produce the gas generating structure. This structure is held from moving under the stress of interruption by the retaining ring which fits up against embossed shoulders on the outside casing. A copper tube forms the case of the limiter and in addition furnishes the condensing surface for the metal vapors. The conducting element is restrained from sliding by embossing the copper tubing, after which the entire assembly is sealed by swedging in the ends of the case. Figure 4 compares the size of the limiter with a 600-volt cartridge fuse of the same current-carrying ability.

Tests

Repeated interruption tests were made throughout the entire current range. In many tests, the limiter was completely surrounded by surgical cotton in accordance with the procedure established by the Underwriters' Laboratories, Inc. In no test was the cotton discolored, thus proving the absence of flame or hot gases external to the limiter. Figure 5 is an oscillogram showing an interruption at a moderate current value. In Figure 6 the interruption of a current in excess of the rating of the limiter is shown. Note the shift in power factor of the circuit caused by the relatively high arc voltage. This characteristic of the limiter is an advantage in that it aids in the interruption of the circuit and protects the system from voltage stresses. Internal pressure is recorded on the oscillogram in Figure 7. The short duration of the pressure is caused by the rapid condensation of the metallic vapor by the copper case and by the cooling of the generated gas. The condensed metal vapor is seen in Figure 8 as irregularities on the inside surface of the blown limiter. Voltage tests were made on blown limiters by putting them on overvoltage immediately after interruption. In all cases the limiters withstood voltages above 2,200 volts continuously for several days until removed from test.

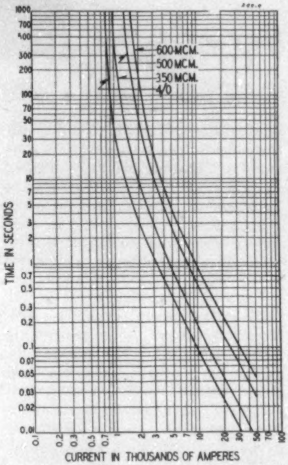

Figure 9. Time–current curves for new limiter

The limiter is designated by the size of cable for which it is intended

The sizes of fusible elements were determined by the co-ordination requirements. Time-current curves for several common sizes are shown in Figure 9. A study of the curves will reveal that any one limiter will be blown without damage to the other limiters on the system, if the fault current is fed to the limiter which interrupts the circuit through at least two other limiters in parallel. This is the case in a properly designed network system, where there are two or more cables per phase, as will be seen by reference to Figure 10, a schematic diagram of a typical load bus unit.

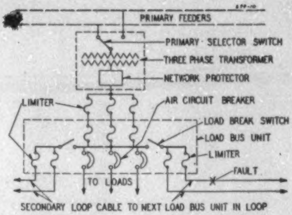

Figure 10. Unit of network system in which selectivity between limiters is automatic

Note that current flows in parallel through all other limiters on the bus to the limiter clearing the fault

Temperature-rise tests were made with the limiter connected to the supply by eight-foot lengths of the proper size type-*R* cable. The limiter was supported in free air and was shielded from air currents by a canvas screen placed completely around the device. Temperature rises were found to be less than 30 degrees centigrade for all sizes at the maximum current ratings of the cable.

Conclusions

Past experience has shown the desirability of a network system and the effectiveness of limiters in providing the proper protection. The new 600-volt limiter described in this paper is serving in a number of industrial plants at the present time, and all experience has been satisfactory. In addition, extensive laboratory testing has adequately proved the new limiter's ability to perform its functions. There has been provided a new 600-volt limiter, small in size, but capable of interrupting large currents without perceptible noise or visible demonstration.

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Modern Impulse Generators for Testing Lightning Arresters

THEODORE BROWNLEE
ASSOCIATE AIEE

NATURAL lightning is very destructive and so elusive that it cannot readily be used for testing. However, the behavior of materials or equipment which are subject to lightning may be studied with artificial lightning from the impulse generator. In impulse testing of insulation strength, it is necessary to control only the wave shape and magnitude of the impulse voltage. There are numerous papers[1–5] describing methods of producing and measuring impulse voltages, and other papers[1,2,6–9] show how to calculate the circuit constants required to produce such waves.

The complete testing of lightning arresters requires, in addition to means for obtaining voltage waves, facilities for the accurate control of both magnitude and wave shape of discharge current. A variety of these impulse current waves are needed to simulate service conditions reasonably. The newly revised AIEE Lightning Arrester Standards No. 28 include tests with 10x20-microsecond waves of 5,000, 10,000, and 20,000 amperes; 5x10-microsecond waves of 65,000 and 100,000 amperes; and 10,000, 15,000, and 20,000 ampere tests with a nominal wave steepness of 5,000 amperes per microsecond. Since the internal impedance of the arrester is a factor affecting the impulse current, the complete testing of various types and ratings of arresters requires impulse generators with considerable flexibility and facility for quick adjustment. This paper describes several impulse generators designed primarily for lightning-arrester testing, and which have proved over years of constant use to have the desirable features of safety, flexibility, reliability, and convenience.

The circuit constants necessary to produce various current waves cannot be calculated exactly, because the resistance of the arrester valve element is not constant but a function of the voltage or current. So far as the author is aware, the methods for determining such circuit constants have not been published although there have been described impulse generators[10,11] which are adapted for lightning-arrester testing with different current waves. Through long experience, methods of approximate calculation have been developed, chiefly by the use of curves, which enable circuit constants to be selected roughly with a minimum of cut-and-try procedure. These methods are general in character and are applicable to unidirectional or oscillatory waves, including, of course, those specified in the AIEE Standards.

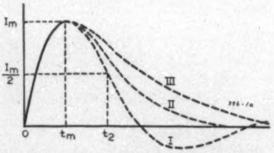

Figure 1a. Typical impulse-current waves

The time to crest for all three waves is designated by t_m, but t_2, as shown, represents the time to half value for wave I only

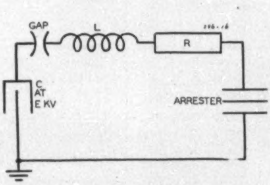

Figure 1b. Typical *RLC* impulse circuit

List of Symbols

The following symbols refer to the impulse-generator waves or circuit of Figure 1:

C = capacitance in microfarads
E = initial voltage on C in kilovolts
L = inductance in microhenrys
R = constant resistance in ohms
R_A = resistance of arrester*
R_T = total circuit resistance = $R+R_A$*
R_1 = critical resistance below which oscillations are produced
I_m = crest of current wave in kiloamperes
t_m = time in microseconds corresponding to I_m
t_2 = time in microseconds corresponding to $0.5I_m$ (on tail)
Δ = ratio of crest current for any half cycle to that of the preceding half cycle in an exponentially damped sine wave
E_m = crest of voltage wave produced by current wave through R_T

Typical Waves and Circuits

Figure 1a shows three current waves** and Figure 1b the customary circuit for producing such waves. The wave shapes I, II, and III are produced by circuits in which the total resistance is respectively

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THEODORE BROWNLEE is research engineer in the lightning arrester engineering department of the General Electric Company, Pittsfield, Mass.

*R_A and R_T are not constant but functions of voltage (or current) and time. Consequently R_A and R_T can only be represented completely as curves of instantaneous resistance plotted as a function of time. Such curves have a minimum at or a little after the maximum current.

**For time measurements in Figure 1a and throughout this paper, the actual zero is used. AIEE Standards No. 28 use the virtual zero which is the intersection on the time axis of a straight line projected through the 10 and 90 per cent points on the front of the wave. There is little practical difference, amounting to the order of only 1 per cent of the time to half value on the tail, for the waves here considered.

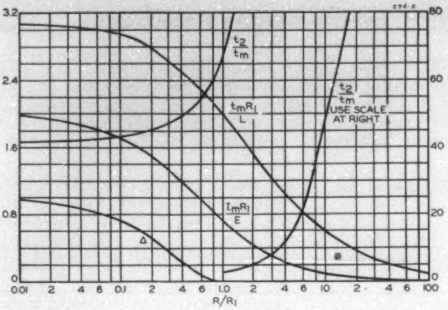


Figure 2. Effect of resistance on waves in *RLC* circuit

less than, equal to, or greater than the critical resistance. This total resistance R_T is composed of the constant resistance R and the nonlinear resistance R_A of the arrester. Valve-type arresters are designed to have a variable resistance R_A which is low at high currents but high at low currents, and R_A is the principal stumbling block to straightforward calculations of impulse circuits.

While both types of resistance may be present in any proportion, it will be shown how to compute the circuit constants when the resistance is predominantly linear or predominantly variable; from these, rough interpolations can be made for various combinations.

Before specific cases are considered, the principles of similarity and proportionality of circuit constants should be emphasized. Waves having similar shapes are those which can be represented by a single plot if either or both co-ordinate scales are multiplied by the necessary factors. In an *RLC* circuit as shown in Figure 1b, the wave shape depends only upon $R_T/\sqrt{4L/C}$. If R_A is negligible, R_T is a constant and the above expression is a simple ratio of definite constants. However, if R_A is not negligible, R_T is really a function of time, as explained in the list of symbols. In this condition the expression is of little use; but with different waves of similar shape, the curves of R_T versus time also are similar, and any two corresponding values of R_T, such as those at maximum current, can be used for comparison purposes.

For current waves of similar shape, the circuit constants and wave specifications are so related that the following ratios are constant.

1. $t_m/\sqrt{LC}$
2. $t_2/\sqrt{LC}$
3. E/I_mR_T
4. E/E_m
5. I_mt_2/CE

It is possible to determine experimentally circuit constants to produce various wave shapes and by use of the above principles to calculate directly the constants required for other waves, similar in shape, but of different magnitudes or durations. Since the resistance may be a function of the current or voltage, the wave shape depends upon the voltage of the impulse generator as well as upon its other constants.

Calculations When Arrester Resistance Is Low in Comparison With Total Circuit Resistance

For the cases where the arrester resistance can be neglected (or lumped with the circuit resistance) the current for any particular circuit can be found easily from the well-known equations:

$$I=(E/\omega L)\epsilon^{-at}\sin\omega t \text{ where } R<R_1 \tag{1}$$

$$=\frac{Et\epsilon^{-at}}{L} \quad \text{where } R=R_1 \tag{2}$$

$$=\frac{E}{L}\,\frac{\epsilon^{-mt}-\epsilon^{-nt}}{n-m} \quad \text{where } R>R_1 \tag{3}$$

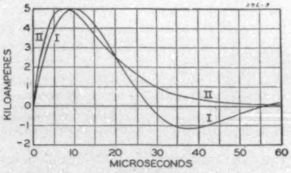


Figure 3. Impulse-current waves calculated by curves of Figure 2

		Wave I	Wave II
Given	C	1	1
	t_m	10	—
	t_2	20	20
	I_m	5	5
	Shape	—	Critically damped
Found From Curves	E	68.5	101
	L	64.0	55.5
	R	6.88	14.9

Wave I. $I=9.5\epsilon^{-0.054t}\sin 0.113t$

Wave II. $I=1.18t\epsilon^{-0.134t}$

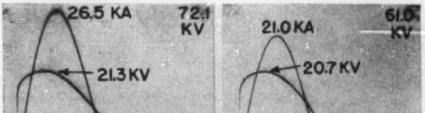


Figure 4. Current and voltage oscillograms on lightning-arrester element in *RLC* circuit of Figure 1b

$C=4.41$; $L=15.4$; $R=0.142$. The same arrester element was used for all six waves with the wave shape determined only by E which is listed in upper right-hand corner of each oscillogram

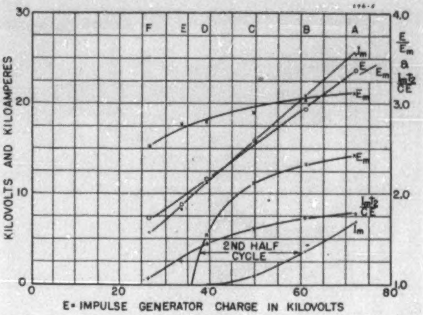


Figure 5. Curves of E_m and I_m taken from oscillograms of Figure 4

Curves of E/E_m and $I_m t_2/CE$ are computed from these results, but are applicable for any other combinations of circuit constants which can produce similar wave shapes

$$a=\frac{R}{2L};\ \omega=\sqrt{\frac{1}{LC}-a^2};\ m=\frac{R-\sqrt{R^2-R_1^2}}{2L};$$
$$n=\frac{R+\sqrt{R^2-R_1^2}}{2L}$$

Equation 1 defines an oscillatory wave such as wave I in Figure 1a, while equations 2 and 3 are for the nonoscillatory waves II and III, respectively.

Equations 1, 2, and 3 are transcendental and it is not possible to calculate directly the equation for a specified impulse such as a 5,000-ampere 10x20-microsecond wave. It is best to assume various circuit constants and plot curves which then can be used directly. It is found that for the *RLC* circuits the shape of the current wave depends only upon the ratio R/R_1 and this ratio is the most convenient parameter for abscissa of the curves. Figure 2 shows a set of such curves with ratios of R/R_1 from 0.01 to 100. The left-hand side of Figure 2 represents oscillatory conditions and the right-hand side nonoscillatory conditions. For a 5x10- or 10x20-microsecond wave, $t_2/t_m=2$, and it is found that such a relation holds only for oscillatory waves for which $R/R_1=0.43$ and $\Delta=0.22$. For the critically damped waves $R/R_1=1$ and $t_2/t_m=2.68$.

An infinite number of circuits and voltages will produce any possible wave, and at the start one constant must be arbitrarily fixed. Usually the first selected constant is either the impulse-generator capacitance or voltage, but in some circumstances the limiting value might be inductance or resistance. Figure 3 shows two waves for which the impulse-generator capacitance was fixed and the remaining constants determined by use of the curves in Figure 2. It is seen that the two waves are not greatly different, but considerably more charge was required for the critically damped wave. For any nonoscillatory wave, the area under the ampere–time wave in ampere-seconds is equal to the charge *CE* (*C* in farads and *E* in volts) but for oscillatory waves an integration for the first half cycle shows the ampere-seconds to be $CE(1+\Delta)$, and providing the resistance can be kept low enough, it is possible to get in the first half cycle nearly twice the ampere-seconds originally stored in the capacitance. This means, however, that the wave will oscillate for many half cycles, which frequently is not a desirable condition.

Empirical Calculations to Include Nonlinear Resistance of Arrester

Valve-type lightning arresters are designed to have low resistance at high currents and high resistance at low currents, and rigorous calculations of circuits containing such resistances are not possible. Figure 4 shows the types of waves which can be obtained in the circuit shown in Figure 1b where the variations in waves are caused only by variations in the charging voltage. Considering only the first half cycle, the current waves are remarkably similar in shape and are all approximately 12.5x22-microsecond waves. This ratio of t_2/t_m of about 1.75 is typical of impulse-generator circuits in which the arrester-valve element comprises most of the resistance, and so the specified ratio of two for 5x10- or 10x20-microsecond waves can be fulfilled approximately without difficulty. If a closer agreement is desired, it can be obtained by using some additional constant resistance.

In addition it should be observed that the first quarter cycle is practically a sine wave and the time to crest of 12.5 microseconds is a close approximation of the calculated time to crest for no resistance where:

$$t_m=0.5\pi\sqrt{LC} \tag{4}$$

which gives $0.5\pi\sqrt{4.41\times15.4}=13$ microseconds. In Figure 5 are plotted the crest measurements of current and voltage from Figure 4, and in addition the ratios E/E_m and $I_m t_2/CE$.

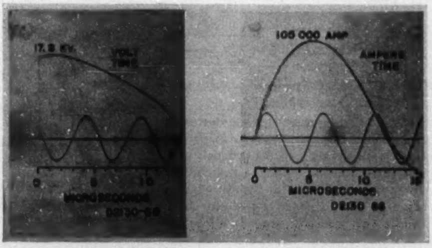
The curves for I_m and E_m apply only for the circuit constants listed for Figure 4, but the ratios E/E_m and $I_m t_2/CE$ are universal in application for circuits wherein the resistance is chiefly arrester valve element. The absolute magnitudes and durations are immaterial for these ratios which depend only upon the wave shapes. The points for the ratios are identified with letters A–F corresponding to the oscillograms A–F in Figure 4. Hence, in any circuit if the wave shape of Figure 4E is desired, E/E_m must be 1.97. and $I_m t_2/CE$ must be 1.31. In the calculation of such a circuit, it is necessary to know or assume the maximum voltage across the arrester and resistance expected at the desired current. The following example shows the general method of attack.

Example

A 75-kv impulse generator is available with a maximum capacitance of seven microfarads and a minimum inductance of two microhenrys. Determine if this equipment is capable of producing a 100,000-ampere 5x10-microsecond discharge through an arrester having approximately 18-kv crest on this wave. Here $I_m t_2/CE=1.9$.

The curve of $I_m t_2/CE$ in Figure 5 goes only to 1.8 so the desired wave, if at all

Figure 6. Voltage and current oscillograms for nominal 100,000 - ampere 5 x 10-microsecond current wave through a valve-type lightning arrester



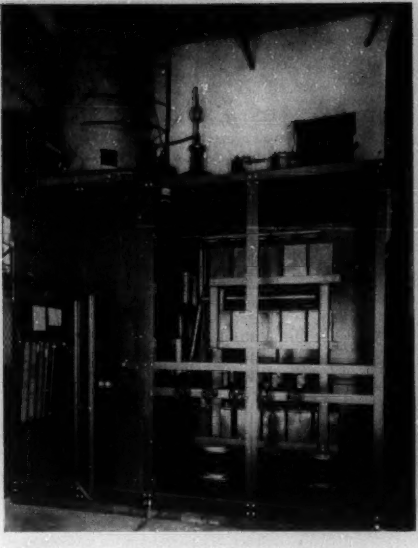


Figure 7. Adjustable-capacitance impulse generator number 1

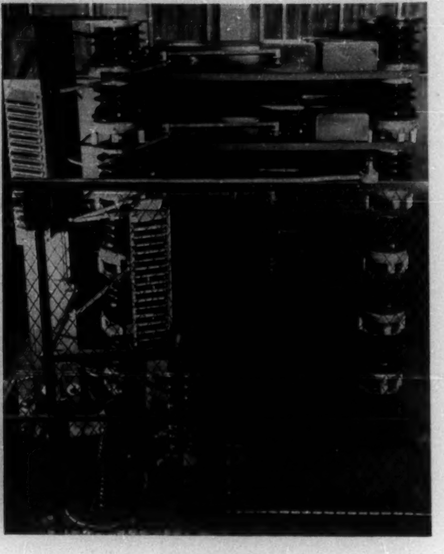


Figure 9. Marx-circuit impulse generator number 2

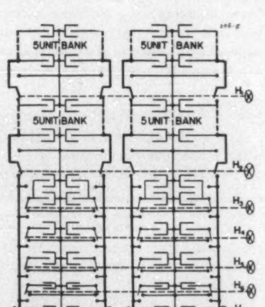


Figure 8. Circuit diagram of impulse generator number 1

Hand-operated switches H_1–H_7 throw capacitor units or banks on the busses *ABCD* or disconnect and short-circuit the terminals to the cases, *EF*. Solenoid-operated switch *S* has three positions: "left" for positive generator charge; "right" for negative generator charge; "middle" automatically grounds all live capacitor points except during charging periods

possible, will be more oscillatory than that shown in Figure 4A. Since some tolerance is allowed, a 4.7x9.4-microsecond wave is acceptable, which gives $I_m t_2/CE = 1.8$. Hence there is sufficient charge available to produce a 4.7x9.4-microsecond wave similar in shape to Figure 4A.

Using equation 4, $t_m = 0.5\pi\sqrt{LC} = 5.9$ microseconds, which is close enough for practical purposes.

For $I_m t_2/CE = 1.8$, the corresponding value of E/E_m is 3.38. This means that sufficient additional resistance should be added to the circuit so that $E_m = 75/3.38 = 22$ kv.

Figure 6 shows waves actually produced under the conditions specified in the above example. However, no resistance was added, so that for the waves of Figure 6 the current is higher and the oscillations greater than in the example.

The foregoing example shows that it is not always possible to fulfill the exact requirements determined by calculation, and in order to test various types and ratings of arresters over a range of currents and wave shapes, the impulse-generator constants and charging voltage must be adjustable over a wide range.

When several ratings of arresters are to be tested with the same wave, the circuit adjustments are minimized by setting up to test the highest rating first, and when other ratings of lower internal resistance are substituted, sufficient resistance is added to keep the total resistance constant.

Often it is found impossible, with existing equipment, to get aperiodic waves coming rapidly to zero as in Figure 4E. If the maximum charging voltage E is too low for a particular arrester rating, the waves will look like Figure 4F. If there is current to spare, the voltage wave can be brought down to zero by the use of a constant resistor in parallel with the arrester or generator capacitance. If the product CE is too small to produce the required wave like Figure 4E, the current can be increased in low ratings of arresters by letting it oscillate somewhat as was done in Figure 6 or Figure 4 (A–D).

Calculation for a Wave of Specified Crest and Nominal Steepness

For a wave specified by steepness instead of duration, it is only necessary to redesignate it in the usual way and calculate the circuit constants as previously shown. For circuits where R is low compared to R_T, the first quarter cycle is almost a sine wave. The nominal steepness, or the slope of the line through the

10 per cent and 90 per cent points, is approximately

$$\text{Nominal steepness} = 1.2 I_m / t_m \qquad (5)$$

or with $t_2/t_m = 1.75$ as in the waves of Figure 4

$$\text{Nominal steepness} = 2.1 I_m / t_2 \qquad (6)$$

For critically damped waves in circuits where R_A is low compared to R_T, similar approximate equations are

$$\text{Nominal steepness} = 1.4 I_m / t_m \qquad (7)$$

or

$$\text{Nominal steepness} = 3.7 I_m / t_2 \qquad (8)$$

for intermediate wave shapes an approximate equation is

$$\text{Nominal steepness} = 1.3 I_m / t_m \qquad (9)$$

and the equation in terms of t_2 can be found using the ratios of t_2/t_m in Figure 2.

Hence, using the equations 5 to 9 any wave based on crest and nominal steepness can be redesignated by crest and duration and calculations made as before.

Control of Impulse-Current Wave Shapes in General Testing

The calculating methods here presented are also applicable where nonlinear resistances, such as Thyrite, are used to control impulse-current wave shapes.[12,13] The use of such resistors permits higher crest currents without oscillation, and shorter wave tails, than can be obtained by use of constant resistance.

Description of Impulse Generators

It is evident from the foregoing material that lightning arrester impulse testing requires an extended range of circuit constants and unless the various circuits can be conveniently and quickly set up, much valuable time will be lost. In addition to the very high voltage and very high current impulse generators necessary for special tests on lightning arresters, there have been built during the last few years four generators at the continuous disposal of the lightning arrester department in Pittsfield.

GENERATOR NUMBER 1 FOR GENERAL TESTING

Generator number 1 is shown in Figure 7 with part of the steel panels removed. The capacitors are all Pyranol-filled 75-kv units especially designed to have low inductance and to withstand repeated short-circuit currents. All units have mid-points tied to the cases. There are 28 capacitors rated 0.24 microfarad and 4 trimmers for fine adjustments rated 0.06 microfarad. The charge and discharge circuits are connected to two pairs of copper-tubing busses which may be operated in series for 150 kv or in parallel for 75 kv. The capacitors are mounted on two three-deck steel frameworks insulated from ground and each other. At 150 kv connection, the cases of capacitors in the front framework are at 112.5 kv and those in the back framework are at 37.5 kv. The handwheels shown in Figure 7 throw two or more capacitors on the lower busses or short circuit them to their respective frameworks. The capacitors in the two upper decks are permanently bussed in parallel but they can be connected either to the lower busses or short-circuited to the framework. The charging equipment is located on top of the steel housing, and the large cylinder contains a combination polarity reversing switch and ground switch which grounds all busses and frameworks except during the charging period.

Figure 8 shows the elementary circuit diagram for the 150-kv connection. For clarity none of the hand-operated switches H_1–H_7 are shown completely closed, but H_1 and H_2, when thrown to the indicated position, connect the two upper decks to the busses $ABCD$ while the capacitors in the lower deck are all removed from the busses and short-circuited by the switches H_3–H_7. By means of the switches any capacitance from 0.04 to 1.75 microfarads in steps of 0.04 microfarad can be instantly selected for 150-kv operation. For 75-kv operation switches (not shown in Figure 8) connect bus D to bus A and mid-point E to mid-point F. Also G is moved from D to BC and J from BC to EF. With these connections the capacitance can be varied from 0.16 to 7 microfarads in steps of 0.16 microfarad. For very long waves, all the capacitor bushings can be bussed together, using the cases for the return circuit, and then 28 microfarads can be obtained for charging voltages up to 37.5 kv.

Tests at moderate currents are made in the oscillograph room at the rear of the generator. High-current tests require short leads and are made inside the steel housing. Some of the tapped resistance and inductance control elements are shown hanging on the wall inside the door. The minimum discharge inductance is about two microhenrys and tests up to 100 kiloamperes can be made on low-voltage arresters. The oscillograms of Figure 6 were obtained using this generator.

The advantages of this impulse generator are:

1. *Safety*. No power can be applied until all safety gates are closed and all capacitance parts are grounded after every discharge.

2. *Convenience*. Capacitance and voltage are variable over wide ranges with little effort. The polarity can be changed by the flip of a switch. The three-electrode gap spacing is controlled by a wooden shaft extending into the oscillograph room, and the charging-voltage control is located on the oscillograph table.

3. *Reliability*. Several years of service without trouble have demonstrated its reliability.

4. *Compactness*. A minimum of floor space is used, the dimensions being $6^1/_2$ by 12 feet.

5. *Totally Enclosed*. The steel housing eliminates stray electrostatic fields, and the exterior is neat even when destructive tests are being made inside. It also reduces the noise when high-current tests are being made.

GENERATOR NUMBER 2 FOR GENERAL AND HIGH-VOLTAGE TESTING

Impulse generator number 2 has a 600-kv six-stage Marx circuit as shown in Figure 9. The Pyranol-filled capacitors are rated 2.2 microfarads at 25 kv and have but one bushing with the case serving as the other terminal. Each stage consists of two groups of four capacitors in series. The primary groups are shown on the left in Figure 9 and the secondary groups at the right can be connected in parallel or short-circuited and left with cases floating. The charging resistors and interstage sphere gaps are mounted on the vertical wooden box at the extreme left. By means of switches the generator can be connected conveniently to operate at reduced voltage and high capacitance, and, if desired, part of the capacitance can be omitted. The following combinations can be obtained with either polarity and any voltage up to the maximum listed:

Maximum Kv	Microfarads Available
50	1.1 to 26 in steps of 1.1
100	0.55 to 6.60 in steps of 0.55
200	0.28...0.55... 0.82...1.1...1.6
300	0.18...0.37... 0.73
400	0.14...0.28
500	0.11...0.22
600	0.09...0.18

The advantages of this generator over number 1 are its higher voltage and greater energy. Tests can be made on arresters up to 75-kv rating at 5,000 amperes, 10x20-microsecond waves, and at appreciably greater currents on lower ratings. Its minimum inductance is higher than for generator number 1 since the Marx circuit necessitates longer connections. Because the minimum inductance at 100 kv is about 3.5 microhenrys, generator number 2 will not produce as high currents as generator number 1 even though it contains nearly twice the energy.

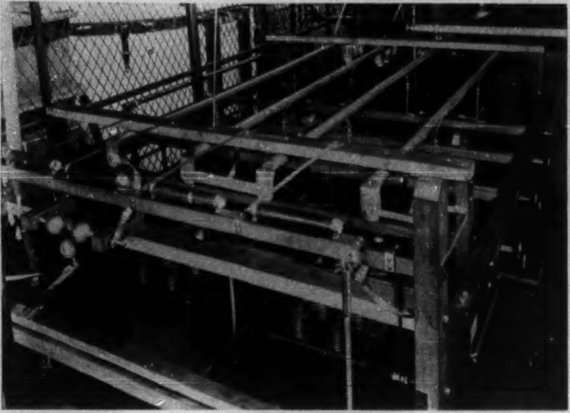


Figure 10. Adjustable-capacitance impulse generator number 3 used for operating-duty power tests

However, for a wide range of voltage and moderate currents of 50 kiloamperes or less, it supplements the other generator. The oscillograms in Figure 4 and all those in a contemporary paper[14] describing a new oscillograph were produced using this generator.

GENERATORS NUMBERS 3 AND 4 USED FOR OPERATING-DUTY TESTS

For convenience, two additional impulse generators are used for operating-duty power tests. Generator number 3, shown in Figure 10, consists of two rows of capacitors which can be connected in parallel or series for 50- or 100-kv operation. The usual connection is 100 kv, and by means of the handwheel-operated switches, the capacitance can be adjusted from 0.125 to 1.9 microfarads in steps of 0.125 microfarad. Units not in use are short-circuited by the switches, but the cases remain at the midtap potential of those in use. All live parts are automatically grounded except when the generator is on charge. The voltage and gap settings are easily adjustable, and the polarity can be reversed if desired.

Generator number 4 is similar in construction and rating to number 3. Either one is capable of sending a 10,000-ampere 10x20-microsecond wave through a 12.5-kv arrester. Number 3 is used for routine tests with a power supply of 500 kva at 3, 6, 12, or 24 kv at 60 cycles, while number 4 is used with a supply of 2,500 kva at 7.5 or 15 kv.

In addition to the four impulse generators described, there are available in the high-voltage engineering laboratory impulse generators rated up to 5,000 kv and a high-current generator[11] which can be connected for a maximum 6.2 microfarads at 150 kv, 14 microfarads at 100 kv, or 56 microfarads at 50 kv. This high-current generator has very low inductance and can produce currents up to 250 kiloamperes. It can be used for regular impulse tests or for operating-duty power tests with either of the power sources used with generators number 3 or 4.

Summary

1. Thorough testing of lightning arresters requires a wide variety of impulse-current waves.

2. The strictly mathematical calculation of circuit constants to produce such waves is limited to the few cases where the circuit resistance is constant.

3. Valve-type lightning arresters have a resistance that is intentionally a function of voltage or current and the determination of the constants of circuits containing valve elements must be done experimentally.

4. Impulse-current waves of the same shape but various durations and magnitudes have circuit constants where $R_T/\sqrt{4L/C}$ is fixed.

5. Once the circuit constants are determined for a particular wave, the constants for similar waves of other magnitudes and durations are easily calculated.

6. The curves of Figure 5 facilitate such calculations for the wide range of wave shapes shown in Figure 4.

7. The testing of various types and ratings of arresters with a number of different waves, requires extremely flexible impulse-generator circuits.

8. Ordinarily the impulse-generator capacitance is the most difficult circuit constant to adjust, but four special impulse generators are described having capacitance values easily adjustable over a wide range. Several years of experience with these generators, and the previously mentioned calculating methods, show that constants for any desired wave, within the capabilities of the equipment, can be computed and set up easily and quickly.

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Analytical Treatment for Establishing Load-Cycle Ratings of Ignitrons

D. E. MARSHALL
MEMBER AIEE

E. G. F. ARNOTT
NONMEMBER AIEE

THE ignitron offers unique possibilities as a control element for high instantaneous alternating demand currents. The demand current is limited only by the capacity of the ionized vapor to conduct the current from the mercury pool to the anode and by probability of arc back, which increases as the peak current conducted by the tube increases. The design of ignitron tubes is a compromise between these two factors. Design for low arc-back rate usually results in anode baffles and shields which tend to increase the deionizing properties of the tube and thus decrease the peak current-carrying capacity.

The above consideration and not failure of the cathode to emit electrons limits the peak current that can be controlled by an ignitron. For various classes of control service, such as resistance welder control, the arc-back problem is minimized by the characteristics of the circuit, and the tubes are designed without baffles or other constrictions. Therefore, the peak to average current ratios can assume higher values than are customary in gas- or vapor-filled tubes in other classes of service.

Experience has indicated that the average current ratings within the above peak current limitation are limited mainly by thermal considerations. These limitations are relatively easy to determine under conditions of continuous operation. One operates the tube for a reasonable period of time with the circuit constants and the firing phase chosen so as to place the maximum arc-back stress on the tube at regularly increasing current steps until arc back or loss of control occurs and then backs off for a reasonable factor of safety to establish a point on the rating curve at 100 per cent duty.

By measuring the loss in the tube at this current the energy which can safely be dissipated by the tube can be determined for continuous duty.

However, tubes in general and welding tubes in particular are seldom operated at continuous duty. A large number of combinations of duty cycle and demand currents is possible for a given size ignitron and is used in commercial practice. It is, therefore, necessary to invent methods of determining rationally the rating of these tubes and presenting the data in a form which is capable of furnishing a definite figure for the permissible demand current at any conceivable combination of current and duty.

Obviously, the tube cannot be economically tested for all the various possible combinations. The present paper describes a method which can be used to interpolate or extrapolate to obtain ratings for any combination of intermittent duty. This method consists in operating the tube at a simple on- and off-duty cycle at a high current value and determining an intermittent rating point in addition to the continuous rating point determined as described above.

Curves of arc drop as a function of demand current are also determined. It will in general be found that the average loss in the tube for equal excellence of operation at high currents will be less than that at continuous duty. It is assumed that this difference in heat-dissipation quality is due to the intermittent production of heat in the tube and is a function of an equivalent "thermal time constant" similar to the equivalent electrical quantity. The accuracy of the method can be checked in practice by repeating the determination of the thermal constants using several different duty cycles. The authors realize that the variation of the temperature of the simple model treated in the following discussion does not completely describe the thermal cycle of an ignitron but believe that by its use a close enough approximation can be achieved for the degree of accuracy required.

Similar methods are now in use employing the familiar concept of maximum averaging time and are satisfactory enough for most purposes but give results that seem artificial under certain conditions. It is hoped that the present paper will be regarded as a theoretical approach to a rating method based on more nearly correct physical principles.

Thermal Action of Equivalent Model

In approaching the problems of establishing ratings for ignitrons on intermittent duty, equations are developed for a model system. This system is one in which heat is lost by conduction, and the heat loss is therefore proportional to the first power of the temperature difference between the model and the cooling medium. The model is considered to be a small mass of metal in which the temperature at any time is uniform throughout. The mass of metal is surrounded by a cooling medium to which heat from the metal is lost by conduction.

The case to be considered is one where energy is put into the system in equal amounts at regularly spaced intervals. During the time that energy is being put into the system the temperature rises, although some heat is lost by conduction. During the off period when no energy is being supplied, the temperature falls.

To develop the general equation for intermittent loading it is necessary to set up relations between the temperature θ' and the energy input W and time t for both the heating and cooling portions of the cycle. The differential equation for the temperature, while energy is being supplied at the rate W, is

$$CMd\theta' = Wdt - K(\theta' - \theta_0)dt$$

If temperature rise $\theta' - \theta_0$ is replaced by θ, and consequently $d\theta'$ by $d\theta$, the equation will express the temperature θ referred to the temperature of the cooling medium as zero, giving

$$CMd\theta = Wdt - K\theta dt \tag{1}$$

where

θ = the temperature of the heated mass referred to the cooling medium temperature as zero
W = average watts input during the heating period
C = specific heat of the metal
M = mass of the metal
K = thermal conduction constant

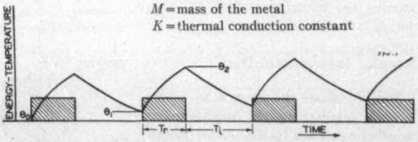

Figure 1. Temperature of model as function of time, with rectangular-waveform energy input

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D. E. MARSHALL is section engineer, special products engineering department, Westinghouse Electric and Manufacturing Company, and E. G. F. ARNOTT is a research engineer, also with Westinghouse Electric and Manufacturing Company, both in Bloomfield, N. J.

Rearranging the terms in equation 1 and integrating:

$$\log_\epsilon (W-K\theta)=\text{constant}-\frac{K}{CM}t$$

and since at $t=0$, $\theta=\theta_1$, the constant $=\log_\epsilon (W-K\theta_1)$, where θ_1 is the temperature at the beginning of the heating period. Solving for θ, the temperature at any time during the heating period is given by

$$\theta=\left(\theta_1-\frac{W}{K}\right)\epsilon^{-\frac{t}{T_c}}+\frac{W}{K}\quad\text{(heating period)}\qquad(2)$$

where

$$T_c=\frac{MC}{K}=\text{time constant}$$

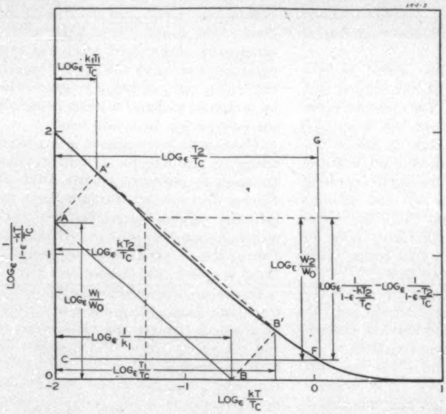

Figure 2 (left). Chart illustrating graphical method of determining rating of model as function of duty cycle and thermal time constant

The temperature during the cooling period can be found from equation 1 by letting $W=0$. This gives

$$\log_\epsilon \theta=\text{constant}-\frac{t}{T_c}$$

At $t=0$ that is, at the end of a heating period $\theta=\theta_2$ as given by equation 2 when t equals T_r, the length of the heating period. This makes the constant equal to $\log_\epsilon \theta_2$ and gives

$$\theta=\theta_2\epsilon^{-\frac{t}{T_c}}\quad\text{(cooling period)}\qquad(3)$$

Equations 2 and 3 are sufficient to determine the temperature θ at any time during load-cycle operation.

Regular Intermittent Duty

Figure 1 shows the temperature θ for a series of regularly spaced equal impulses supplied as shown by the shaded blocks. The time factor $k=T_r/(T_r+T_i)$ is the ratio of the time during which the load is on, to the period or length of the cycle. On the initial cycle θ_1 is equal to θ_0, and θ_2 is given by equation 2 where t is set equal to T_r. For each successive heating period a different value of θ_1 must be used, and this is of course the value of θ at the end of the previous cooling period. After a sufficient number of impulses equilibrium will be reached, and then all the successive values of θ_1 will be equal and the values of θ_2 will also be equal. This simply means that in each "off" period the system loses just the amount of heat which it gained in the previous "on" period.

The allowed value of the watts W during the on period is determined by the maximum temperature allowed. Therefore the value of θ_2 must be found after equilibrium has been reached. For this condition $\theta=\theta_1$ at $t=T_i$ in equation 3 and rearranging

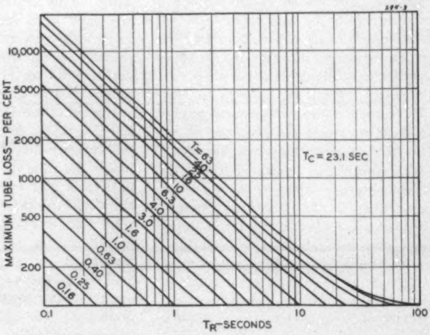

Figure 3 (right). Curve showing rating of model at a given thermal time constant equal to 23.1 seconds

$$\theta_2=\theta_1\epsilon^{\frac{T_i}{T_c}}\qquad(4)$$

Also, θ_2 in equation 4 must equal θ from equation 2 when $t=T_r$ and therefore

$$\theta_2=\theta_1\epsilon^{\frac{T_i}{T_c}}=\left(\theta_1-\frac{W}{K}\right)\epsilon^{-\frac{T_r}{T_c}}+\frac{W}{K}\qquad(5)$$

Solving for θ_1

$$\theta_1=\frac{W}{K}\left(\frac{1-\epsilon^{\frac{T_r}{T_c}}}{1-\epsilon^{\frac{T_r+T_i}{T_c}}}\right)\qquad(6)$$

Substituting this value of θ_1 in the second term of equation 5

$$\theta_2=\frac{W}{K}\left(\frac{1-\epsilon^{\frac{T_r}{T_c}}}{1-\epsilon^{\frac{T_r+T_i}{T_c}}}\right)\epsilon^{\frac{T_i}{T_c}}$$

and simplifying

$$\theta_2=\frac{W}{K}\left(\frac{1-\epsilon^{-\frac{T_r}{T_c}}}{1-\epsilon^{-\frac{T_r+T_i}{T_c}}}\right)\qquad(7)$$

We may now designate the period of the intermittent cycle, (T_r+T_i), by the symbol T as is customary. Then since the ratio of conducting time to the period $T_r/(T_r+T_i)=T_r/T=k$, equation 7 becomes

$$\theta_2=\frac{W}{K}\left(\frac{1-\epsilon^{-\frac{kT}{T_c}}}{1-\epsilon^{-\frac{T}{T_c}}}\right)\qquad(8)$$

Equation 8 gives the value of the maximum temperature reached at equilibrium in terms of the watts input W, the period T, and the "on" time $kT=T_r$. The safe or allowed value of θ_2 can be obtained from the allowed watts, W_0, for continuous loading. In this case $k=1$ and equation 8 gives

$$\theta_{2\,\max}=\frac{W_0}{K}\qquad(9)$$

If the requirement for safe operation is that θ_2 shall never exceed W_0/K, it can be replaced by this value in equation 8, giving

$$\frac{W}{W_0}=\left(\frac{1-\epsilon^{-\frac{T}{T_c}}}{1-\epsilon^{-\frac{kT}{T_c}}}\right)\qquad(10)$$

This equation gives the allowed value of input watts W, which can be used for a time, $T_r=kT$ for a repeated loading cycle of period T.

In order to use a graphical construc-

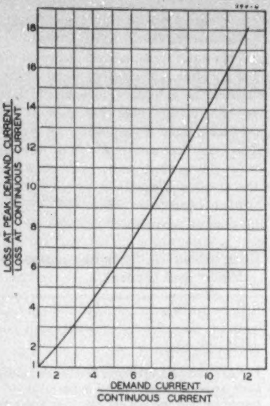

Figure 4. Curve showing increase in tube loss as function of increase in tube-demand current

tion from which the value of time constant can be determined, and rating curves can be laid out, it is convenient to consider equation 10 in the following manner. For two values of k, but the same period T

$$\frac{W_1}{W_0}=\frac{1-\epsilon^{-\frac{T}{T_c}}}{1-\epsilon^{-\frac{k_1T}{T_c}}} \tag{11}$$

and

$$\frac{W_2}{W_0}=\frac{1-\epsilon^{-\frac{T}{T_c}}}{1-\epsilon^{-\frac{k_2T}{T_c}}} \tag{12}$$

Dividing equation 11 by equation 12

$$\frac{W_1}{W_2}=\frac{1-\epsilon^{-\frac{k_2T}{T_c}}}{1-\epsilon^{-\frac{k_1T}{T_c}}} \tag{13}$$

This expression suggests the relations

$$W_1\propto\frac{1}{1-\epsilon^{-\frac{k_1T}{T_c}}} \tag{14}$$

and

$$W_2\propto\frac{1}{1-\epsilon^{-\frac{k_2T}{T_c}}} \tag{15}$$

Figure 2 shows $\frac{1}{1-\epsilon^{-\frac{kT}{T_c}}}$ plotted against kT/T_c. This curve will give the value of W/W_0 by dividing the value for k_1T/T_c by that for T/T_c. If the value of T_c is known, specific values of T_r and T can also be derived from this curve.

Application to Ignitron Ratings

As an illustration of the use of this method, rating curves will be obtained for a small water-cooled metal ignitron. Since all of the energy loss is eventually transferred to the cooling water, the problem will be regarded as one where heat is lost by conduction, as in the system covered by the preceding sections.

The test data were as follows:

1. Continuous duty
Tube loss at satisfactory operation $=W_0$

2. Intermittent duty
Tube loss at equally satisfactory operation $=W_1$
$T_r=$ 0.5 second
$T=11.1$ seconds
$k=$ 0.0448

3. $\frac{W_1}{W_0}=18.0$

To find T_c substitute in equation 10

$$\frac{W_1}{W_0}=18.0=\frac{1-\epsilon^{-\frac{11.1}{T_c}}}{1-\epsilon^{-0.0448\left(\frac{11.1}{T_c}\right)}}$$

This equation can be solved by trial for $11.1/T_c$. The solution is 0.48 ± 0.005

$$\text{or } T_c=\frac{11.1}{0.48\pm0.005}=23.1\pm0.3 \text{ seconds}$$

Since ignitrons are rectifiers the current conducted by them on a-c circuits is not continuous but consists of a series of half sine waves. The foregoing theory assumes that this intermittent power input can be represented by a continuous function of the time. The accuracy of this assumption can be checked by use of equation 4 where

$$\frac{\theta_2}{\theta_1}=\epsilon^{\frac{T_i}{T_c}}$$

Using the data of the above example and a line frequency of 60 cycles per second

$$\frac{T_i}{T_c}=\frac{\frac{1}{120}}{23.1}=0.00036$$

and therefore

$$\frac{\theta_2}{\theta_1}=1.0004$$

This result indicates that in this case the variation in temperature due to the rectified current pulsation in the tube can be neglected in comparison with the longer period load cycling.

Graphical Calculation and Presentation of Data

Assume that test data are available to establish a rating at a given T and T_r, and that the ratio of the loss in the tube at continuous duty to that at the given duty is known, then the above method will permit the calculation of a value for the thermal time constant, T_c.

The thermal time constant will allow a system of interpolation of the results of the rating tests to other load cycles in a systematic way which is mathematically continuous. The present method, wherein a "maximum averaging time" is used, has the effect of limiting the maximum time of impulse to values less than the maximum averaging time even for demand current loads of only a few per cent higher than continuous load rating. This new method goes over smoothly to the continuous rating.

Graphical Calculation of T_c

The function $\frac{1}{1-\epsilon^{-\frac{kT}{T_c}}}$ is plotted in Figure 2 on logarithmic co-ordinate scales. Our test data give us two values of tube loss (W_1 and W_0) at two values of T_r equal to kT_1 and T_1 respectively, where T_1 is the impulse period of the rating test. It is desired to determine the value of T_c which fixes the scale of the diagram, Figure 2, for the particular tube being rated.

From equation 11 we can write

$$\log_\epsilon\frac{W_1}{W_0}=\log_\epsilon\frac{1}{1-\epsilon^{-\frac{k_1T_1}{T_c}}}-\log_\epsilon\frac{1}{1-\epsilon^{-\frac{T_1}{T_c}}}$$

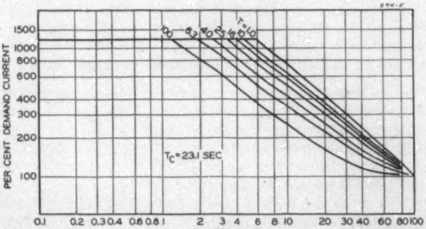

Figure 5. Example family of tube-rating curves as function of impulse time, T_r, and impulse period, T, for thermal time constant, T_c = 23.1 seconds. Calculated from Figures 3 and 4

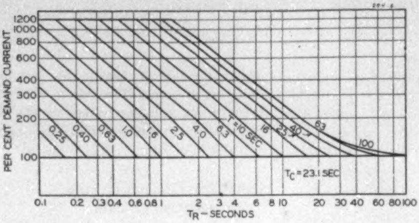

Figure 6. Example family of tube-rating curves as function of per cent duty and impulse period. Calculated from Figure 4

It is seen that the relationship existing between W_1/W_0 and k_1 for given values of T and T_c is in general given by the first term of the above equation, since when T and T_c are fixed, the second term is independent of k_1 and becomes of a constant value which can be subtracted graphically as is done in Figure 2. In order to determine T_c, it is necessary to determine the amount to be subtracted from the ordinate of the curve in Figure 2, in order to make the remainder of the curve fit the conditions as laid down by the test.

This can be done most easily in the following manner. Since the argument of the plotted function is kT/T_c and since the time difference of the test points is defined by k_1, the difference in the abscissa of the test points on the curve at A' and B', when found, must be equal to

$$\log_\epsilon \frac{T_1}{T_c} - \log_\epsilon \frac{k_1 T_1}{T_c} = -\log_\epsilon k_1 = \log_\epsilon \frac{1}{k_1}$$

Therefore, the slope of the chord at $A'B'$ will be

$$\frac{\log_\epsilon \dfrac{W_1}{W_0}}{\log_\epsilon \dfrac{1}{k_1}}$$

A convenient way to lay off this slope is to draw it in a corner of the diagram as shown at AB. In order to fix T_c, it is only necessary to find two points on the curve separated the same distance and at the same slope as the hypotenuse of the triangle AOB. This is done for the example case as shown dotted in the diagram. The lower point at B' thus found on the curve corresponds to log T_1/T_c and since T_1 is fixed by the test conditions T_c can be calculated. This value can be checked by substitution in formula 10.

Graphical Presentation of Rating Data

After T_c is determined, the rating curve for any other impulse period T_2 can be laid out. The point corresponding to $k=1$ will lie at F, distant log T_2/T_c from the vertical axis of the diagram. Lines CF and GF form the axes for a new curve whose abscissa is proportional to log kT_2 and whose ordinates are proportional to log W_2/W_0 as shown on the diagram.

By tracing these curves on Figure 3 the curves shown are obtained. This gives a family of curves showing the permissible ratio of tube heating loss to loss at continuous duty at a number of values as a function of $kT=T_r$. It will be noted that when $T=T_r$ the W/W_0 ratio$=1$. At low values of T the curves are nearly straight lines, but at large values they become curved and are not spaced as far apart as for corresponding lower values of T. A value of T is reached (63 in the curve) where no further advantage is gained by long cooling periods except for low values of demand ($T=100$ in the curve).

The above curve pertains only to actual energy loss in the tube. This can be translated into current values by plotting the corresponding current values as transformed by curve Figure 4, which gives the loss ratio between continuous rating and demand current as a function of the ratio of the corresponding currents.

Curve Figure 5 gives the rating ratio curves as a function of T_r and T for the example value of $T_c=23.1$ seconds. These curves are sharply cut off at the peak current value of 1,200 per cent.

The same information is given in curve Figure 6 in another form. Here, each of the abscissa is in values of k as per cent of the value of T used as parameter on the individual rating curves. This may be compared more directly with the present rating curves where the abscissa is plotted in per cent duty. T here compares with the present "maximum averaging time."

Comparison of Thermal Action of Model and Ignitron

In comparing the model with the actual ignitron, the following considerations are worthy of note:

1. The temperature variation of the model finds no counterpart in any temperature assumed by any part of the ignitron which is susceptible to measurement. The nearest thing might conceivably be the equivalent temperature of the mercury vapor in the tube. Since this cannot be measured, the equations are set up so as not to make a numerical knowledge of any temperature necessary.

2. The criterion is assumed to be that no duty cycle shall impose a higher value of equivalent temperature on the tube than that determined to be safe at continuous operation. This assumption requires considerably more investigation than the authors have been able to do. However, since the action of ionic devices is very rapid, it would seem reasonable that any effect on arc back due to the equivalent temperature could act in such a short time that instantaneous values of this temperature would be critical.

Summary

The above theory suggests a method of defining the intermittent rating of an ignitron tube through the use of a so-called time constant. This method results in a smooth transition from intermittent to continuous duty ratings. It is possible to adapt the time-constant concept to the calculation of permissible ratings for more complicated duty cycles than are considered in the present paper.

The authors hope to be able, in the future, to check the actual performance of tubes against the above theory, especially at values approaching the continuous current rating.

Modern Cathode-Ray Oscillograph for Testing Lightning Arresters

E. J. WADE
ASSOCIATE AIEE

T. J. CARPENTER
ASSOCIATE AIEE

D. D. MacCARTHY
ASSOCIATE AIEE

THIS oscillograph was designed to meet the specialized needs of a group that uses the cathode-ray oscillograph as the principal tool in the study of transients in lightning-arrester development. The instrument is desirable for many purposes, especially those requiring very high writing speeds in connection with nonrepetitive transients where photographic records must be obtained. Other requirements of primary importance in the design were convenience of operation; accuracy of measurement of current, voltage, and time; accuracy of the timing; and linearity and rectilinearity of the plate pattern.

The cathode-ray oscillograph with the film in the vacuum was first described by Dufour,[1] and improvements have been described by numerous investigators such as Rogowski and his associates,[2] Burch and Whelpton,[3] Whipple,[4] Norinder,[5] to mention only a few.

General Description of the Oscillograph

The essential elements of any cathode-ray oscillograph include a cathode and anode operated in a vacuum to provide the electron beam; deflecting means; and a photographic film or fluorescent screen. The electrons from the cathode are accelerated to a high velocity by the cathode voltage, pass through an aperture in the anode, pass between the deflecting plates or coils, and impinge on the film or screen. The oscillograph described in this paper includes these elements and additional elements to make the instrument suitable for recording transients of extremely short duration. A focusing coil increases the intensity of the beam, trapping plates control its duration independently of the cathode voltage, and high-voltage circuits apply the cathode and beam-trapping voltages in the correct split-micro second sequence. An automatic regulator holds the oscillograph pressure at the desired value. The cathode voltage is held constant by a voltage regulator. These features that provide ease of control, fidelity of recording, and high writing speed are described more completely.

A view of the instrument is shown in Figure 1 and a diagram of the high-voltage circuits in Figure 6. The cold cathode is operated at 60 to 80 kv obtained from an impulse generator. The film chamber, deflection tube, and beam-trap tube are metal; both metal and glass cathode tubes have been satisfactory. The deflecting tube contains a pair of sweep plates to move the beam along the time axis and a pair of deflection plates to move the beam in a direction perpendicular to the sweep. The trap, consisting of three pairs of parallel plates, is placed between the anode and focusing coil.

Vacuum System

The vacuum system, Figure 2, was designed to maintain a pressure in the cathode tube sufficiently high to give a copious discharge, while the pressure in the remainder of the oscillograph is low enough to minimize scattering of the beam and fogging of the film.[6] This differential in pressure is provided by a controlled leak from the atmosphere to the cathode tube and by the restriction in flow of air from the cathode tube to the remainder of the oscillograph at the anode hole. The direction of air flow isolates the cathode tube from condensation-pump vapor without the necessity of a trap.

When the oscillograph is pumped down from atmospheric pressure, a rough vacuum is first obtained with the condensation pump by-passed through valve 5; valves 3 and 4 open, and valves 1 and 2 closed. To obtain a higher vacuum, valve 5 is then closed, and valves 6 and 7 are opened by a switch controlling the interlocked solenoids. The condensation-pump heater circuit and valve 5 will be opened and valves 6 and 7 closed by a flow switch if the cooling water for the condensation pump stops.

After the entire oscillograph has been evacuated to a low pressure, valve 3 is closed and valves 1 and 2 are manually adjusted to allow a gradual increase in the cathode-tube pressure. At this time, the vacuum system is switched to automatic operation with valves 3 and 4 controlled by a contact-making vacuum gauge to regulate the rate of leak to the discharge tube. Interlocked solenoids normally hold valve 3 closed and valve 4 open. When the pressure in the discharge tube has increased to the desired value because of the leak, the gauge causes the momentary opening of valve 3 and closing of valve 4, reducing the leak-chamber pressure. The ratio of expansion to leak-chamber volume is about two per cent, and the pressure in the leak chamber is much greater than in the expansion chamber so the rate of leak to the discharge tube is also reduced by about two per cent by the operation of valves 3 and 4. The vacuum gauge continues these automatic adjustments in rate of leak as frequently as is necessary, up to three times per minute, to maintain pressure in the cathode tube within a tolerance of 0.1 micron. Sudden changes in cathode-tube pressure are prevented by the smoothing action of the leak chamber.

Focusing Coil

The focusing coil has a steel shroud and pole pieces to give a short magnetic field of uniform intensity over the aperture to minimize aberration. The focusing current is held constant, independent of variation in line voltage, by a gas regulator tube. The effect of the focusing coil on the beam intensity is shown in Figure 3 by comparing the 5-millimeter width of the 1000-volt calibration line taken with the focusing coil unenergized, with the 0.5-millimeter width of the dense zero line taken with the proper focusing coil current. A fine trace can be obtained with no focusing coil provided the anode hole is very small, but this greatly reduces the writing speed.

Beam Trap

There are three pairs of cross-connected beam-trapping plates, as described by Burch and Whelpton.[3] With perfectly balanced plates, voltages insufficient to trap the beam will not deflect it. Actually it is impossible to adjust the plates perfectly; therefore, the trapping voltage is kept to a minimum during the sweeping time. Voltages in excess of about one kilovolt deflect the beam so that it cannot pass through an aperture located between the two upper pairs of trapping plates. The beam is cut off below the anode except during a relatively short interval adequate for sweeping it across the film, and conse-

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E. J. Wade is research engineer, T. J. Carpenter electrical engineer, and D. D. MacCarthy engineer of the lightning arrester department, all of the General Electric Company, Pittsfield, Mass.

quently fogging of the film is reduced. With impulse excitation of the cathode, a beam trap is not a necessity as with continuous excitation, but it is advantageous to cut the beam off while the cathode discharge is stabilizing and after the beam has been swept across the film.

Cathode-Voltage Regulator

The charge on the capacitors of the impulse generator supplying the cathode voltage is made independent of the supplyline voltage by a regulator of the electronic type. If variation of cathode voltage is permitted, the deflection will vary as is shown in Figure 4 by the two 1,600-volt calibration lines differing by three per cent which were taken with the regulator disconnected. Two zero lines were also taken but coincide since variations in cathode voltage do not affect the position of the zero line.

Elimination of Spurious Deflection

Magnetic fields deflect the beam, but a constant field, such as that of the earth, merely affects the position of the zero axis and is not objectionable. The synchronous switch (see Figure 6) prevents random timing of the oscillograph operation with respect to the periodic variation in magnetic field from nearby 60-cycle apparatus. It prevents errors such as are shown in Figure 5 by the separation of 0.5 millimeters between two zero lines taken with the switch stopped.

The metal deflection tube in this oscillograph prevents errors such as may occur in oscillographs with glass deflection tubes due to charges transferred from the beam to the glass walls.

The deflection and sweep plates were designed and adjusted so that the deflection produced by the two sets is at right angles and is proportional to the voltage.

High-Voltage Circuits and Sequence of Operation

It would be impossible to obtain oscillograms of nonrecurrent transients of short duration without proper timing of the cathode-voltage and sweep circuits of the oscillograph, and synchronization with the transient to be studied.

A push-button control starts a sequence of operations in the high-voltage circuit of Figure 6. These operations include:

1. Charging the cathode generator.
2. Application of voltage to the cathode.
3. Trapping the beam while the cathode discharge stabilizes.
4. Sweeping the beam across the film.
5. Initiation of the impulse generator so that discharge occurs during the sweep.
6. Holding the beam off the film after the sweep is completed.
7. Grounding the high-voltage circuits.

In order to obtain constant sweeping speed, the sweep plates are operated at equal voltages of opposite polarity with respect to ground. For this reason, much of the high-voltage circuit is symmetrical with respect to ground. The operation of this circuit is best explained by considering each step in the order of occurrence.

Cathode-Generator Charging Period

Operation of the control button starts the charging of the cathode generator. The Marx circuit capacitors C_9 and the reservoir capacitor C_1 are charged to 20 kv, negative, and a positive charge of 20 kv is placed on the similar capacitors C_9' and C_1'. Simultaneously, the sweep-supply capacitors C_6 and C_6' in circuit 5 charge to equal voltages of opposite polarity through resistors R_{16} and R_{16}'. A fraction of this voltage, determined by the ratios of R_{14} to R_{15} and R_{14}' to R_{15}' in circuit 4 is applied to the sweep capacitors C_8 and C_8' to bias the cathode beam. Also a fraction of the voltage on the supply capacitor C_6 is applied to the beam-trap plates by the high-resistance potentiometer R_{22}, R_{23}, in circuit 8, compensated by capacitors C_{13}, C_{14} for fast response. Capacitor C_5 in circuit 3 is charged to 40 kv.

Application of Voltage to Cathode

When the capacitors of the cathode-voltage generator have been charged to the proper value of about 20 kv, the volt-

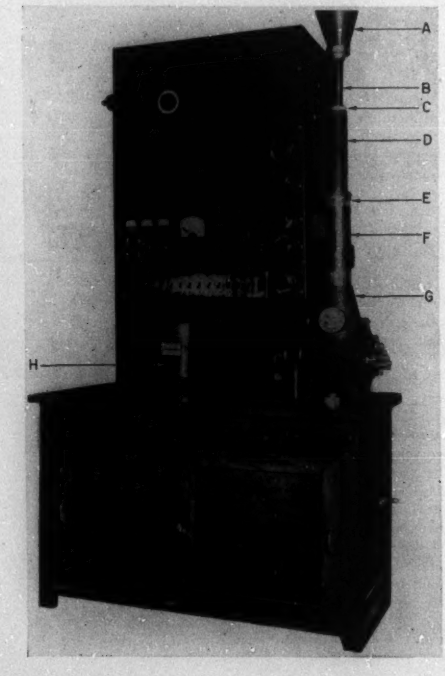


Figure 1. Oscillograph viewed from the front

A—To cathode generator
B—Cathode tube
C—Anode
D—Beam trap
E—Focusing coil
F—Deflection chamber
G—Film chamber
H—Vacuum-regulator gauge

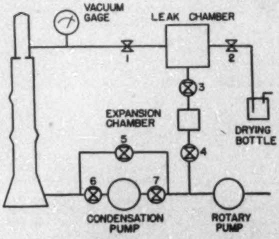


Figure 2 (right). Vacuum control system

Valves 1 and 2 are manually adjusted. Solenoid valves 3 and 4 are controlled by the contact-making vacuum gauge. Solenoid valves 5, 6, and 7 are interlocked

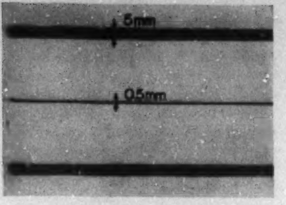


Figure 3. Effect of focusing

The focusing coil was unenergized for the 1,000-volt calibration lines, but was energized for the zero line

age regulator automatically opens the low-voltage charging circuit and releases the moving arms of the synchronous switch and the grounding contactor. The operating time of these switches is long compared with the longest sweeping time of the oscillograph. When the arms of the synchronous switch touch the middle pair of contacts, the cathode generator discharges as soon as the rotating arm reaches the closed position, which times the discharge with respect to the 60-cycle circuits. The gap G_1 discharges, and 80 kv negative is applied to the oscillograph cathode.

Gap G_2 discharges after an interval provided by the charging time of C_2 in circuit 1 for stabilization of the cathode discharge.

Initiation of the Impulse Generator

After G_2 sparks, gap G_3 is delayed by the time required to charge C_3 in circuit 2; the delay can be adjusted by the variable inductance. When G_3 sparks, an initiating impulse travels to the three-electrode gap of the generator[7] supplying the impulse to be measured. These circuits require careful adjustment to time the transient discharge to occur early in the period while the beam is crossing the film, which may be as short as 0.25 microsecond with a fast sweep.

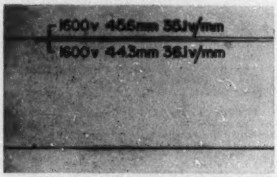

1600v 45.6mm 35.1v/mm
1600v 44.3mm 36.1v/mm

Figure 4. Effect of variation in supply voltage on deflection

Oscillograms taken with cathode-voltage regulator disconnected

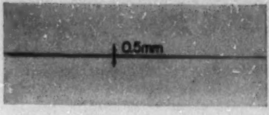


Figure 5. Effect of stray magnetic fields

Two zero lines taken with synchronous switch stopped; separation is caused by difference in instantaneous value of magnetic field from 60-cycle equipment

Removal of Beam-Trap Voltage

The sparking of G_2 applies voltage to the line Z terminated by capacitor C_4 in circuit 3. This voltage disturbs the balance of voltage across the four-electrode gap S_1–S_2–S_3–S_4, which sparks and grounds electrodes S_1 and S_4 simultaneously. The capacitor C_5 discharges through R_{13} and the four-electrode gap, thus insuring low arc resistance and thorough grounding. A four-electrode switching gap, not previously used in such circuits, was necessary rather than the usual three-electrode gap. The two end gaps S_1 and S_4 must operate at equal voltages of opposite polarity to give the necessary symmetry of circuits; another electrode S_2 provides the connection to ground, and hence a fourth initiating electrode S_3 was introduced.

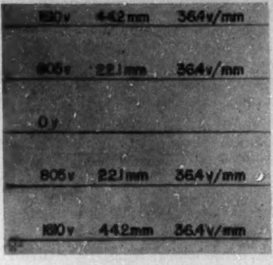

1610v 44.2mm 36.4v/mm
805v 22.1mm 36.4v/mm
0v
805v 22.1mm 36.4v/mm
1610v 44.2mm 36.4v/mm

Figure 7. Consistency of deflection and absence of fogging

Five traces are superimposed on each of 5 lines. Superposition is possible because of the vacuum, cathode-voltage, and calibration-voltage regulators and the synchronous switch

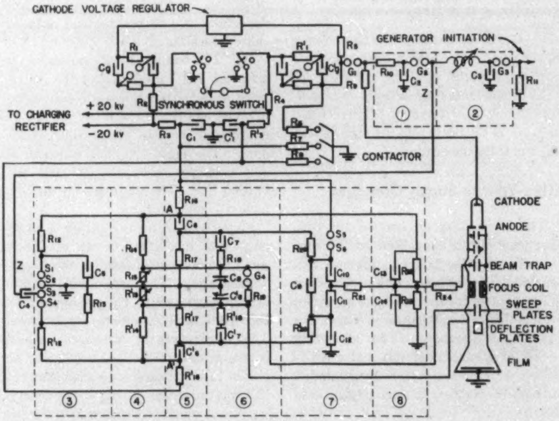
The points A and A' in circuit 5 are grounded by the four-electrode gap. As a result, the beam-trap voltage is removed by the compensated potentiometer in circuit 8, and the beam appears between the deflection plates, but is held at the edge of the film by the charges acquired by C_8 and C_8' during the charging period.

Sweeping the Beam

Grounding of A and A' also puts 20 kv positive on R_{17} in circuit 5 and 20 kv negative on R_{17}' supplied from C_6 and C_6'. As a result, current flows through the blocking capacitors C_7 and C_7', resistors R_{18} and

Figure 6. High-voltage circuits

1—Sweep delay
2—Impulse-generator delay
3—Grounding
4—Deflection-plate bias
5—Supply
6—Sweep
7—Beam cutoff
8—Initial beam hold-off


CATHODE VOLTAGE REGULATOR
GENERATOR INITIATION
TO CHARGING RECTIFIER
+20 kv
-20 kv
SYNCHRONOUS SWITCH
CONTACTOR
CATHODE
ANODE
BEAM TRAP
FOCUS COIL
SWEEP PLATES
DEFLECTION PLATES
FILM
Cg
C'g
R1
R'1
R2
R3
R'3
R4
R5
R6
R7
R8
R9
R10
R11
C1
C'1
C2
C3
G1
G2
G3
G4
Z
A
A'
S1
S2
S3
S4
S5
S6
C4
C5
C6
C'6
C7
C'7
C8
C'8
C9
C10
C11
C12
C13
C14
R12
R'12
R13
R14
R'14
R15
R'15
R16
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R'17
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R'18
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R21
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R24
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②
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⑧

R_{18}', and into sweep capacitors C_8 and C_8', which supply balanced positive and negative voltages to the deflection plates. This current reverses the sweep-plate bias voltage so that the beam is swept across the film. The plate voltage is limited by sparkover of G_4 after which R_{19} maintains sufficient voltage to hold the beam off the film.

Beam Cut-off

Voltage on R_{17} and R_{17}' also causes current to flow through R_{20}, C_9, R_{20}' in circuit 7 which builds up a positive voltage on S_6, while S_5 is held at 20 kv, negative, by the reservoir capacitor C_1. During the sweep when C_9 was charging, the voltage on the beam trap was unchanged, because circuit 7 is symmetrical and balanced to ground since C_{12} compensates for the capacitance of the gap S_5–S_6. After a time delay, the gap S_5–S_6 sparks, applying voltage from C_1 that unbalances the voltages on capacitors C_{10} and C_{11}. A fraction of this voltage appears on the beam trap through R_{21} and R_{34} and traps the beam.

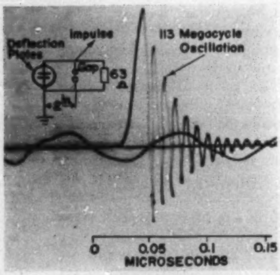

Figure 8. High writing speed

Voltage oscillation in a small circuit following gap breakdown. Timing wave is 10 megacycles; sweeping speed is 44 centimeters per microsecond and the maximum writing speed is 3,000 centimeters per microsecond

High-Voltage Supply Grounded

The moving-contact arms of the synchronous switch have been falling continually since the holding contactor was tripped by the cathode-voltage regulator. When contact is made with the grounded stops, the cathode-generator capacitors C_g, C_g' are grounded. The grounding contactor was released also and when it closes, the other capacitors are grounded so that the operator can make adjustments in safety.

Operating Controls

Oscillator timing waves are provided by five pretuned circuits covering the range of 0.1 to 10 megacycles per second. Sweeping speeds which cross the film in 0.25 to 200 microseconds may be selected by means of a tap switch varying C_8 and C_8'. The maximum convenient sweeping speed is about 45 centimeters per microsecond. Special deflecting-plate switches change the circuits for recording the several functions such as the timing wave, calibration voltage, and impulse voltage or current. In operation, the desired conditions are set up by means of those switches, and the sequence of operations of the circuits to make the exposure is initiated by a single push button.

Performance of the Oscillograph

Several factors have been discussed that make the oscillograph free from spurious deflection. Figure 7 shows a zero line and deflections due to positive and negative calibration voltages of 805 volts and 1,610 volts. This oscillogram demonstrates the accuracy and consistency of the instrument, since five separate traces were superimposed on each line without causing an appreciable increase in the width of the lines. The sensitivity was constant at 36.4 volts per millimeter for each calibration line. The 25 traces on the film have not caused fogging.

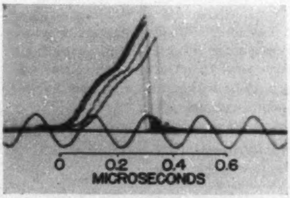

Figure 9. The consistent timing of oscillograph and generator is illustrated by 5 volt-time oscillograms of gap breakdown on one film. The variation in timing is 0.08 microsecond

Figure 8 shows an oscillogram of a 113-megacycle oscillation in an extremely small local circuit following the breakdown of a gap. The beam crossed the film in 0.25 microsecond. The maximum writing speed on this oscillogram is about 3,000 centimeters per microsecond, which is ten per cent of the speed of light. To obtain this oscillogram, a rectangular circuit was connected directly to the deflecting plates, which provided most of the circuit capacity. The opposite side of the circuit contained a small gap that was sparked over by the impulse generator to start the oscillation. A resistor was connected in parallel with the gap to prevent deflection by voltage induced in the plate circuit previous to gap breakdown. Dividers are necessary for the measurement of high voltages and will limit the accuracy with which ultrafast transients can be recorded, rather than the writing or sweeping speeds of the oscillograph.

Comparison With Hot-Cathode Oscillograph Having Permanently Sealed Vacuum

The sealed-in high-speed oscillograph with the film outside the vacuum, on which improvements also have been reported[8,9] within recent years, occupies the position of an every-day general-purpose instrument for transient work. It is simpler in construction and operation and capably records all but the ultrafast transients for which the instrument described in this paper is especially valuable.

Conclusions

The oscillograph described should be regarded as a precision research tool capable of recording ultrafast phenomena. Due to the complexity of the circuits, the operator must use considerable care and skill to realize the best performance. The authors believe it represents marked progress in the design of ultrahigh-speed oscillographs for the measurements of transients and that several features add definitely to performance characteristics hitherto attained. During the two years in which this instrument has been in constant use, the accuracy, consistency, high writing speed, and convenience of operation have been of great benefit in the study of lightning arresters and problems of protecting insulation.

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6. COLD-CATHODE CATHODE-RAY OSCILLOGRAPH

Application of Apparatus and Conductors Under Various Ambient-Temperature Conditions

R. E. HELLMUND
FELLOW AIEE (Deceased)

P. H. McAULEY
ASSOCIATE AIEE

Synopsis: Various electrical standards are reviewed from the viewpoint of the selection and interpretation of ambient temperature values. Equivalent aging temperatures for insulation have been calculated from recorded ambient temperature data for several typical outdoor and indoor locations. From these the suitability of ambient values now used for the establishment of temperature rises for rating purposes is discussed. These values seem to be well chosen, but some clarification in meaning and in methods of using them for rating and application purposes is desirable. For apparatus rated on a 40 degrees centigrade ambient basis, appreciable margins in permissible temperature rise exist under many conditions and for many places of application. Permissible increases in loading for certain motors without exceeding conservative hot-spot temperatures are suggested, subject to the limitations imposed by other operating considerations.

Standards for Electrical Apparatus

THE permissible temperature rises for rating purposes given in various commercial standards for apparatus and conductors were obtained by considering the maximum continuous temperatures for insulation and selecting certain values of ambient temperature. The ambient values most commonly used are:

40 degrees centigrade—For commercial standards for the majority of all electrical apparatus and for some cable-rating purposes.

30 degrees centigrade—For commercial standards for wires and cables and, with qualifying statements, for the newly proposed standard for transformers.

25 degrees centigrade—For standards for rotating apparatus on electrical vehicles and in some Underwriters' regulations.

50 degrees centigrade—For standards for marine apparatus for which 40 degrees centigrade is not considered adequate.

Generally speaking, these values are well chosen for the various cases indicated, but there is some inconsistency between the wording of the standards and the methods and generally accepted theory of applying apparatus. Some standards simply specify that apparatus will have the proper rating when the temperature of the cooling medium does not exceed 40 degrees centigrade. In at least one case the statement "at no time exceeds 40 degrees centigrade" is used. In others 40 degrees centigrade is specified as the limiting ambient temperature of the cooling air or other gases, with a supplementary statement that the ambient temperature in locations where electrical apparatus is operated in the United States rarely exceeds 40 degrees centigrade. There is a general impression that 40 degrees centigrade is exceeded in only a few locations, but this is not confirmed by actual records. Weather bureau statistics show that outdoor temperatures have at times exceeded 40 degrees centigrade in 30 out of 60 stations.[3] Thus, in indoor applications, where considerable heat is generated by losses in large electrical apparatus, heat engines, steam pipes, and industrial heating equipment, 40 degrees centigrade will at times be exceeded in a large number of applications. However, the total percentage of time during which 40 degrees centigrade is exceeded is as a rule very low, not more than 0.5 per cent for most outdoor locations and a somewhat higher percentage for some indoor locations. Nevertheless, with a strict interpretation of the statement that the cooling medium must not exceed 40 degrees centigrade, a large percentage of applications would have to be considered as special. In actual practice, standard apparatus having a temperature-rise rating based on a 40 degrees centigrade ambient is actually applied more or less indiscriminately with success in all but unusually hot locations. This is to be expected in the light of various investigations on the life of insulation, which have shown conclusively that life or rate of deterioration is dependent upon both time and temperature.

Since the temperature-rise rating presumes continuous operation at the standard ambient temperature, it may be expected that satisfactory life of insulation at rated load will be obtained when this ambient temperature is maintained continuously. If this is true, it follows directly from these investigations on the life of insulation that equal life will be obtained in spite of short periods at temperatures above 40 degrees centigrade, if values below 40 degrees prevail for the greater part of the time. This is the condition found nearly everywhere in practice; ambient temperatures usually vary over wide ranges and include values below 40 degrees centigrade for most of the time.

On the basis that a continuous ambient is used for rating purposes, it is merely necessary to make sure that the prevailing ambient temperatures, which vary continually in practice, are equivalent in their aging effects to this standard value. This will result in the most effective application of apparatus. A method of determining the aging temperature equivalent to any given varying temperature is available in the eight degrees centigrade rule,[1,2] which states that the aging effect of temperature doubles for each eight degrees increase in temperature. This eight degrees centigrade rule is not a precise law and cannot be applied indiscriminately for all insulation, but it does serve as a useful guide for analytical purposes.

Equivalent Aging Temperatures

The use of the eight degrees centigrade rule is illustrated in Figure 1. Curve *A* was obtained from available outdoor temperature records for Dallas, Tex. It shows the percentage of time during which certain temperature values were exceeded. To obtain corresponding machine temperatures, the hot-spot temperature rise

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R. E. HELLMUND was chief engineer of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., at the time of his death, May 1942. P. H. MCAULEY is in the engineering laboratories and standards department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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of 65 degrees centigrade permissible for many forms of class-*A* insulation is added to curve *A*, giving curve *B*. By using the eight degrees centigrade rule, it is possible to calculate a constant temperature *D* (appendix II) which has the same effect in deteriorating insulation as curve *B*. By subtracting 65 degrees centigrade from *D*, the value *C* is obtained. This is a single value equivalent to the ambient temperature curve *A*. The validity of this operation depends, of course, on the empirical eight degrees centigrade rule, but its application is conservative. It is assumed that the insulation temperature follows changes in ambient temperature exactly. Actually, insulation temperatures lag and the effect of the higher ambient temperatures is somewhat less than curve *B* indicates.

Dallas is one of the hottest places in the country. Although the maximum temperature exceeds 40 degrees centigrade, the equivalent yearly outdoor temperature is only 22 degrees. Figure 2 shows curves and values for other locations and the equivalent temperatures are from 12 to 22 degrees centigrade. Therefore, outdoor apparatus can be safely applied almost anywhere if the standard temperature rises are based on an ambient of 25 degrees centigrade. However, a further assumption that heat from sun radiation can be neglected is necessary. It has been shown that this assumption is correct in most cases, because wind velocities nearly always existing outdoors, though rather low, compensate for sun radiation. There are a few exceptions to this, such as meters or transformers mounted in well-protected corners of buildings and subject to considerable sun exposure, the influence of which may then at times be appreciable.

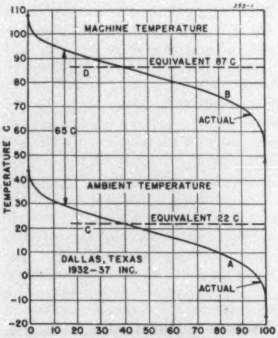

Figure 1. Actual and equivalent ambient and machine temperatures for outdoor locations in Dallas, Tex., 1932–37 inclusive

Most types of electrical apparatus are at times located indoors and the consideration of outdoor temperatures is in itself of restricted value. However, it is useful as a basis for the study of indoor temperatures for which no comprehensive statistics are available or likely to be available in the near future. Therefore, a logical method is to use outdoor statistics and make tests on the differences between outdoor and indoor temperatures for a limited number of typical indoor locations.

In buildings heated for human comfort, a more or less constant temperature is maintained during the cooler seasons of the year. A constant value of 25 degrees centigrade represents the higher limit of temperatures prevailing during the heating period. At other times it might be assumed that the indoor temperature can be maintained the same as the outdoor temperature in a substantial ideally ventilated building. In practice there are, however, a number of factors tending to increase indoor temperatures even when no appreciable amounts of heat are generated inside. These factors are: heat from occupants and lighting fixtures, the poor heat exchange generally given by open windows, the fact that windows are kept closed a part or all of the time, and, finally, heat caused by sun radiation.

Figure 3 shows simultaneous outdoor and indoor records taken in an office in a substantially built modern office building having northwestern exposure with sun radiation through the windows only after 5 p.m. At the end of the office hours, about 5 p.m., the windows are closed and night temperatures are appreciably higher indoors. During the second day shown it was cloudy after 5 o'clock, and the maximum temperature was only slightly higher indoors. In contrast, the marked influence of the late afternoon sun appeared during the third day, resulting in a difference of 5.6 degrees centigrade (ten degrees Fahrenheit) at 6:30 p.m. Similarly the influence of sun exposure with closed windows, as may be found in unoccupied storerooms and similar places with southern exposure, can be appreciable. The high peak on the first day indicated in Figure 3 was caused by the recording instrument being directly exposed to the sun. The effect is quite marked, although the instrument case is light in color and ventilated. If apparatus with dark heat-absorbing surfaces is subjected to sun radiation through windows, the effect may be quite marked because of the lack of compensating outdoor breezes.

Similar records have been obtained in a powerhouse basement and a factory building. The powerhouse basement was without windows and poorly ventilated, but the recorder was at least ten feet from direct heat sources such as steam pipes. The factory building was a single-story structure, with a covering of boards plus sheet iron on the walls and paper on the roof. Several small ovens operating at temperatures up to 175 degrees centigrade were located in the vicinity of the thermometer. It was placed near the south windows as well, which resulted in considerable influence from sun radiation.

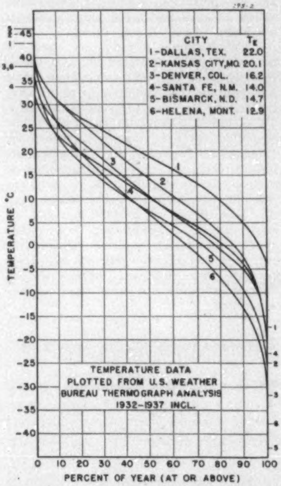

Figure 2. Actual and equivalent outdoor ambient temperatures for six cities of the United States

From these tests the curves of Figure 4 have been estimated for certain indoor conditions and the equivalent ambient temperatures calculated. The tests covered only a 100-day period in midsummer, but the data have been adjusted to an annual basis compared to the outdoor curve for the period of test and then applied to the Dallas outdoor curve. A constant indoor temperature of 25 degrees centigrade has been assumed when lower temperatures prevailed outdoors. Figure 5 shows the same procedure applied to the outdoor curve above 25 degrees centigrade for Bismarck, N. D. This is one of the hotter locations in the northern section of the United States, as divided in Figure 2 of AIEE Standard No. 1.[3] Although maximum temperatures are high, they do not persist as in more southern climates, and

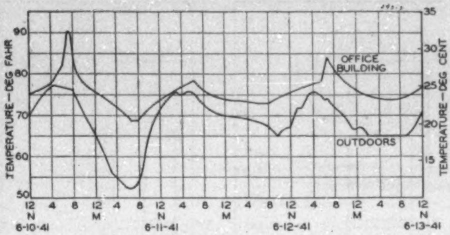

Figure 3. Recorded temperatures outdoors and in a modern office building at East Pittsburgh

equivalent aging temperatures are correspondingly lower than in Dallas. The equivalent aging temperatures for these selected locations in Dallas and Bismarck lie between 25 and 32 degrees centigrade for the most part, with the Dallas powerhouse 36.5 degrees centigrade. In the latter case, even with a 25 degrees centigrade minimum temperature, 30 degrees exceeded most of the time, and 40 degrees exceeded 20 per cent of the time, the equivalent temperature is still well within the 40-degree standard. In practice all sorts of variations will be encountered in individual applications, but for either locality it seems that, regardless of high maximum temperatures, equivalent aging temperatures as high as 40 degrees centigrade are likely to be found only under very unusual circumstances. Since Dallas is one of the hotter places in the country this statement may be applied to apparatus locations in buildings generally.

Figure 5 seems to indicate that, except with a great deal of heat generated and poor ventilation, the equivalent aging temperature will rarely exceed 27 degrees centigrade for large sections of the country. This applies particularly to many highly industrialized sections of the United States. Moreover, the 27 degrees centigrade value is based on the assumption that indoor winter temperatures are 25 degrees. Actually, the temperatures maintained in industrial establishments in winter are nearer 20 degrees centigrade, which results in equivalent aging temperatures for the year certainly no greater than 25 degrees.

Enclosures and Buildings

The inside temperatures of enclosures provided for the specific purpose of housing electrical apparatus are usually higher than the outside. In relatively small enclosures with restricted ventilation, such as steel cubicles for switch-gear, a distinction is made between inside and outside ambient temperatures. Extreme differences have of course been encountered when the enclosure had no ventilation. These conditions require special consideration in each case. On the other hand, when apparatus is enclosed in a regular building, the temperature in the building is considered the ambient temperature for the apparatus.

In this connection some interesting tests have been reported on an indoor transformer installation.[4] A ventilated building about 18 by 25 feet and 24 feet high housed three power transformers. In Figure 6 the heaviest lines represent the outline of the building, with cooling air flow indicated by the arrows. Figure 6a shows isothermal lines midway between two transformers, and Figure 6b shows the isothermal lines for a section through the center line of a transformer. The numbers given on the lines indicate the difference between outside and inside temperatures. The average temperature in the building is about eight degrees centigrade above the outdoor temperature. In the case of Dallas this would give an equivalent temperature of about 30 degrees centigrade. However, the lines above the transformer show an excess of 11 to 15 degrees centigrade over outside temperature. Applied to the Dallas equivalent temperature of 22 degrees centigrade, this gives ambient values of 33 to 37 degrees. If auxiliary apparatus were installed above the transformers, it would be operating under these ambient conditions. This conclusion is made under the assumption that the building is fully ventilated at all times; however, the ventilation may be restricted in cooler seasons of the year, resulting in equivalent ambient temperatures of 35 to 40 degrees centigrade at points above the transformers. It is believed that this general situation is frequently found in buildings housing electrical apparatus.

A somewhat different case is an industrial building subject to considerable sun radiation or other heat sources. Temperatures in the upper regions of the building, where some types of apparatus may be installed, may be appreciably higher than in the lower parts where temperatures usually are measured. Although some of the previous cases derived from tests do not show equivalent ambient temperatures in excess of 35 degrees centigrade, equivalent temperatures close to 40 degrees may be expected in some parts of many buildings. The short-time maximum temperature in such locations is likely to be 50 to 60 degrees centigrade.

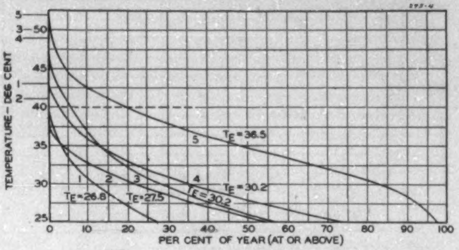

Figure 4. Estimated prevailing and equivalent ambient temperatures for typical locations in Dallas, Tex.

1. Outdoors 1932–37 inclusive—above 25 degrees centigrade
2. Office building—northern exposure—open windows daytimes
3. Office building—southern exposure—closed windows
4. Building with industrial heat sources—thin walls
5. Powerhouse—poorly ventilated area

Preferred Standard Ambient Temperatures

From these studies, the suitability of the ambient temperature values used for temperature-rise standardization purposes can be considered. For maximum simplicity in the application of apparatus, the ambient temperature of 40 degrees centigrade has been well chosen. The temperature conditions for the very small percentage of cases where apparatus so standardized cannot be safely applied are so unusual that they will be readily recognized. (An equivalent temperature of 40 degrees centigrade usually permits occasional maximum values of 50 to 60 degrees; consequently, misapplications are easily avoided.)

On the other hand, it might be contended that 40 degrees centigrade is contrary to maximum economy, because in a large percentage of applications equivalent ambient values of 30 to 35 degrees are not exceeded. The question of what would be gained by using 35 degrees centigrade as a

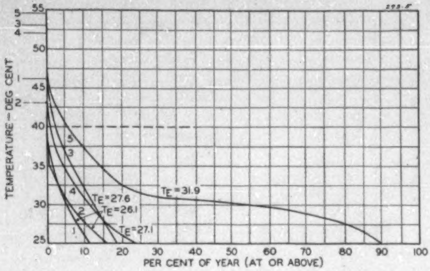

Figure 5. Estimated prevailing and equivalent ambient temperatures for typical locations in Bismarck, N. D.

1. Outdoors 1932–37 inclusive—above 25 degrees centigrade
2. Office building—northern exposure—open windows daytimes
3. Office building—southern exposure—closed windows
4. Building with industrial heat sources—thin walls
5. Powerhouse—Poorly ventilated area

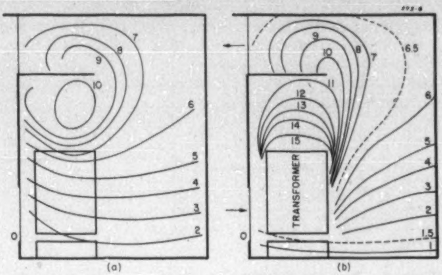

Figure 6. Isothermal lines around a large transformer in a ventilated enclosure

(*a*) Between two transformer units
(*b*) About the center line of one unit

basic ambient value might therefore arise. It would permit hot-spot temperature rises[3] of 70 instead of 65 degrees centigrade for class-*A* insulation, and 95 instead of 90 degrees for class-*B* insulation, gains of 7.7 and 5.6 per cent respectively. With apparatus having chiefly copper losses, this means increases in rating of only 3.9 and 2.8 per cent respectively; with apparatus having both iron and copper losses, gains of 7 and 5 per cent might be realized.

For these gains it seems wrong to sacrifice the present satisfactory situation which permits almost indiscriminate application of standard apparatus drawn from storerooms all over the country. A great deal of this apparatus has line-voltage magnet coils, and adjustment of the load to suit special ambient temperature conditions is not possible; the same is true with motors permanently built into or connected to driven machines. In these cases special apparatus would be required, if the temperature rating of standard apparatus were not based on a sufficiently high and universally applicable ambient temperature. The cost probably would more than balance any economic gain from a lower ambient as a basis for standard apparatus. It is of course possible to load certain machines somewhat below their normal rating, if the ambient temperature is higher than that used as a basis for the rating; however, it is just as easy to apply a machine to a somewhat higher load, if its rating is based on 40 degrees centigrade, and it is used in a locality with a lower equivalent ambient value.

Considering further that there are certain inaccuracies in the eight degrees centigrade rule used as a basis for this study, and that there is usually not much known about the temperature conditions for many applications, it certainly seems advisable to retain the margin of safety and the simplicity afforded by the use of 40 degrees centigrade as an ambient value for general purposes. A further reason for adhering to this value is that frequently apparatus is installed under conditions which interfere with proper ventilation.

In Figure 7, curve *A* has been estimated to show the percentage of all apparatus that can be applied without special consideration for temperature-rise ratings based on different standard ambient temperatures. In the absence of extensive statistical data no claims can be made for the accuracy of the curve. It does illustrate the basic considerations involved and the greater applicability of stock apparatus, the higher the standard ambient temperature on which ratings are based.

In spite of the advantage of the 40 degrees centigrade value for general use, it must be admitted that the use of lower values such as 30, or even 25 degrees, is justified where some conditions of application or economic considerations apply. One such case is rubber-insulated wire, some varieties of which are suitable for a continuous maximum temperature of only 50 degrees centigrade. The permissible rise is 20 degrees centigrade with an ambient temperature of 30 degrees, and only 10 degrees with an ambient of 40 degrees; in other words, the increase of rating possible with 30 degrees is 41 per cent. The greater part of this wire is used in places where an equivalent temperature of 30 degrees centigrade is not exceeded. This is roughly illustrated by curve *B* of Figure 7. (The eight degree centigrade rule may not apply to rubber-insulated wire, and curve *B* may not be quantitatively correct.) It follows that the sacrifice in economy resulting from an ambient of 40 degrees centigrade for this wire is entirely too great for the advantages gained thereby. This is especially true, because in the few locations where higher temperatures prevail, standard wire can be used merely by reducing the rating. Hence, the storeroom problem, which would complicate the situation in connection with many other types of apparatus, is of no particular importance. On the whole, it seems that ratings for rubber-covered wire based on a standard ambient of 30 degrees centigrade are well justified.

Another case of interest is that of transformers. Curve *C* has been estimated as applying to transformers and shows approximately the percentage of transformers which can be applied on a 30-degree centigrade basis; this percentage is high because most transformers are located outside. A rather small percentage of 30 degrees centigrade transformers require special consideration in their application. Also, since transformers are nearly always applied by engineers fully familiar with all the problems involved, ratings based on a 30 degrees centigrade ambient seem well justified. (The proposed transformer standards specify that nominal rating can be used if the average temperature during any 24 hours does not exceed 30 degrees centigrade, with a maximum of 40 degrees, which gives a continuous hot-spot temperature of 95 degrees. This provision in itself will result in an equivalent value somewhat in excess of 30 degrees centigrade for the 24-hour period, as here developed. However, since there will be only a few days during the year with such high temperatures, it is estimated that the provisions in the transformer standards are about the same as an equivalent temperature of 30 degrees centigrade or less.)

Curve *D* applies to rotating apparatus for electric vehicles. In view of the necessity to keep weight to a minimum, and because such apparatus is nearly always outdoors, the present practice of a 25 degree centigrade standard ambient is justified. For the sake of maximum simplicity in the standard structure, a value of 30 degrees centigrade for railway work seems preferable and would hardly interfere with maximum economy. The five degree centigrade difference represents a very small percentage of the customary temperature rises in this type of apparatus.

There are other cases where a standard ambient temperature of 30 degrees centigrade would be justified. For example, some domestic or office appliances are used almost exclusively in locations where an equivalent temperature of 30 degrees centigrade is not exceeded while the devices are in use. There are practically no homes or offices in which the equivalent temperature exceeds 30 degrees centigrade while occupied. Even if higher temperatures prevail when windows are closed, this is of no particular importance because the devices are not then loaded and will not overheat.

Therefore, considering both simplicity and economy, it seems well to retain the 40 degrees centigrade value but to recognize that a 30-degree value is justified for some apparatus and applications. It will of course also be necessary to use the 50 degree centigrade value for those few locations, such as holds of ships, where 40 degrees is not sufficient. An even higher standard ambient value may be necessary in special cases.

The previous considerations all are based on the deterioration of the conventional insulating materials, with the idea of assuring satisfactory life of apparatus from that point of view. The influence of temperature conditions on the oxidation and operation of contact surfaces or upon the operation of electronic devices and other types of apparatus may, of course, be subject to different laws and thus require separate consideration. Very few quantitative data seem to be available for such studies, but AIEE standards coordinating committee 7 is working on these problems. Regardless of the results, it would seem best to use the same basic ambient values for standardization purposes, with the understanding that satisfactory results are obtained if these ambient temperatures are maintained continuously. Any difference between the characteristics of other types of apparatus and ordinary electromagnetic apparatus can be taken care of in the methods of application.

Conclusions

Ambient temperature values equivalent to the continually changing temperatures encountered in practice have been derived for various localities. From these data it is concluded that the standard ambient temperature values selected for use in establishing temperature rises for rating purposes in the various standards are, in general, sound. It is suggested, however, that some clarification in meaning and in methods of using these selected values for rating purposes is desirable.

The data also show that for apparatus rated on a continuous 40 degree centigrade ambient basis, certain margins in temperature rise exist under many conditions and for many places of application. Recognition of this situation is particularly valuable at this time, when it is often necessary to increase loads on industrial equipment. Under these rating conditions an equivalent ambient temperature of 25 degrees centigrade may permit overloads which increase operating temperature-rises by 15 degrees without exceeding permissible hot-spot temperatures, when this is compatible with other operating considerations.

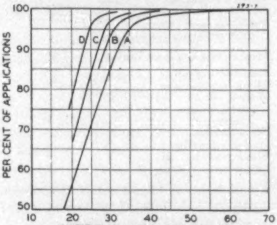

Figure 7. Estimated curves for different apparatus applications, showing the percentage of applications for which standard apparatus based on different ambient temperatures is suitable

A. General
B. Cables
C. Transformers
D. Railway apparatus

Apparatus ratings primarily are based on a continuous hot-spot temperature of 105 degrees centigrade for class-*A* insulation, although transformer standards specify 95 degrees. At times doubt has been expressed that the 105 degrees centigrade temperature will give satisfactory life of insulation, since laboratory tests have demonstrated loss in mechanical strength of materials continuously subjected to 105 degrees. On the other hand, hot spots in many machines occur in slots where the insulation is well supported and where some loss of mechanical strength can be tolerated. Extensive trials on actual machines at 105 degrees centigrade for long periods have shown very satisfactory life.

To eliminate this doubtful point, it can be assumed that the equivalent hot-spot temperature should not exceed 100 degrees centigrade. This takes advantage of only ten of the 15-degree margin and gives a permissible temperature-rise of 75 instead of 65 degrees, or an increase of 15.4 per cent. For general-purpose motors based on a 40 degree centigrade temperature-rise by thermometer, an extra ten degrees is available, permitting an increase in this case of 36.5 per cent. (Actually, the service factor for these machines requires the ability to carry a 15 per-cent overload with a 40 degree centigrade ambient maintained continuously.) Table I shows permissible increases in loads for motors, considering temperature only and taking advantage of these temperature-rise increases of 36.5 and 15.4 per cent. With the total losses increased by these percentages, permissible increases in copper losses are given in columns 3 and 5. Columns 4 and 6 show the increases in currents corresponding to these copper losses.

Table I. Permissible Per-Cent Increases in Motor Losses and Currents for 100C Hot-Spot Temperatures at 25C Ambient

Assumed Loss Distribution		General Purpose 40C Rise by Thermometer		Ratings Based on 50C Rise by Thermometer	
Iron	Copper	Copper Loss	Current	Copper Loss	Current
0.25	0.75	49	22	20.5	10
0.35	0.65	56	25	25.5	12
0.50	0.50	73	31.5	31	14.5

Since hot-spot temperatures are not directly proportional to total losses but depend also upon distribution of losses between copper and iron, these figures may be somewhat optimistic. It does appear reasonably safe, however, to increase currents on 50-degree-rise machines by 8 to 13 per cent and on general-purpose 40-degree-rise machines by 20 to 30 per cent under the conditions stated. For induction motors torque usually increases somewhat faster than the current. It is assumed, of course, that operation is at rated voltage. If operation is not at rated voltage, part of the margin indicated may not be available for increased loads.

Furthermore, this paper deals only with the effect of temperature on the aging of electrical insulation. There are other factors that should be considered when contemplating placing an overload upon a rotating machine.[5] Mechanical strength of shafts, bearings and couplings, commutators, lubrication, and so forth, must be recognized as limiting factors also and should receive due consideration. Furthermore, utilizing these temperature margins may reduce insulation life from that commonly obtained under operation at substandard temperatures.

Unfortunately, simple conclusions covering all conditions encountered in practice are not possible, but it is hoped the data presented will be of assistance in determining increased loads for existing machines and apparatus in this time of emergency. Also, the statistical data of AIEE Standard No. 1 should be helpful in determining the localities where prevailing ambient temperatures might make increased loading of equipment possible.

Although the data given here are primarily intended to be helpful under present emergency conditions, it is also hoped that the views presented will be convincing evidence of the futility of suggestions for the use of a great variety of ambient temperature values and departures from standard temperature-rises in commercial transactions. Certain variations in ambient temperatures always will occur but wide confusion would result from attempts to reflect these into the system of rating apparatus.

Appendix I. Ambient Temperature Specifications in Standards

(The wording referring to ambient temperature in the different standards is far from uniform. The letters in these data indicate certain general classifications but do not cover all the variations in wording even where no distinctions in meaning are intended. Furthermore, where apparatus may be water-cooled, supplementary statements are given.)

	50C	40C	30C	25C	24C
AIEE AND AMERICAN STANDARDS ASSOCIATION					
Metal-tank mercury-arc rectifiers		A			
Industrial control apparatus		B			
Electric-railway control apparatus		B			
Capacitors		B			
Apparatus bushings		C			
Air switches and bus supports		B			
Relays associated with power switchgear		A			
Protector tubes		A			
Fuses above 600 volts		B			
Automatic stations		B			
Switchboards and switching equipment		B			
Switchgear assemblies		A			
Lightning arresters		A			
Electric-arc-welding apparatus		B			
Resistance welding apparatus		B			
Electrical installations on shipboard	A	—			
Railway motors		—	—	B	
A-c power circuit breakers		B			
Rotating electrical machinery		B			
Transformers, regulators, and reactors		D			
NATIONAL ELECTRICAL MANUFACTURERS' ASSOCIATION					
Industrial control		B			
Motors and generators		A			
Oil circuit breakers		E			
Signalling transformers		—	—	—	F
General-purpose specialty transformers		E			
UNDERWRITERS LABORATORIES					
Industrial control		A			
Motor-operated fans		—	—	C	
Branch circuit and service circuit breakers		—	—	H	
Service cables		—	—	—	F
Auxiliary breakers					I
Motor-operated appliances					G
INSULATED POWER CABLE ENGINEERS ASSOCIATION					
Cables		A			
NATIONAL ELECTRICAL CODE					
Conductors		—	J		

GENERAL CLASSIFICATION

A. Equipment conforming with these standards shall be suitable for carrying the rated load when and where the ambient air temperature at the equipment does not exceed ——C.

B. Apparatus conforming with these standards shall be suitable for operation at the standard rating when and where the temperature of the cooling medium does not exceed ——C.

C. Apparatus bushings conforming to these standards shall be suitable for operation at their standard ratings when and where the temperature of the external cooling medium does not exceed 40C maximum or 30C average for any 24-hour period.

D. Apparatus conforming to these standards shall be suitable for operation at rated load with rated secondary voltage, provided the temperature of the cooling air at no time exceeds 40C, and the average temperature of the cooling air during any 24-hour period does not exceed 30C.

E. The standard ambient temperature of reference when the cooling medium is air shall be 40C.

F. The standard ambient temperature shall be 24C.

G. It is assumed that the ambient temperature will be ordinarily about ——C.

H. The temperatures given in these requirements are based on a room temperature of 25C.

I. The tests shall be conducted with the switch at room temperature, preferably 24C.

J. Capacities are based on room temperature of 30C.

Appendix II. Calculation of Equivalent Aging Temperature by Eight Degree Centigrade Rule

The continuous temperature equivalent to a given temperature distribution curve for a period of time is calculated by dividing the curve into convenient trapezoids or rectangles and applying equations 1 and 2.

$$A = t_0 \frac{(e^{KT_2} - e^{KT_1})}{K(T_2 - T_1)} \text{ for a trapezoid} \qquad (1)$$

$$A = t_0 e^{KT} \text{ for a rectangle} \qquad (2)$$

A = aging units

t_0 = time in convenient units (hours or per cent)

$e = 2.718$ $\qquad K = 0.0865$

T_2 = maximum temperature of trapezoid in degrees centigrade

T_1 = minimum temperature of trapezoid in degrees centigrade

T = constant temperature of rectangle in degrees centigrade

The aging units for all the sections of the curve are added, and equation 2 is applied to this sum for the total time involved. Solving for T gives the equivalent aging temperature for the curve.

With $K = 0.0865$ the aging units are doubled for an eight degrees centigrade increase in temperature.

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Effect of Lightning on Thin Metal Surfaces

K. B. McEACHRON
FELLOW AIEE

J. H. HAGENGUTH
ASSOCIATE AIEE

Synopsis: In the search for means for measuring the properties of natural lightning much can be learned from the evidence left at points struck by lightning. This paper deals principally with such evidence and the process of evaluating the characteristics of the lightning strokes responsible for the evidence.

For six years a nickel-plated, 18-inch copper sphere, 878 feet above the ground atop the WSM radiator at Nashville, had been collecting data in the form of holes and pits due to lightning strokes to the sphere. A total of 150 holes of varying sizes were found together with 300 pits. A laboratory setup was made consisting of the high-capacity impulse generator together with a d-c generator so arranged that the known characteristics of lightning with respect to the so-called continuing current could be duplicated. With this equipment holes having the same appearance could be produced in copper sheets, and from the results of the test a calibration curve was produced. At the same time calibration curves were obtained for other metals in addition to copper. From these data an expectancy curve was obtained between coulombs and per cent of holes. The average hole corresponded to 15 coulombs while the maximum size hole corresponded to 240 coulombs.

No satisfactory calibration was obtained for the pits, many of which no doubt were the result of high current peaks having a low coulomb content.

It is interesting to learn that of the 150 holes in the sphere 89 were found in the upper half and 61 in the lower half, with the great majority appearing in a six-inch belt around the seam at the sphere's equator.

The results of some laboratory high-current impulses on metal sheets are also shown, indicating pressure effects without appreciable burning.

A lightning stroke to a metal-roofed rural home is described, the largest hole in the roof indicating a charge of 210 coulombs. The home was not wired, but a part of the lightning discharge traveled a distance of 162 feet to the wired house next door to puncture the cellar wall and contact the neutral ground rod driven in the cellar. Another channel was found extending to the base of the service pole where two ground rods were driven. It was estimated that the discharge removed approximately 250 cubic feet of earth in its travel. Available data indicate the stroke was negative and had a total charge of more than 240 coulombs with a suggested peak current of the order of 200,000 amperes. Although the struck home was quite badly damaged, there was no fire, nor was there any trouble with the electric equipment in the wired home.

The results of this work indicate that sheet metal may be made to yield certain data of value with reference to lightning.

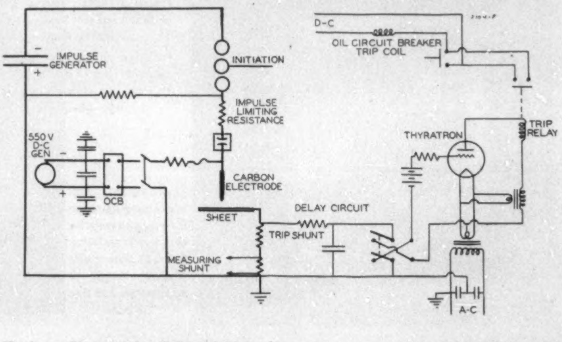
LIGHTNING strokes to objects on the earth have been characterized by two major effects[1-4] one causing explosive effects[5,6] and the other often resulting in fire. In many cases both effects have been present. The explosive effects are now known to be due to sudden increases in current which may reach a crest of several thousand or even a hundred thousand amperes or more in a few millionths of a second, decaying to half of the crest value in a time of the order of 40 microseconds. It is the expansion effects in the air resulting from these rapid current changes which cause the thunder which we hear. The burning effects are the result of the flow of current of relatively low magnitude of the general order of 200 or 300 amperes[7] for times which may be as long as 1.5 seconds, but on the average, persist for about 0.3 second or about 7,000 times as long as the average time to half value of the current peak. Correlating photographic and oscillographic data for strokes to the Empire State building having a height of 1,275 feet, strokes began at the building in about 80 per cent of the cases. As the height of the earth object becomes less, the percentage of cloud-initiated strokes increases. It is believed that for transmission-line heights the contacting strokes are always cloud initiated. Currents in earth-initiated strokes measured at the earth end begin with a relatively small value of current—a few hundred amperes—which develop into a continuing current when contact is made with the cloud. Superimposed current peaks may develop later, with varying time intervals and magnitudes, all of these peaks being cloud-initiated just as in the case of the stroke to objects of ordinary height on the earth's surface.

Laboratory Lightning

The impulsive nature of lightning strokes has been known for a long time, and it was natural to consider the cloud as one pole of a condenser and the earth as the other. Thus the laboratory lightning maker noted the similarity between the discharge of a charged laboratory capacitor and natural lightning. It looked the same, and the sound effects were there also. Furthermore, wood could be blown apart as in nature, and many of the distance–time–voltage or current phenomena found in connection with the operation of outdoor circuits could be reproduced. And so with the development of the Marx-circuit impulse generator and the cathode-ray oscillograph, data became available proving the similitude of the laboratory and the natural lightning as to the nature of the phenomena involved.

The continuing part of the natural lightning stroke was not simulated, as its existence was not shown oscillographically until 1937. The lack of knowledge of this component explains the inability of investigators to make the lightning fulgurites found in nature when discharging a lightning generator into wet sand. The impulsive nature of the current blew the

Figure 1. Circuit to control duration and amplitude of long-duration low-current discharges, preceded by impulse current



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K. B. McEACHRON is research engineer, high-voltage practice and designing engineer, power transformer engineering department, and J. H. HAGENGUTH is electrical engineer, both with General Electric Company, Pittsfield, Mass.

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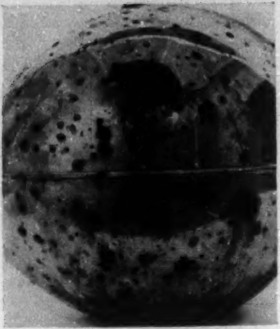


Figure 2. Side view of lightning-damaged 18-inch nickel-plated copper sphere installed for six years at top of WSM vertical radiator 878 feet above ground

sand out of the container, and it was not until currents of a few amperes were used, continuing for a second or so, that fulgurites could be formed.

The charge represented by the current peak is very small indeed. The average measured in natural lightning is only a fraction of a coulomb, whereas the charge represented by the continuing current may be of the order of 200 coulombs or more. Even very large impulse generators with voltages suitable for general testing work will have a charge of considerably less than ten coulombs. It is clear, therefore, that to simulate the complete lightning stroke a generator of more than 20 times the present capacity is needed, and therefore it is necessary as an economic expedient to make use of generators such as illustrated in Figure 1, where the discharge is initiated by a high capacitance generator, with a follow current supplied by a 500-volt d-c generator. Such an arrangement, though not suitable for long arcs, was quite satisfactory for the tests on metal sheets described in this paper. Modifications of this circuit have been used during other long-duration lightning current tests. More than one high current impulse may be superimposed using a circuit of this general character.

Lightning Strokes to Sphere at WSM

Our interest in the use of thin metal as a means of collecting lightning data was aroused when William Montgomery, Jr., engineer for WSM radio station of the National Life and Accident Insurance Company at Nashville, Tenn., sent us the sphere illustrated in Figure 2. This nickel-plated, 18-inch diameter sphere made of 19-mil copper had been in service at the top of WSM's vertical radiator, 878 feet in height, for a period of six years. During that time it was struck frequently by lightning and in the summer of 1937 was thrown to the ground during a lightning storm which damaged the tuning coil at the base of the tower as shown in Figure 3. The sphere was damaged in its fall on the reverse side but had been mounted in the position shown in Figure 2.

The large number and varying size of holes and pits found indicated that the sphere could be made to tell a story about lightning strokes if a proper calibration for the holes could be found. The globules of copper found around many of the holes indicated clearly that the holes were produced by continuing currents rather than by current peaks, which would have produced sufficient pressure in many cases to have blown the molten metal away.

Tests were begun, therefore, with the circuit shown in Figure 1, using a variety of current and time values until the appearance of the holes produced corresponded closely to those found on the WSM sphere. A few of the test holes are reproduced in Figure 4. A calibration curve is shown in Figure 5 together with a similar curve for galvanized iron. All tests were made with the sheet positive, electrode negative.

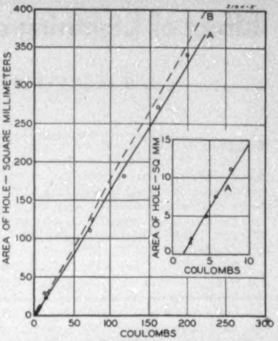


Figure 5. Relation between coulombs in the arc and resultant area of holes burned in (*A*) 15-mil galvanized iron sheets, (*B*) 20-mil copper sheets

Sheet positive, electrode negative polarity

Using the calibration curve of Figure 5 for nickel-plated copper sheets, the experience curve for the entire sphere in terms of coulombs versus holes was obtained and is compared, in Figure 6, with the curve of strokes versus coulombs as obtained for the Empire State building. Considering the fact that the number of holes per stroke is not known in the case of the WSM sphere, the agreement seems quite good when it is remembered that in many cases there may be more than one hole per stroke. The Empire State building data come from measurements of oscillograms and are known to represent the complete stroke. Of course, the difference in height and location no doubt has also had some effect on the recorded results.

It is a matter of considerable interest to note the number of holes in the sphere below its equator compared with those above. A curve similar to that of Figure

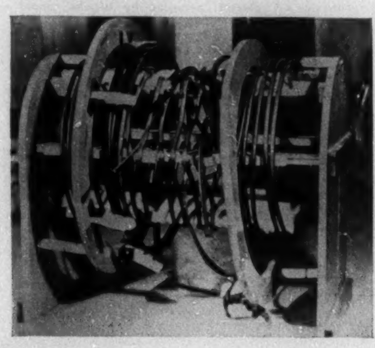


Figure 3 (left). Tuning coil of WSM radiator damaged by lightning stroke

Probable current crest 66,000 amperes—positive polarity. $^5/_8$-inch (outside diameter) tubing. 40-mil wall

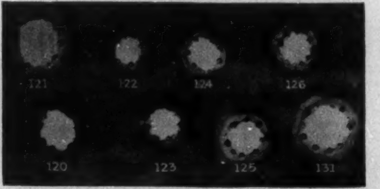


Figure 4 (right). Samples from d-c tests on 20-mil copper sheets to determine relation between current amplitude, length of application, and size of hole

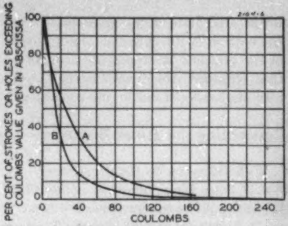


Figure 6. Expectancy curves for coulombs conducted in lightning strokes

A—Oscillographic measurement Empire State building
B—Hole calibration for WSM sphere

6 for the upper and lower halves shows ten coulombs at the 80 per cent point for the upper half and five coulombs for the lower, while for values below 50 per cent the curve for the upper and lower halves is about equal to and practically coincides with that shown in Figure 5 for the whole sphere. The total number of holes in the upper half is 89 and in the lower is 61. Within a three-inch belt above the equator, 44 holes were found, while in a similar zone below the equator, 41 holes were counted, with five holes in the seam around the equator. Thus, 60 per cent of the holes were located on 32 per cent of the total surface of the sphere. In view of the distance to the cloud, the field surrounding the sphere is quite uniform except for the effect of its support which would lead one to expect a shielded zone near the point of attachment, and this is just what was found. If the strokes represented by the holes were all initiated by the tower, one would expect streamers to be formed as the cloud field became more intense from all over the sphere's surface except close to the point of support. Chance conditions of ionization in the atmosphere probably have considerable effect in determining which of the streamers from the sphere will develop into the stroke.

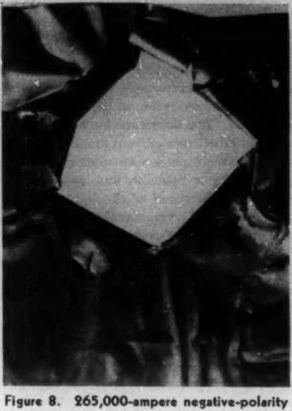


Figure 8. 265,000-ampere negative-polarity discharge through 10-mil plain copper sheet

Side away from arc. Sheet used as one electrode

It is quite likely also that the seam around the equator had a considerable effect in promoting the formation of streamers at the time of a stroke. Drops of rain water, particularly at the seam may have added to this effect. The top of the sphere was apparently shielded to some extent by a probable extension of the sphere support above the top of the sphere. The details of the attachment are not known.

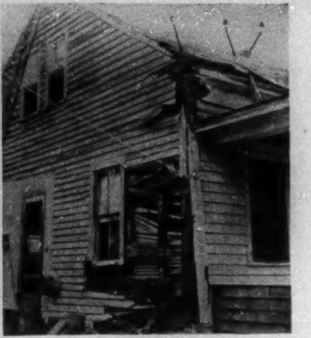


Figure 10. Exterior view of Amherst house struck by lightning

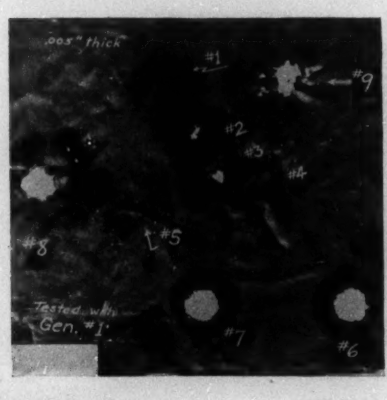
(*A*) Position of chimney. (*B*) Location of large hole. (*C*) Location of several small holes

Three hundred pits were found, many of these being accounted for as direct strokes having relatively high current magnitude, but having a low coulomb content (two coulombs or less). It does not appear to be possible to draw any definite conclusion concerning these pits in terms of current magnitude. It is possible, of course, that some of them are part of strokes which burned holes, and, if so, the effect on the calibration of holes in terms of strokes would be small on account of the small coulomb content.

In considering the interpretation to be put upon the results obtained by this sphere, it should be recorded that the number of separate strokes is not known, although Mr. Montgomery believes, based

Figure 7 (left below). 40,000-ampere negative-polarity discharge through five-mil nickel-plated copper sheet

Side-facing arc. Small holes or burns numbers 1 to 5, sheet used as one electrode. Large holes numbers 6 to 9, sheet mounted between electrodes



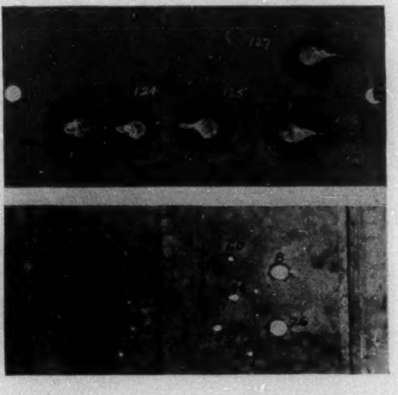


Figure 9 (right). Holes burned by relatively low-current long-time laboratory discharges

Top, $^3/_8$-inch steel plate. Bottom, 0.015-inch galvanized tin. Side facing electrode

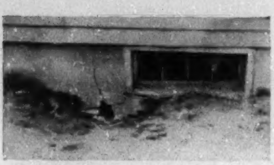


Figure 11. Damage to cement basement wall of A. T. Rouleau house, August 4, 1939, caused by lightning which came in across ground surface from nearby Joslyn house which was struck directly

Pelham Road near Amherst, Mass.

on his records, that 24 direct hits is a reasonable number to use. It is probable that this number is too low. With 150 holes, and assuming equal distribution of holes for all strokes, one arrives at seven holes per stroke which, from Figure 6, would indicate an average coulomb value of $7 \times 14 = 98$ coulombs, which is probably much too high, although the maximum hole indicates 240 coulombs. It must also be recognized that more than one stroke could contribute to the same hole as well as the same stroke making several holes.

At WSM, magnetic links had been installed on the antenna lead-in and guy wires at the time the sphere came down and the tuning coil of Figure 3 was damaged, which indicated a current of +66,000 amperes in the antenna lead-in and +6,400 amperes in two of the guy wires and a total of −73,000 amperes in six other guy wires. This record may cover more than one stroke, but it does indicate the highest currents recorded. It is interesting to point out that the most severe current peak measured with the cathode-ray oscillograph at the Empire State building[7] had a crest current of 58,000 amperes, contained 4.9 coulombs to half current value, and was positive.

Tests made in the laboratory on tubing similar to that of the tuning coil shown in Figure 3 indicate that the turns of the coil begin to pull together at a current crest of 75,000 amperes. The copper tubing itself begins to flatten at around 118,000 amperes. The difference between the effects of laboratory current amplitudes and the 66,000 amperes measured in the actual stroke is probably due to different wave shape, as well as the fact that two concentric coils were involved in the actual stroke. Furthermore, the heavy discharge current may have been preceded by a high coulomb–long duration discharge heating the copper tubing sufficiently to produce crushing at much lower crest currents. The laboratory current waves were oscillatory waves of 86 microseconds period with the initial peak of negative polarity. The tuning coil consisted of $^5/_8$-inch outside diameter tubing with 40-mil wall thickness.

Lightning Strokes to WGY

In 1938 a 20-mil chromium-plated copper cylinder was mounted on top of the then new WGY 635-foot radiator at Schenectady. The top of the cylinder was closed with a hemisphere of the same metal and thickness. When this was removed for examination in 1941, seven holes and several hundred small pits were found. The radiator was known to have been struck three times, the magnetic links showing −3,000, −7,800, and −30,000 amperes.

Of the seven holes, six were located in the 12-inch diameter hemisphere, mostly in the upper part, but none at the highest point of the sphere as installed. The cylindrical portion had a height of 14 inches, and one hole was found six inches from its lower edge. The pits were most numerous in the area above a line nine inches from the bottom of the cylinder. Twenty-nine of the pits had an appearance similar to that obtained from high-current laboratory discharges of one or two coulombs.

Concerning the Holes Burned

With reference to the appearance and size of holes some comment is desirable. While the WSM sphere showed some very small holes—as small as three square millimeters—such small holes could not be produced in the laboratory in copper sheets. The smallest hole obtained had an area of 28 square millimeters. However, in galvanized iron sheets, holes as small as 1.5 square millimeters could be produced.

This effect has been attributed to the close proximity ($^1/_4$ to $^1/_2$ inch) of electrode to the sheets under test. A tapered carbon electrode was used to avoid the effect of the metal of the electrode on the calibration. Although the electrode begins to glow at 60 coulombs, very little gas is given off. Brass electrodes were tried but consistent results were not obtained.

It is expected that the only difference to be obtained from a longer arc would be the possibility of forming smaller holes in copper sheets. Within the range investigated—spacing $^1/_4$ inch to $^1/_2$ inch—the character of the holes is not changed, indicating that the electrode effect is not appreciable. The close proximity of the electrode possibly has the effect of spreading the core of the arc wider than would be the case in a lightning stroke. However, the holes produced showed the same characteristics as those produced by natural lightning as evidenced on the WSM sphere.

The tests on metal sheets up to 100-

Figure 12. Lightning damage at W. H. Joslyn house, August 4, 1939. Pelham Road near Amherst, Mass.

Furrow in ground was dug by lightning as it left Joslyn house and headed toward neighboring Rouleau home

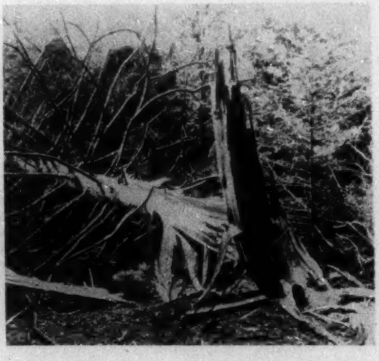


Figure 13. Destruction of pine tree by lightning discharge

Top of tree blown off by high current peak. Stump burned by continuing discharge mil thickness indicate the following relations:

$$C=\frac{A}{25}\times t^{0.9} \quad (t=0 \text{ to } 35 \text{ mils})$$

$$C=\frac{A}{245}\times t^{1.54} \quad (t=35 \text{ to } 150 \text{ mils})$$

where

C = coulombs
A = area of hole in square millimeters
t = thickness of sheet in mils

Such equations are reasonably accurate for copper and galvanized iron and may be used to estimate the coulombs in a given lightning stroke. The use of the curves of Figure 5 for the individual metal is more accurate, but tests of this nature do not permit close calibration, the variation from the curves shown in Figure 5 being as much as 40 per cent for some individual points.

That the hole burning is a function of coulombs rather than i^2t has been verified by the fact that equal holes are burned when equal coulombs in the arc are produced with widely different current amplitudes. The test currents ranged from 50 amperes to 600 amperes, which is in the range of natural lightning continuing discharges. Tests at high currents with high coulomb values have not been made, but the calibration data given in Figure 5 should not be used for high currents. Bellaschi[2] similarly showed that burning of solid heavy electrodes is dependent upon coulombs.

In Figure 7 are shown holes burned in a five-mil, nickel-plated copper sheet with discharge currents of 40,000 amperes crest. The small holes and burns numbered 1–5 were produced with the sheet grounded. The larger holes 6–9 were obtained with the sheet midway between the line electrode and the ground electrode. The jagged character of the holes is apparent, especially in number 9 where the electrode distance was increased from $^5/_{16}$ to $2^3/_4$ inches. There is no evidence of the copper beads lining the hole as is observed on the sphere and the corresponding test sheets. As the current amplitude is increased, the explosive effect is increased as shown in Figure 8, which gives the effect of a 265,000 ampere 30-coulomb discharge to a grounded copper sheet of ten-mil thickness. The same impulse applied to a 25-mil copper sheet did not rupture it but did produce a large dent in its surface. On the other hand, a 25,000-ampere one- to two-coulomb discharge will form a bright bead on a 20-mil copper sheet but will not puncture the sheet. In the explosive type or high current discharge many factors have to be weighed besides current amplitude, while the charge in the long-duration low-current continuous discharge can be computed with fair accuracy.

The tests indicate a possible source of fire with thin metal-roofed buildings, if inflammable material is close to the metal or in such position that molten metal may drop on combustible materials. If thin metals are to be used for covering, then elevated air terminals should be used to keep the arc away from the building and a suitable path to ground provided to prevent damage at joints in the metal structure.

Tests on Steel Plate

In view of possible lightning strokes to metal containers of explosive gases of liquids, tests were made with the equipment shown in Figure 1 with the following result. A $^3/_8$-inch steel plate was tested with 430 coulombs in the arc. Results of such tests are shown in Figure 9. The plate could not be punctured but a craterlike hole was formed of $^3/_{16}$-inch depth and 180 square millimeters area. To puncture such a plate several thousand coulombs would be required.

Lightning Stroke to Home

Of the many lightning damage cases which the authors have investigated, that which occurred near Amherst, Mass., during the summer of 1939 is perhaps one of the most interesting. Lightning struck the home shown in Figure 10, resulting in the external damage shown. The upper portion of the chimney had been eliminated, and the chimney was cracked throughout its length. A hole having an area of 345 square millimeters was found in the 15-mil galvanized-iron shingle roof at a point about three feet from the chimney and in the direction of the damaged corner of the building shown in Figure 10. Several small holes were found in a shingle, which apparently had been located at the corner of the roof above the damaged corner. No evidence of fire could be found anywhere, although in the kitchen there was a considerable burn between a sink and a nearby pipe, which had a combined effective resistance to ground of 47 ohms. This was the only ground in this house. There was also considerable burning between the cover of an ice box and the zinc lining. Nearly all of the windows on the ground floor were blown outward, showing the high pressure developed within the house. Two persons were in the house, one upstairs to the right of the chimney, and the other in the kitchen to the left of the door shown in Figure 10. Neither was hurt.

There was no wiring of any sort in this home. In Figure 11 is shown another home, 155 feet away, which had a three-wire service with the usual appliances connected. In the cellar was a driven ground of 430 ohms. In addition, two grounds having a combined resistance of 215 ohms were driven at the service pole located 110 feet from the house of Figure 10, and 80 feet from the house of Figure 11. A portion of the path of the lightning discharge is shown in Figure 12. At the point illustrated the channel in the earth was about three feet in width and about 15 inches in depth. It continued with two-foot width and about the same depth for a distance of 74 feet where it split into two parts, one to continue 88 feet to the ground rod in the cellar of the house of Figure 11, and the other to travel 81 feet to the ground rods driven at the base of the service pole. The electric equipment in the wired house was in operating condition after the storm, although the cellar windows were blown out as the result of the pressure developed in the spark to the driven ground rod inside close to the cellar wall.

From magnetic effects observed, it was concluded that the current in the neutral wire was of the order of 30,000 amperes of a slow rate of rise and indicated a negative-polarity stroke to the unwired house of Figure 10. A calibration of the shingle burns indicates a coulomb value of 210 for the larger burn and 30 coulombs for the smaller burns.

In reconstructing what happened, it is probable that the stroke was negative, beginning with a current peak of the order of 200,000 amperes, followed by a continuing current having a coulomb value of 240. The physical damage seen in Figures 10 and 12 resulted from the high current peak. After the chimney exploded, apparently the path shifted to the burned points found on the roof. With the high coulomb value of the continuing current, one would have expected a fire to ensue, but no evidence of this could be found, indicating that the inflammability of the various paths was such as not to cause fire.

This case is given here, because some knowledge exists as to the magnitude of the lightning current which caused the damage observed. This again illustrates what may happen when good grounds are not available when lightning strikes.

The pine tree shown in Figure 13 is mute evidence of the same type of lightning discharge just described. This tree struck by lightning exploded a few feet

Abnormal Currents in Distribution Transformers Due to Lightning

J. M. BRYANT
MEMBER AIEE

M. NEWMAN
ASSOCIATE AIEE

Synopsis: Abnormal currents in distribution transformers due to lightning are analyzed both theoretically and from field experiences. Under certain conditions of direct stroke transferred through the arrester to the neutral of the secondary winding, excessive currents may wreck the secondary windings by electromagnetic forces. Long-duration surges, or shorter repeated surges, saturate the transformer cores producing greatly increased surge currents in the primary windings which influence fuse failures. The saturation of the cores by unidirectional lightning surges also brings about increased power-frequency magnetizing currents influencing sectionalizing-fuse and circuit-breaker operations.

A GREAT deal of research has been done and many papers have been written dealing with the problems of overvoltages under lightning conditions. It has been assumed generally that, because of the relatively high inductance of transformer windings, very little lightning current builds up within them. There are, however, certain conditions under which the lightning current may enter the secondary windings of grounded-neutral distribution transformers and rise to such magnitudes as to develop electromagnetic forces sufficient to destroy that winding by mechanical stress. Also, for long-duration single or multiple lightning surges enough current builds up through the primary winding to saturate the core; and the consequent rate of increase of current in the winding becomes more rapid, and the current is finally limited only by the d-c resistance of the primary winding. These latter currents are generally insufficient to damage the winding, but they are important in the determination of fuse failures[1] and sectionalizing operation under lightning conditions.

Another source of abnormal transformer currents is that of large magnetizing inrush currents brought about by surge conditions, which will be primarily important in the operation of fuses or breakers. These various phenomena of abnormal transformer currents are discussed further in this paper, with special reference to distribution transformers on rural lines.

Direct-Stroke Lightning Currents in the Secondary Windings

Under certain circumstances of direct lightning strokes to the primary line, very large currents may go through the secondary winding when the lightning arrester functions to protect the primary. Occasionally the effect of such currents is completely to disrupt the windings, as shown in Figure 1. There is no present protection scheme in use which protects against such conditions. The transformer was connected as shown in Figure 2. The lightning arrester effectively by-passed to the ground lead the resulting current from a direct stroke to the line, and the primary winding was perfectly protected, as determined by subsequent test. The voltage of the secondary wires rose, together with that of the transformer case, to the value of the surge-impedance drop of the grounding wire impedance and ground resistance, and another flashover to ground occurred, this time on the customer's premises. Current in the two wires on each side of the grounded neutral to the customer ground fault went through the secondary winding. Since the current went from the transformer case at raised potential into the neutral connection of the transformer coil, and from there into the winding halves in opposite directions, the magnetic fields of the two halves of the secondary winding canceled out to a high degree, because of the close coupling. The leakage-reactance drop and also the resistance drop were not sufficient to flash over the bushing or the normal shunt safety gaps in use. Thus the current continued to go into the secondary coil sections in opposing directions; the extreme resulting mechanical forces due to electromagnetic reaction distended the secondary winding, as shown at X in Figure 1, and actually cut several turns in two by jamming them against the core laminations.

Such conditions to a greater or lesser extent, must occur in almost every case of a direct stroke to line near a transformer installation. The paths for the lightning current through the transformer secondaries are indicated by arrows in Figure 2. The relative amount of current going into the windings varies with the relative ground resistances; in this case it was probably of the order of 20,000 amperes

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J. M. BRYANT is professor and head of the electrical engineering department, and M. NEWMAN is associate research engineer in the electrical engineering department, University of Minnesota, Minneapolis, Minn.

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above the ground. Examination of the upper portion of the tree and the debris scattered over a considerable area showed no signs of burning; yet the stump was on fire when the owner of the land ran to the point where he saw the lightning strike. Evidently this stroke also began with a high current peak blowing the tree apart. The stroke persisted as a continuing current to the stump of the tree, which was still grounded, setting it afire. This was going on while the tree and the debris were in the air.

The data which have been collected thus far seem to indicate that the use of thin metal surfaces makes a fairly satisfactory means for collecting data with respect to the number of coulombs contained in natural lightning strokes, excluding the effects of the high current peaks which may be present.

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in each transformer section, but in most cases the current is apparently low enough to be harmless.

Insufficient field data are available on the frequency of such failures, and therefore it is uncertain whether protection against such faults is economically feasible. Also, it is beyond the scope of this paper to consider protective means beyond mentioning a few obvious measures such as:

1. Reduction of arrester ground resistance to a value considerably less than the customer's ground, although the customer's ground must also be kept low for his safety.

2. Application of very low breakdown-value protectors across the secondary bushings.

3. Placing of small reactors in the transformer secondary leads inside the tank, which will cause the bushing gaps to flash over and protect the secondary winding.

4. Placing the two secondary leads inside a waterproof metallic cable, the metallic covering of the cable serving as the neutral lead grounded to the case, and also serving as a supplement to the arrester ground when the cable is run underground to the customer.

Primary Winding Currents Under Long-Duration Surge Conditions

Very little surge current is built up in the primary winding until the transformer core reaches saturation, when the effective primary-winding inductance drops sharply; short-duration surges do not produce saturation. Long-duration or repeated surges equivalent to long surges can build up considerable currents, often sufficient to blow the usual primary protective fuse. The determining factors in this saturation phenomenon can be more readily analyzed by taking a specific field example of a 7,200-volt rural distribution system feeding a network of three-kva transformers, shown in Figure 3.

There are two possible conditions under which long surge waves may be present on the system. The arresters on the system may be functioning and, if they are of the conventional type, they will limit the peak voltage to about 55 kv, but this will drop at a slower rate than the incident wave; thus, in effect, producing a lengthened surge. On the other hand, especially in the case of repeated discharges, long surges may be present on the system without any arresters functioning on some of the later of the repeated discharges which may have dropped to below the breakdown point of the arrester. In the latter case the duration of the surge is influenced by the discharge path both from cloud to ground and within the cloud itself. Recent investigations[2,3] show such waves may be from 1,000 to 20,000 microseconds duration. A border-line case which would fit the above conditions of the arresters functioning or not functioning could be represented on the particular 7,200-volt line by a surge expressed as

$$e=E_s\epsilon^{-\alpha t}=55{,}000\epsilon^{-0.00028t}$$

This expression corresponds to a wave having a crest value of 55 kv and dropping exponentially to half value in about 2,500 microseconds. It will be noted that the second term of the more general expression for a surge, $e=kE(\epsilon^{-\alpha t}-\epsilon^{-bt})$, which is omitted, represents the rate of rise of the front of the wave. The duration of the "front" of the wave is so short in relation to the very long duration of the "tail" of the wave that it plays little importance in the determination of the final current through the winding; and therefore, the corresponding term is left off to simplify the mathematical presentation.

This surge wave is so long relative to the time of travel along the entire transmission line that the same potential can be considered applied at all the transformer installations, and the surge impedance of the line can be considered a negligible factor. This becomes clear when it is considered that the length of the wave of 2,500 microseconds corresponds to a time such a wave could travel 475 miles, or would travel back and forth in successive reflections on an average 50-mile rural system about ten times while dropping to half value. Hence the conditions can be considered essentially the same at each of the distribution transformers and may be schematically represented as shown in Figure 4.

The curves C and F, Figure 4, show the resulting current rise in the primary winding with the surge occurring at different points C' and F' on the normal 60-cycle wave, shown in Figure 5. The current begins to be appreciable only as the transformer core saturates. The hysteresis loop A-B-C-D-E-F-G in Figure 5 gives the normal variation of B-H as the transformer core changes its values of B to produce the equal but opposite sinusoidal electromotive force, $iR+N(d\Phi/dt)\cdot 10^{-8}$,

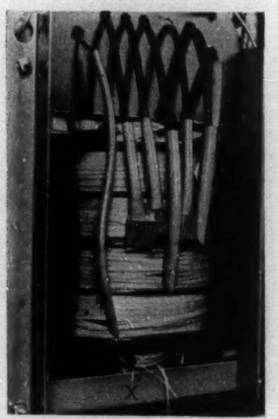


Figure 1. Showing the effect at *X* of electromagnetic stresses caused by lightning currents entering the secondary sections of a distribution-type transformer at the neutral-wire connection

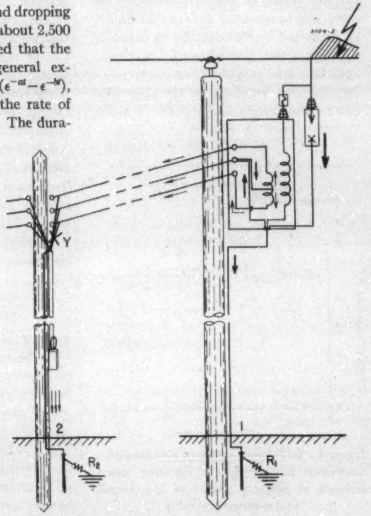


Figure 2. Schematic diagram of a typical rural-distribution-transformer connection with arrows indicating how the lightning current may penetrate the transformer secondaries

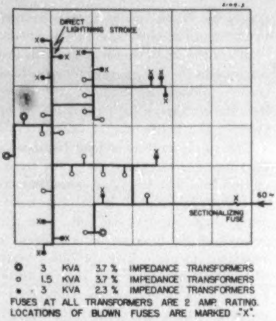

DIRECT LIGHTNING STROKE
60 ~
SECTIONALIZING FUSE
3 KVA 3.7 % IMPEDANCE TRANSFORMERS
1.5 KVA 3.7 % IMPEDANCE TRANSFORMERS
3 KVA 2.3 % IMPEDANCE TRANSFORMERS
FUSES AT ALL TRANSFORMERS ARE 2 AMP. RATING.
LOCATIONS OF BLOWN FUSES ARE MARKED "X".

Figure 3. Typical 7,200-volt rural single-phase section in which fuse failures were caused by lightning current penetrating the primary windings of the distribution transformers

which balances the applied electromotive force, $E_m \sin \omega t$. The B and electromotive force relationships are indicated in dashed lines on Figure 5, from which it is clear that a surge impressed at C' on the 60-cycle voltage wave conforms to C or maximum flux value, on the hysteresis loop, will cause a maximum flux increase along S; and, therefore, will result in the shortest time to core saturation. Similarly a surge at point F' on the 60-cycle wave would result in the longest time to saturation. The calculation of the growth of current must take into account these various states of magnetization of the core; and because the B-H curves cannot be expressed mathematically by a simple equation, the fundamental equations must be solved graphically or in a step-by-step manner, as given in the appendix. From these calculations and the resulting curves of Figure 4, it is clear that the time denoted by t_o elapsing before the core saturates and appreciable currents can develop varies with the point of surge incidence on the 60-cycle wave and lies in the range $t_o = 1{,}100 \pm 600$ microseconds.

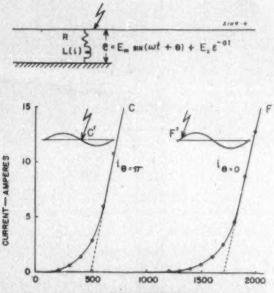
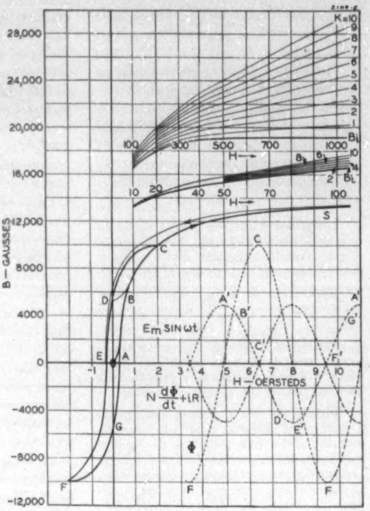


Figure 4. Initial current rise in a distribution-transformer primary due to lightning surges incident at different points on the normal power-frequency cycle

Figure 5. Magnetization curve for the core of the typical distribution transformer

The dashed lines indicate relative flux-voltage relations under normal operation



The above range of t_o was determined for one specific 7,200-volt transformer of three-kilovolt-ampere rating. It follows, however, from the calculations (see appendix) that the time t_o is independent of the transformer kilovolt-ampere rating and of the voltage rating of the transformer, so long as the ratio of its voltage to the surge voltage remains the same. It also follows, from the calculations and the curves of Figure 5, that for the larger values of current as the core saturates, the relation of B to H increments or $\Delta B/\Delta H$ becomes a constant, and the problem may be solved as with constant parameters. The relation for the current due to a surge potential $e = E_s \epsilon^{-at}$, may be written as

$$i = \frac{E_s}{R - aL}\left[\epsilon^{-at} - \epsilon^{R/L(t-t_o)}\right]$$

where L is the inductance of the circuit with the core saturated, t_o is $1{,}100 \pm 600$ microseconds, depending on the point of surge incidence on the power frequency cycle; the values of i up to $t = t_o$ being relatively small and considered zero.

The derived simplified equation lends itself readily to the examination of possible currents flowing into various transformers having different impedance characteristics. Thus, Figure 6 shows possible currents due to the surge alone flowing into two transformers of different impedances but of the same kilovolt-ampere and kilovolt ratings. These currents are of sufficient magnitude to cause the protective fuses in the transformer-primary-circuit leads to fail, as was proven by instances of fuse failures on *unenergized* lines under lightning conditions. Conclusive evidence that the relative currents are as calculated is given by the field case summarized in Figure 3, where every fuse failed at a 2.3 per cent impedance transformer but where none failed at any of the higher impedance transformers. The greater current associated with the lower impedance transformer, as shown

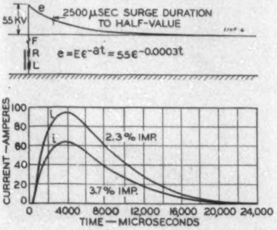


Figure 6. Incident surge wave and resulting current in the primary windings of three-kilovolt-ampere, 7,200-volt distribution transformers having per cent impedance values as marked on the curves

in Figure 6, accounts for the selective action noted. The solution of the fuse-failure problem is definitely not, however, the use of high-impedance transformers. In this particular case all of the fuses were of the same rating; and none failed on the high-impedance transformers, because these transformers were overfused relative to their impedance and, therefore, were actually unprotected against normal overloads.

Conclusion

Direct lightning strokes may cause very large currents to penetrate the secondary windings of distribution transformers with destructive effects, even though the primaries are perfectly protected. It is important, therefore, to determine the actual extent and frequency of occurrence of this type of failure in practice.

Long-duration surges cause relatively large currents to build up also in the primary windings of distribution transformers, as analyzed in this paper. Field experience with fuse failures and laboratory research by the writers with simulated surge conditions, have checked the validity of the analytical approach. Oscillographic records obtained in this research agree closely with Figure 6. Conclusions on the effect of variations of incidence of a surge on the 60-cycle wave follow readily and accurately from the detailed analysis in the appendix and elsewhere in this paper. Published experiments by Bergvall and Beck[4] with direct currents equivalent to long-duration surges applied to transformers are also in reasonable agreement.

The calculations of Table I in the appendix, extended over a number of cycles, show that even after the surge voltage has completely disappeared a high magnetizing current transient results, due to the high degree of saturation in the core. Reference to equation 5 in the appendix shows that the flux increment, due to a small surge incident on the transformer 60-cycle wave at a point corresponding to low core flux value, may be written

$$\sum \Delta B = \sum \frac{\omega B_m}{E_m} E_s \epsilon^{-at} \Delta t$$

since iR would then be negligible during the surge-incidence time. The total flux increment due to a surge $E_s\epsilon^{-at}$ would be

$$\int_0^\infty \frac{\omega B_m}{E_m} E_s \epsilon^{-at} dt = \frac{\omega B_m E_s}{a E_m}$$

It follows, therefore, that every surge, no matter how small, would cause a certain shift in flux and would produce a magnetizing transient. These transients, although usually more severe, are of the same character as the magnetizing inrush currents experienced when energizing a transformer at certain points on a 60-cycle wave. This fact is of importance in examining possible faulty operation of fuses and breakers. An interesting proof that such "induced" magnetizing inrush currents may cause faulty fuse and breaker operation is given by cases of recent outages during periods of severe sun-spot activity which resulted in long-duration relatively constant voltages across the transformers, which although quite different in magnitude were comparable in effect to long-duration repeated lightning surges.

Although complete protection methods have been developed, further researches and field studies are necessary to determine the frequency of occurrence of these problems in actual practice.

Table I. Step-by-Step Solution of Current Due to a Lightning Surge, $55{,}000\epsilon^{-0.0003t}$ Superposed at the 180-Degree Point of the Normal Power-Frequency Cycle

$$\Delta B = -10000\ \Delta\cos(0.000377t + \pi) - 67700\ \Delta\epsilon^{-0.0003t} - 0.1\,\bar{i}\,\Delta t$$

①	②	③	④	⑤	⑥	⑦	⑧	⑨	⑩	⑪	⑫	⑬	⑭
t	Δt	ωt	cos ωt	$10^4\Delta\cos\omega t$	at	ϵ^{-at}	$\Delta\epsilon^{-at}$	$-67700\Delta\epsilon^{-at}$	ī	$-0.1\,\bar{i}\Delta t$	ΔB	Σ ΔB	i = 0.01 H
t	Δt	.000377×①	cos ②		.0003·①	$\epsilon^{-⑥}$		-67700·⑧			⑤+⑨+⑪		
0		0	1.0000		0	1.0000						10 000	0.02
100	100	0.0377	0.9993	−7	0.03	0.9704	−0.0296	2010	0.03	−0.3	2000	12 000	0.045
200	100	0.0754	0.9971	−22	0.06	0.9418	−0.0286	1940	0.10	−1.0	1920	13 920	0.16
300	100	0.1131	0.9936	−35	0.09	0.9139	−0.0279	1890	0.3	−3	1850	15 770	0.52
400	100	0.1508	0.9886	−50	0.12	0.8869	−0.0270	1830	0.93	−9	1770	17 540	1.35
500	100	0.1885	0.9823	−63	0.15	0.8607	−0.0262	1770	2.0	−20	1690	19 230*	2.70
600	100	0.2262	0.9745	−78	0.18	0.8353	−0.0254	1720	4.3	−43	1600	20 830	5.90
700	100	0.2639	0.9654	−91	0.21	0.8106	−0.0247	1670	8.3	−83	1500	22 330	10.80**
800	100	0.3016	0.9549	−105	0.24	0.7867	−0.0239	1620	13.1	−131	1380	23 710	15.40
900	100	0.3393	0.9430	−119	0.27	0.7634	−0.0233	1580	17.5	−175	1290	25 000	19.70
1000	100	0.3770	0.9297	−133	0.30	0.7408	−0.0226	1530	21.7	−350	1180	26 180	23.63
2000	1000	0.7540	0.7280	−2017	0.60	0.5488	−0.1920	13000	35.9	−3590	7390	33570	48.26
3000	1000	1.131	0.4257	−3023	0.90	0.4066	−0.1422	9620	50.8	−5080	1520	35090	53.33
4000	1000	1.508	0.0628	−3629	1.20	0.3012	−0.1054	7140	50.7	−5070	−1560	33530	48.13
5000	1000	1.885	−0.3090	−3718	1.50	0.2231	−0.0781	5280	43.5	−4350	−2790	30740	38.83

* Complete saturation of iron in core, beyond which $\Delta B = K\Delta H$.

** Values beyond B-H curve obtained from $\Delta i = 0.01\,\Delta H = 0.01\Delta B/K$.

Appendix. Solution for Current in Transformer Due to Lightning Surge Superposed on the Normal Power Cycle

The fundamental relationship for the transformer with a total voltage e across it, composed of the normal operating voltage $E_m \sin \omega t$ and a superposed surge voltage $E_s\epsilon^{-at}$ may be written as

$$\begin{aligned} e &= E_m \sin(\omega t+\theta) + E_s\epsilon^{-at} \\ &= N(d\phi/dt)\times 10^{-8} + iR \\ &= A(dB/dt) + iR \end{aligned} \qquad (1)$$

where ϕ represents the total value of the flux linking the primary winding, including the air space around the core, that is, the leakage inductance $L(di/dt)$ drop. This leakage inductance is introduced in the calculation by choosing the graph corresponding to the proper value of K in Figure 5, the value of K being equal to the ratio of total leakage flux (excluding the contribution of the core) to the leakage flux which would be obtained in the space occupied by the core. External inductance of transmission lines, and so forth, can also be included in determining the value of K, as suggested by Steinmetz[5] in solving the somewhat similar problem of determining magnetizing inrush current. The value of the constant A can be determined from the physical characteristics of the winding or from the known maximum flux density under normal operating voltage conditions. Neglecting the iR term for normal magnetizing currents, and for normal steady-state conditions

$$E_m \sin \omega t = A(dB/dt)$$

from which

$$dB = (E_m/A) \sin \omega t dt \qquad (2)$$

$$B = (E_m/A)\int \sin \omega t dt = (-E_m/A) \cos \omega t$$
$$B_m = E_m/\omega A$$

and hence,

$$A = E_m/\omega B_m \qquad (3)$$

Thus, substituting this value of A in equation 1 for surge conditions

$$\begin{aligned} e &= E_m \sin(\omega t+\theta) + E_s\epsilon^{-at} \\ &= (E_m/\omega B_m)(dB/dt) + iR \end{aligned} \qquad (4)$$

For a solution of equation 4, dB/dt may be written in finite increment form $\Delta B/\Delta t$, giving

$$\Delta B = (\omega B_m/E_m)(e - \bar{i}R)\,\Delta t \qquad (5)$$

The values of the total potential e composed of $E_m \sin(\omega t+\theta)$ and $E_s\epsilon^{-at}$ in equation 5 must be the average values over the interval Δt. To avoid the approximation of finding the average, $E_m \sin(\omega t+\theta)dt$ may be written as $-d[(E_m/\omega)\cos(\omega t+\theta)]$, and $E_s\epsilon^{-at}dt$ as $-d\left[\frac{E_s\epsilon^{-at}}{a}\right]$ so that, in substituting increments for the differentials, equation 5 may be written as

$$\Delta B = -B_m \Delta \cos(\omega t+\theta) - \frac{\omega E_s B_m}{a E_m}\Delta\epsilon^{-at} - \frac{\bar{i} R\omega B_m \Delta t}{E_m} \tag{6}$$

The values of i remain to be averaged over the interval Δt, so that, where variations of i are rapid, it may be necessary to use shorter intervals of t. The values of ΔB are added to the preceding values to give the total B, and the corresponding values of i are determined from the B-H curve of Figure 5 in the step-by-step manner indicated in Table I. The relation of i to H depends upon the number of turns in the winding, N, and the mean length of the magnetic path, l;

$$H = \frac{0.4\pi N i}{l} \text{ or } i = \frac{Hl}{0.4\pi N} \tag{7}$$

For a typical three-kilovolt-ampere, 7,200-volt distribution transformer it was found that for $E_s = 55$ kv, as shown for the surge $e = E_s \epsilon^{-at}$, and for the actual constants as determined from the transformer of $B_m = 10{,}000$ and $E_m = 7{,}200\sqrt{2}$ or 10,200 volts, and $R = 280$ ohms, equation 6 for ΔB could be expressed with convenient coefficients for step-by-step calculations, to within two to three per cent probable error, as

$$\Delta B = -10{,}000\,\Delta\cos(0.000377t+\theta) - 67{,}700\,\Delta\epsilon^{-0.0003t} - 0.1\bar{i}\,\Delta t \tag{8}$$

with t and Δt expressed in microseconds, and where, for the particular transformer $l = 32$ inches and $N = 6{,}240$ turns, the values of i are given by

$$i \approx 0.01H \tag{9}$$

with H corresponding to the various values of B determined for the proper value of K (in this case $K = 3$) from Figure 5.

The initial value of B in Table I is taken as that corresponding to the point on the normal 60-cycle voltage wave at which the surge is incident. The particular value taken in the given tabulation is that value nearest saturation, or with an initial value of $B = B_m = 10{,}000$, corresponding to the incidence of the surge at the zero point C' on the 60-cycle wave, or where $\theta = \pi$ in equation 6. It is clear from the tabulation and from the corresponding plotted curve in Figure 4, that the values of current are relatively small until the time when the core is very near the saturation point, or until $t_0 = 500$ microseconds in this case. Similar calculations for a point of incidence of the surge corresponding to $\theta = 0$ in equation 6 and to F' in Figure 5 gives, for the equivalent time to saturation t_0, the value of 1,700 microseconds. Thus the time elapsing before the core saturates and appreciable currents can develop varies with the point of surge incidence on the normal voltage wave, and for the given ratio of

$$(E_s/E_m) = \frac{55{,}000}{10{,}200} \text{ lies in the range}$$

$$t_0 = 1{,}100 \pm 600 \text{ microseconds} \tag{10}$$

The above value of t_0 was determined for one specific 7,200-volt transformer of three-kva rating. It is of interest to refer back to equation 6 from which it will be noted that while i is still small, that is, before $t = t_0$, the values of ΔB due to the surge voltage are proportional to (E_s/E_m). This proportionality means that for the same ratio of surge voltage to line voltage the values of $t_0 = 1{,}100 \pm 600$ microseconds will hold closely, irrespective of the transformer rating, and will vary only in response to (E_s/E_m). Thus, since there is a fairly well-established ratio of allowable surge voltage to 60-cycle voltage on distribution lines the values for t_0 are valid for all distribution transformers, irrespective of kilovolt and kilovolt-ampere ratings.

Another interesting development which follows upon examination of Table I and Figure 5 is that, as the core saturates the relation $(\Delta B/\Delta H)$ becomes a constant and the problem may be solved approximately with equivalent parameters. If one neglects the much smaller component of the 60-cycle voltage effective under the surge conditions of saturation, the current due to a surge potential $e = E_s{}^{-at}$ may then be found from,

$$i = \frac{E_s}{R - aL}\left[\epsilon^{-at} - \epsilon^{-(R/L)(t-t_0)}\right] \tag{11}$$

where L is the inductance of the circuit with the core saturated, t_0 is $1{,}100 \pm 600$ microseconds dependent on the point of surge incidence on the 60-cycle wave, and the values of i up to $t = t_0$, being small, may be neglected.

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Current Ratings of Electronic Devices for Intermittent Service

R. E. HELLMUND
FELLOW AIEE (Deceased)

Synopsis: Electronic devices are used extensively for switching, relaying, welding, and similar service resulting in intermittent loads. In the paper methods for assigning standard ratings suitable for such intermittent loads are investigated, both for single-anode tubes and tube circuit combinations. Vapor-filled tubes with oxide-coated filaments are treated somewhat in detail in order to develop the basic considerations. Other types of tubes, such as the pool-type and cold-cathode, are only briefly discussed. A method of rating which seems generally applicable is proposed, with the suggestion that its suitability for the great variety of conditions encountered be further investigated.

THE assignment of ratings especially suited for intermittent service is desirable, if it results in more economic use of devices than the present practice of assigning continuous ratings. The subject is of particular importance in connection with tubes, because they are used so extensively for intermittent operations such as switching, relaying, signaling, spot-welding, and so forth. It is intended in this paper to examine the possibilities of assigning intermittent service ratings to electronic devices, giving due consideration to the methods proposed in the recent AIEE report No. 1-A on "General Principles for Rating of Electrical Apparatus for Short-Time, Intermittent, or Varying Duty," by standards co-ordinating committee 4.[1]

In any analysis of the rating of electronic devices the following must be kept in mind:

1. The flow of current through each anode, and consequently through a single-anode tube, is intermittent in nearly all applications. This is shown in Figure 1, case A_1, which illustrates the current flowing with a single-anode rectifier. The same condition applies with a single-phase full-wave rectifier for each anode. Case C_1 shows the current in each single-anode tube for three-phase rectification; similarly E_1 applies to six-phase arrangements. In some applications the time of current-flow is even less, as in the ignitor circuit tubes of ignitrons, illustrated by case G_1. If the application is such that the current-flow periods repeat continuously, it is customary to consider it continuous service, which for each anode means continuously repeating pulsations of the current. Regardless of this continuous feature, it must be realized that dependent upon the duration of each pulsation and the amount of idle time between pulsations, different current rating values may have to be assigned for the different cases of Figure 1 and similar variations, in order to bring about economical use of the electronic devices.

2. In all electronic arrangements consisting of two or more single-anode tubes, careful distinction must be made between the rating of individual tubes and that of the complete tube combination. Cases B_1, D_1, and F_1 show the output of two-, three-, and six-tube arrangements corresponding to the single-anode currents of cases A_1, C_1, and E_1, respectively.

3. The output of the tube combination may be continuous as discussed so far, or intermittent as illustrated for equal time on and off in B_2, D_2, and F_2 of Figure 1. The currents in the individual single-anode tubes for these cases are shown in A_2, C_2, and E_2 respectively. The current here is intermittently pulsating. G_2 shows an intermittent load for short-time individual pulsation as might be found in the individual ignitor circuit tubes used in seam-welding. Ratings for such intermittent tube loads may be different from those of the previously described continuously pulsating loads.

Rating of Single-Anode Tubes

Depending upon the type of tube and type of load, the permissible rating of a single-anode tube may be limited by any one of the following factors:

(*a*). *Heating of the Tube as a Whole.* With the usual assumption of constant voltage-drop in gas-filled tubes, it is customary to consider the average current as the principal factor in this respect. Investigations by Knowles and McNall[2] indicate, however, that with short-time pulsations in a hot-cathode tube the assumption of constant voltage-drop does not hold true, and this may have to be considered under some conditions. (In the case of rectification of frequencies of hundreds or thousands of cycles, there is considerable additional anode heating due to positive ion bombardment of the anode during the inverse part of the cycle, a fact which, of course, must receive due consideration.)

(*b*). *Heating at the Lead-in Conductors and Seals.* This is influenced largely by the rms current, and if it is too high difficulties may develop. (Unfortunately the heating of the lead-in conductors and of the tube as a whole are interdependent, making a clear-cut distinction between the limits imposed by the average current and the effective current difficult.)

(*c*). *Values of the Individual Current Pulsations and Their Duration and Frequency.* These may be important factors in limiting tube ratings for a number of reasons dependent on the physical phenomena entering into the operation of various types of tubes. (The sparking tendency of oxide-coated filament tubes and the back-fire phenomena in pool-type tubes are examples of this.)

(*d*). *Requirements for Satisfactory Life of the Tubes.* Even though the limitations imposed by items a, b, and c have been taken into account to assure satisfactory operation, rating values may have to be made the subject of further adjustments to assure satisfactory life of the tube. (The life of oxide-coated filaments or the life of the covering of cold cathodes are examples of this. The electron emission in a hot-cathode tube decreases during life, and in many cases the end of life is determined by lack of cathode emission. Therefore, tests or ratings involving peak-current-carrying capacity, as well as any other current rating, of the tube must take this into account.)

Separate consideration will have to be given to each type of tube for various service conditions before any plan toward a reasonably uniform method of rating can be devised. As a contribution toward a satisfactory system of rating a number of typical cases will be considered in the following.

Vapor-filled tubes with oxide-coated filaments will be considered first. Rating data for those tubes usually give the continuous average current rating and a limiting peak or crest value for the current. The latter ranges in some tubes from four to six times the continuous average current value, or conversely, the continuous average is 25 to 16.6 per cent of the specified peak value. In Figure 1 the rms and average current values are indicated in per cent of the crest values for each of the cases shown. It will be noted that in all cases of the single-anode tube currents except A_1 and C_1 the average current is less than 16.6 per cent. This in turn means that with the data indicated above, which are those usually given, the average current is the limiting factor in cases A_1 and C_1, while in all other cases the specified peak current limits the permissible load, regardless of the duration and frequency of the current impulses. A question therefore arises as to whether better utilization of these devices might not be brought about by some other method of specifying ratings more suitable for loads with short-time pulsations. Knowles and McNall have made an investigation on the sparking point of

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R. E. HELLMUND was chief engineer of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., at the time of his death, May 1942.

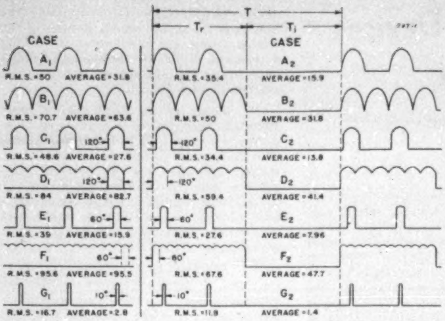

Figure 1. Typical load current for tubes and tube combinations

Cases A_1, A_2, C_1, C_2, E_1, E_2, G_1, G_2—Single anode currents

B_1, B_2, D_1, D_2, F_1, F_2—Currents for multianode arrangements

A_1 to F_1—Continuous loads

A_2 to F_2—Intermittent loads

Rms and average current values in per cent of crest values are indicated in each case

the filament as depending upon current and time. Some of their results are rearranged in Figure 2. It will be noted that with the minimum filament voltage of 2.25 volts, peak currents of 2.75 amperes for long durations of the pulsation are indicated. The curve also shows that the permissible peak loads can be increased by 2 per cent, 11 per cent, 32 per cent, and 80 per cent, for 180, 120, 60, and 10 degrees of the 60-cycle wave respectively; especially in the last two cases the gain is appreciable and seems to make it worth-while to standardize on several ratings suitable for service with various short-time pulsations.

The underlying physical phenomena make it necessary to specify for each of such ratings:

1. The wave shape of the impulse.
2. The crest value of the impulse.
3. The duration of the individual impulse.
4. The frequency at which the impulses can be safely repeated.

1. *Wave shape.* The tests by Knowles and McNall were made with a rectangular impulse wave. In actual practice a variety of waves is likely to be found, some of which are shown in Figure 3. Although all of these waves have the same crest value and the same total time of current-flow, it is obvious that the effect of wave A, for instance, upon the sparking tendency and other characteristics of the tube is likely to be considerably different from that of wave F. Since it is obviously impracticable to establish a multitude of standards for all these wave shapes, a few typical ones will have to be selected as a basis for standardization; the choice will be determined largely by the wave shapes encountered in practical applications. A near rectangular or a sinusoidal wave, or both, may at times be selected for this purpose. In the application of tubes waves approaching a rectangular wave shape (see cases C to G of Figure 1) are at least as frequent as sine waves (A and B). The ease with which tests, especially life tests, can be made with a given type of wave should probably also have an important influence upon the final choice. Multiphase arrangements of single-anode tubes on 60-cycle circuits with at least six phases (six tubes) give a wave shape approximating a rectangular wave (Figure 1, case E_1), and thus seem to be very convenient for test purposes. Therefore, standardization with such a wave shape of the impulse as a basis seems advisable in addition to standardization with a sinusoidal wave shape. Tests for standards with shorter impulses can be made either by increasing the number of phases (tubes) or by using basic frequencies above 60 cycles, depending upon the case. (In case of rectangular waves the steepness of the wave front may have to be specified, as destructive cathode bombardment may result if full anode current is drawn in time less than ionizing time of tube.)

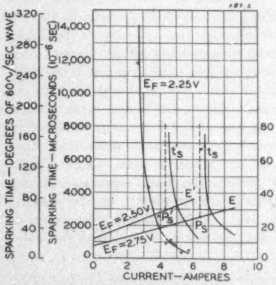

Figure 2. Current values dependent upon time above which cathode sparking may occur in a vapor-filled oxide-coated-filament tube

2. *The crest value of the impulse* has the advantage of simplicity over the use of rms or average values, since it obviates in many instances the necessity for calculating these latter values.

3. *The time of the individual impulse* may be expressed in units of time such as milliseconds or microseconds; this practice has the advantage of being most generally applicable to all conditions found in actual practice. However, in some applications it may be convenient to express the time in degrees of some standard frequency, such as 60 cycles. (If a near-rectangular wave with a peak value and time specified is used as a basis for standardization, a question naturally arises as to how a tube so rated can be applied to pulsations having different wave shapes, such as shown in Figure 3. A practical and safe method can be based on the assumption that current values below a certain percentage (possibly below 50 per cent) of the peak value will have but negligible effect upon either the sparking tendency or the life of the tube. If this is true, the time t during which the current exceeds such percentage can be safely considered the effective time of the impulse, as illustrated in Figure 3. For example, an application with a wave shape as in case F would use a tube standardized on a much shorter rectangular impulse duration than would be used in D, although the total time of the current flow is the same in both cases.)

4. *The frequency at which the impulses can be safely repeated*, either continuously or for a limited period, must be given with the rating data. The tests on sparking tendency by Knowles and McNall were carried on with single impulses. If several such impulses were to follow each other at very close intervals, their effect might approach that of one longer interval, and sparking might result. If on the other hand the impulses are repeated at not too frequent intervals, the only result is likely to be a slight increase of the temperature of the filament, which in general will have a favorable effect on the operation at the lower filament voltages and no particularly harmful effect at the higher filament voltages. If the impulses are repeated too frequently for extended periods, limitations may also be reached, because of exceeding the safe average or rms currents of the tube, or because the life of the tube is decreased below satisfactory values. Various other limitations may have to be considered in this connection. The maximum frequency of impulses at which any of the limitations are reached can be readily determined by test, so that usually it is not difficult to establish a maximum frequency that is safe. In many arrangements connected to commercial circuits with a definite frequency, such as 60 cycles, a pulsating frequency in each single-anode tube in excess of the circuit frequency cannot be readily obtained and therefore is not of any practical value.

Since this paper deals essentially with current ratings, little mention has been made of the voltage conditions in the tube. It is of course appreciated that the various factors relating to current ratings

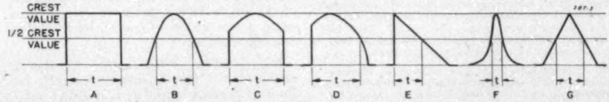

Figure 3. Typical wave shapes of current impulses which may flow in electronic devices

may be appreciably influenced by the operating voltage, the inverse voltage, the rate of rise of the inverse voltage, and so forth. This simply means that the complete rating structure must take such influences into account where they are of importance.

If pulsations as defined by the quantities previously discussed can be repeated continuously and without overheating the tube or any of its parts, no further information on the current rating is required. Practical applications of this kind are illustrated in E_1 and G_1 of Figure 1, as in these cases the average or rms values indicated are not likely to give unsafe temperatures. On the other hand, for applications A_1 and C_1 the average currents are likely to be in excess of safe values with continuously pulsating service. However, the current values of cases A_1 and C_1 can be safely applied for intermittent pulsating service, such as illustrated by cases A_2 and C_2. In rating tubes for such service, some additional rating information must be given. The most convenient method of rating to be applied here seems to be the duty-cycle method described in AIEE report No. 1-A. However, in applying this method to the case at hand, it requires some modification. The method calls for the specification of the maximum load time per duty cycle and a time factor k indicating the maximum ratio of load time or "on" time to the total time per load cycle. Due to pulsating nature of the load current, adherence to the exact wording of report 1-A would be confusing. In Figure 1 the time T_r indicates the time of the cycles during which pulsations occur, and T indicates the time of the total number of system cycles for one duty cycle. It is obvious that confusion can be avoided by giving the maximum permissible number of pulsations per load cycle and the ratio k of these pulsations to the total number of cycles of the system frequency per load cycle. These two values must be so chosen that satisfactory operation and life of the tube is assured. (Consideration has been given to the use of the service-factor method described in report 1-A, but it was found that the use of this method would unnecessarily complicate matters for the specific case without giving any compensating advantage. This really is to be expected, because the tube ratings under discussion depend upon many factors other than the rms current, which is one of the basic values of the service-factor method.)

In some respects the considerations up to this point have been specific to vapor-filled tubes with oxide-coated filaments. However, many of the conditions are the same as for most other types of tubes, at least qualitatively. Quantitatively, there may be marked differences, because the limitations are imposed by different physical phenomena. In an ignitron, for example, tendencies toward back-fire or the possibility of losing control of the tube by the ignitor will govern the amplitude and time of the current impulses and the permissible number of impulses per load cycle; the ratio of the permissible peak currents to the average and rms values is, however, many times larger than that with oxide-coated filament tubes; again various voltage conditions may influence some of the current ratings to a considerable degree. (While some of the restricting factors for the ignitron have been mentioned merely to point out certain differences, it should not be inferred that the ignitron involves more factors for satisfactory operation than a hot-cathode tube. A complete analysis is likely to indicate that on the whole the hot-cathode tube has more restrictions and complicating factors.)

Conditions with the cold-cathode tube are somewhat peculiar, as pointed out by G. H. Rockwood.[3] He gives a curve showing that with every load current of these tubes there is a certain total life in hours of current flow. He also indicates how the life of these tubes can be calculated from his curve for other than rectangular wave shapes. With this information it is possible to calculate for any required type and amplitude of load current the percentage of idle time which must be allowed for to assure a desired total life of the tube. In this manner the time factor k can be determined. There is no need in this case to specify the maximum number of impulses per load cycle, because the "on" time can be distributed in any desirable manner over the entire life of the tube. It will thus be seen that even the cold-cathode tube, although very different from the mercury-pool or the hot-cathode tube may be fitted into the general pattern of rating structure previously described.

The method of rating single-anode tubes suggested here should enable the designer of tube circuits to apply the tubes to best advantage. It unfortunately means the assignment of a number of different ratings corresponding to a variety of practical applications encountered. However, there seems to be no way of avoiding this with the many different phenomena and considerations entering into the operation of tubes. Any attempt to pattern tube ratings after the familiar type of ratings of most electromagnetic apparatus is contrary to the basic principles involved and therefore bound to result in misapplications or uneconomical use of the devices. If single-anode tubes are used by themselves, as may be the case with half-wave rectification, it may be advisable to give in addition to the rating structure described, the rms current and possibly the average current for the various ratings to assist in the selection of conductors, protecting devices, and so forth, connected in the tube circuit.

Rating of Tube Combinations and Circuits

For many practical applications, several single-anode tubes are used in combination, and in addition other apparatus such as transformers, reactors, capacitors, resistors, and so forth, are often employed in the combination, which has either a certain output as in the case of rectifiers or inverters, or a certain carrying-capacity as in applications involving control functions. The heating of most of the additional apparatus is determined by rms current values and usually the output or carrying-capacity of the entire combination in rms values is of principal interest to the user. (There are some exceptions to this, for instance, battery chargers, where the average current value is of importance.) For these reasons, and also because most users are accustomed to rms ratings, it is usually desirable to base the ratings of the combination arrangements wherever possible on rms values unless the load is of a type requiring pulsations, such as the ignitor circuit of an ignitron. (See G_2 of Figure 1.) For cases B_1, D_1, and F_1 of Figure 1, a continuous rms rating can readily be given, and for cases B_2, D_2, and F_2 the assignment of an intermittent or load-cycle rms current rating seems to be desirable. In most applications similar to D_1 and F_1 the difference between the rms and average ratings is of little practical importance, and in cases like B_1 filters are often used and minimize the difference. In B_2, D_2, and F_2 it is necessary on account of the tube characteristics previously discussed to specify the maximum duration of the "on" period per load cycle. It thus seems simplest to complete the rating structure by merely adding the time factor k, which is also limited by the tube characteristics as previously outlined.

Whenever additional apparatus is used in the circuit combination, certain rating limitations may be imposed by this apparatus. This may at times result in the

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assignment of a time factor k smaller than that called for by the tubes. For the purpose of selecting certain standard devices such as fuses, switches, and so forth, connected in series with the tube device, the rms currents corresponding to the various load-cycle ratings should be known. Although with the factor k specified, the service factor can be readily calculated, it may nevertheless be desirable to give in the rating structure the service factor in addition to the time factor k. However, the service factor here cannot, as in most other apparatus, be construed as meaning that the "on" current and the "on" time can be varied at will as long as the service factor rms value is not exceeded. (Careful study of individual types of tubes and tube combinations may disclose some cases where this interpretation can be used. For the sake of flexibility in application, it is of course desirable to at least explore in many cases the possibilities of service-factor ratings which permit variation of current and time values at least below the rated current, and preferably also above this value, within the limit of an rms rating indicated by the service factor.)

Unfortunately the load-cycle method proposed is not very flexible in its application. It restricts the application of each load-cycle rating to but a limited variety of load-cycle conditions. The time $T = T_r/k$ (in which T_r is the specified maximum "on" time per load cycle) corresponds in some ways to the so-called "averaging" time used to some extent in tube specifications. Satisfactory results will be obtained with the rated current, if there are several load cycles instead of one during the time T, as long as the ratio of the total "on" time T_r to T does not exceed the value k. Additional load-cycle ratings have to be given for applications with different current values.

It may be advisable to illustrate briefly here the rating and application method for cold-cathode tubes. Assuming, for instance, a current of 50 milliamperes in the tube described by Rockwood, the life is found to be about 42 hours a tube. Therefore, if six such tubes are used in a six-phase arrangement, giving approximately rectangular wave shapes for the impulses of each tube, the combination would have a life of about 250 hours at 50 milliamperes. This, then, simply means that if a tube combination of this type is applied in service where a life of 10,000 hours is required, the factor k should not exceed the value of 0.025, which may be entirely practical for the relay service mentioned by Mr. Rockwood. For this case the maximum "on" time per load cycle could be given as 250 hours, although this would be of no practical significance in the usual relay application having a large number of short operating periods. The principal condition is that during the 10,000-hour life the total load time does not exceed 250 hours. (This is merely given as an illustration. With the relay service under consideration by Rockwood, an arrangement of one or two tubes is more likely to be used, and in such cases the impulses will be sinusoidal.)

The examples in Figure 1 all deal with rectification. Another function of tubes is that of switching or controlling alternating current for such purposes as spot welding, for instance. The individual single-anode tube here again carries pulsations which are either sinusoidal half waves as in Figure 3B, or sinusoidal waves with delayed starts as in Figure 3D. The current handled by the usual 2-tube combination is alternating instead of direct current as in cases B_2, D_2, and F_2 of Figure 1, and the load is as a rule intermittent. The operation of the individual anodes is the same as previously discussed; the conditions with regard to multianode arrangements are different only in so far as the current ratings for the combination should be expressed in rms values of alternating current, with all other factors handled as previously indicated.

Summarizing, the rating structure suggested here comprises:

For Single-Anode Tubes or Each Anode of a Multianode Tube	**For Combination Arrangements**
A. Continuous Ratings	
1. Wave form of impulses **2. Crest value of impulses** **3. Time of impulses** **4. Frequency of impulses**	**1. Rms nominal current rating**
B. Load-Cycle Ratings for Intermittent Service	
Same as above supplemented by— **5. Maximum number of impulses per load cycle (or equivalent time)** **6. Time factor k**	**2. Maximum active time per load cycle** **3. Time factor k** **4. Service factors in addition to k**

In devising a system of ratings, it must be considered that under many conditions the maximum "on" time T_r per load cycle and the time factor k are interdependent. Usually k has to be decreased with an increasing value of T_r. This is always the case when heating considerations are the determining factor. (See references 4 and 5 which demonstrate this in connection with machines and other apparatus. For tubes this is more fully discussed in a companion paper by Marshall and Arnott.[6]) For this reason T_r should not be chosen any larger than necessary for practical applications. By keeping T_r low, the value of k is favorably affected.

The above rating structure for continuous loads of single-anode tubes is not proposed to the exclusion of the present method of giving the average current for continuous ratings of tubes. It is intended essentially for cases where it results in more economical application of tubes than the conventional method, or where it is found more convenient. The use of the rating structure on the left side of the table is not necessarily limited to single anodes. It may also be found convenient for multianode arrangements primarily intended for supplying pulsations. If it should happen with cases G_1 and G_2 of Figure 1, for instance, that a single tube does not permit a frequency of pulsations high enough for a given application, a two-tube arrangement can be used to good advantage and may then be rated as indicated on the left side of the table. The rating structure on the right side is intended for all cases where direct or alternating currents are desired, and where pulsations or harmonics may or may not be present as an undesirable by-product of the circuits employed.

For load-cycle ratings the nominal rating is the value of the current during the load or "on" period of the load cycle. The service factor S is the ratio of the rms current for the entire time of the load cycle to the nominal rms current value during the "on" period.

Whenever tubes have a limited life, the expected life in hours must necessarily enter into the rating structure somehow, because the choice of the quantities given in the table may be materially affected by the assumed life. A typical example of this is the cold-cathode tube previously described.

As a rule, all of the quantities of the table except the service factor S, which may be given for convenience, are essential to definitely limit the application of the device to service it can handle satisfactorily. (There are a few exceptions to this, such as the cold-cathode tube where the indication of the maximum active time per load cycle is of no practical importance.) However, the importance of the different quantities may vary appreciably with different tubes and applications, as may the reasons why the factors must be limited to the values given. In hot cathode tubes, for instance, the crest value of the impulses is quite a decisive value, while in many other types of tubes the maximum crest value is not such an important limitation. The maximum active time per load cycle may be primarily limited by heating considerations of various structural parts in hot-cathode tubes, while back-fire considerations or loss of proper control (also largely influenced by heating) may be most essential in some pool-type tubes. The time factor k, which determines the minimum idle periods, may be chiefly determined by heating consideration in a hot-cathode tube, while in a cold-cathode tube its value is determined essentially by the expected life of the tube. However, regardless of what these numerous variations may be, it seems that the suggested rating structure satisfies almost every conceivable condition that could be encountered in practice with continuous or intermittent service, although some exceptions to this can undoubtedly be found. It is fully realized that many factors (especially various voltage conditions) not discussed here in detail will have to be given careful consideration in the setting of current ratings. It is obvious that in the brief review of rating structures given here it would be impractical to attempt a comprehensive treatment of all phenomena involved. Some of the more important considerations not covered here will undoubtedly be pointed out in the discussion.

With the load-cycle current ratings so far discussed it has been assumed that they are intended for typical intermittent loads with definite requirements, as, for instance, the operation of relays designed for such current. Many applications like this are found in practice and can be taken care of by a single load-cycle rating. However, with many other tube arrangements, it may be desirable to give sufficient rating information to facilitate their application for a variety of intermittent service conditions. In Figure 4 the curve shown is intended to indicate the maximum "on" time permissible for a specific tube arrangement for various nominal load currents. Since it seems undesirable to list a great number of different current ratings, only a few, such as Pn_1, Pn_2, Pn_3, and Pn_4, are indicated in the figure. For each of these the maximum "on" time per load cycle can be given, as indicated by the values T_1, T_2, T_3, and T_4. In addition the value k would have to be given for each case. This then would make possible the application of the arrangement within its range of satisfactory performance for different applications, as intermediate values could be readily interpolated. A question arises, however, as to how a single nominal commercial rating should be selected for such a condition, especially for service where any of the loads are at times applied for considerable periods, as may occur with spot-welding arrangements for instance. There is not much choice between the different ratings in selecting one as a nominal commercial rating under these circumstances. The continuous rating P is so far from representing actual service conditions that it would not be a very satisfactory nominal rating. The maximum rating P_m is not very well defined because the time T_5 in Figure 4 may range from one cycle in the case of spot welding to the conventional period of one minute used in connection with the larger rectifier equipment.

One possible method of selection might be to establish certain standard values for the maximum "on" time and select ratings according to such time values from the curve. Another possibility would be to set certain standard ratios between the nominal and the continuous rating, or between the nominal and the maximum rating, the latter to be based on the time values of practical significance for the particular type of tube or service, such as one cycle for spot-welding arrangements and one minute for large rectifying equipment. Some measure of uniformity could be accomplished by always using preferred numbers for these ratios, as indicated in Figure 4, where all the steps between the different ratings have been selected to be 25 per cent, corresponding to the coefficient of the ten series of preferred numbers. In this case the load Pn_3, which is about halfway between the continuous and short-time maximum rating, has been indicated by a heavy line to serve as the nominal commercial rating. It would seem highly desirable to devise some standard method for settling some of these questions, as it would greatly assist in the gradual evolution of a reasonably uniform method for electronic devices.

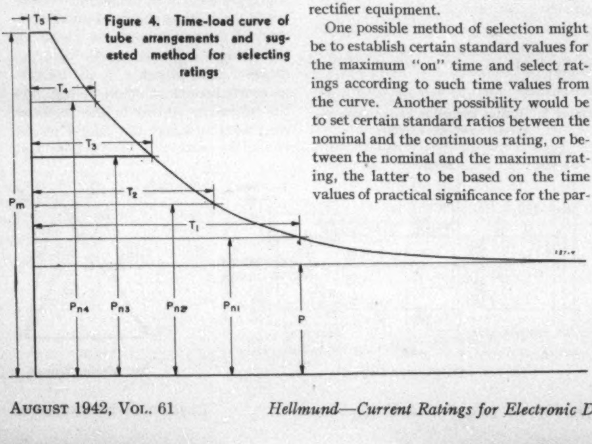

Figure 4. Time-load curve of tube arrangements and suggested method for selecting ratings

As previously stated, the suggestions given here are merely preliminary; final recommendations should not be given until considerably more work has been done in analyzing the various conditions. However, it is hoped that the brief outline will stimulate others to investigate various practical arrangements and applications for the purpose of determining whether the suggestions made here can be used, or whether they can be altered in some way to make them even more generally applicable. It is obvious that no system of rating applicable to all conditions can be devised; however, it seems entirely possible to develop a system capable of broad application and requiring only slight modifications for specific cases. At any rate, it should be possible to at least establish a nomenclature defining such terms as time factor, service factor, and so forth, which will be generally applicable and definite in its interpretation in all applications.

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Ignitor Excitation Circuits and Misfire Indication Circuits

A. H. MITTAG
MEMBER AIEE

A. SCHMIDT, JR.
ASSOCIATE AIEE

Synopsis: Several circuits are available for excitation of ignitron rectifiers. Static magnetic-impulse circuits are used for most applications. Several such circuits are discussed in some detail. Ignitor requirements are given, and circuit requirements affecting the choice of excitation circuits are indicated.

While ignitor failures are rare, they should be detected promptly to utilize all apparatus to the best advantage. Two conditions may arise

1. When two anodes operate in parallel from a single transformer winding.
2. When a single anode is connected to each transformer winding.

Means for detecting ignitor misfiring are described for both conditions.

An ignitron is an electric valve in which the cathode spot or electric arc is established every cycle at the beginning of the conduction period of the main anode and is allowed to extinguish at the end of the conduction period. The arc is established by means of an ignitor, which is an element made of a high-resistance material and partly submerged in the cathode mercury pool. The ignitor is excited or fired by passing an electric current of sufficient strength through it into the cathode to establish a cathode spot or arc. Static magnetic-impulse excitation circuits have certain definite advantages over other ignitor excitation circuits.[1,2] The circuits here described satisfy certain operating conditions.

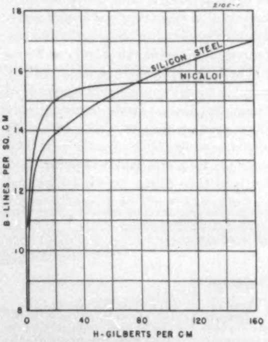


Figure 1. D-c magnetization curves

As current is built up in the ignitor to the firing value, power is consumed by the ignitor. At the instant the cathode spot appears most of the ignitor current transfers to the electric arc instead of passing through the whole length of the ignitor to the cathode, and the voltage across the ignitor suddenly collapses to arc-drop value. It is therefore desirable to build up the ignitor current rapidly to the firing point to limit the losses and the heating of the ignitor. Further, since the ignition current varies considerably from cycle to cycle, a rapid build-up of ignitor current will result in a more uniform firing angle.

Magnetic-Impulse Excitation Circuits

To obtain a rapid build-up of current or peaked current wave, a nonlinear reactor is used

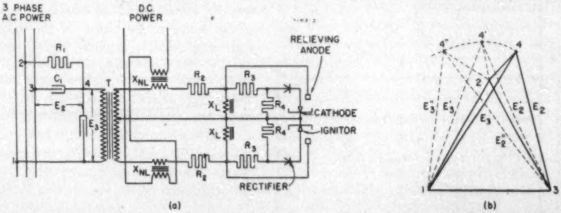
In the high impedance region of such a reactor, that is, below its saturation point, there is substantially no ignitor current. As the impedance decreases, the rate of change of ignitor current increases. An abrupt transition to a peaked current wave requires a sharp knee in the magnetization curve of the reactor; a rapid rise of current requires a low saturated reactance. The use of an alloy such as Nicaloi (see Figure 1) and a well-interleaved core construction result in

1. Sudden initiation of impulse current.
2. A maximum ratio of unsaturated to saturated reactance values. A capacitor may be used as a storage reservoir so that other circuit reactance will not reduce the rate of rise of the firing impulse current.

In Figure 2 is shown a magnetic-impulse ignitor excitation circuit. Here a nonlinear reactor X_{NL} is used for each ignitor. This is necessary, because the reactor is biased with d-c ampere-turns to obtain a high peak current in the a-c winding in one direction but not in the other. The high peak current is allowed to pass through the ignitor in the positive direction, that is, from ignitor to cathode, while the low negative current is blocked by a dry-plate rectifier in series with the ignitor and instead flows through the linear reactor X_L. This circuit gives a peaked current wave having a base 60 electrical degrees long, that is, the arc is maintained for 60 electrical degrees of the fundamental frequency. In order to relieve the ignitor and the rectifier in series with the ignitor of this current after the arc has been established, a relieving anode and transfer resistor R_3 are used. The resistance R_4 reduces the inverse voltage impressed on the rectifier. The resistance R_2 limits the current in the circuit. Voltage control of the d-c output of an ignitron rectifier can be obtained by retarding the firing of the ignitors. The capacitors and resistance connected between the power supply and the primary of the transformer T, together with the amount of d-c saturation of the nonlinear reactors X_{NL}, permit voltage control by shifting of the ignitor firing.

The vector diagram shows how this shifting of phase is accomplished. Vector 1-4 is the voltage impressed on the primary of the transformer, and vector 4-3 the voltage impressed across the condenser C_1, and triangle 1, 2, and 3 represents the three-phase supply voltage. As the saturating current in the nonlinear reactors is increased, the point 4 travels

Figure 2. Ignitor excitation circuit using d-c biased nonlinear reactors



(a) Circuit diagram

(b) Vector diagram

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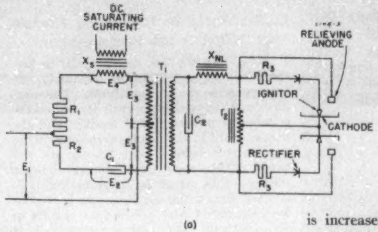
A. H. Mittag is of the consulting engineering laboratory and A. Schmidt, Jr. is development engineer, both with General Electric Company, Schenectady, N. Y.



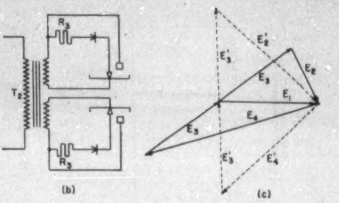


Figure 3. Ignitor excitation circuit using saturable reactor X_s for controlling phase of firing

(a) Circuit diagram
(b) Modified circuit diagram
(c) Vector diagram

on the locus, 4, 4′, and 4″. Thus the voltage impressed on the transformer advances as the saturating current is increased, and therefore the angle of ignitor firing advances with increasing saturating current. The purpose of resistance R_1 is to hold the amplitude of the voltage E_3 more nearly constant. The advantage of this firing circuit is the long period over which the arc is maintained.

Figure 3 shows an excitation circuit in which one nonlinear reactor X_{NL} is used to fire two ignitors. The nonlinear reactor is allowed to saturate in both directions, thus giving peak currents in both directions. An autotransformer T_2 is used to provide a return path for the ignitor current from the cathodes of the ignitrons. In case the cathodes must be insulated from each other, this can be done by making the autotransformer an insulating transformer as shown in Figure 3b. The shifting of the angle of ignitor firing in this case is accomplished by a saturable reactor X_s, together with a capacitor C_1. The resistances R_1 and R_2 keep the voltage wave across the transformer T_1 symmetrical and in this way insure that the two ignitors will fire 180 electrical degrees apart. Capacitor C_2 helps to increase the peak volt-amperes delivered to the ignitors.

In the vector diagram, E_3 is the voltage impressed on the transformer T_1 with low saturating current in the saturable reactor, E_2 is the voltage across the capacitor C_1, E_4 is the voltage across the a-c winding on the saturable reactor, and E_1 is the line or supply voltage. As saturating current in the saturable reactor is increased, the a-c voltage across the saturable reactor decreases, and the voltage across the capacitor C_1 increases as shown by the dotted vectors. The voltage E_3 on the transformer advances from E_3 to E_3' and consequently the ignitor firing advances. The voltage drops across the resistances R_1 and R_2 are neglected to simplify the diagram. In this firing circuit the arc is maintained for about 25 degrees. The advantages of this circuit over the one shown in Figure 2 are that the power consumed is less and the apparatus is smaller.

Figure 4 shows still another type of excitation circuit which shortens up the period over which the arc is maintained to about 18 degrees. This reduces the power drawn by the circuit still further, being only about 200 watts per ignitor. A modification of the autotransformer similar to Figure 3b will provide for insulated cathodes. The phase-shifting network consists of a saturable reactor X_s, a linear reactor X_1, and a capacitor C_1.

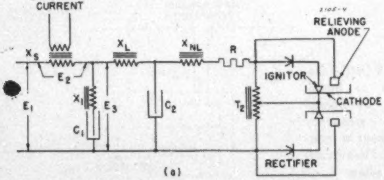
The vector diagram shows how the voltage E_3 varies in phase, with substantially constant amplitude as the voltage vector E_2, which is the a-c voltage across the saturable reactor, is changed in amplitude by means of the d-c saturating winding. The angle of firing of the ignitors shifts with the angle of the vector E_3.

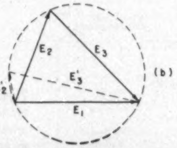
Figure 4c shows the current in the nonlinear reactor, the voltage impressed on the ignitor, and the supply voltage.

Sometimes it is desirable to operate the main anodes of two ignitrons in parallel, and in that case the ignitors must be fired simultaneously in order that the main anodes will divide the current equally. This is best accomplished by firing four ignitors from one ignitor excitation circuit as shown in Figure 5. The current dividing autotransformer T_3 causes the two ignitors connected to the ends of the same transformer to be fired practically simultaneously. If the other transformers, reactors, resistors, and capacitors in the excitation circuit are built for the same voltage, but twice the current rating, we will have a circuit that is twice the size in kilovolt-ampere rating and will draw twice as much current and power from the line to fire twice as many ignitors. However, it has been found that the peak volt-amperes that this circuit is capable of delivering to a hard firing ignitor have been practically doubled. There is therefore a distinct advantage when operating ignitrons in parallel in firing their ignitors by this arrangement.

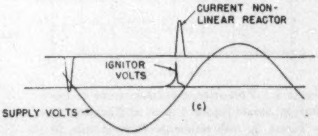
In order that various kinds of ignitor excitation circuits can be compared, it is necessary to have some way of measuring the peak voltage and current that the circuit is capable of delivering to the ignitor. In a circuit designed to use relieving anodes a variable resistance is connected in series with one ignitor between the ignitor and the blocking rectifier. In case the circuit is designed to operate without relieving anodes, a relieving anode should be used during this test connected as shown in Figure 6. This variable resistance can then be considered as being part of the ignitor, and the two together represent a high-resistance ignitor. The amount of current required to fire the ignitor can be varied by changing the depth of immersion of the ignitor in the cathode. With deep immersion a high current is required, and with shallow immersion a low current. For each different ignitor immersion depth, the resistance R is increased to such a value

(a) Circuit diagram





(b) Vector diagram



(c) Voltage and current waves

Figure 4. Low-loss ignitor excitation circuit

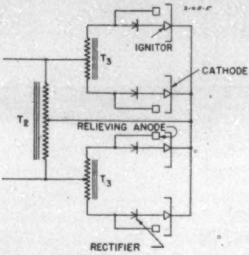


Figure 5. Method of firing two ignitors in parallel

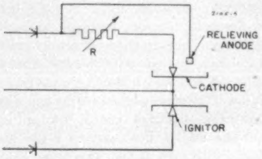


Figure 6. Circuit for measuring output of excitation circuit

that the ignitor misfires occasionally. With this setting the peak voltage across the resistance and ignitor is measured during a misfire. This is the peak voltage the circuit is capable of delivering simultaneously with the peak current. The peak current is determined from the voltage across *R*. A curve can then be plotted showing the peak current and the peak voltage the circuit is capable of delivering to the ignitor. Such curves are shown in Figure 7. A circuit test made by substituting a resistance for the ignitors can give misleading results, because a resistance does not have the nonlinear characteristic of an ignitor at the instant of firing.

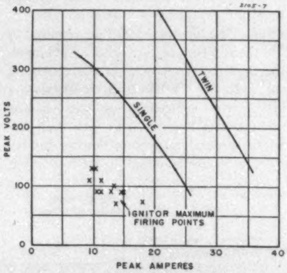


Figure 7. Volt-ampere output curves of excitation circuit Figure 4 and modification as in Figure 5, with maximum firing points of typical ignitors

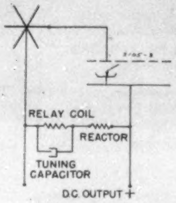


Figure 8 (left). Detection of ignitor misfire by fundamental - frequency component in d-c ripple

Figure 7 shows the peak volts and amperes that a typical circuit as in Figure 4 is capable of delivering to the ignitors. The curve labelled "single" is for the circuit as shown in Figure 4 and the curve labelled "twin" is for the circuit in Figure 4 when modified according to Figure 5 and using apparatus with twice the current rating. The points indicated by crosses in Figure 7 represent maximum peak voltage and current to fire typical ignitors. This shows that the circuit is putting ample power into the ignitors.

Detection of Excitation Failure

During operation of an ignitron rectifier one or more anodes may fail to conduct, due to a fault in the ignitor firing circuit or in one of the ignitors. Failure may be occasional or persistent.

Persistent misfire of one anode in a rectifier where two anodes are operating in parallel results in increased load being carried by the companion anode with a corresponding increase in arcback probability. In other circuits misfire of an anode may result in increased duty on one or more of the other anodes, with

1. Corresponding greater arcback probability.
2. Increase in transformer heating.

In any case, an indication of faulty operation is desirable, so that the fault may be remedied and normal operation continue.

Excitation failure may be detected by:

1. Action of the power circuit.
2. Action of the excitation circuit.
3. Action of auxiliary electrodes in the rectifier.

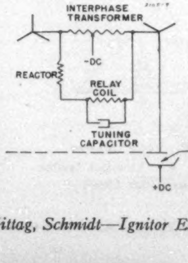


Figure 9 (left). Detection of ignitor misfire by fundamental - frequency component in interphase transformer voltage

I. Detection of Excitation Failure by Action of the Power Circuit

1. If one anode of a rectifier fails to fire, there will be a component of supply frequency in the d-c voltage ripple. The magnitude of this component will depend on the number of phases of the rectifier and the nature of the load. A relay connected to the d-c output voltage and having its coil tuned to the supply frequency will indicate misfire. This circuit is shown in Figure 8. A variation of this arrangement is to connect the tuned relay across a d-c reactor in series with the load. Either arrangement is most effective when the number of phases is small, and the d-c load is fairly reactive.

2. When an interphase transformer is included in the rectifier circuit, a voltage component of supply frequency occurs across the interphase transformer during misfire. A relay or other indicating device tuned to the supply frequency and connected across the interphase transformer will indicate the presence of a missing anode. The connections are shown in Figure 9. In a quadruple-wye circuit, an induction relay with two tuned coils may be used, each coil being connected to one of the 180-cycle interphase transformers.

3. Figure 10 shows a circuit which utilizes the increased positive voltage between anode and cathode, when the anode does not fire. This arrangement is most suitable for rectifiers operating without phase retard.

4. Figure 11 shows a circuit where unbalance between the neutral currents of a double-wye rectifier saturates a reactor and decreases its impedance.

5. Missing of an anode in one wye of a double-wye rectifier generally results in a disproportionate decrease of current in the other anodes of that wye. This is the basis of the circuit in Figure 12, which is affected by the difference between anode currents in the two wyes.

6. The missing of one anode will result in unequal currents in the supply line. This

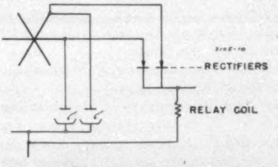
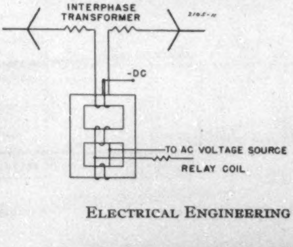


Figure 10. Detection of ignitor misfire by positive voltage of missing anode

Figure 11 (below). Detection of ignitor misfire by wye-current unbalance



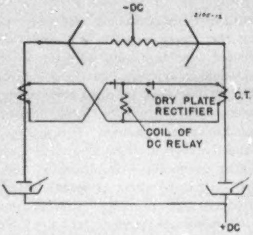


Figure 12. Detection of ignitor misfire by anode-current unbalance

may be recognized by visual observation of line ammeters or by a negative-phase-sequence relay. Operation of a double-wye rectifier circuit on five anodes results in a large negative-phase-sequence second harmonic component, due to the unbalance between the two secondary wyes. A negative-phase-sequence relay is less useful for misfire detection as the number of phases is increased.

7. When two anodes are operated in parallel from the same transformer winding, it is possible to utilize the current-dividing reactor, if one be used, as an indicator of misfire. Failure of an anode to fire would result in a much higher voltage than normal across the current-dividing reactor. This voltage may be utilized to operate an indicating device. Another method of using the current-dividing reactor is to design the reactor so that the core acts as the magnet of a relay and design the relay structure so that there will be sufficient flux to attract the armature only when the unbalance between the anode currents corresponds to misfire of one anode at 50 per cent or more load on the rectifier. Figure 13 is a photograph of such a reactor.

8. Another method of detecting misfire in the case of parallel anodes is to connect in each anode lead a current transformer designed to permit open circuit operation. If the secondaries are connected in opposition, there will be no voltage when both anodes fire, but a resultant voltage if one is missing. This may be used to operate a suitable relay. The resultant voltage will be present if all the pairs of oppositely connected secondaries are joined in series.

It should be noted that the above schemes for detecting missing of one of a pair of parallel anodes will not detect simultaneous missing of both anodes. Such missing will require one of the previously described circuits.

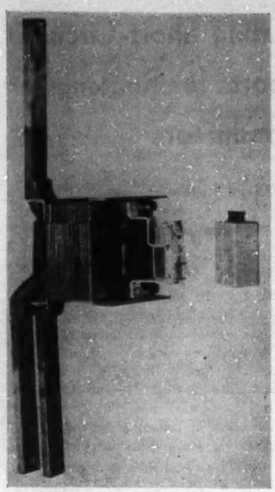


Figure 13. Combined current-dividing reactor and misfire-detection relay

9. When a rectifier is carrying a steady load, as in electrolytic processes, and anodes are not operated in parallel, missing of an ignitor may be detected by observation of the d-c ammeter in the output circuit of the rectifier. This will show a distinct flicker each time misfire occurs. The anode transformer also gives an audible indication of misfire.

II. Detection of Excitation Failure by Action of the Excitation Circuit

Failure of an element of the excitation circuit may result in one of a number of conditions, depending upon the nature of the failure. Since the ultimate purpose of failure detection is to determine if an ignitor is missing, the preferred location of a misfire device is in the circuit of the ignitor itself. Such a device becomes involved as it must function during

1. **Ignitor short circuit.**
2. **Ignitor open circuit.**
3. **Insufficient power to fire ignitor.**

Moreover, there must be one such device for each ignitor. Action of some other circuit than the excitation circuit appears desirable for misfire detection.

III. Detection of Excitation Failure by Action of Auxiliary Electrodes in the Rectifiers

The ignitron rectifier cell usually has an anode baffle which is excited from a different source than the other electrodes. Since current cannot flow in the baffle circuit unless a cathode spot is formed, current indicating means in series with the baffle or voltage indicating means across the baffle resistor will detect misfire.

Location of Missing Ignitor

With the exception of the scheme just described for indicating the presence of baffle current, the above misfire indication circuits do not indicate the specific ignitor which is misfiring. If an oscilloscope is at hand, missing may be located by observation of ignitor voltage. A clampon or tong ammeter may be used to detect current in the main anode. If a low-reading tong ammeter is available, it may be used to measure the current in the baffle. A compass held near an anode bus will show when an anode is not firing.

Conclusion

Static magnetic-impulse excitation circuits have definite advantages in many applications of ignitron rectifiers. Three circuits which have been described have different modes of operation, and their choice is determined by operating requirements. Excitation circuits are generally designed with an ample margin of power over ignitor requirements and in twin circuits for parallel operation. This margin is automatically increased for hard-firing ignitors.

An ignitor may stop firing even with ample excitation power. This condition may be detected in a number of ways. A choice of methods is available for specific applications.

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Formulas for Calculating Short-Circuit Stresses for Bus Supports for Rectangular Tubular Conductors

THOMAS JAMES HIGGINS
ASSOCIATE AIEE

THE mechanical forces acting on bus-bar supports during short circuit are determined in some measure by all of the following:

The magnitude and decrement factor of the short-circuit current.

The mutual electromagnetic forces exerted among the individual conductors.

The natural frequencies, motional resistance, and physical arrangement of the bus-bar structures.

A theoretical treatment, substantiated by experimental data, of the individual and collective effects of these factors on the magnitude of bus-bar support stresses and a method of calculating numerically these effects are to be found in a comprehensive paper by Schurig and Sayre.[1] In shorter articles based on the just mentioned paper Schurig, Frick and Sayre,[2] Tanberg,[3] and Specht[4] have presented certain charts, nomograms, and short-cut methods facilitating the numerical calculations involved in an actual problem. However, regardless of the method of calculation adopted, a determination of bus-bar support stress yet requires calculation of the electromagnetic forces exerted on the conductors carried on the support.

The mutual electromagnetic force exerted between two isolated, long nonpermeable conductors, spaced D inches between their parallel axes and carrying direct currents of I and I' amperes, is commonly calculated from the formula

$$f=(5.4kII'/D)10^{-7} \text{ pounds per foot of bus length} \quad (1)$$

or, if absolute units are used, from

$$f=2kII'/D \text{ dynes per centimeter of bus length} \quad (2)$$

Herein k is a parameter, a variously named constant, of magnitude determined by the geometry and relative position of the two conductor cross sections. In a group of conductors the force acting on any one conductor is calculated by using equation 1 to compute in turn the force exerted on the conductor by each of the remaining conductors and then adding these forces vectorially. If alternating instead of direct currents flow in the conductors, then providing f, I, and I' are taken as instantaneous values, and providing skin and proximity effects are negligible—this latter condition is commonly satisfied if rectangular tubular conductors are used and is sufficiently satisfied for strap conductors if, as is usual, the bus bar considered is either a single nonlaminated strap or is constructed of properly transposed laminated conductors—equation 1 is yet used, and k is the same as for direct currents. Accordingly, simple arithmetic aside, determination of the *electromagnetic* force acting on a given support reduces, in essence, to determination of the needed values of k.

Of essential interest to the designer of polyphase bus structures are the values of k for a group of identical conductors of solid or tubular rectangular cross section with axes in a plane. The formula for k for conductors of *solid* rectangular cross section was derived a quarter of a century ago; as regards rectangular tubular conductors, no formula for k seems to have been published hitherto. Yet as the mechanical and electrical advantages stemming from use of this cross section have resulted in an extensive and currently increasing use of it for feeding heavy current loads—to be noted in particular is its use in new bus installations supplying batteries of welders (in airplane, tank, and automobile factories) and banks of electric furnaces (for alloy and tool steels, aluminum, and various other metals) necessitated by the defense program—it is most desirable to have formulas with which one can quickly calculate the value of k for any conductor spacing or rectangular cross section. Such formulas are derived in this paper.

It is assumed that the conductors are nonmagnetic, right-cornered, and of such length that end effects are negligible, and that they carry currents uniformly distributed over the cross sections of the individual conductors. Of these assumptions all but that of right-corneredness are consistent with or are closely approximated in current practice. But although rounded corners are found on rectangular tubular conductors—to minimize "edge effect"—treating them as right-cornered introduces error negligible with reference to the accuracy required in calculating bus-bar stresses and enables solution of an otherwise intractable problem.

As regards values of k for strap conductors (which usually have right corners), the formulas derived in this paper—a strap conductor is a special case of a rectangular tubular conductor—offer an attractive alternative to the means of determination now used: curves, found in the references previously cited and in most electrical handbooks, these being plotted originally by Professor H. B. Dwight[5] from the above-mentioned formula derived by him. It is illustrated in section III that from equations 17 or 18 one can calculate rapidly, with but *pencil* or *slide rule*, the value of k for two symmetrically located, identical strap conductors.

The mentioned formulas for solid and tubular rectangular conductors obtained, ease and rapidity of use are illustrated by the numerical solution of several problems typical of bus design.

I. The Formal Solution for k

As derived in a previous paper by the writer,[6] the electromagnetic energy associated with a unit length of an isolated circuit constructed of two identical rectangular tubular conductors, symmetrically located as in Figure 1, is

$$W=w^2[W(r,s)+W(R,S)+2W(t,S+t)+2W(R+t,t)-2W(R+t,S+t)-2W(t,t)] \quad (3)$$

where, typically, $W(r, s)$ is defined by

$$W(r,s)=(1/6)[2F(D,s)-F(D+r,s)-F(D-r,s)+2F(r,s)-2F(0,s)+4\pi r^2s(3D-r)] \quad (4)$$

and, typically, $F(r, s)$ is defined by

$$F(r,s)=(r^4-6r^2s^2+s^4)\log(r^2+s^2)^{1/2}+4rs(r^2-s^2)\tan^{-1}(r/s)-r^4\log|r| \quad (5)$$

All units are in the absolute system: the current density w in abamperes per square centimeter; D, r, and s in centimeters; W in ergs per centimeter of line length.

As is well-known, the mutual electro-

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THOMAS JAMES HIGGINS is assistant professor of electrical engineering, The Tulane University of Louisiana, New Orleans, La.

magnetic force exerted between the two conductors is given by

$$f = \partial W/\partial D \text{ dynes per centimeter of bus length} \qquad (6)$$

Equating equations 2 and 6 and solving for k yields

$$w^2 k = [D/2(rs-RS)^2]\partial W/\partial D \qquad (7)$$

Substituting equation 3 in equation 7 gives

$$k = [D/2(rs-RS)^2]\left[\frac{\partial W(r, s)}{\partial D} + \frac{\partial W(R, S)}{\partial D} + 2\frac{\partial W(t, S+t)}{\partial D} + 2\frac{\partial W(R+t, t)}{\partial D} - 2\frac{\partial W(R+t, S+t)}{\partial D} - 2\frac{\partial W(t, t)}{\partial D}\right] \qquad (8)$$

From equation 4 we have

$$\frac{\partial W(r, s)}{\partial D} = (1/6)\left[\frac{\partial}{\partial D}\{2F(D, s) - F(D+r, s) - F(D-r, s)\} + 12\pi r^2 s\right] \qquad (9)$$

The bracketed expression in equation 9, considered as a function of r alone, can be expanded in a power series about the point $r = D$. Substituting this expansion in equation 9 we have

$$\frac{\partial W(r, s)}{\partial D} = (1/6)\left[\frac{\partial}{\partial D}\left\{-r^2\frac{\partial^2 F(D, s)}{\partial D^2} - \frac{r^4}{2\cdot 3!}\frac{\partial^4 F(D, s)}{\partial D^4} - \frac{r^6}{3\cdot 5!}\frac{\partial^6 F(D, s)}{\partial D^6} - \dots\right\} + 12\pi r^2 s\right]$$

$$= (1/6)\left[-r^2\frac{\partial^3 F(D, s)}{\partial D^3} - \frac{r^4}{2\cdot 3!}\frac{\partial^5 F(D, s)}{\partial D^5} - \frac{r^6}{3\cdot 5!}\frac{\partial^7 F(D, s)}{\partial D^7} - \dots + 12\pi r^2 s\right] \qquad (10)$$

The details of the expansion and expressions for the various derivatives, in terms of the dimensions and spacing of the conductors, are given in the appendix. Substituting appropriately in equation 10 and collecting terms, we have

$$\frac{\partial W(r, s)}{\partial D} = 2r^2\left[[2s \tan^{-1}(s/D) - D\log\left(1+\frac{s^2}{D^2}\right)\right] + \frac{r^4}{3}\frac{s^2}{D(D^2+s^2)} + \frac{r^6}{45}\left[\frac{1}{D^3} + \frac{-D^3+3Ds^2}{(D^2+s^2)^3}\right] + \dots$$

$$= 2DM(r, s) \qquad (11)$$

where $M(r, s)$ is defined by

$$M(r, s)/r^2 = 2m\tan^{-1} m - \log(1+m^2) + \frac{n^2m^2}{6(1+m^2)} + \frac{n^4}{90}\left[1 + \frac{-1+3m^2}{(1+m^2)^3}\right] \qquad (12)$$

where $m = s/D$ and $n = r/D$.

Substituting equation 11 in equation 8 yields

$$k = [D^2/(rs-RS)^2][M(r,s) + M(R,S) + 2M \times (t, S+t) + 2M(R+t, t) - 2M(R+t, S+t) - 2M(t, t)] \qquad (13)$$

If $R = S = t = 0$, we have solid rectangular conductors, and equation 13 becomes

$$k = (1/m^2r^2)M(r, s) \qquad (14)$$

Having determined k—note that k is a dimensionless quantity; thus D, r, and s can be measured in any unit of length—the electromagnetic force f can be determined from equation 1.

II. Approximate Expressions for M(r, s)

In most calculations the conductor cross sections and spacing are such that in equation 11 the terms involving powers of n are negligible. If so, equation 12 reduces to

$$M(r, s)/r^2 = 2m \tan^{-1} m - \log(1+m^2) \qquad (15)$$

Although equation 15 can be calculated directly by the aid of appropriate tables or by use of a slide rule, it is more desirable to have $M(r, s)/r^2$ expressed directly in powers of m. Accordingly, substituting in equation 15, the usual power series for $\tan^{-1} m$ and for $\log(1 + m^2)$ and collecting terms, we have the more desirable form

$$M(r, s)/r^2 = m^2 - m^4/2\cdot 3 + m^6/3\cdot 5 - m^8/4\cdot 7 + \dots \qquad (16)$$

where $m < 1$.

Substituting equation 16 in equation 14 we have for solid conductors

$$k = 1 - m^2/2\cdot 3 + m^4/3\cdot 5 - m^6/4\cdot 7 + \dots \qquad (17)$$

where $m < 1$.

Occasionally, in equation 12 the term in n^2 is not negligible in comparison with the first two terms. In this case, expanding $(1+m^2)^{-1}$ in powers of m and adding to equation 16 we have

$$M(r, s)/r^2 = m^2\left(1+\frac{n^2}{6}\right) - m^4\left(\frac{1}{6}+\frac{n^2}{6}\right) + m^6\left(\frac{1}{15}+\frac{n^2}{6}\right) - \dots \qquad (18)$$

where $m < 1$.

III. Some Illustrative Examples

EXAMPLE 1

Conductors a, b, c of a three-phase bus are of four- by one-half-inch strap, spaced six inches between adjacent axes, with four-inch edges perpendicular to the plane of axes. To calculate k for conductors a and b, and for conductors a and c.

Part A. $D = 6$ inches; $r = 0.5$ inch; $s = 4$ inches; $n = 0.5/6 = 1/12$; $m = 2/3$. From equation 12 for the terms taken in order

$$M(r, s)/r^2 = (4/3)\tan^{-1}(2/3) - \log(13/9) + 1/2808 + \dots$$
$$= 0.7840 - 0.3677 + 0.0004 = 0.4167$$

Accordingly, from equation 14

$$k = (9/4)(0.4167) = 0.938$$

For comparison, we have from equation 17

$$k = 1 - 4/54 + 16/(81)(15) - 64/(729)(28) + 256/(6561)(45) - \dots$$
$$= 1 - 0.0741 + 0.0132 - 0.0031 + 0.0009 = 0.937$$

Part B. $D = 12$ inches; $r = 0.5$ inch; $s = 4$ inches; $n = 0.5/12 = 1/24$; $m = 1/3$. As n and m are smaller even than in part A, k is best calculated directly from equation 17.

$$k = 1 - 1/54 + 1/(81)(15) - 1/(729)(28) + \dots$$
$$= 1 - 0.0185 + 0.008 = 0.982$$

These two values of k computed from equation 17—with a ten-inch slide rule and with purposely exaggerated accuracy—indicate the rapidity with which this expression yields values of k for strap conductors. As in practice but the first two digits are used in actual computations, but the first two or three terms in equation 17 need normally be calculated.

EXAMPLE 2

Conductors a, b, c of a three-phase bus are 2.5- by 2.5-inch square tubular conductors, 0.5 inch thick, spaced 10 inches between adjacent axes. To calculate k for conductors a and b

$D = 10$ inches; $r = s = 2.5$ inches; $R = S = 1.5$ inches; $t = 0.5$ inch. For the data given we have from equation 13

$$k = (100/16)[M(2.5, 2.5) + M(1.5, 1.5) + 2M(0.5, 2) + 2M(2, 0.5) - 2M(2, 2) - 2M(0.5, 0.5)] \qquad (19)$$

To determine k each of the terms of equation 19 must be calculated. From equation 18 we have for the terms taken in order

$$M(2.5, 2.5) = 6.25(0.063151 - 0.000692 + 0.000019) = 0.39049$$
$$M(1.5, 1.5) = 2.25(0.022584 - 0.000086) = 0.05062$$
$$M(0.5, 2) = 0.25(0.040017 - 0.000267 + 0.000004) = 0.00994$$
$$M(2, 0.5) = 4.00(0.002517 - 0.000001) = 0.01006$$
$$M(2, 2) = 4.00(0.040267 - 0.000277) = 0.15996$$
$$M(0.5, 0.5) = 0.25(0.002501 - 0.000001) = 0.00063$$

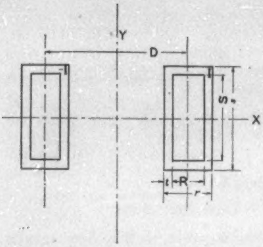

Figure 1. Rectangular tubular conductors symmetrically located

Substituting these values in equation 19 we have for k

$$k=(100/16)[0.39049+0.05062+0.01988+0.02012-0.31992-0.00126]$$
$$=0.999+$$

This example is of particular interest, as, in addition to illustrating the method of calculating k for rectangular tubular conductors, it reveals that for *square tubular conductors*—and these are commonly used—*the value of k for practical conductor spacings is unity.* If the conductors are rectangular with long sides perpendicular to the plane of axes, k will be less than unity; and if the short sides are perpendicular to the plane of axes, k will be greater than unity: in either case k can be calculated as above.

Appendix

Considered as a function of r alone

$$F=2F(D, s)-F(D+r, s)-F(D-r, s)$$

can be expanded in a power series about the point $r=D$. Accordingly, by Taylor's theorem we have

$$F=-r^2\frac{\partial^2F(D, s)}{\partial D^2}-\frac{r^4}{2\cdot 3!}\frac{\partial^4F(D, s)}{\partial D^4}-\frac{r^6}{3\cdot 5!}\frac{\partial^6F(D, s)}{\partial D^6}-\ldots$$

where

$$F(D, s)={}^1/_2(D^4-6D^2s^2+s^4)\log(D^2+s^2)-D^4\log D+4(sD^3-Ds^3)\tan^{-1}(D/s)$$

$$\frac{\partial F(D, s)}{\partial D}=(2D^3-6Ds^2)\log(D^2+s^2)-4D^3\log D+4(3sD^2-s^3)\tan^{-1}(D/s)-3Ds^2$$

$$\frac{\partial^2F(D, s)}{\partial D^2}=6(D^2-s^2)\log(D^2+s^2)-12D^2\log D+24Ds\tan^{-1}(D/s)-7s^2$$

$$\frac{\partial^3F(D, s)}{\partial D^3}=12D\log(D^2+s^2)-24D\log D+24s\tan^{-1}\times(D/s)$$
$$=12D\log(D^2+s^2)/D^2-24s\times\tan^{-1}(s/D)+12\pi s$$

$$\frac{\partial^4F(D, s)}{\partial D^4}=12\log(D^2+s^2)-24\log D$$

$$\frac{\partial^5F(D, s)}{\partial D^5}=-\frac{24s^2}{D(D^2+s^2)}$$

$$\frac{\partial^6F(D, s)}{\partial D^6}=24\left[\frac{1}{D^2}-\frac{D^2-s^2}{(D^2+s^2)^2}\right]$$

$$\frac{\partial^7F(D, s)}{\partial D^7}=48\left[-\frac{1}{D^3}-\frac{-D^3+3Ds^2}{(D^2+s^2)^3}\right]$$

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The Carbon Arc—a Valuable Industrial Tool

W. C. KALB
FELLOW AIEE

Synopsis: The versatility of the carbon arc as a source of both visible and invisible radiation is shown to be due, in part, to the three basic types of operation to which it is adapted and also to the fact that the character of its energy emission can be modified by changes in core composition. It is used in its various forms for many photochemical and irradiation processes, some of which require specific bands of ultraviolet radiation or close reproduction of the effects of natural sunlight. The carbon arc is preferred in other instances, because the optical requirements of the application necessitate a light source of small area and extreme brilliancy. Several industrial and commercial uses are cited together with the characteristics which give preference to the carbon arc as a source of radiation.

THE present widespread industrial use of the carbon arc is due to certain characteristics or combination of characteristics in which it is superior to other available sources of artificial radiation. Capable of efficiently producing a large volume of light, the original commercial application of the carbon arc was in the field of general illumination. It has been used extensively for street lighting and for the illumination of factories, auditoriums, and other large areas. Its present value is due, in large measure, to its versatility as a source of invisible as well as visible radiation and extends into many fields other than those of general illumination. In some instances preferred solely for its visible radiation, the carbon arc is selected for other applications because of the character and intensity of its invisible rays, while the choice at times may be due to the combination of visible and invisible rays which it supplies.

Besides being a highly efficient source of radiant energy in the visible and adjacent invisible bands of radiation, the carbon arc is highly flexible in respect to quality of emission. This flexibility is due, in part, to the three basic types of operation to which the electric arc between carbon electrodes can be adapted:

1. Low-intensity operation between solid or "neutral-cored" electrodes.

2. Low-intensity operation between electrodes having cores containing flame-supporting materials.

3. High-intensity operation.

Distinguishing characteristics of these three types of arcs have been described in previously published articles.[1,2] Briefly, the low-intensity arc between solid or neutral-cored carbons is adapted to a relatively simple type of lamp mechanism. At high arc voltage it is a very efficient source of ultraviolet in the range from 3,700 to 4,000 angstroms. The positive crater of the d-c low-intensity arc provides a steady concentrated light of the greatest brilliancy and whiteness available from a purely incandescent source. With a peak brilliancy of 180 candles per square millimeter and an average color temperature of about 3,550 degrees Kelvin, this type of arc has been extensively used for searchlights and for motion-picture projection. It has yielded its dominant position in these fields only to the higher brilliancy, whiteness, and efficiency of the high-intensity carbon arc.

The low-intensity flame-type arc, with the greater portion of its radiation emanating from the long arc stream at a brilliancy on the order of eight candles per square millimeter, is not so well adapted as the other basic types of arcs to use with optical systems that require a light source of small area and very high brilliancy. However, due to the large area of the flame, this type of arc emits a large volume of radiation which is produced at high electrical efficiency. An important advantage of the flame arc is the wide range in quality of radiation that can be obtained by varying the composition of the core[3] and the uniformity in both quality and intensity of radiation obtained from a particular type of carbon, when operated at specified arc conditions. When cored with certain rare earth compounds, the flame arc produces radiation so similar to that of natural sunlight in spectral composition that for many applications its effects are practically identical with those of sunlight. This light is the closest approach to natural sunlight of any artificial source having sufficient intensity for numerous industrial applications. Other combinations of core materials provide high emission of infrared or ultraviolet and permit emphasis on specific bands of ultraviolet, corresponding to the wave lengths at which most rapid reaction is obtained in certain photochemical and irradiation processes.

The high-intensity carbon arc is characterized by its very high crater brilliancy, 350 to 1,200 candles per square millimeter, and—with the core composition used in all but certain high-intensity therapeutic carbons—by the even distribution of all colors in its spectrum. The crater brilliancy of 1,200 candles per square millimeter is obtained from a new positive carbon only recently made available for use. A brilliancy of 2,000 candles per square millimeter has been attained with an experimental type of carbon not yet available for commercial application. The appearance of the d-c high-intensity arc is illustrated in Figure 1. Color temperature of the light from high-intensity arcs used in motion-picture projection averages about 5,800 degrees Kelvin. Maintenance of the high-intensity effect is dependent on the position of the electrodes in relation to each other. This relationship is maintained by continuous and accurately controlled feeding of the electrodes, and, in some of the larger high-intensity arc lamps, the positive carbon

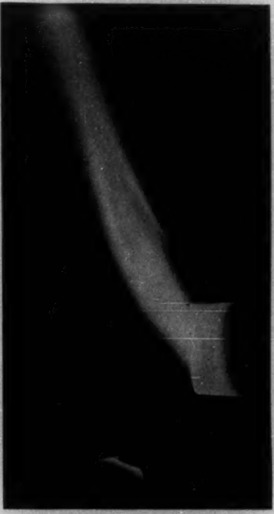

Figure 1. High-intensity carbon arc with rotating positive electrode

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W. C. Kalb is commercial engineer, carbon sales division, of the National Carbon Company, Inc., Cleveland, Ohio.

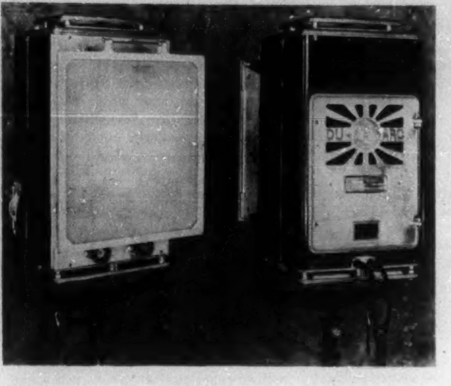

Figure 2. Twin-arc, motor - controlled lamp used for broadside lighting

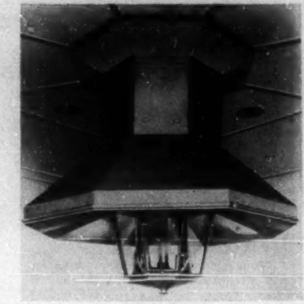

Figure 5. Four-arc carbon-arc solarium unit

is rotated, in order to maintain a symmetrical crater formation. The principal use of this highly concentrated source of illumination is for searchlights and for motion-picture production and projection.

Summarizing the advantages which give the carbon arc an important place in industry, the following points may be noted as available in one or more of the basic types:

1. Very high output of radiant energy from a single source.
2. Flexible quality of energy emission.
3. Close approximation to natural sunlight.
4. Uniform quality and intensity of emission at specified arc conditions.
5. Output unaffected by age or period of operation.
6. High intrinsic brilliancy, adapted to optical systems requiring a highly concentrated source of radiation.
7. Shape of light source and distribution of brilliancy adapted to simple optical systems.
8. High efficiency in production of both ultraviolet and visible radiation.

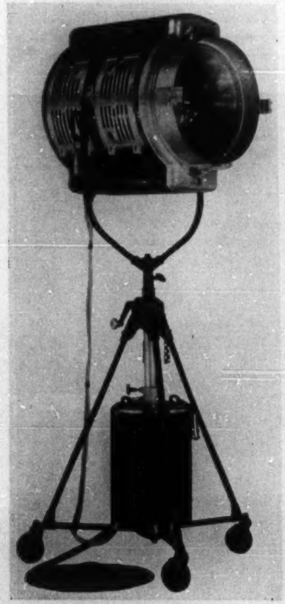

Figure 3. High-intensity carbon-arc lamp with Fresnel-type lens

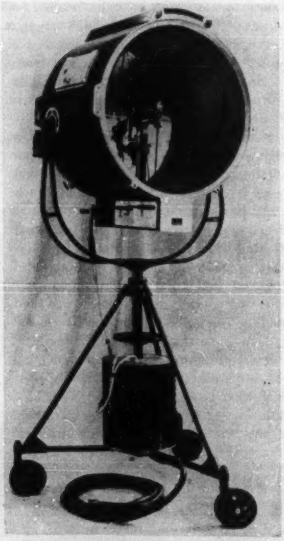

Figure 4. 36-inch sun arc

The carbon arc was adopted at an early date as an artificial source of illumination for photography and allied photochemical industrial processes, such as blueprinting, photoengraving and photolithography. High actinic power, color quality, and adaptability to high illumination intensities are among the major reasons carbon-arc lighting was adopted by these industries and are still major factors in the extension of its use. Early photographic emulsions, sensitive principally to blue, violet, and ultraviolet radiation, react rapidly to the light of the low-intensity arc. This was the only type of arc available when photographers first turned to artificial lighting. As the sensitivity of photographic emulsions was extended through green to yellow and orange wave lengths, the white-flame arc, by that time available, was adopted, because its higher output of visible radiation gave greater speed and better photographic quality than the neutral-cored carbon arc. With modern photographic emulsions, sensitive to all colors of the spectrum, the advantages of the even distribution of color in the light from the white-flame arc have been further emphasized.

The high photographic speed of the white-flame carbon arc and its similarity to daylight made it the preferred artificial light source for photoengraving and photolithography, even when production in these industries was predominantly monochromatic. The growing popularity of natural-color reproduction in the graphic arts emphasizes the value of a light which provides all of the spectral

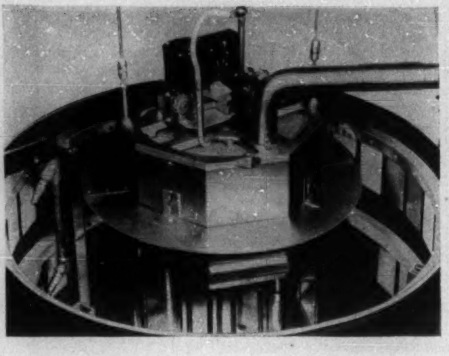

Figure 6. Interior of carbon-arc accelerated-weathering unit

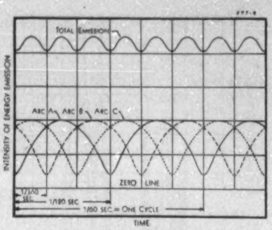

Figure 9. Energy emission from three-phase 60-cycle flame arc

colors at essentially equal intensity. High-intensity arcs are used in photolithography for large-sized reproduction by the projection method.

The motion-picture studios depend chiefly on carbon-arc lighting for natural-color photography. This also is due to the need for an even balance of the primary colors. The color balance of the light from carbon arcs, as adapted to studio use, conforms so closely to that of sunlight that the two are considered essentially equivalent for photographic purposes. The type of lamp generally used for broadside lighting is shown in Figure 2. This lamp has a motor-driven feeding mechanism which maintains very steady light output and is extremely quiet, permitting operation in proximity to the microphone without difficulty. It uses a special white-flame carbon producing light with a color temperature of 4,650 degrees Kelvin almost perfectly balanced to the color sensitivity of motion-picture film, and requiring no color-correcting filter.

High-intensity arc lamps equipped with a Fresnel-type lens, as illustrated in Figure 3, are used for back lighting, cross lighting and key lighting and for both wide and narrow beam front and effect lighting. High-intensity "sun arcs," with 24-inch and 36-inch mirrors, Figure 4, are used for extremely long throws, for sharply outlined shadows, or to provide a clearly defined beam of light through the general illumination. Because of their ready adaptability to the needs of the studio and to modern photographic technique, many cinematographers are using carbon arcs as the principal source of illumination for monochromatic as well as for color productions.

For background projection or process photography a light source of tremendous power is required to project the background scene through the screen at an intensity comparable to that of the lighting on the set. Only the high-intensity carbon arc has sufficient power for this purpose. In fact, three background projectors are sometimes used, synchronized with each other and with the camera, each projecting the same scene in exact register through the screen. The precision of mechanism and optical elements which has made possible this intricate projection system is a major engineering accomplishment.

The high-intensity carbon arc is an ideal light source for searchlights, because its tremendous output is concentrated in a very small area. This permits the light

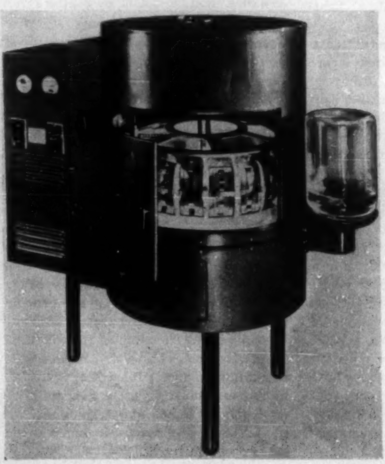

Figure 7 (left). Carbon-arc accelerated-fading unit

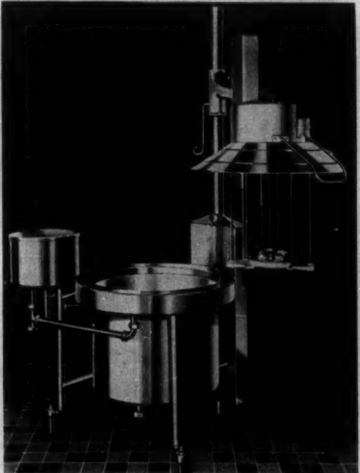

Figure 8 (right). Three-phase carbon-arc milk-irradiating unit—open

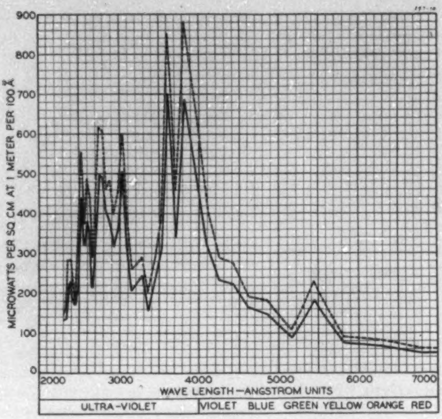

Figure 10. Distribution of radiant energy from flame arc used in milk irradiation

Dotted curve—At 80 amperes, 60 volts, a-c
Solid curve—At 80 amperes, 50 volts, a-c

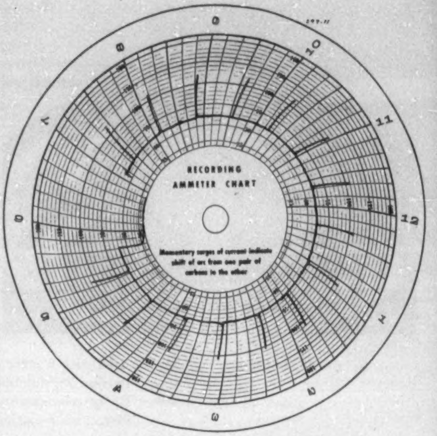

Figure 11. Recording-ammeter chart from unit shown in Figure 6

to be focused by the lens or reflector of the searchlight into a beam of small divergence and high penetration. The high-intensity searchlight with 16-millimeter positive carbon, operated at 150 amperes and 78 volts, and using a 36-inch reflector, projects a beam of considerably more than 100-million beam candle power and, with a 60-inch reflector, the beam candle power is well over 500 million. At 195 amperes and 84 arc volts the beam candle power is 30 to 50 per cent higher than at 150 amperes. Exact candle power of the beam is dependent on shadow losses, glass absorption, and the optical characteristics and reflection factor of the mirror.

Carbon arcs were used for motion-picture projection from the earliest days of the industry and are in practically universal use for theater projection today. No other commercially available light source provides light of suitable power, concentration, and color quality to meet the projection requirements of motion-picture theaters. The present trend is toward replacement of low-intensity projection lamps with high-intensity lamps of simplified design. This trend results from increasing recognition of the need for higher levels of illumination and also from popular demand for fidelity of color reproduction in the presentation of natural color features. Approximately 50 per cent of the light from the low-intensity carbon arc is in the orange and red portion of the spectrum. This color composition overemphasizes the red components in color pictures. Theatrical film of this type is processed for projection with light having approximately equal intensities of the three primary colors, a condition which the high-intensity arc fulfills most acceptably. The development of high-intensity projection lamps which require little more than one kilowatt at the arc has put the cost of high-intensity projection light essentially on the same level as that of low-intensity light, and has made it available to theaters which could not previously afford to operate high-intensity lamps. Other applications of the carbon arc for projection purposes include stereopticon projectors, spotlights, and theatrical "effect" lighting.

Since the discovery by Finsen that radiation from the carbon arc provides a cure for certain physical disorders, carbon-arc lamps have been extensively used in light therapy. The flame-type carbon arc is at present preferred for this purpose, because of its versatility in respect to quality of radiation. With one type of carbon close simulation of the physiological effects of sunlight is obtained. Other types give powerful emission in specific bands of ultraviolet. Still another type has mild ultraviolet emission, combined with a high output of penetrating radiation of longer wave length. All of these carbons can be burned in the same lamp. A large carbon-arc solarium unit, such as that illustrated in Figure 5, provides radiation of suitable intensity for light therapy over a circle of 20 standard solarium cots.

The increasing importance of quickly determining the resistance of various materials to deterioration under the action of sunlight has resulted in the almost universal adoption of the carbon arc as the source of radiation in accelerated testing units. This, again, is due to the fact that the quality of radiation delivered and the resulting effects are comparable to those of natural sunlight. Furthermore, radiation can be provided at an intensity which greatly accelerates the rate of deterioration and, being available 24 hours a day at constant intensity every day of the year, allows conclusive evidence of product quality to be obtained in much less time than is possible by means of outdoor tests.

In accelerated weathering units, such as that illustrated in Figure 6, test samples are carried on a rack which revolves slowly around the arc. At a portion of their revolution the samples are exposed to a water spray. The weathering action of water, air, thermal shock, and radiation of sunlight quality are thus combined in one test, the conditions of which can be exactly reproduced at any time and in any location. The unit illustrated employs a 60-ampere, 50-volt single-phase, a-c flame arc operated through a transformer from a 230-volt line. There are two upper and two lower carbons, but the arc burns between only one pair at a time. This unit accommodates 64 samples at one time. Paints, lacquers, enamels, varnishes, plastics, rubber and rubberlike materials, roofing and building materials, materials for leggings, tents, and tarpaulins, and special types of glass are included in the list of products for which accelerated testing is now a standardized procedure. Comparative records made by users of these units indicate that an exposure of 30 hours in the unit described is approximately equivalent to one month of outdoor exposure.

Units designed for comparing the color fastness of dyes, dyed fabrics, paper, plastics, and other products do not have the water spray that is provided in the weathering unit but usually have provision for maintaining an adjustable degree of humidity. The unit shown in Figure 7 has a 30-ampere, 40-volt, single-phase arc lamp, operated through a transformer from a 230-volt line. This lamp has three upper and three lower carbons, but the arc burns between only one pair at a time. This allows it to operate continuously for 24 hours without retrimming. The revolving rack carries 18 sample holders, curved to the mean radial distance of $10^1/_4$ inches from the arc.

Recognition of the dietary importance of vitamin D has led to extensive use of carbon-arc lamps as a source of ultraviolet for increasing the vitamin-D potency of milk. Several types of carbon-arc milk irradiators are in use, one of the most efficient of which employs a 12- to 14.4-kw, three-phase, carbon-arc lamp with 80 amperes and 50 to 60 volts at each arc. This unit is shown in open position in Figure 8. In operating position the lamp is centrally located within the large drum. Besides making available in small space a high intensity of radiant energy, the three-phase carbon arc insures a continuously high level of energy emission as illustrated in Figure 9. Ultraviolet, the activating agency, does not penetrate milk to appreciable depth, and portions of the rapidly flowing film of milk in an irradiator may reach the exposed surface only during low-emission portions of the energy cycle from a single-phase source. Experience has demonstrated that the effectiveness of the irradiation process is increased by maintaining a continuously high level of energy emission. Since the flame-type arc operates more efficiently on alternating than on direct current, due to the use of reactance instead of resistance ballast, the polyphase arc is the most practicable means of maintaining a high-emission level.

Some objection to the irradiation process for increasing vitamin-D potency of milk has resulted from the use of sources of ultraviolet which produce considerable ozone and result in impaired flavor. The carbon arc is free from this objection. Coltman and MacPherson[4] have shown, by a method of spectroscopic analysis capable of clearly detecting 14 parts per hundred million, that no ozone above that concentration is present in the gases surrounding the carbon arc. Furthermore, producers of both fluid and evaporated milk have found that a vitamin-D potency of 400 USP units per quart can be obtained by carbon-arc irradiation without impairing the natural flavor of the milk.

Figure 10 shows the distribution of radiant energy from the type of flame carbon found most effective for milk irradiation. With these carbons the unit shown in Figure 8 is capable of activating 8,000 to 10,000 pounds of fluid milk per hour to a potency of 400 USP units per quart.

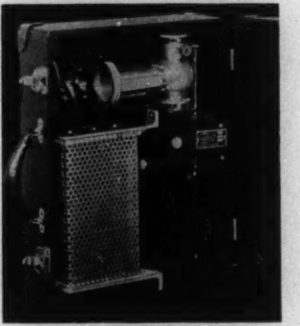

Figure 12. Portable ultraviolet carbon-arc lamp in carrying case

Uniformity of radiation from the large carbon-arc lamps used in the industrial testing and irradiating units here described is obtained by a motor-driven carbon feeding mechanism which provides automatic control of arc current. Accuracy of control has been greatly improved by the use of a 96-pole, 75-rpm reversible synchronous motor to drive the feeding mechanism. Because of its slow speed the rotor of this motor has little momentum and does not have the tendency to overrun, after opening of the motor-control relay, which is characteristic of high-speed motors. Hunting is thereby practically eliminated. Steadiness of arc-current control realized in practice is shown by the recording ammeter chart from an accelerated weathering unit reproduced in Figure 11. The momentary peaks at approximately 40-minute intervals indicate the shift of the arc from one pair of electrodes of the dual carbon trim to the other.

The applications of the carbon arc which have been cited are representative of the more extensive uses of this versatile source of radiation for industrial purposes. Carbon-arc lamps are used also to provide light of daylight quality, closely controlled in color composition and intensity, for critical matching of color. Ultraviolet radiation from carbon arcs is used in the processing of tobacco and other commercial products and in the preparation of certain pharmaceutical materials. Spectroscopic carbon and graphite electrodes of very high purity are used for spectroscopic analysis and as a source of radiation of reproducible intensity and spectral energy distribution for high temperature calibration and for color standardization.[5] The ash content of these special electrodes is considerably below 0.001 per cent. A portable carbon-arc lamp fitted with ultraviolet filter, Figure 12, is used in both laboratory and field for fluorescence inspection and analysis. Many of these lamps are used in crime detection. Other important industrial applications of the carbon arc, entirely outside the field of illumination, are in electric welding and in electric arc furnaces used for a variety of electrochemical and electrometallurgical processes. Although no longer before the eye of the general public, as when carbon-arc lamps hung on every street corner, this source of radiant energy is today more widely used and of greater industrial value than at any time in the past, and further extension of its commercial and industrial applications may confidently be expected.

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A New Moving-Magnet Instrument for Direct Current

H. T. FAUS
MEMBER AIEE

J. R. MACINTYRE
ASSOCIATE AIEE

Synopsis: A new type of d-c instrument is described in this paper. It comprises a diametrically magnetized cylindrical moving-magnet element, a hollow copper damping cylinder surrounding the moving element, a fixed coil, control magnets having a very high coercive force, and an enclosing magnetic shield made of mumetal. The field of the control magnet and the field of the fixed coil have a relatively large angular displacement. The angular position of the vector sum of these fields varies with the current through the coil, and the moving element follows this vector. The scale distribution is nearly uniform for a 90-degree scale. The characteristics compare favorably in many respects with those of a D'Arsonval instrument.

OERSTED'S discovery that a compass needle tends to set itself at right angles to a wire carrying an electric current provided the basis for the design of the first electromagnetic indicating instrument, which consisted of a coil of wire surrounding a magnetized needle.[1] This in turn led to the development of the astatic galvanometers of Nobili and Kelvin and of the sine and tangent forms of galvanometer. These latter instruments, which depended upon the earth's field for their control torques, were obviously unsuited for use as portable instruments.

Ayrton and Perry, in designing the first portable d-c ammeter, used a large permanent magnet to produce a controlling field. In this instrument the polarization of the vane was derived from the fixed permanent magnet rather than from its own permanent magnetism. Since the field of the control magnet had to be so strong that the earth's field was negligible by comparison, the field of the coil had to be correspondingly strong in order to deflect the vane. This made the sensitivity of the instrument inherently low, since so many ampere turns were required to excite the field. Modifications of the Ayrton–Perry instrument are used for automobile ammeters, and similar applications where low cost and ruggedness are important, and where sensitivity and high accuracy are not required.

The principle of the permanent-magnet moving-coil galvanometer as developed by Sturgeon, Kelvin, and D'Arsonval was used by Weston in the design of the first moving-coil portable instrument. This principle is now generally used in all d-c instruments of high accuracy and high sensitivity.

Development of New Moving-Magnet Design

Although highly sensitive suspended magnet instruments have been built, no portable moving-magnet instruments have come into general use. "Moving-magnet instrument" is here intended to denote an instrument in which the magnetomotive force of the moving element is much greater than that produced by the fixed coil or by the fixed control magnets.

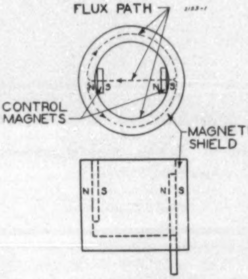

Figure 1. Path of control-magnet flux in magnetic shield

In an instrument of high sensitivity, the field of the fixed coil will be rather weak because of the small power available for producing it. This means that the instrument must be well shielded against stray fields which, even in the case of the earth's field, would be comparable in strength with the field produced by the instrument coil. This is accomplished by enclosing the moving magnet, coil, and control magnets in a cylindrical cup of magnetic material of high permeability and low coercive force. Permalloy or mumetal[2] are suitable materials for this shield.

Controlling torque may be obtained either from a magnet or from a spring. In this instrument a magnet was preferred because of ease of adjustment and other considerations. Since the control magnet must be placed within the shield, it must be very short in the direction of its magnetization and must have its magnetization unaltered by the field of the moving magnet. It is apparent that a material of very high coercive force is required. This requirement is met by an alloy of silver, aluminum, and manganese which was first described by Potter.[3,4] This alloy has a coercive force $({}_JH_c)$ of 6,000 oersteds and a residual induction of 610 gausses. It is unique among permanent-magnet materials in having a coercive force of this high value. The high coercive force enables the control magnets, which are in the form of thin strips, to be magnetized in the direction of their thickness. As shown in Figure 1, these magnets are mounted on opposite ends of a diameter inside the cup so as to produce a control field in the direction of this diameter. The strength of this field may be decreased or increased by inserting or withdrawing one magnet through a hole in the bottom of the shield as shown in the right-hand magnet in Figure 1. This adjusting motion is parallel to the axis of the cylinder and does not affect the direction of the control field in the plane in which the moving magnet turns. Thus, it controls the sensitivity without affecting the zero setting or the scale distribution of the instrument.

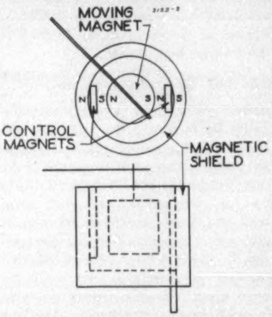

Figure 2. Relative positions of moving magnet, control magnets, magnetic shield, and pointer

The moving magnet should have a small moment of inertia in order to give good responsiveness and damping. At the

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H. T. Faus is development engineer, and J. R. Macintyre is design engineer, aircraft instrument department, both with General Electric Company, West Lynn, Mass.

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same time it should provide a strong field in order to give as high torque as possible. Since the moving element must revolve through an arc of 90 degrees or more, the largest volume of permanent-magnet material can be put into a rotor which is bounded by a surface of revolution. From these considerations it appears that a cylindrical shape will be suitable for the moving magnet. A cylindrical magnet, magnetized along its diameter, has a very small ratio of length to cross-sectional area and, hence, should have a much larger ratio of coercive force to residual induction than that of any of the commonly known magnet steels. The silver alloy used in the control magnets does not have a sufficiently high residual induction for this application. A very good material is an oxide magnet made of a mixture of iron and cobalt oxides.[5] This material has a coercive force of approximately 950 oersteds, a residual induction of 2,200 gausses, and a $B \times H$ maximum value of approximately 10^6. The specific gravity of this material is half that of steel. Since the factor of merit of an electrical instrument depends on the weight of the moving element, this material is equivalent to steel having twice this $B \times H$ maximum value. Figure 2 shows the cylindrical magnet mounted within the shield and control-magnet assembly.

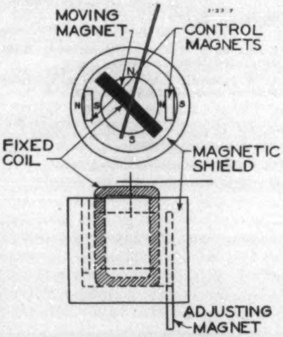

Figure 3. Diagram of complete moving-magnet-instrument element

The field of the moving magnet has no appreciable effect on the permanent magnetization of the control magnets, because their coercive force is so high as to require a much stronger field to magnetize or demagnetize them. The field of the control magnets is much weaker than that of the moving magnet and likewise has no appreciable effect on its permanent magnetization. The shield material is of such low coercive force as to be practically incapable of acting as a permanent magnet. It is evident that under the conditions of operation no appreciable shift in the permanent magnetization of any of the parts is possible, and hence the instrument will have negligible hysteresis errors.

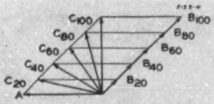

Figure 4. Vector diagrams of fluxes at intervals of 20 per cent of full-scale current

(*A*) Control magnet flux

(*B*) Coil fluxes for currents indicated by subscripts

(*C*) Resultant fluxes

The current-conducting coil is placed in the circuit to produce a magnetic flux at an angle of 135 degrees from the flux of the control magnets. This coil is shown schematically in Figure 3.

The vectorial relationship of the control-magnet flux and coil flux is shown in Figure 4. Vector B represents the coil flux. This flux is proportional to the current flowing in the coil and is shown in various magnitudes with the subscript indicating the percentage of maximum current. These B subscript vectors add to vector A to produce vector C with subscripts as shown.

The polarized rotor will turn to a position parallel to vector C subscript. It should be noted that, although vector C may vary in magnitude as well as direction, only the directional changes can cause the rotor to turn.

In Figure 5 the angular position of vector C is plotted with the per cent coil current. Since this angular position is the same as the pointer position, the plot shows the scale distribution obtained from an instrument of this type. The maximum deviation from uniformity, as shown by the straight dotted line, is five degrees.

It is at once apparent, if changes in magnitude of vector B produce directional changes in vector C, that like changes in magnitude of vector A will also produce the same effect. Use of this fact is made in calibrating the instrument to a predetermined full-scale current value. This is accomplished, as previously mentioned, by sliding the adjusting magnet of Figure 2 in or out of the metal shield cup, and thus changing the amount of control-magnet flux as represented by the magnitude of vector A.

Figure 6 is a cut-away view of one of the instrument elements.[5]

The element shown has polished steel pivots moving in brass bearings. This type of bearing can be used where torque-to-weight ratios are sufficiently high.

The copper damping cylinder is used to bring the rotor to rest rapidly, by means of eddy currents induced by the rotor.

The field coil is wound at an angle across the molded coil form and not parallel to the rotor axis, as shown schematically in Figure 4. This was done for mechanical reasons and in no way changes the previously discussed considerations, since the field of the coil has a component in the plane of vector B.

Zero adjustment is accomplished by rotating the whole element assembly by means of an arm welded to the bottom of the shielding cup.

The magnetic shielding is completed by means of a mumetal washer on the top of the molded coil form.

The element as measured on the outside of the shielding cup is approximately three-fourths inch in diameter, and one-half inch long.

Conclusions

In production quantities the element has been applied to only one device. This is a dual-range storage-battery testing type of voltmeter shown in Figure 7. Here it was possible to use brass bearings. In all applications seen by the authors of moving-coil permanent-magnet instruments to similar devices, jewelled bearings are used.

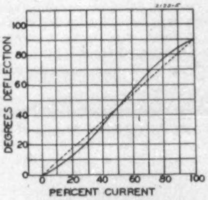

Figure 5. Scale-distribution curve for moving-magnet instrument

Table I shows the physical constants of these voltmeters.

The elements are adjusted to fit predetermined scales to an accuracy of ±2 per cent of full-scale reading.

No hysteresis error (measured by taking calibration at the same point with descending and ascending currents) can be detected.

The instrument, when placed in a stray field of ten gausses in such a manner as to produce maximum errors, changes read-

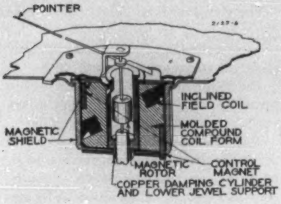

Figure 6. Cross section of moving-magnet instrument

ings approximately one-half of one per cent of full-scale reading.

The temperature errors of these indicators show some peculiar characteristics because of the high magnetic temperature coefficient of the silver alloy control magnets. With 10 per cent copper in the circuit, the element shows an error of −4 per cent at −40 degrees centigrade and +2 per cent at +50 degrees centigrade. With 35 per cent copper the error at −40 degrees centigrade is +1 per cent, and at +50 degrees centigrade approximately 0 per cent. The per cent errors are deviations from readings taken at 20 degrees centigrade. This makes it possible to build a voltmeter of low temperature coefficient with a high ratio of coil to series resistor resistance. Temperature compensation of ammeters may be obtained by a shunt resistor having a low temperature coefficient of resistance.

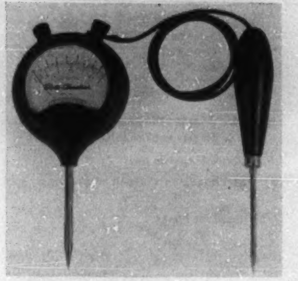

Figure 7. Battery-testing voltmeter with moving-magnet element

The element as used in these battery testers withstood considerable abuse. Several were tested by dropping 30 inches onto hard wood and concrete floors. No changes were noted except in one case where the adjusting magnet came loose.

The authors feel the element has inherently lower cost features and will withstand more abuse than the conventional moving-coil permanent-magnet element. By extending the design to the use of jewels, the sensitivity can be increased. Although it cannot reach the higher sensitivities of moving-coil permanent-magnet design, this moving-magnet element can be produced in sensitivities high enough to fit many applications now using the latter design.

Table I

1. Scale angle	90 degrees
2. Scale radius	$1^{15}/_{32}$ inches
3. Scale length	$2^{5}/_{16}$ inches
4. Full-scale torque (T)*	0.35 millimeter-gram
5. Weight (W)	0.205 gram
6. Ratio T^*/W	1.7
7. Ratio T^*/W 1.5	3.9
8. Response	1.5 seconds
9. Rating	3/10 volts
10. Ohms per volt	20
11. Coil resistance	20 ohms
12. Coil watts (full-scale)	0.05
13. Coil ampere-turns (full-scale)	14

*The full-scale torque shown (T) should be multiplied by approximately 3/2 to be comparable to the conventional 90-degree torque of moving-coil instruments. This is for the reason that the torque-versus-angular-displacement curve of the moving-magnet instrument is roughly sinusoidal and not linear as in the case of the moving-coil instrument. The 3/2 figure comes from extending the slope of the torque-displacement curve from near the origin, to the 90-degree point. This is logical, since it is the increment of torque-near-zero displacement which determines the inherent friction "stickiness" of the indicator.

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Sleet Problems on Electrified Railroads

H. F. BROWN
MEMBER AIEE

Synopsis: Sleet storms, or more properly ice storms, have always presented serious difficulties to the operators of overhead electric conductors of all classes. For more than 25 years some electric-power companies have been using the circulation of electric current to heat the conductors, either to melt off the ice or to prevent it from forming. The technique of such procedure is well-known and will not be dealt with in this paper, other than to present fundamental theoretical data.

Electrified railroads operating with overhead contact systems have similar ice storm problems, and, in addition, a number of special problems caused by such ice. These problems are outlined in this paper, and some means which have been used or proposed for their solution are described. Operating experience with one special application to a case of severe exposure to ice storms and high wind velocities is cited. Some of these special railroad problems have not yet been satisfactorily solved.

General Problems

THE accumulation of ice caused by freezing rain, also known as sleet or glaze, on overhead electric conductors has always presented problems to be dealt with by operating engineers as well as by the designing engineers of such systems. Ice loads must be taken into account in calculating the strength of the conductors and their supporting structures, and the National Electrical Safety Code[5] has set forth the different values of radial thickness of ice which may be expected to occur in the various parts of the United States. However, even the most liberal provisions for ice loads are exceeded during the occasional unusually severe ice storms, and the conductors and even their supporting structures may then fail.

Ice storms are frequently followed by a drop in temperature accompanied by high-velocity winds, and both the designers and the operators are then faced with additional problems, for to the static ice loads may be added dynamic forces caused by swaying loads, and under certain critical wind velocities ice-covered conductors will whip up and down and "dance" violently even to the extent of destroying themselves or their attachments to their supporting structures.

To mitigate all these effects, which cannot always be provided for economically in the design, ice melting by heating the conductors with electric current has been resorted to by many power companies. This is done by loading the lines to capacity where possible, by load concentration or exchange with connected supply lines, or by the circulation of sufficient electric current at a voltage lower than normal; and routine procedures have been established during such storms to remove or to prevent the forming of ice on the conductors by these means.

The theory of such current requirements and the various operating techniques used have been reported in the technical press and in papers before operator associations from time to time during the past few years, some of which are listed herein for reference.

Special Railroad Problems

Electrified railroads having overhead contact systems and associated power feeders must deal not only with the same general problems of this nature that electric-power-transmitting companies have, but in addition they have a number of associated problems peculiar to themselves to contend with during ice storms. Some of these are:

1. On the contact wire, the ice accumulation, in itself a poor conductor, interferes with the current collection if it becomes too thick and causes severe arcing, which can seriously damage the contact conductor by burning and pitting; and can even cause flashovers where clearances to grounded structures are limited or reduced. Frequently the collector shoe can be burned in two by such arcing before many miles have been traveled. Contact wire wear is increased by the roughened collector shoe, and the fine polish acquired by months of lubrication or otherwise, may be destroyed.

2. The static ice load on the catenary contact system may become great enough to distort the vertical alignment by greatly increasing the sag at the center of the span, causing the collector shoe to skip and arc at the suspension points, since the pantograph is usually sluggish in its action under such conditions. Where heavy ice accumulations are known to be possible, the designer should weigh this factor in the selection of span length, especially if high-speed operation is involved, since this distortion will increase directly with the square of the span length for the same weight of ice. With inclined catenary construction, the increase in static load may distort the horizontal alignment enough to dewire the collector shoe on long spans on curves of long radius. Here again the remedy lies with the designer by using shorter spans or applying adequate horizontal bracing.

3. The additional wind load on the ice-covered contact system may result in further alignment distortion caused by horizontal sway, or in vertical "dancing," which, even if not severe enough to damage the overhead system, is in itself detrimental to continuous current collection and to high-speed operation and continuous tractive effort. Violent dancing may under extremely severe conditions wreck the line. Adequate bracing to take care of horizontal sway may have no beneficial effect on and may even augment this vertical dancing under critical conditions.

4. The ice accumulation on the pantographs and overhead collector shoes may add so much extra weight to these parts as to completely nullify the normal spring tension which holds the shoe against the contact wire. Then the pantograph will drop away from the wire or will not rise again after having been depressed, where the contact wire is lower than normal, as under a highway bridge or other low overhead structure. A means of augmenting the normal spring pressure, by added air pressure or otherwise, is one means of overcoming this difficulty; but it must be remembered that the pantograph pressure cannot be made too great without affecting the contact system.

5. Electric melting of ice on the overhead contact system may require larger currents than are available, since such systems are, in general, designed with more conductivity and for heavier currents than in the average power-transmission lines, which are usually of much higher voltage. Further, even when large currents can be applied, they may concentrate in the messenger or in some associated wire other than the contact wire, as some contact systems may be designed to have most of the conductivity in some member other than the wearing wire. In general, it is desirable to have the heating take place in all the members of the contact system, to overcome the difficulties outlined in paragraph 2 above as well as those in paragraph 1.

6. Unlike ice-melting schemes used on power-transmission lines, any electric ice-melting scheme used on an overhead contact system must allow of being superimposed on the contact system circuit while it is normally energized with traction power for train operation; that is, the heating current must be of such characteristics that it will not interfere with the electric operation of trains supplied with power from that same wire or circuit. For example, it is obvious that direct current cannot safely be superimposed on a-c circuits without the danger of saturating the a-c equipment and rendering it inoperative or possibly damaging it by overheating. If it becomes necessary to remove the traction power to apply a heating current of different voltage or characteristic—as may be done often on power transmission lines—there would be no advantage of such a method over the suspension of elec-

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H. F. Brown is assistant electrical engineer with the New York, New Haven, and Hartford Railroad Company, New Haven, Conn.

tric operation entirely and the substitution of steam or other form of motive power for the duration of the storm—an extremely inefficient, if not impossible procedure on a system having dense traffic.

Within the past two or three years, several unusually severe ice storms in the northeastern part of the United States have caused all of the electrified railroads in this section which operate under an overhead contact system to give more attention to these special problems, with the objective of preventing the ice from forming on the contact wire or collector parts, or of removing the accumulated ice before the deposit becomes unduly troublesome.

This paper will discuss some of the various solutions which have been found or suggested for these special railroad problems, rather than those which have generally been applied to power transmission lines. Some of the fundamental data are applicable to these special problems, as well as the general problems, and hence are included herein for reference.

Electric Heating of Overhead Conductors

Pender's Handbook gives the following formula for the value of the current, I, required to heat a conductor:

$$I=K\sqrt{\frac{Td^3}{r}}$$

where

T is the temperature rise in degrees centigrade
d is the diameter of the conductor in inches
r is the resistance of the conductor in ohms per mil-foot at final temperature
K is a constant depending upon surface condition of wire; whose value is (approximately) 800 in still air and 1,000 in open air for solid wire; and 1,200 in open air for stranded conductors

F. R. Longley of Western Massachusetts Companies in an excellent paper on this subject presented at a symposium of System Operators of New England at Montpelier, Vt., June 25, 1937, has developed the curves shown in Figures 1 and 2, which show the amperes required to heat conductors in still air, and the cooling effects of cross winds on the temperature rise, respectively. His formula was developed from data reported by Schurig and Frick.[2] It is to be especially noted that low wind velocities cause a marked reduction in temperature rise, while wind velocities in excess of 15 or 20 miles per hour do not produce much additional reduction. It is obvious that all these formulas are applicable to railway feeders and to the various wires of the catenary contact system.

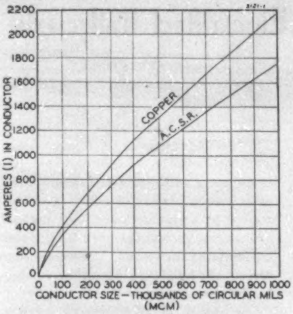

Figure 1. Current required to raise the temperature of a conductor from 0 to 150 degrees centigrade in still air (Longley)

This current is recommended for ice melting. Maximum temperature rise out of doors probably will not exceed 120 degrees centigrade, due to cooling effect of minimum crosswise wind velocity of one mile per hour. Conductors will not anneal below 175 degrees centigrade

For copper $I=17.5\ (\text{MCM})^{0.696}$
For steel-reinforced aluminum $I=14.3\ (\text{MCM})^{0.696}$

Mechanical Removal of Ice From Contact Wires

Most of the railroads which have been electrified in the northeastern part of the United States have rather frequent and dense traffic—in most cases one of the factors responsible for the electrification. Under such conditions, the usual ice storm presents no especial problem, as the ice or glaze is scraped off or shaken off from the contact wire before it has a chance to build up to a troublesome thickness, by the frequent passages of the pantographs. This is one method of ice removal which power-transmitting companies cannot use. On branch lines where traffic is light, or during the night or at other times when traffic is less frequent, if the ice formation and accumulation is mainly because of infrequent train operation, a special car or locomotive (electric) may be assigned to operate frequently to and fro over such lines to keep these lines as clear by this means as those having denser traffic.

A method of prevention of the formation of ice coatings which has been tried out on some of the 600-volt d-c urban lines, especially where trolley coaches with sliding collectors are used, and where even a light ice covering presents a very real problem to continuity of service, has been to coat the contact wire with a waxy or greasy substance which will not become wet enough by light rain to allow the formation of a continuous film but will cause the water to collect in drops. This may be partially successful in light ice storms of short duration, but since the drops which adhere and freeze have the same surface tension as the rain drops, they finally unite to form a continuous coating. Very possibly the waxy coating causes the ice coating to be less adherent, but when the temperature drops, it remains on the wire until scraped off by the collector shoe. This measure obviously is ineffective for very severe storms and is not economically applicable to the contact systems of electrified railroads.

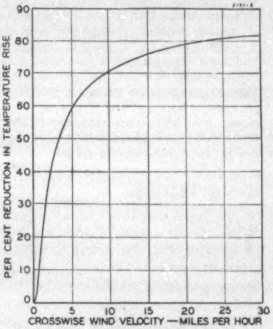

Figure 2. Cooling effect of crosswise wind on heated conductor (Longley)

Special Cases and Their Treatment—an Example

Certain sections of railway line may be exposed to unusual conditions favorable to the frequent and very rapid formation and accumulation of ice and to very high winds. On such sections even frequent traffic is not in itself sufficient to keep the contact wires clear of ice, and some other method of ice removal must be employed if service is to be maintained.

An example of such a section, having unusually severe exposure to both heavy and rapid accumulations of ice and to very high wind velocities, is that part of the electrified system of the New Haven Railroad known as the New York Connecting Railroad which operates over the Hell Gate Bridge in New York City between the Bronx and Long Island. For a distance of nearly four miles this part of the railroad is on a viaduct which is from 75 to 165 feet above the ground or river level and at a considerable distance from any shielding structures. Ice seems to form very rapidly under certain conditions in this section and at times when it is not troublesome elsewhere, and the wind velocities in winter, especially after such storms, assume unusually high values. Consequently, this part of the overhead line has been damaged by ice and wind to the extent of being rendered inoperative electrically on several occasions since its construction in 1917. In contrast to this unsatisfactory record, the remainder of the line from New York to New Haven has operated successfully through a number of severe ice storms since its completion in 1914. With the exception of one extremely severe ice storm in December 1915, when accumulations were two inches in radial thickness in many places, causing the breakage, chiefly at splices, of some number 3 American Wire Gauge copper-stranded conductors used in 300-foot spans for control circuits, this overhead system has withstood a number of severe ice and wind storms (including the hurricane of 1938) without serious damage.

Mechanical Methods Applied

A number of studies of and changes in the Hell Gate Bridge section have been made in an endeavor to overcome the difficulties with ice and wind. Very possibly a fundamental difficulty is that some of the spans used are too long for such severe exposure. In 1927 an extensive horizontal wind-bracing system was applied to the catenary system in this section. This bracing did not eliminate the vertical "dancing" of the catenary spans, and in 1933 some additional vertical stays were added to the horizontal bracing system. Obviously these measures did nothing to eliminate the ice problem. Ice on the contact wires was scraped off when possible by special "ice breaker" locomotives, operated as a routine procedure during each ice storm. The vertical dancing was still so great at times that even these locomotives were unable to keep their pantographs on the wire. The difficulties from ice and high winds continued on the feeders, which were on occasions during subsequent ice storms broken in two and torn from their supporting pin insulators in many places by the violent wind whipping. The ground wire suffered the same fate, and it is safe to say that there are but few spans in this entire section where some of these conductors have not been broken and spliced. The feeders are 4/0 American Wire Gauge hard drawn stranded copper, having a normal sag of five feet in a 300-foot span, and it is a rather awesome sight to see these heavy conductors whipping up and down in a storm for 10 or 15 minutes at a time, with a frequency of about 25 vibrations per minute and with maximum amplitudes of twice the sag at the center of the spans.

In 1937 a number of experimental changes was made to the feeders, including the installation of some experimental dampening devices similar to the Stockbridge dampeners, but of various weights and dimensions. Some experimental spring suspensions and spring dampening devices were included, and in a number of spans the feeders were removed from the pin insulators and attached to suspension insulators. None of the dampening devices proved of great value during the following winter, but the wind whipping seemed to be materially reduced on those spans where the suspension insulators had been substituted for the original pin insulators. These experiments led to the general substitution in 1941 of suspension insulators for pin insulators on all the feeders throughout this entire section, except on the very short spans in the 1,100-foot section across the main arch span of the Hell Gate Bridge, where pin insulators with a flexible mechanical clamping arrangement are used.

The ground wire also has been flexibly suspended, so that any structure vibration caused by the whipping of the catenary will not be communicated to this wire. Some additional lateral and cross bracing of the catenary contact system was also installed in 1941, and to date these changes have seemed to be effective, as no serious difficulty has been experienced this past winter, although the season has not been a severe one.

Electrical Methods Considered

During the summer of 1939 serious consideration was given to the possibility of melting sleet from the wires in this section by electric heating.

There are four tracks in this section, an eastbound and westbound track each for freight and for passenger service, not connected by crossovers except at some distance east of the section, so that this section is operated as two two-track railroads. There are four contact wires with catenary construction at 11,000 volts to rail, and four feeder circuits, also 11,000 volts to rail, but 22,000 volts to the contact circuits, thus making with the rails a three-wire system. All contact circuits and feeder circuits are sectionalized at switching stations at each end of the exposed section approximately four miles apart. All trolley contact circuits are connected to a "trolley" bus, and all feeder circuits are connected to a "feeder" bus at each station, with a 3,000-kva "balancer" transformer having center point of winding connected to rails, connected across these trolley and feeder busses at each station. The west switching station is unattended and is operated by supervisory control from the east switching station, which is located at a signal interlocking tower, thus making the east station the more desirable location for any additional switching or heating apparatus.

In order to electrically remove the ice after it had been once formed, it was assumed that the temperature of the wire would have to be raised about ten degrees Fahrenheit between the two switching stations. It was calculated that about 750 amperes would be required in the catenary system (consisting of a bronze messenger, copper auxiliary wire

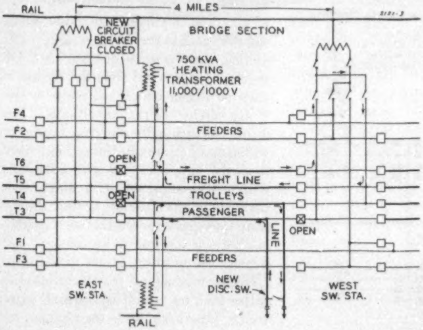

Figure 3. Scheme I proposed for heating contact wires on Hell Gate Bridge

and 4/0 bronze contact wire) to cause enough current (approximately 200 amperes) to flow in the contact wire to raise its temperature the required ten degrees Fahrenheit, since the greater part of the current would flow in the auxiliary wire and a smaller part in the messenger. The d-c resistance of this four-mile section of catenary is approximately 0.5 ohm, which gives a drop of 375 to 400 volts between stations. The a-c impedance is approximately ten per cent higher.

The problem, therefore, was to circulate in a loop circuit between sectionalizing points approximately 750 amperes at 900 to 1,000 volts a-c, or about 750 kva. A smaller amount of power and current would be required if a smaller rise in temperature would keep the lines clear when applied before and during the ice storm. The air temperature during an ice storm is always between 30 and 34 degrees Fahrenheit and if the temperature of the wire could be maintained at, say, 37 degrees Fahrenheit ice could not form on it.

Two schemes, both believed to be practical, were proposed and developed. Scheme I, shown diagrammatically in Figure 3, contemplated using two special 11,000/1,000-volt 25-cycle transformers, having their 11,000-volt winding connected to the trolley bus through a circuit breaker at the east switching station. The 1,000-volt winding would be provided with five or six taps at 50-volt intervals, with tap-changing facilities to suit temperature conditions, and would be insulated to withstand the normal contact-wire voltage plus the usual surges experienced on the system. This winding of each transformer would be connected through disconnecting switches, one to the two freight-line contact wires, and the other to the two passenger-line contact wires. These switches would be closed prior to the advent of the ice storm, and if the storm developed, the circuit breaker would be closed by the tower operator, and two of the trolley circuit breakers would be opened at the east switching station to disconnect one side of each loop circuit from the bus to prevent short-circuiting the low-voltage windings. The heat would then be on the bridge section without further switching operations, and train operation could be maintained and handled as required with no interference from the superimposed heating power. Two trains, both passenger or both freight, when passing near the easterly end of the section might temporarily bypass a small amount of this current, but it would be negligible because of the high impedance of the locomotive transformers to the 1,000-volt heating current.

The passenger line diverges from the freight line at the west switching station and continues for another mile, where it joins another system. If it were desirable to heat these circuits, a new disconnecting switch could be installed at the end of this section as shown in the diagram, closed during an ice storm, and one of the circuit breakers on this line could be opened at the west station to avoid short-circuiting this part of the line. This scheme could be expanded by additional switches, or by separate transformers to heat the feeders in the same manner, although this was not felt to be so urgent at that time.

This scheme was believed to be practicable but was not applied—partly because of the necessity for investing in the special transformers, when at that time it was unknown just how efficacious the heating current would be on the trolley circuits, but mainly because another method, involving no investment or additional equipment of any kind, was immediately available for experimental work by the simple expedient of power exchange between supply points on either ends of this section.

This second scheme is shown in Figure 4 and involved the exchange of traction power from the supply station several miles east of this section, over one circuit at a time in the section, either trolley or feeder, to the supply point several miles west of the section. At both supply points power is purchased from the same power company, but under separate contracts, because the supply at the western end is to a different railroad company over whose lines the New Haven circuits extend. This supply is through a 7,200-kva motor-generator set, designed for power flow in either direction, which was ideal for the purpose in mind.

It will be seen that scheme II involved the use of a single trolley-rail circuit for contact-wire heating, or a feeder-rail circuit for feeder heating, in the bridge section, which necessitated some experimental tests to satisfy the local communication interests that no inductive disturbances serious enough to cause any interference with their plant could be detected; and after some further tests to develop operating technique, this scheme was adopted as a regular operating procedure to be used throughout the duration of ice storms and has been in successful operation for three seasons. This scheme has been more fully described elsewhere.[4] One of the features of the power exchange is that it is done on a half-hour basis; that is, power is taken from one supply for one half-hour and delivered (less losses) to the other supply, after which interval the power flow is reversed for the next half-hour. This is because both contracts have the maximum demand on an hourly basis, and in this way neither contract demand can be exceeded by the power exchanged. A typical power curve illustrating this feature is shown in Figure 5.

With scheme II ice already formed can be melted from the feeders in from six to ten minutes and from the catenary in about twice this time, depending upon the temperature and wind conditions and the amount of power exchanged, which is limited by the capacity of the motor generator set at the western supply.

With the improvements made during the past year in the feeder system in this section, there is some question as to the value of ice removal from the feeders, if they no longer dance and whip so violently under storm conditions. The writer has personally observed dancing or whipping of feeders in this section, when formerly supported on pin insulators, with no ice coating whatever, and at temperatures above freezing, so that there is some question as to whether such dancing is always a function of ice coating; and if not, the solution of the problem of "dancing conductors" may be mechanical rather than (or as well as) thermal. More winter experience with the changes re-

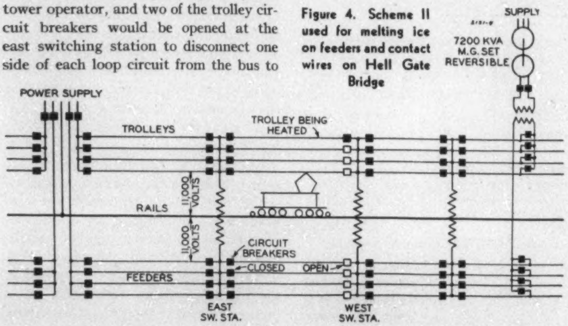

Figure 4. Scheme II used for melting ice on feeders and contact wires on Hell Gate Bridge

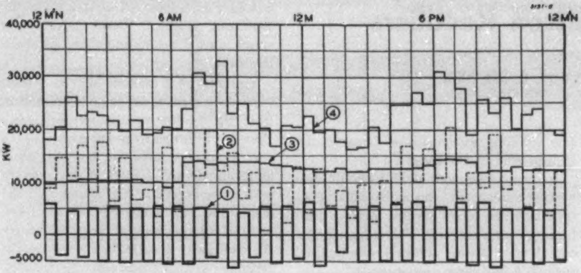

Figure 5. Power-load curve for March 4, 1940

①—Ice-melting power exchanged with west supply

②—Total purchased power, excluding ice-melting power from west supply

③—Power produced at railroad-owned plant

④—Total railroad-system load, excluding ice-melting power exchanged

cently made must be had before it can be definitely stated that this problem has been solved.

Further Considerations

It will be noted that scheme II involves the continuity of service of all equipment involved in the power exchange; and if the equipment at the western supply point, which is not in duplicate, should be out of service for any reason during an ice storm, the scheme could not be used. This actually did happen during a very severe ice storm early in 1941, and, consequently, later in the year the proposal was made that a scheme similar to scheme I, outlined above, be installed until it was learned that it would be impossible to obtain the necessary transformers because of the war.

A third scheme therefore, to be used only as an alternate to scheme II, has been devised and is now being installed. This scheme consists of taking one trolley or feeder circuit at a time in this section and switching the westerly end through a water rheostat, adjusted to allow the passage to ground (rail) of approximately 800 amperes of traction power supplied from the east end. Most of this power is necessarily lost, being dissipated through heat in the water rheostat; possibly five per cent is used in heating the wire. This scheme requires a number of disconnecting switches at the west switching station, a large tank of water with a continuous supply, and the necessary drains, to dissipate the large amount of waste heat, also sending operators to this unattended station and housing them there for the duration of the storm. It is expected that this installation will be ready for the next winter season.

It is obvious that a similar method might be applied to any electrified railroad, a-c or d-c, if the economics of the situation warranted.

Ice or Sleet on Pantographs and Collectors

The problem of ice accumulations on pantographs and collector shoes has been approached from a number of different angles. The usual and time-honored practice is to work the pantograph up and down by means of its own operating mechanism at frequent intervals during ice storms to jar off the ice accumulations mechanically. Obviously this means cannot be employed on long high-speed runs. It is also customary practice to use higher spring pressure during the winter, partly for the purpose of compensating for the expected additional ice load, and partly to exert more pressure on the wire for knocking off the ice on it. Mention has already been made of the possibility of augmenting the normal pressure at will by auxiliary air pressure, to overcome excessive added weight.

Another proposal is to provide steam jets on top of the locomotive to blow steam from the train-heating boiler at will over the pantograph frame in the lowered position. Obviously this scheme is not applicable to pantographs on motorcars, since they carry no steam-heating equipment. Another method which has been proposed for a-c electrified railroads is to have the pantograph frame split electrically at the base and all up through the frame, connected electrically only at the top by the contact shoe, and to circulate through this split frame a low-voltage current applied across the base from a separate special transformer, sufficient in value to raise the temperature of the entire frame and shoe. This current would be applied when necessary by the engineman. The traction power circuit would be connected to one side only of the base. Obviously, this scheme is not applicable to pantographs on equipment operated by direct current, at least without additional conversion apparatus.

Coating the frame and shoe with a waxy or greasy substance has been tried and is thought by some to be helpful in delaying the formation of adherent ice on pantographs. This measure has already been commented on in connection with the mechanical removal of ice from contact wires, and those comments apply here. It is possible that accumulated ice can be more easily jarred off the pantograph by working it up and down, when such coatings are applied, but again, this is a function of the temperature as well as the thickness of the accumulation.

Summary

Ice storms cause special problems on electrified railroads which power transmission systems do not have, most of them involving the current collection.

Ice accumulations may be removed from overhead contact systems by mechanical or electrical means.

Electrical heating of the contact system has been used satisfactorily on one a-c electrified railroad for a special case, and three methods of doing this have been devised, some of which may be applicable to other railroads.

The problems caused by high winds on ice-covered conductors are shared with other power-transmission systems and are serious ones. Changes to existing plant may help, but the possibilities of these difficulties should not be overlooked in the original design.

Ice accumulations on pantographs present special problems which are recognized and are being solved in various ways.

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Sealed-Tube Ignitron Rectifiers

M. M. MORACK
MEMBER AIEE

H. C. STEINER
ASSOCIATE AIEE

IN the past decade the advantages of the rectifier, when compared to other forms of conversion equipment, have led to the development of multianode steel tank and glass-bulb rectifiers. The desirable structural features and operating characteristics, however, have not been fully realized when pump-evacuated tanks and fragile glass-bulb rectifiers have been applied to small conversion units of low-voltage rating. Development of a means of starting or igniting[1] a cathode spot, each positive half cycle, on a pool of mercury has stimulated the design of a practicable half-wave or single-anode rectifier. The low density of ionization during the inverse cycle so reduced the shielding necessary to prevent arcback and, in consequence, the arc losses, that these rectifiers could be efficiently applied in the lower-voltage (250 volts d-c) fields.

Sealed, permanently evacuated steel ignitron tubes have been used in the control[2] of resistance welding currents for the past ten years; and in such service these electronic devices have given a degree of control, speed, and reliability not previously matched by mechanical means. Sealed, permanently evacuated steel ignitron tubes for rectifiers ranging in capacity from 75 to 400 kw (at 250 to 600 volts d-c), have been developed and placed in service, and it is the purpose of this paper to describe the design, characteristics, and performance of these rectifiers.

The Field for the Permanently Evacuated Tube-Type Rectifier

The field of application of the mercury-arc rectifier is considerably enlarged by the use of permanently evacuated tubes in circuits which have output characteristics similar to those of a d-c generator with nominal field control. Despite almost universal use of alternating current for generation and distribution, direct current still provides the most suitable source of power where adjustable speed or controlled torque characteristics are desired. D-c motors for machine tools, business machines, magnetic chucks, and separators, grinding machines, and elevators are typical examples of these d-c loads.

In comparison with other forms of converting equipment, the sealed-tube ignitron rectifier offers several distinct features for such service. The equipment is completely static, thereby eliminating special foundation requirements. The rectifier is quiet in operation which permits installation in locations where any appreciable noise would be objectionable. The over-all efficiency is high and practically constant over the entire load range. The no-load losses are small when compared with rotating equipment of like capacity. Rectifier equipments are factory-assembled, thereby minimizing installation time and expense.

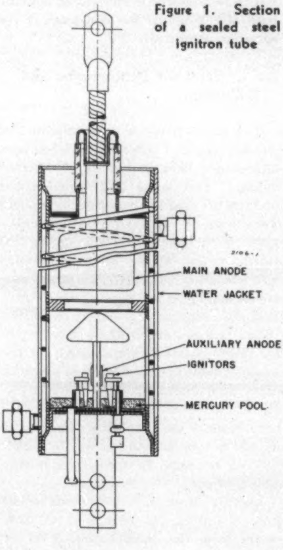

Figure 1. Section of a sealed steel ignitron tube

Tube Design and Operation Characteristics

Two sizes of ignitron tubes have been developed. The general tube design and construction are shown in Figure 1. The tube elements consist of a main anode, mercury-pool cathode, two ignitors, an auxiliary or holding anode spray, and deionization shields. Water jacket, flow spiral, and inner cylinder are of stainless steel.

Ignition[3] of a cathode spot is accomplished by passing an impulse of current from the ignitor to the mercury pool. Passage of current from the ignitor to the pool generates sufficient heat to break momentarily the contact of the mercury with some crystal in the ignitor. If the magnitude of the current, its duration, and the voltage gradient along the ignitor are sufficient, a cathode spot is established.

Figure 2 shows the variation in ignition voltage, current, and time for a typical ignitor, when supplied from the voltage of the main anode. When the ignitor is started at the beginning of the applied voltage wave, the current rise is slow, and the time required to reach the critical ignition value is long. When the applied voltage is high, as near the middle of the wave, the critical ignition current is quickly reached and the time correspondingly short.

Arc-drop voltages are given in Figure 3 in terms of anode current and outlet water temperature. These data were determined from cathode-ray-oscillograph measurements, while the tubes were supplying an inductive load of sufficient magnitude to insure essentially flat-topped current waves. Arc losses represent a design compromise between the minimum obtainable with no baffling and those with the shielding required to produce satisfactory freedom from arcback.

Cathode spots on a free surface of mercury become unstable when the instantaneous current decreases below approximately three amperes. To provide stable operation in this current range, an auxiliary anode has been added to the tube elements. This anode is usually excited from a low-voltage source inphase, or slightly ahead of the period of main anode conduction.

The tubes are water-cooled. Water-flow rates, cooling area, and temperature are determined by the requirements of removing the arc losses while maintaining vapor[4] pressure control. The outlet water temperature varies inversely with the load as is shown in the performance data of Figure 4. Maximum water temperatures are determined by the point at which the vapor pressure reaches arcback conditions. Minimum water temperatures are determined by the ability of the tubes to conduct current without "surging." This phenomenon is the result of insufficient vapor pressure to provide ions for the load-current demand. Momentarily con-

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M. M. Morack and H. C. Steiner are in the vacuum-tube engineering department, General Electric Company, Schenectady, N. Y.

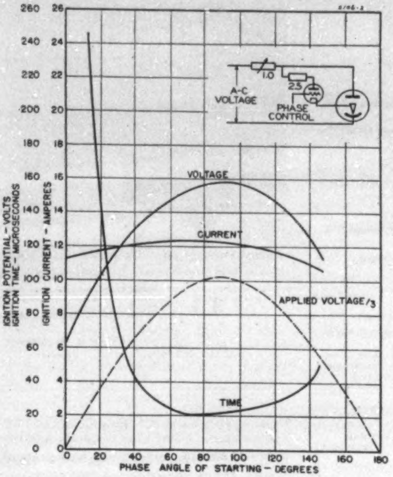


Figure 2. Ignitor characteristics

duction ceases, and the rapid current changes in transformer and lead inductance generate high voltages. Surging is usually limited to the first few cycles of conduction and for the particular designs does not occur at full-load currents with water temperatures as low as ten degrees centigrade. At less than full load the temperature may be lower. Optimum operating temperatures are in the range of 30 to 50 degrees centigrade, since under these conditions the arc losses are nearly a minimum, and there is more reserve vapor pressure control to take care of current demands.

Ignitron tubes in common with most vacuum-tube devices have elements of relatively small mass. The time for averaging the heating effect of sustained current demands is therefore a matter of minutes rather than hours as found in more massive machinery. This time is adequate for clearing feeder fuses, starting motors and for the usual types of overloads that are encountered in service.

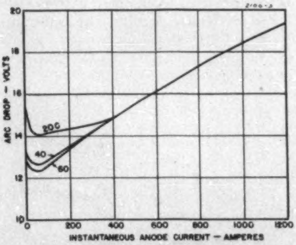


Figure 3. Arc-drop characteristics for cooling water temperatures of 20–60 degrees centigrade

Rectifier Design and Performance

Power Circuits

The choice of rectifier power circuits permits the grouping of different numbers of tubes to obtain various rectifier capacities. Figure 5 shows some of the more common of these circuits, together with the number of rectifying elements required, and the nominal ratings which are obtained in 250-volt service. These nominal ratings are made on the basis of the continuous duty. The usual overload factors are 125 per cent for two hours and 200 per cent for one minute. For most applications the circuits in which several tubes conduct simultaneously are preferable. In such circuits the total load is divided. Arc losses are therefore a minimum, since the instantaneous tube currents are low, and current capacity is a maximum. In certain cases, however, ease of control makes circuits preferable in which each tube carries the full load current. Rectifiers[5] which supply field currents for synchronous machines are in this category. To provide rapid response for both increasing and decreasing line voltage, the rectifier must be capable of inversion as well as of rectification, and the control is simplified when circuits of the three-phase, quarter-phase, or six-phase type are used.

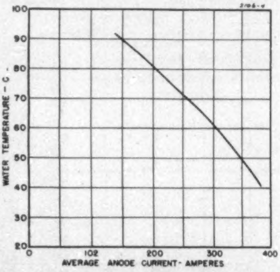
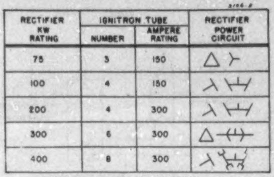


Figure 4. Arcback temperatures for operation at 250 volts in a three-phase single-way circuit



RECTIFIER KW RATING	IGNITRON TUBE		RECTIFIER POWER CIRCUIT
	NUMBER	AMPERE RATING	
75	3	150	
100	4	150	
200	4	300	
300	6	300	
400	8	300	

Figure 5. Table showing typical power circuits and rectifying elements for 250-volt operation

Two-wire service is readily obtained from the usual rectifier circuits. Three-wire service may be obtained in two ways. A balancer set capable of carrying unbalanced currents of the order of 25 per cent of the set rating is usually sufficient to provide the required neutral. Three-wire systems may also be obtained electronically through the use of a three-phase, double-way[6] circuit shown in Figure 6. A practicable installation may consist of two-wire circuits to carry the main part of the load and sufficient three-wire circuits to carry the unbalanced load.

Ignitor Excitation

Anode excitation[7] is the most direct method of establishing a cathode spot. In this case a portion of the load current is diverted through the ignitor by means of a

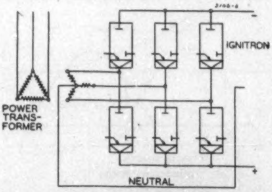
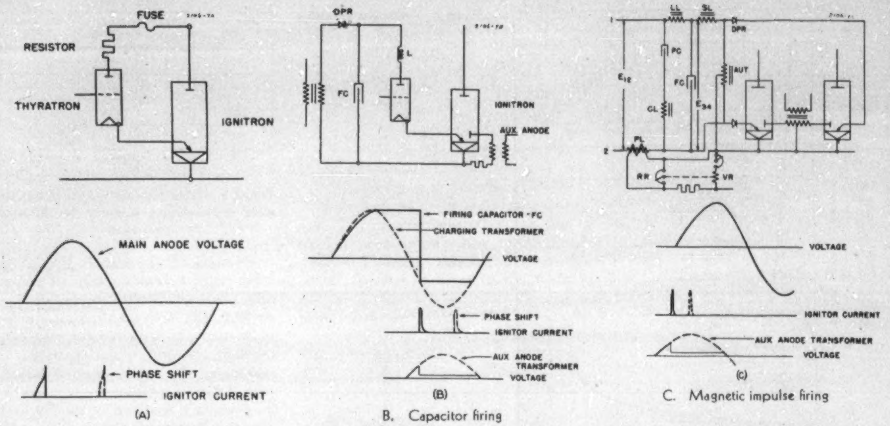


Figure 6. Three-phase double-way rectifier for three-wire service


FUSE
RESISTOR
THYRATRON
IGNITRON
MAIN ANODE VOLTAGE
PHASE SHIFT
IGNITOR CURRENT
(A)
DPR
L
FC
IGNITRON
AUX ANODE
FIRING CAPACITOR - FC
CHARGING TRANSFORMER
VOLTAGE
PHASE SHIFT
IGNITOR CURRENT
AUX ANODE TRANSFORMER VOLTAGE
(B)
1
LL
SL
DPR
PC
FC
AUT
E12
E34
CL
2
PL
RR
VR
VOLTAGE
IGNITOR CURRENT
AUX ANODE TRANSFORMER VOLTAGE
(C)

A. Anode firing

B. Capacitor firing

C. Magnetic impulse firing

Figure 7. Ignitor excitation systems

suitable thyratron control tube. Establishment of the main arc effectively shorts the excitation system and thereby limits the average ignitor current to practicable values. This system is shown schematically in Figure 7A. The anode firing excitation system has two limitations. When the rectifier is feeding a counter electromotive-force load, and the current is low, the anode currents tend to become discontinuous. The voltage which can be applied to the ignitor becomes the difference between the instantaneous transformer voltage and that of the counter electromotive-force load. This voltage difference is so low that either a relatively long period is required for the ignitor current to reach the critical ignition value or the counter electromotive-force voltage decreases sufficiently to provide the required ignition current. Momentarily, therefore, the rectifier ceases to deliver its normal output voltage. This effect can be corrected by providing sufficient series inductance in the d-c load to assure that the current is continuous. Even though the load current is continuous, it may drop below the critical ignition value. In effect the ignitor resistance is suddenly placed in the circuit. The resulting reduction in output voltage can produce a very annoying light flicker when lamps are part of the connected load.

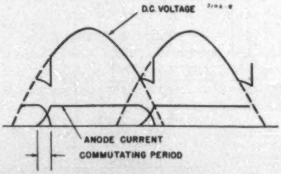

D.C. VOLTAGE
ANODE CURRENT
COMMUTATING PERIOD

Figure 8. Diagram showing voltage loss due to commutation

One form of separate excitation is shown in Figure 7B. In this case a capacitor (*FC*) is charged through dry-plate rectifier (*DPR*) and discharged at the proper instant through a thyratron tube. Phase control is obtained in the usual manner through the thyratron grid. An inductance in the discharge circuit establishes the desired rate of rise in the ignitor current. The circuits are so designed that a relatively high peak current, approximately 25 to 30 amperes, is available prior to ignition. This current value is in excess of the usual requirements, but, since the available energy is fixed, a design factor must be included for any possible contingency. The duration of the impulse is approximately 500 microseconds and the average ignitor current one-half ampere. Losses are of the order of 100 watts per ignitor. The ignition current is of such short duration that the cathode spot is maintained for the remainder of the main-anode conducting period by an auxiliary anode. Average current in this circuit is of the order of two amperes and losses 50 watts.

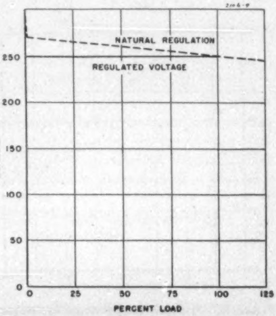


Figure 9. Typical output-voltage characteristics obtained by phase control

The required peak excitation current may be obtained[8,9] magnetically as shown in Figure 7C. Operation of the circuit is as follows: The linear reactor (*LL*) has an iron core with an air gap and is designed to give a constant reactance up to rated voltage and frequency of the circuit. The saturating reactor (*SL*) is built with a closed high-permeability core and a winding which will draw a large magnetizing current at the saturating point. This peak of current occurs when the capacitor (*FC*) is charged to its maximum voltage. The capacitor discharges through the saturating reactor, giving an impulse of ignitor current. A dry-plate rectifier (*DPR*) prevents reverse current from flowing in the ignitor circuit. The autotransformer (*AUT*) allows one circuit to supply excitation power to two tubes, which are in 180-degree phase relation.

The phase position of the impulses

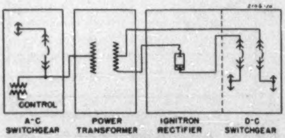

CONTROL
A-C SWITCHGEAR
POWER TRANSFORMER
IGNITRON RECTIFIER
D-C SWITCHGEAR

Figure 10. Line diagram showing rectifier component parts

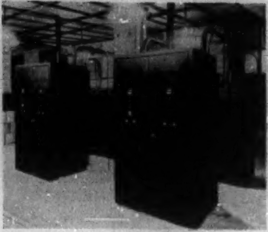


Figure 11. Metal-enclosed ignitron rectifier with automatic d-c switchgear for general purpose use

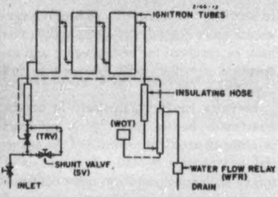


Figure 13. Water-cooling system of a 75-kw ignitron-tube rectifier

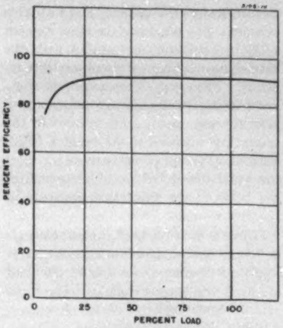


Figure 14. Over-all efficiency for a 250-volt 75-kw ignitron rectifier

which establish the cathode spot are controlled by shifting the supply voltage (E_{34}) to the excitation circuit. This is accomplished by means of the network, consisting of reactor (*CL*) and (*PL*), and capacitor (*PC*). The reactor (*PL*) is capable of gradual change in reactance over a 10 to 1 range by means of d-c saturation. The reactor (*CL*) and capacitor (*FC*) operate with a circulating current larger than that drawn by the excitation circuit and act as a power source for this circuit. The phase position of the voltage (E_{34}) can be adjusted by controlling the point of saturation in the phase-shifting reactor. In this manner a 40-volt-ampere d-c source may be used to vary the firing point of the ignitor over an angle of approximately 50 degrees.

Fault Protection

Fault protection must be provided for a-c and d-c short circuits and for tube arcback conditions.

A-c protection for the transformer circuits is taken care of most satisfactorily by means of circuit breakers. The interrupting capacity of such a breaker is determined by the system capacity from which power is obtained.

The d-c protective equipment should provide for the usual functions of overload and short circuit. Again breakers are usually preferable, at least for the main feeders.

In spite of almost complete freedom from arcback when circuit, tube, and load conditions are properly co-ordinated, provision must be made for the possibility of arcback. Anode fuses of proper characteristics and capacity have proved satisfactory for this service. Differential protection can be obtained by the coordination of the tube current-carrying capacity with the transformer current-limiting reactance. The proper relationship of these two factors permits the remaining tubes in the circuit to carry the transformer short-circuit current without arcing back, for the eight or ten cycles required for the fuse to open. Likewise d-c short circuits may be cleared with the rectifier remaining available for service.

Voltage Control and Regulation

Regulation in rectifier circuits is due primarily to the voltage lost during the commutation period. At this time two anodes are conducting simultaneously, and the output voltage is the average of the instantaneous voltages. These conditions are shown in Figure 8. The voltage loss[10] during the commutation period is

Voltage drop $= PFLI$

where

P = number of phases or anodes which commutate consecutively

F = system frequency

L = commutating inductance of one phase in henries

I = d-c current at the beginning of current transfer

The commutating reactance is that of any two phases whose anodes conduct in turn. It may be determined by shorting the transformer primary and applying excitation current to the secondary phases.

Rectifiers of this type usually have voltage regulation of six to seven per cent. This regulation is often greater than is desirable. To mitigate this effect, as well as compensate for variation in the voltage supply to the rectifier, the phase of the ignitor impulse is varied with respect to the positive half cycle of anode voltage to control the starting point of the anode current.

For magnetic excitation circuits, the simplest method of controlling the rectifier output voltage is to vary the d-c saturating current in accordance with the variation in the d-c output of the rectifier. In Figure 7C the voltage sensitive element (*VR*) of a torque-type regulator is connected across the d-c voltage to be regu-

Figure 12 (left). Installation of a portable ignitron rectifier for mining service

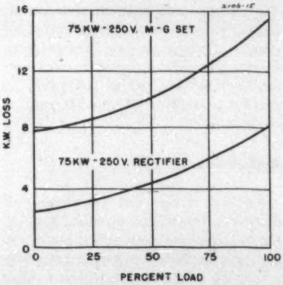


Figure 15 (right). Comparison of motor-generator and ignitron-rectifier losses lated in series with a reactor and a variable resistor. The regulator rheostat element (*RR*) is connected in parallel with the saturating windings of the phase shift reactors. The voltage drop across the rheostatic element varies from a maximum to zero, thereby varying the current in the saturating winding of the reactor (*PL*). Parallel operation of one or more rectifiers can be obtained by use of an equalizer bus between the regulating elements of the various units.

Figure 9 shows a typical characteristic in which the rectifier voltage output is regulated from zero to full load. Beyond full load the output voltage follows the natural regulation.

Where extremely rapid response is desired, such as in the case of an electronic exciter operating to control the output voltage of a synchronous condenser, an electronic regulator[5] may be used.

EQUIPMENT DESIGN

In general, the equipment design allows complete factory assembly of the units. The tubes, excitation, and control form one unit; the transformer another; and the required switchgear a third. A typical rectifier equipment is shown on the line diagram of Figure 10 and consists of the following components:

(*a*). Metal-enclosed manually or automatically operated a-c switchgear.

(*b*). Main, interphase, and control power transformers which are oil- or Pyranol-filled, natural or forced air-cooled, depending upon location and service.

(*c*). Metal-enclosed water-cooled ignitron mercury-arc rectifier with manually or automatically operated d-c switchgear.

Figure 11 shows a rectifier installation that is suitable for general purpose use such as lighting, power, or elevator loads.

Mine-service rectifiers require special designs in that the available head room is limited. Figure 12 shows a typical unit in which the rectifier, the transformer, and the switchgear form an articulated train. The cars are sufficiently short to permit moving around the usual track curves encountered in mine service. Portable rectifiers are particularly adapted to mine service, because they permit movement to new load centers as the coal is progressively removed.

Application

Rectifier ratings and the number of units required depend upon the application. In essential service it is considered good practice to have in reserve a stand-by unit which will permit the largest rectifier in service to be removed. Where requirements are less severe, and where the units may be removed for maintenance and inspection, rectifiers may be used to full capacity.

Elevator loads, particularly in hotels, apartment houses, and office buildings provide an excellent example of the essential service type of load. For reasons of safety such transportation must continue in operation. A satisfactory solution of the problem is to provide two complete and independent rectifiers with automatic change-over in case of d-c voltage failure. Simple switching provides preselection of the normally operating and reserve rectifiers, and the order may, of course, be periodically changed to equalize the use of the two units.

Tap water is usually used for removing the rectifier-arc losses. Full-load arc losses in 200-kw 250-volt rectifiers are approximately six per cent. Hence the transfer of this loss into the water rather than into the air is desirable, particularly in locations having limited ventilation facilities. The cooling water is not contaminated in passing through the tube jackets, and may be used in plant processes. Figure 13 shows the usual arrangement in which water is supplied to the rectifier at line pressure and passes through regulating valves to the tube jackets. Valve *SV* is a shunt valve used to adjust minimum flow. Valve *TRV* is controlled thermostatically and regulates the water flow in response to load. Thermostat (*WOT*) protects against water over temperature while the water flow relay (*WFR*) will remove the rectifier from operation on loss of cooling water flow. The flow relay utilized depends for its operation upon a physical displacement requiring both hydraulic pressure and water flow.

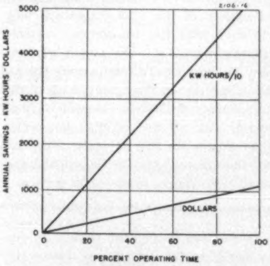


Figure 16. Annual savings for a 75-kw 250-volt rectifier

In general, most waters which are suitable for industrial or public use are satisfactory for cooling requirements. Waters which readily deposit lime or silt are to be avoided. The stainless-steel jackets are resistant to most corrosive elements, and the usual temperature of operation is not conducive to lime deposits. Stainless steel, however, is subject to slight corrosion by waters containing chlorides. Generally waters containing below 20 parts per million are satisfactory. Most public water supplies do not contain more than 8 to 15 parts per million.

Where local water conditions are unsatisfactory for direct use, heat exchangers may be employed. For mine service, air-to-water heat exchangers have been found to be the more practicable. Mine waters, in general, are very corrosive and very dirty. Air temperatures, on the other hand, are relatively constant and sufficiently low to make air-to-water heat exchangers attractive.

Maintenance

Rectifier maintenance outside of the usual inspection which should be given any electrical apparatus is primarily a matter of tube renewals. The number and frequency of such renewals depend upon the ultimate tube life. This ultimate life logically seems to depend upon the ignitor or loss of tube vacuum. To provide for the former, sealed tubes have two ignitors, one of which is held in reserve. Actually, examination of ignitors which have operated for two to three years shows that no appreciable erosion has taken place. Experience over a four-year period indicates there is no impairment in the tube vacuum.

A direct approach to the problem of maintenance is to evaluate the annual savings in power cost resulting from the higher efficiency and lower no-load losses of rectifiers. Figure 14 shows the over-all rectifier efficiency, and Figure 15 the kilowatt loss of a typical 75-kw motor–generator set and an equivalent ignitron rectifier at various loads up to full rating. Between 20 and 80 per cent of rated load, the difference in losses varies from 5.5 to 6.8 kw. Since the usual load characteristics are such that the load on either a motor–generator set or a rectifier will rarely lie outside of this range, the annual savings in kilowatt hours obtained by using a rectifier can be approximated very closely by multiplying the number of hours in service by 6.1. The gross saving depends upon the cost of electrical energy. Although power costs vary considerably, rates of one to three cents per kilowatt hour are representative. Figure 16 shows the annual saving on the basis of a power cost of two cents per kilowatt hour in

Practical Design of Counterpoise for Transmission-Line Lightning Protection

E. HANSSON
MEMBER AIEE

S. K. WALDORF
MEMBER AIEE

I. Introduction

EXPERIENCE has shown that preventive lightning protection, involving the installation of overhead ground wires and the co-ordination of tower-ground resistances with insulation level, can be quite effective in preventing flashovers due to lightning. Studies presented in an earlier paper[1] indicated that the efficiency of preventive protection can be made practically 100 per cent for high-voltage lines having insulator flashover levels of 1,600 kv and above. If the flashover level is reduced, the efficiency of protection becomes less.

Tower-ground resistance directly affects the protection efficiency obtained, and for satisfactory line performance resistance usually must be reduced below the values obtained with tower foundations alone. Buried counterpoise offers a practical means for accomplishing this, and, although many papers have been written on the theoretical aspects of the counterpoise, there is very little published on the problems encountered in actual installations.[2] This paper gives some methods that have been used in the installation of buried counterpoise at approximately 1,800 steel towers of the Pennsylvania Water and Power Company, as well as some of the results obtained.

When a counterpoise installation was contemplated, it was first necessary to determine the level of tower-footing resistances theoretically desirable. Next it was estimated how closely this objective could be approached and the amount of counterpoise conductor required. The data and methods presented are not sufficiently comprehensive to be applicable to all cases in all localities, but they may prove useful as a guide in making similar installations.

II. Determination of the Optimum Tower-Footing Resistance

Experience has shown that in most cases flashovers will not occur at towers where the footing resistances are kept below the value indicated by the relation[3]

$$R=\frac{V}{I(1-CF)}$$

where

R = Tower-footing resistance
V = Impulse-flashover level of line insulation
I = Maximum natural tower lightning current
CF = Coupling factor between overhead ground wires and line conductors

This equation fixes the upper limit of footing resistance for any assumed value of tower current I, I usually being taken in excess of any tower currents that have ever been measured in the field.

Towers have suffered flashovers when the footing resistances and tower currents were appreciably less than the upper limit set by the above expression.[1,4] This has occurred more frequently than can be attributed to freak conditions. Apparently there is an impedance voltage drop in tower structures and grounding systems which accompanies heavy lightning discharges and which causes flashovers when the ohmic resistance drop appears low. As a practical consideration the effect has been evaluated in terms of tower currents.

For instance, on a 66-kv line having eight $4\frac{3}{4}$-inch insulators the upper limit for tower-footing resistance is obtained by substituting in the preceding equation

$$R=\frac{V}{I(1-CF)}=\frac{635{,}000}{150{,}000(1-0.25)}=5.6\text{ ohms}$$

It is assumed that 150,000 amperes is approximately the largest tower current that has ever been measured under natural lightning conditions. It would be logical to expect that towers having footing resistances of this level or less would not be subject to flashover; yet it has been found that flashovers have occurred on towers having footing resistance of this order of magnitude, when the indicated tower currents have been only about 60,000 amperes.[1] This leads to a modification of the desired level of tower-footing resistance based on economic considerations. With insulator flashover levels of about 600 kv, footing resistance can be considered the controlling factor for indicated tower currents up to only 60,000 amperes. Due to the uncertain

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E. Hansson is transmission engineer and S. K. Waldorf test engineer, both with Pennsylvania Water and Power Company, Baltimore, Md.

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terms of the percentage of time the set is in operation. In general the average savings exceed the average replacement cost.

Conclusion

The design and application of rectifier equipment using sealed ignitron tubes has involved the solution of many new and interesting problems. Light flicker, control, voltage regulation, the problems of excitation, overload, and arcback protection have necessarily formed a part of the evolution of this type of rectifier equipment. The basic problems have been solved, and it is believed that the continuous and reliable operation of the sealed steel-tube rectifiers will demonstrate that these electronic devices have the same high standards of performance as the older forms of conversion equipment.

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Table I. Optimum Tower-Footing Resistances for Several Transmission Lines

Line Operating Voltage (Kilovolts)	Suspension Insulator Units	Nominal $1^1/_2$x40-Microsecond Flashover of Insulators (Kilovolts)	Optimum Tower-Footing Resistance (Ohms)
66	7–$4^3/_4$ in.	565	5
66	8–$4^3/_4$ in.	635	6
132	12–$5^1/_4$ in.	1,015	10
220	20–$5^1/_4$ in.	1,605	16

accuracy of tower-current measurements, it has been suggested that tower currents measured by surge-crest ammeter links mounted on tower legs be multiplied by a factor of 2 to correct for the currents carried by cross members of steel towers.[5] This gives a maximum safe tower current of about 120,000 amperes and a practical working value of about seven ohms for the optimum tower-footing resistance on an actual transmission line having an insulator flashover level of 635 kv. As all the factors in the equation are not known exactly under natural lightning discharge conditions, it has been found that a good working rule for the desired footing resistance is that it shall be not more than one ohm for each 100 kv of insulation flashover level, using the $1^1/_2$x40 microsecond value. This is referred to as the optimum tower-footing resistance.

Some examples of optimum tower-footing resistances are given in Table I.

III. Selection of Counterpoise Length

Usually counterpoise is installed principally as a convenient auxiliary means for reducing tower-footing resistance. For this reason the amount of counterpoise conductor used is that required to bring the ground resistance as close as practicable to a desired level. There are some notable exceptions to this principle. The section of the Boulder Dam–Los Angeles lines in high resistance territory[6] and the High Knob section of the Wallenpaupack Tap[5] are published instances of effective counterpoise installations where ground-resistance criteria do not apply. It is significant that in both of these cases lines operate at high voltage and have high insulation levels.

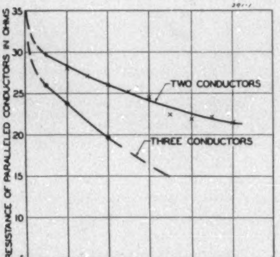

Figure 1. Effect of spacing on ground resistance of parallel buried conductors as determined by test

Based on traveling wave theory, Bewley and Hagenguth[7] have suggested that counterpoise conductors should be no less than 200 feet long. This minimum length has proved to be a satisfactory one in the field, from the viewpoint of both installation and performance. The maximum length that has been used on the 66-kv lines considered in this paper has been 400 feet where spans are greater than 800 feet, and continuous from tower to tower where the spans have been less than 800 feet. On 132-kv and 220-kv lines where the insulation levels are higher (see Table I), continuous counterpoise has been installed where ground resistances could not be reduced sufficiently by buried conductors 200 to 400 feet long. The basis for this difference in procedure for the two classes of insulation levels is the observed phenomena that the 66-kv towers flash over when low measured ground resistances are due to good grounds at some distance from the tower bases,[1] and that the higher-voltage lines can apparently withstand heavy discharges even though the necessary low ground resistances are obtained only by counterpoise extending for some distance from the tower foundation.[5,8]

IV. Selection of Number and Spacing of Counterpoise Conductors in Parallel

Dwight[9] has published formulas for calculating the ground resistances of buried conductors, and they show that the greater the spacing between parallel wires the lower is the ground resistance of the system.

Table II. Reduction of Ground Resistance Due to Addition of Third Counterpoise Conductor Midway Between Two Others

Distance Between Two Outer Wires (Feet)	Ground Resistance in Ohms		Per Cent Reduction Due to Third Wire
	Two Outer Wires	All Three Wires	
10	28	26	7.1
20	26	23.8	8.5
40	21.8	19.7	9.6

To indicate the practical limits for spacing, a set of measurements was obtained with seven 400-foot buried lengths of bare solid copper rod of one-fourth-inch diameter. The conductors were laid in straight parallel lines, 18 inches below the surface of the ground, five and ten feet apart. After a suitable weathering period, the resistances to ground of the individual conductors were 37, 37, 35, 35, 35, 30.5, and 32 ohms. The results plotted in Figure 1 emphasize the fact that two or more wires in parallel with reasonable spacings cannot produce so low a resistance as might be expected from the common relation for parallel resistances.

Table II, based on the data of Figure 1, gives a comparison of the ground resistance of two parallel conductors with that of a set of three when a third conductor is installed midway between the original two. It can be seen that the third conductor reduces the ground resistance only slightly, but the reduction in the surge impedance would be greater theoretically than that in ground resistance.

Another investigation was made in the field to determine the relative merits of the installation of two counterpoise conductors parallel to each other on one side of a tower, as positions 1 and 2 in Figure 2B, and the installation of the two conductors on opposite sides of the tower, as in Figure 2A. Four wires, 540 feet long, were buried in the ground in the pattern shown in Figure 2B, with 26-foot spacing between parallel conductors. The ground resistances were as given in Table III, and show about a 25 per cent advantage for the arrangement of Figure 2A.

Based on field data, Table IV gives the order in which it has been found that

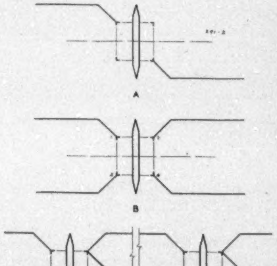

Figure 2. Arrangement of counterpoise conductors

Table III. Ground Resistance of Counterpoise Conductors at Test Installation Arranged as Shown in Figure 2B

Counterpoise Conductor Designations	Ground Resistances in Ohms
1	26.2
2	24.
3	25.3
4	26.9
1 and 2	18.5
3 and 4	18.8
1 and 4	14.1
2 and 3	13.2
All	10.1

counterpoise can be most effectively installed to reduce footing resistances to a desired level. The individual conductors are spaced as far apart as right-of-way restrictions allow, and the grounding conditions at each tower determine the type and extent of the counterpoise system to be installed.

V. Estimating Counterpoise Conductor Requirements

When tower foundations have been placed and a few weeks are allowed to elapse for the soil to settle around them, ground resistances are measured and used as indicators of the amount of counterpoise required. This procedure does not usually delay construction; when necessary, counterpoise can be laid and its resistance measured with a transmission line in operation. Incidentally, this fact has permitted counterpoise to be applied readily to the improvement of old lines. Tower-footing resistances have been found to be more reliable indicators of counterpoise requirements than spot soil-resistivity measurements, because of the abrupt changes in soil characteristics which occur in the territory under consideration. This territory varies from comparatively level ground, partly wooded, partly cultivated, up to hilly country where abrupt changes in elevation of a few hundred feet occur. Wherever rolling or hilly country is encountered, the soil consists of heavy clays liberally interspersed with broken shale or large boulders. In level country near sea level, the soil is generally sand or river silt.

Table IV. Recommended Order for Installation of Counterpoise Conductors

1. Two 200- to 400-foot conductors at opposite sides of tower (Figure 2A)
2. Four 200- to 400-foot conductors (Figure 2B)
3. Conductors extended to adjacent tower on one side, then to adjacent tower in opposite direction as required (see section III)
4. Third conductor laid midway between other two (Figure 2C)

It would be desirable to predetermine two characteristics of a counterpoise installation:

1. The levels to which ground resistances can be reduced from given initial tower-foundation resistances.
2. The amount of counterpoise conductor which will be required to attain the desired result.

No satisfactory method of doing either of these things was found until some actual field experience was accumulated. Based on data such as that given in section VII, estimates could be made of the average wire needed and final ground resistance to be attained for towers having foundation resistances in a given range. Even then, accurate estimates could not be made unless a large number of towers was considered, because the actual installation at a given tower frequently varied a good deal from the average result. Each tower and span was considered separately, taking into account situations in which long counterpoise conductors were required, and the installation would serve to improve conditions at both towers at the end of the span. In some cases it was found that certain towers would themselves require short counterpoise, while the neighboring tower would require a continuous one. An installation of this type, designed to make satisfactory the worse of two grounding conditions, would reduce the final ground resistance of the low-resistance tower much below the optimum value.

At those towers where large amounts of counterpoise conductor were called for, not more than three conductors in each direction from the tower were specified, because of considerations which were discussed in section IV.

VI. Ground-Resistance Measurements

A common method for making ground-resistance measurements is the three-electrode method.[10] With counterpoise conductors several hundred feet long, the required spacings of the two temporary probes used in the three-electrode method become impractically large. A simple and practical substitute for the three-electrode method is the direct reference method, using the completed transmission line and its grounds as the reference electrode.[10] The ground resistance measured by this method includes that of the reference electrode as well as that of the system being investigated. The ground resistance of an entire transmission line is usually only a fraction of an ohm, making the observed resistance the ground resistance of the counterpoise system for all practical purposes.

When applying the direct reference method, the counterpoise conductors are disconnected from the tower at which it is desired to obtain measurements and also from the next adjacent tower on each side, if the counterpoise is continuous. The counterpoise conductors are then electrically connected in parallel, and the resistance measured between their common point and the tower. The overhead ground wire serves to connect the tower to the rest of the system. It has been found by tests that the ground resistance of a counterpoise system and a tower in combination is approximately the same as that of the counterpoise system alone. The reason for this phenomenon is that the tower foundation lies within the extensive counterpoise system and does not contribute appreciably to the conductivity of the system as a whole.

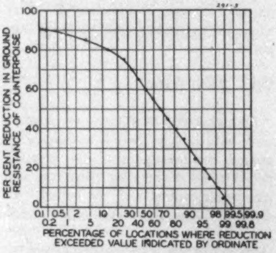

Figure 3. Reduction of counterpoise ground resistance due to weathering, at 483 tower locations

Counterpoise installed with a wire-laying plow

In this connection it is of some importance to note that the same effect is present when ground rods of moderate length are used at tower foundations. Due to the size of a tower foundation in relation to that of the rod and the small amount of additional soil contacted, the ground rod has been found to reduce ground resistances only slightly below that of the foundation alone. Exceptions occur in those special cases where a conducting stratum has been reached by rods of moderate length or very long rods have been driven.[11]

Practical considerations when making field measurements require that care be taken that corroded contacts do not cause inaccurate measurements. Defective electrical contacts may appear in the ground

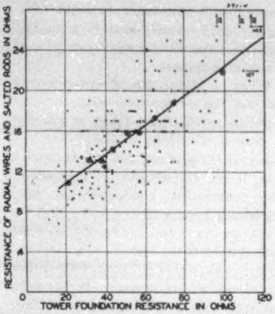

Figure 4. Ground resistances produced by four 50-foot buried radial wires terminating in salted rods at 171 locations on the Safe Harbor–Westport 220-kv line

at bonded joints in the counterpoise conductors and also at the tops of towers where the overhead ground wires are supported.

VII. Field Data

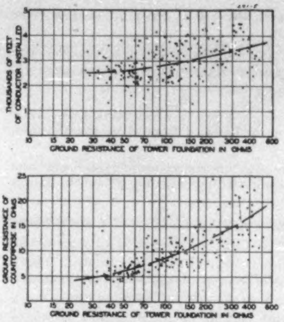

Figure 5. Counterpoise characteristics at 216 locations on the Holtwood–Coatesville 66-kv line

In many cases, counterpoise is continuous from tower to tower and serves to reduce ground resistance at two towers

Following the installation of counterpoise with a plow, there is a decrease in ground resistance during the ensuing period while the weather packs the soil into better contact with the buried conductor. This phenomenon has been found to be a highly variable one, indeterminate at any given location except by actual measurements of ground resistance. For instance, it can be seen in Table V that the variations in reduction have been from 0 to 91 per cent of the resistances measured at time of installation. After the initial weathering, further changes in counterpoise resistance have been found to be inappreciable. When making an installation, it has been the practice to allow for a reduction in ground resistance in order to limit the amount of wire used. In making this allowance, a balance was struck between an occasional installation of an excessive amount of counterpoise conductor as against the probable hazard of an occasional high footing resistance. In some cases where little or no reduction materialized, additional counterpoise was installed after weathering had occurred. Figure 3 can serve as a basis for establishing a working rule.

Table V. Reduction of Ground Resistance of Counterpoise Due to Weathering at 483 Tower Locations

Per Cent Reduction $\left(1-\frac{\text{Final R}}{\text{Initial R}}\right)$	Tower Locations		Percentage Exceeding Maximum Reduction of Range
	Number	Percentage	
0	3	0.6	99.4
1– 5	3	0.6	98.8
6–10	2	0.4	98.4
11–15	7	1.5	96.9
16–20	6	1.2	95.7
21–25	15	3.1	92.6
26–30	12	2.5	90.1
31–35	15	3.1	87.0
36–40	27	5.6	81.4
41–45	35	7.3	74.1
46–50	33	6.8	67.3
51–55	36	7.5	59.8
56–60	44	9.1	50.7
61–65	36	7.5	43.2
66–70	43	8.9	34.3
71–75	39	8.1	26.2
76–80	62	12.8	13.4
81–85	45	9.3	4.1
86–90	19	3.9	0.2
91	1	0.2	0

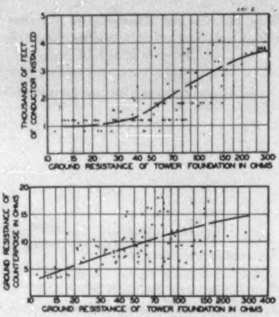

Figure 6. Counterpoise characteristics at 100 locations on the Safe Harbor–Perryville 132-kv line

The longer counterpoises are usually continuous and serve to reduce resistances at two towers

On the first line to which the principles of preventive lightning protection were applied, attempts were made to reduce the ground resistance of tower foundations by the addition of four 50-foot buried radial wires terminating in salted eight-foot ground rods. The results of this work are shown in Figure 4. In addition to the individual points shown on the graph, the encircled crosses each represent the average for 19 locations. This method of improving ground resistance was not satisfactory, because it did not reduce most of the tower-footing resistances below the desired level of 16 ohms. Subsequently footing resistances were improved by the addition of counterpoise conductor.

Figures 5 and 6 show the characteristics of counterpoise on two lines on which counterpoise alone was used to improve footing resistances. On the 66-kv line it was desired to obtain footing resistances below six ohms. On the 132-kv line the optimum value was ten ohms. As would be expected from the relative magnitude of the two desired final values, the economical maximum amount of wire which could be installed at a given location was reached more often on the 66-kv

Table VI. Summary of Counterpoise Installations Where Ground Resistances Were Measured After Weathering Occurred

Line	Number of Tower Locations	Ground Resistances in Ohms				Number of Locations Where Measured Exceeds Optimum	Feet of Conductor Installed Per Tower Location	
		Tower Foundation		Counterpoise				
		Maximum	Average	Optimum	Measured Maximum		Maximum	Average
Safe Harbor–Westport–Takoma (220 kv)	116	400	150	16	31	52	2,900	1,071
Safe Harbor–Riverside (220 kv)	234	450	77	16	18	8	4,650	1,172
Safe Harbor–Perryville (132 kv)	167	343	58	10	20	72	3,000	1,388
Holtwood–York (66 kv)	151	525	137	6	20	110	2,100	1,570
Holtwood–Coatesville (66 kv)	216	511	147	6	25	167	2,400	1,612

line than on the 132-kv line. It must be borne in mind that the lightning performance of these two lines is very good even though the optimum conditions were not attained in many cases.

The wide scattering of points in Figures 4, 5, and 6 are attributed to the abrupt changes in soil conditions which were frequently encountered. Where salted rods were used, they frequently could not be driven to their full length of eight feet due to rocky conditions. Where counterpoise was used, lower resistances to ground were obtained in cultivated fields and pasture land than in woods, and in low sections, than on hills. As the counterpoise was laid, it frequently would pass from one soil condition to another, producing noticeably different results on one side of a tower from those produced on the opposite side. In spite of the large variations in the results obtained at individual towers having similar foundation resistances, data of the type given have been useful in estimating the average requirements of projected installations.

It has not been the practice to remeasure the ground resistance of counterpoise at every location after weathering has occurred. It has been measured only at those locations where the values at time of installation were greater than the optimum values. For this reason complete data cannot be given for all locations on every line. However, in Table VI an indication is given of the magnitude of reductions in ground resistance that have been obtained by the use of counterpoise. The criterion of the worth of preventive lightning protection is the outage record of the lines to which it has been applied. Such a record is given in Table VII, and counterpoise has played an important role in minimizing interruptions due to lightning.

VIII. Summary

1. There is apparently an optimum value of tower-footing resistance for a given level of insulation on a transmission line. This has been evaluated empirically as approximately one-hundredth of the kilovolt flashover value of the insulation.

2. Three parallel wires in a counterpoise system seem to be the largest practical number. Such an installation provides six electrical paths away from a tower foundation.

Table VII. Lightning Outages of Lines Having Counterpoise as Part of Preventive Lightning Protection

Line	Years of Record	Outages	Outages Per 100 Miles of Line Per Year
Safe Harbor-Westport-Takoma (220 kv)	10	1	0.11
Safe Harbor-Riverside (220 kv)	4	1	0.50
Safe Harbor-Perryville (132 kv)	7	1	0.42
Holtwood-York (66 kv)	6	2	1.46
Holtwood-Coatesville (66 kv)	4	3	2.55

3. The counterpoise system installed at each tower on a line must be graded to the conditions at the tower location if uniformly low ground resistances are to be obtained at reasonable cost. Uniform low ground resistances along a line are to be preferred to a uniform counterpoise system.

4. The ground resistance of a counterpoise system decreases for a period following installation due to settling of the soil around the buried conductors. After the initial weathering period, seasonal variations usually are small in the territory where observations were made.

5. The amount of counterpoise conductor installed and the final resistance obtained at towers having similar foundation resistance varied a good deal due to abrupt changes in soil conditions in the territory studied. In spite of the variations, the average results have been helpful in estimating conductor requirements for new work.

6. Preventive lightning protection, in which counterpoise plays a prominent part by reducing tower-footing resistances, is very effective in preventing flashovers on high-voltage lines.

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Electric Control for Steam Boilers on Diesel-Electric and Straight Electric Locomotives

E. H. BURGESS
NONMEMBER AIEE

ELECTRIC control is a necessity in the operation of the modern steam boiler if the boiler is expected to have maximum efficiency, safety in operation, and constant operation at the various outputs required. Installation and operating conditions vary over a wide range; therefore, to meet these conditions the controls used differ in type and quantity in order to supply those actually needed for good operation, as well as those required to comply with rules and regulations under which the installations are permitted. Generally, the method of operation of such boilers can be classified in two categories, namely, automatic and semiautomatic, and these two classes will be discussed in this article.

Electric control is further separated into two component parts, where one group of items consists of the operating necessities for actual boiler operation, and the other of safety features required under legal regulations (including those needed for the safety of the equipment itself in case of failure of any major part).

Automatic-Control Boilers

Operating Necessities

To analyze the operation and use of each of the control items used for a complete automatic installation, it will be advisable to start with the basic boiler and add each electric item in the order of its use for normal operation.

Current for igniting the atomized fuel is generally obtained from high-voltage transformers or magnetos, with the majority of installations using the transformer, because of its ability to give a very hot spark for complete combustion. With transformer ignition a source of alternating current is required, and this is obtained from a suitable rotary converter where the necessary direct current is taken from the locomotive batteries or control source.

To provide constant ignition under all operating speeds of the boiler motor—including the off cycle of the boiler when no steam is being used—the converter is connected directly across the line and is controlled by the master control switch. The rotary converter serves another purpose also by supplying alternating current for the electric-eye operation, which will be discussed later under safety controls.

The pressure switch, operating directly from steam pressure, actuates the balance of the controls to regulate the required output. Where various output capacities are required, a multifinger switch is used to vary the motor speed in accordance with steam output and it automatically cuts in the motor-speed relays to keep the steam pressure up to the operating point.

As less steam is used, the pressure switch causes the contact fingers to operate in a definite sequence to slow down the motor, and thus only sufficient steam is made to replace that amount which is being used. When the steam consumption becomes greater than the output, the contact fingers operate in the reverse cycle and cause the motor to speed up in order that sufficient steam should be available for the requirements.

Connected in the oil line is a solenoid valve used to stop the flow of oil when such operation is required. The valve coil receives its current from the main relay of the motor control and is only energized when the relay is closed and the motor running. During the off cycles of the boiler the motor relays are de-energized; therefore, the oil solenoid valve is closed to prevent any oil from collecting in the combustion chamber which might cause a minor explosion when the fire again lights. This valve is also kept de-energized when the main motor is being run, only for the purpose of filling the boiler with water, and is accomplished by an interlock in the master control switch.

An important item in electric controls is the time-delay relay which has several functions during the normal operation. When the boiler is ready to run, and the proper switches have been turned to their operating position, the time-delay relay is momentarily energized, but the contacts remain closed for a predetermined length of time. Unless the oil ignites and keeps burning during this predetermined time, the contacts will open and shut the boiler down. If the fire continues to burn, then the relay coil will become energized through the electric eye or stack switch operation. During the off cycles of the boiler, it is also necessary to keep this relay energized in order that the motor will again start when steam is required, and this is accomplished by an interlock on the motor relay or a separate pilot relay. The time-delay relay is generally adjustable, so that the predetermined time setting can be adjusted to suit the conditions required for various sizes and types of boilers.

Safety Features

On any boiler installation safety control features must be included in order to comply with laws or regulations, and also to insure safety in operation of the boiler itself. To provide some means of stopping the boiler in case the fire does not properly ignite or goes out because of some defective part of the controls, some means must be provided to detect this condition, and transmit the proper impulse to the motor controls. Two methods are generally used, one being the photoelectric-cell type and the other a stack switch whose contacts are operated by a bimetallic helix.

The photoelectric-cell method consists of locating the photoelectric cell so that it is affected by the oil flame in the combustion chamber, causing current to flow to the amplifying unit. The rectifying and amplifying unit used in conjunction with the photoelectric cell includes a sensitive relay that remains energized whenever the proper flame is burning in the boiler. If the flame should go out while the motor is still running, the sensitive relay acts on the time-delay relay, which will stop the motor after the predetermined time lag. During normal operation, where the pressure switch stops the motor and cuts off the flow of oil, a pilot-relay interlock keeps the time-delay relay energized even though the sensitive relay contact is open.

Stack-switch control is similar to the photoelectric cell except that the contacts are opened and closed mechanically by the bimetallic helix which is, in turn, affected by the temperature of the gas surrounding it in the smoke stack. These

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E. H. Burgess is electrical engineer with the Vapor Car Heating Company, Inc., Chicago, Ill.

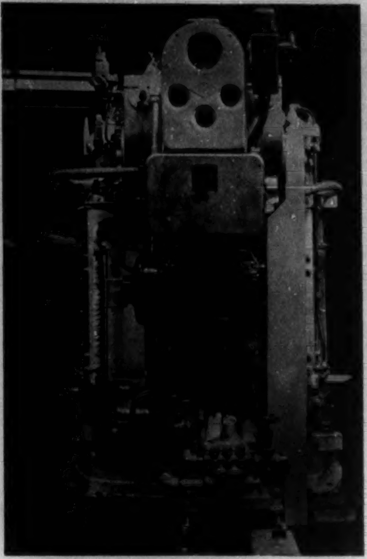
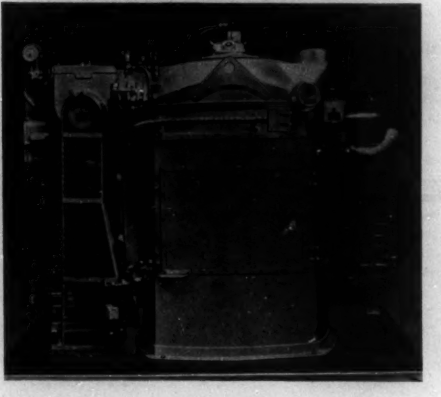


Figure 1. Front view of automatic steam boiler



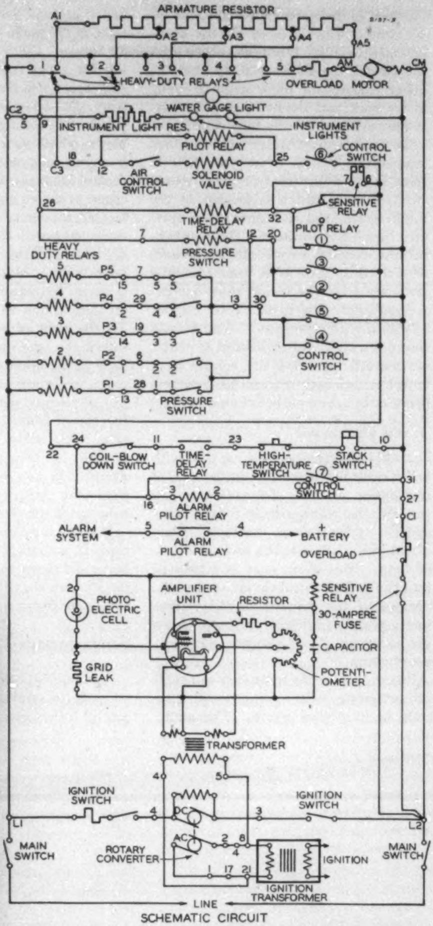

ARMATURE RESISTOR
A1
A2
A3
A4
A5
1
2
3
4
5
HEAVY-DUTY RELAYS
OVERLOAD
AM
MOTOR
CM
C2
5
9
WATER GAGE LIGHT
INSTRUMENT LIGHT RES.
INSTRUMENT LIGHTS
PILOT RELAY
CONTROL SWITCH
C3
18
12
AIR CONTROL SWITCH
SOLENOID VALVE
25
26
TIME-DELAY RELAY
32
SENSITIVE RELAY
PILOT RELAY
7
PRESSURE SWITCH
12
20
HEAVY DUTY RELAYS
P5
P4
P3
P2
P1
13
30
CONTROL SWITCH
PRESSURE SWITCH
22
24
COIL-BLOW DOWN SWITCH
11
TIME-DELAY RELAY
23
HIGH-TEMPERATURE SWITCH
STACK SWITCH
10
CONTROL SWITCH
31
27
C1
16
ALARM PILOT RELAY
ALARM SYSTEM
ALARM PILOT RELAY
+ BATTERY
OVERLOAD
PHOTO-ELECTRIC CELL
AMPLIFIER UNIT
RESISTOR
SENSITIVE RELAY
30-AMPERE FUSE
CAPACITOR
GRID LEAK
POTENTI-OMETER
TRANSFORMER
IGNITION SWITCH
IGNITION SWITCH
L1
L2
DC
AC
MAIN SWITCH
ROTARY CONVERTER
IGNITION
MAIN SWITCH
IGNITION TRANSFORMER
LINE
SCHEMATIC CIRCUIT

Figure 2 (left). Side view of automatic steam boiler

Figure 3. Schematic wiring diagram for described boiler

CONTROL SWITCH OPERATING POSITIONS

POSITION	1	2	3	4	5	6	7
TEST	●	●			●		●
OFF	●						
START			●			●	
RUN			●	●		●	

contacts are normally open and they close when a relatively low stack temperature has been reached such as one resulting from a continuous burning fire.

Where air, under pressure, is used to atomize the fuel oil, an air control switch is incorporated in the controls to insure that sufficient air of proper pressure is fed to the oil nozzle, so that the combustion will be complete and retain a clean fire. The air control switch is usually set to open the control circuit when a reduction of air pressure in the feed line reaches a point that will affect the combustion quantity of stack smoke and efficiency.

To protect against excessive high steam temperature, a high-temperature switch is incorporated in the control circuit to open the motor circuit when the temperature limit is reached. One type of switch uses an alloy that melts at this temperature and, in doing so, opens a spring set contact. To reset, the molten alloy must first cool and harden in order to hold the contacts closed for further operation of the motor controls.

A periodic blowdown of the coils is necessary to remove sediment and other foreign materials from the coils and pipe lines connected with them. To facilitate this operation a coil blowdown valve is installed in the water intake line, and incorporated with it is an electric push-button assembly. When the valve is open the electric circuit is open, so that the boiler cannot run while steam is being blown back, and the boiler cannot be restarted until the valve is fully closed.

To guard against excessively high stack temperature, which is an indication of some faulty adjustment or badly sooted coils, a stack switch is located in the smoke stack, and its contacts are operated by a bimetallic helix. The high temperature contacts are normally closed and, once opened, have to be reset manually and only after the temperature has decreased sufficiently to enable the contact mechanism to re-engage. Installations that use a stack switch instead of photoelectric cell for a low fire control have both low and high temperature contacts operated by a common helix in one housing.

ALARM MECHANISM

Audible or visual alarms are used with boiler controls, depending on the type of installation and rules of the operator. In general, the alarm circuit consists of a suitable relay whose contacts, when closed, ring a bell or light a series of lamps, or both. The alarm relay is connected into the boiler control circuit so that the opening of the contacts of the various safety features will not only shut down the boiler, but it will also give the designated alarm.

Figure 1 shows the front view of a typical automatic boiler equipped with controls as mentioned above. The multifinger pressure switch is shown to the right of the gauge box and in front of the air intake. Control cabinet directly below the gauge box contains the starting and speed-selection switch, time-delay relay, pilot relay and terminal-connection panel. The master motor for driving the blower wheel, water pump, and oil pump is shown mounted directly beneath the control cabinet. At the lower right-hand corner is shown a cutout box in which is located the electric push button used in conjunction with the coil blowdown valve.

Figure 2 shows the side view of the same boiler. At the upper left position is shown the side view of the multifinger pressure switch as previously described. To the right of the pressure switch is shown the ignition transformer and the high-voltage wires connected to the spark plugs which are near the top of boiler. The oil solenoid valve is shown directly at the top of boiler above the peep sight case. Just below the smoke-stack connection is shown the stack switch, mounted in the smoke hood, and to its right the high-temperature switch is mounted in the steam line.

Figure 3 is a schematic wiring diagram used in conjunction with this type of boiler and shows the connections between the various electrical operating controls and safety features.

Semiautomatic-Control Boilers

Semiautomatic-control boilers are essentially the same as the fully automatic-control boilers except for a few operating differences. The pressure switch is of the single-finger type, which opens and closes in accordance with the steam pressure selected but has no effect on the speed of the driving motor other than start and stop. Speed selections of the motor are made with a manual selector switch which connects to the motor various amounts of resistance, depending on the position of the switch. By this means the operator adjusts the motor speed, so that the steam output of the boiler will be satisfactory for the load it has to carry. Where motors used are of such size that motor starters are required, the necessary step control relays and starting resistance are needed in addition to the manual selector switch for actual speed-control setting.

Controls of this type are naturally limited to installations where the load is relatively constant for considerable lengths of time, or where an attendant is readily available to change the motor speed setting as often as necessary.

Electrical controls for steam boilers play such an important part in their operation, that any amount of time spent in developing the correct type is warranted by the practical results obtained. Full-time operation at maximum output and highest efficiency can be had only with electric controls that perform their respective functions in the way intended. The items mentioned in this article have been selected, from past experiences, to be necessary for proper operation and complete control of all functions of the boiler, including the safety requirements.

Electronics of the Fluorescent Lamp

MARK A. TOWNSEND
ASSOCIATE AIEE

THE fluorescent lamp is one of the recent and rapidly growing additions to the field of electronic devices. In order to understand properly the characteristics of the fluorescent lamp, it is necessary just as with other electronic devices, to know something about the fundamental principles of operation. It is the purpose of this paper to describe the operating principles of this type of lamp, both as an electrical conductor and as a radiation generator. Following a brief outline of the properties of radiant energy as they concern lamp design, a description is given of the phenomena occurring in the separate regions of the lamp. The final section of the paper is concerned with the over-all characteristics of the lamp considered primarily as a circuit element.

The purpose of any electric lamp is to convert electric energy into radiant energy. The usefulness of a lamp depends not only upon the efficiency with which it converts the input energy into radiation, but also upon the suitability of the produced radiation for the particular application involved. The following description of radiant energy is presented to indicate requirements placed on a lamp as regards radiation output. In order to indicate the similarity of lamps to other electrical equipment, radiation is described in terms of frequency rather than by the more conventional wave-length designation. For the benefit of those more familiar with the wave-length description, important values are given in both units.

Radiant Energy

To be useful as light, the radiant energy produced by a lamp must fall within the region of the spectrum to which the human eye is sensitive. This region is shown in relation to the remainder of the electromagnetic spectrum by the chart of Figure 1. Although the ultraviolet and infrared regions of the spectrum immediately adjacent to the visible are not directly useful for illumination, they are increasing in importance for other uses, and the term lamp has been extended by common usage to include sources of radiation in these regions of the spectrum. The range of frequencies covered by commercially available lamps is shown in the expanded portion of Figure 1.

It is desirable to control the processes of energy conversion within a lamp so that the proper frequency of radiation is obtained for each intended application. This may be illustrated for visible light by the curve of Figure 2, which shows the average response of the light-adapted human eye as a function of frequency. This may be compared with the response of a radio receiver tuned to a frequency of 540×10^{12} cycles per second, corresponding to a wavelength of 5,550 angstrom units (A). It is apparent that the greatest possible light-producing efficiency would be obtained with a lamp which converted all the energy input into radiation at this frequency. Such a lamp would be a green light source having an efficiency of approximately 620 lumens per watt. However, the usefulness of such a lamp would be limited because of its color. When a lamp produces radiation at any frequency other than 540×10^{12} cycles per second, its luminous efficiency is necessarily lower than the above value. For example, a lamp which converted all the input energy into radiation within the visible region having the same spectral distribution as average daylight would have a luminous efficiency of only 220 lumens per watt.

In the incandescent lamp, the spectral distribution of the output radiation is continuous throughout the spectrum and is controlled by the operating temperature of the filament. Since the energy distribution is continuous, it is not possible to confine the output to a narrow range of frequencies, except by filtering out and thus wasting the undesired radiation. In a gaseous discharge lamp the frequency of the output radiation is fixed by the kind of gas, each gas having certain characteristic frequencies which it can radiate. The relative amounts of energy radiated at the different frequencies can be controlled by the type of discharge. A good example of this method of control is given by two types of mercury lamps: in the low-pressure germicidal lamp most of the radiation from the mercury is concentrated in the short-wave ultraviolet, while in the high-pressure capillary lamp a major portion of the radiation is produced in the visible and long-wave ultraviolet regions. In the fluorescent lamp the output frequency is controlled by the choice of the fluorescent coating, as will be described later in this paper. However, the operation of the fluorescent lamp also involves a gaseous discharge in which, as in the simple discharge lamp, the most important controlling factors are the nature of the gas and the type of discharge. The electronics of the fluorescent lamp are concerned with the operation of this gaseous discharge, both as a circuit element and as a radiation generator.

Principle of the Fluorescent Lamp

The process by which the fluorescent lamp converts electric energy into visible radiation is analogous to that of two energy converters connnected in series. The first of these is a gaseous conduction lamp which is designed to convert electric energy into ultraviolet radiant energy. This lamp usually takes the form of a glass tube having a length of from 10 to 50 diameters. Within the tube is a small drop of mercury producing a mercury vapor pressure corresponding to the temperature of the tube wall. Also present is a low pressure of argon gas. Specially designed electrodes at each end of the tube are connected to the external circuit by means of vacuum-tight glass-to-metal seals. In operation this constitutes a low-pressure mercury vapor discharge lamp which can be made to convert 50 per cent or more of the electric energy input into ultraviolet radiation of wavelength 2,537 angstrom units corresponding to a frequency of $1,182 \times 10^{12}$ cycles per second.

The second energy converter in the fluorescent lamp is a frequency changer which converts the $1,182 \times 10^{12}$ cycles per second radiation into visible radiation with frequencies between 750 and 430×10^{12} cycles per second (4,000–7,000 angstrom units). This frequency converter consists of a coating of fluorescent powders which is applied to the inside surface of the discharge tube. This frequency conversion process is relatively high in efficiency so that, when it is combined with an efficient source of the exciting radiation, a high over-all lamp efficiency is produced.[1,2] Although a major portion of this paper is devoted to the gaseous conductor, a brief description of the characteristics of fluorescent powders is necessary in order to illustrate the require-

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Mark A. Townsend is lamp development engineer, lamp development laboratory, General Electric Company, Cleveland, Ohio.

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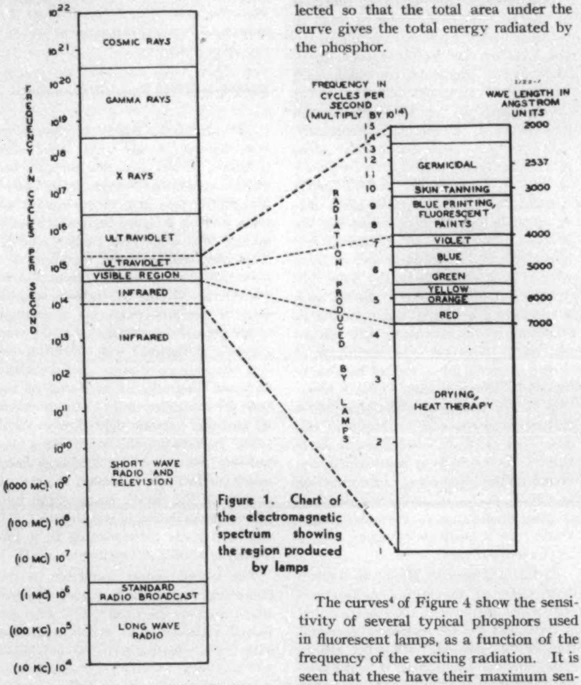
ments which the gaseous conductor must meet.

Fluorescent Materials

Certain materials, known as phosphors,[3] have the property of emitting visible light when they are exposed to exciting agents such as cathode rays, X rays, and ultraviolet radiation. The light thus produced is called fluorescent light, and most engineers are familiar with this property as used in the fluorescent screen of the cathode-ray oscilloscope and the X-ray fluoroscope. However, by far the most efficient method of exciting these phosphors is the use of ultraviolet radiation, as in the process used in the fluorescent lamp.



10^{22}
COSMIC RAYS
10^{21}
10^{20}
GAMMA RAYS
10^{19}
10^{18}
X RAYS
10^{17}
10^{16}
ULTRAVIOLET
10^{15}
ULTRAVIOLET
VISIBLE REGION
INFRARED
10^{14}
INFRARED
10^{13}
10^{12}
10^{11}
10^{10}
SHORT WAVE RADIO AND TELEVISION
(1000 MC) 10^{9}
(100 MC) 10^{8}
(10 MC) 10^{7}
(1 MC) 10^{6}
STANDARD RADIO BROADCAST
(100 KC) 10^{5}
LONG WAVE RADIO
(10 KC) 10^{4}
FREQUENCY IN CYCLES PER SECOND
FREQUENCY IN CYCLES PER SECOND (MULTIPLY BY 10^{14})
WAVE LENGTH IN ANGSTROM UNITS
RADIATION PRODUCED BY LAMPS
15
14
13
12
11
10
9
8
7
6
5
4
3
2
1
GERMICIDAL
SKIN TANNING
BLUE PRINTING, FLUORESCENT PAINTS
VIOLET
BLUE
GREEN
YELLOW
ORANGE
RED
DRYING, HEAT THERAPY
2000
2537
3000
4000
5000
6000
7000

Figure 1. Chart of the electromagnetic spectrum showing the region produced by lamps

The color of the fluorescent light depends upon the type of phosphor and the methods by which it is prepared and is not, in general, influenced by the frequency of the exciting radiation. Table I lists some of the phosphors used in fluorescent lamps and gives the predominant color of the light which they emit. By properly mixing these different phosphors, one can obtain almost any desired color of light. The curves of Figure 3 show the distribution of the energy output with respect to frequency for daylight, blue, and pink fluorescent lamps. The continuous curves represent the energy radiated from the phosphor; the dotted rectangles represent the relatively small amount of visible energy produced by the mercury discharge.[4] It should be pointed out that the curves given in Figure 3 are plotted with respect to frequency, and they therefore differ in shape from curves plotted with a uniform wavelength scale. Ordinates have been selected so that the total area under the curve gives the total energy radiated by the phosphor.

The curves[4] of Figure 4 show the sensitivity of several typical phosphors used in fluorescent lamps, as a function of the frequency of the exciting radiation. It is seen that these have their maximum sensitivity in the region near $1{,}182 \times 10^{12}$ cycles per second (2,537 angstrom units), which is the radiation most efficiently produced by the low-pressure mercury discharge which makes up the gaseous conductor of the fluorescent lamp.

The Gaseous Conductor

A glass tube filled with neutral gas atoms, as, for example, an unexcited fluorescent lamp, is an almost perfect insulator. Even if electrons are freely introduced into the gas, only small currents can be made to flow with the usually available voltages, so long as the gas atoms remain electrically neutral. The reason for this is that the space charge produced by the electrons limits the flow of current. Only by breaking up the atoms of the gas into charged particles, or in other words, "ionizing" the gas, can reasonably large currents be made to flow. This process of ionization is essential to the operation of almost every type of gaseous conductor. In the case of a rectifier, thyratron, or similar device, the passage of current is the chief function of the discharge, and conditions are preferably arranged so that a minimum amount of energy is absorbed by the conductor. The primary purpose of the gas discharge lamp, on the other hand, is to produce radiation. As will be described later in the paper, radiation is obtained from "excited" atoms which have absorbed energy from the discharge. Hence, in the discharge lamp, conditions are preferably arranged so that as much energy as possible is absorbed by the conductor in the formation of excited atoms.

The gaseous conductor of the fluorescent lamp may be separated into three main regions, namely, the cathode region, the anode region, and the positive column. The most important of these is the positive column, which includes most of the length of the lamp, and in which most useful excitation (and hence radiation) is produced. The cathode and anode regions serve to connect the positive column to the external circuit. These are regions of low light-producing efficiency and, for this reason, conditions in general are chosen so that they consume as little as possible of the total lamp wattage. On alternating current the same electrode structure must serve alternately as anode and cathode, although the electronic phenomena are greatly different for the two functions. Before considering in detail these regions of the discharge, a brief description will be given of the fundamental processes of excitation and ionization.

EXCITATION AND IONIZATION

Although there are many ways in which atoms may be excited and ionized, in a gas discharge lamp, we are primarily interested in excitation and ionization by electron collision. For the purposes of our description an atom may be pictured as a positively charged nucleus surrounded by a number of electrons. The mass of the nucleus is very heavy compared to that of an electron. In the normal state of the atom, the number of electrons is just sufficient to neutralize the positive charge of the nucleus and in addition the internal energy of the atom is at its minimum value. If this normal atom is struck by an electron, the collision may be as between elastic spheres, and, due to its great mass, the atom will absorb very little energy from the electron. The small amount of energy which is absorbed in such a collision appears as a change in velocity of the atom (increase in thermal energy of the atom). If, however, the colliding electron possesses more than a certain minimum amount of energy, there exists a probability that the atom will make an inelastic collision and absorb a definite amount of energy from the electron. In this process one (in rare cases more than one) of the electrons of the atom is changed from its normal state to a state of higher potential energy, and the atom is said to be "excited." In the new state the electron is more loosely attached to the nucleus but is still a part of the original atom; hence, an excited atom is still electrically neutral. For each different kind of gas atom certain characteristic discrete states of potential energy are possible, and these are usually called energy levels. An atom does not remain in an excited state indefinitely, but in most cases very quickly returns to the normal state and releases the energy of excitation in the form of radiant energy. The frequency of the radiation emitted during the transition from one level to a lower level depends upon the energy difference between the levels, and is given by the equation:

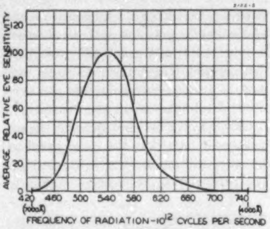


Figure 2. Average relative response of the light-adapted human eye as a function of frequency

$$f = 242.9 \times 10^{14} V \text{ cycles per second} \qquad (1)$$

where f is the frequency of the emitted radiation and V is the energy difference expressed in electron-volts (an electron-volt is the energy gained by an electron in traversing a potential difference of one volt). When several possible energy levels lie between a given excited state and the normal state, this transition may occur in several steps.

The lowest energy level (neglecting metastable states) from which a mercury atom can return to the normal state corresponds to an energy difference of 4.86 electron-volts. This transition, by equation 1, produces radiation at a frequency of $1{,}182 \times 10^{12}$ cycles per second, which is the radiation desired for exciting the phosphor in the fluorescent lamp.

Ionization is a process similar to excitation except that the energy absorbed by the atom in question is sufficient completely to remove one or more of its electrons. An atom which has lost an electron is positively charged and is known as a positive ion. Ionization may be produced in one step, if the colliding electron has energy greater than a certain value known as the minimum ionizing potential. This value for mercury is 10.38 electron volts. Other types of ionizing collisions exist, but there is evidence that they are of lesser importance under the conditions of the fluorescent lamp.

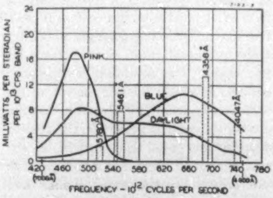


Figure 3. Spectral distribution of radiation from 15-watt fluorescent lamps of different colors

The manner in which these fundamental processes of excitation and ionization are utilized in a fluorescent lamp will now be considered.

The Cathode Region

The cathode in a fluorescent lamp is of the type commonly known as oxide-coated (or Wehnelt) and consists of a coil of tungsten wire activated by means of a coating of alkaline-earth oxides such as barium and strontium oxides. During starting, the cathode is preheated, and for this purpose two external leads are provided to permit passage of the heating current directly through the cathode coil. After starting, the discharge provides sufficient heating of the cathode, and external heating is not required.

In normal operation only a small portion of the cathode area is heated by the discharge to an electron emitting temperature, and the available zero-field emission current is considerably less than the total lamp current. Under these conditions a positive-ion space charge builds up at the cathode and produces an electric field at the cathode surface. The region of high electric field is called the cathode sheath. This field very greatly increases the electron emission in a manner which is characteristic of activated surfaces.[5] The generally accepted picture of the cathode phenomena under these conditions is as follows.[6]

Electrons emitted by the cathode are accelerated by the field which exists in the above-mentioned positive-ion cathode sheath. The voltage drop across this region is known as the cathode fall and has a value slightly larger than the minimum ionizing potential of the gas which surrounds the cathode. The electrons from the cathode thus acquire energies capable of ionizing and exciting the atoms with which they collide in the region beyond the positive ion sheath. The positive ions produced in this region diffuse in all directions, and those moving toward the cathode counteract the space charge of the electrons and create the field of the cathode as described above. Because the positive ions have masses many times greater than the electrons, they move much more slowly, and therefore relatively few are required to counteract the space charge of the electrons. In the case of mercury, the relative velocities of an electron and a positive ion, both having the same energies, are approximately 600 to 1.

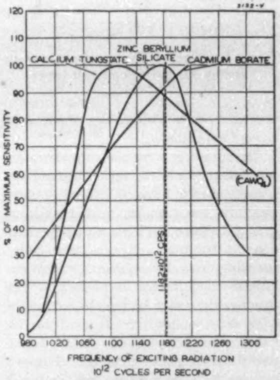


Figure 4. Relative sensitivity of typical phosphors used in fluorescent lamps, as a function of frequency of exciting radiation

An approximate calculation of the

zero-field emission current under actual lamp-operation conditions will serve to illustrate the order of magnitude of the quantities involved. For an eight-watt lamp cathode operating at 0.18 ampere, observed values of emitting area and temperature are approximately 0.01 square centimeters and 1,180 degrees Kelvin, respectively. Using the data given by Dushman,[7] the zero-field emission current is calculated to be 0.75 ampere per square centimeter, giving a total current of 0.007 ampere. Thus, for the particular conditions here described, the zero-field emission is only about four per cent of the total lamp current. This figure agrees in order of magnitude with the value of ten per cent given by Found[6] for a similar type of cathode. That the observed current is so much greater than this zero-field value is explained by the above-mentioned action of the electric field at the cathode. In addition a small fraction of the current is carried by the positive ions which move toward the cathode.

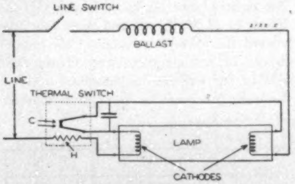


Figure 5. One of the common automatic starting circuits for fluorescent lamps

The electrons from the cathode, after producing ions in the region beyond the cathode sheath, have relatively low energies. By some mechanism not well understood, these electrons acquire a random distribution of velocities corresponding to a Maxwellian distribution. (When velocities are distributed in this manner, they may be represented by an equivalent temperature T_e, just as the velocities of the atoms of a gas are represented by the gas temperature. Thus we can speak of an electron temperature.) These low-energy electrons and the positive ions both diffuse from the cathode glow region and after a short distance enter a region where the voltage gradient begins to rise. This is where the positive column begins.

The Positive Column—Conduction Processes

The positive column is a region characterized by a uniform voltage gradient and a uniform degree of ionization throughout its length. Electrons entering from the cathode region gain energy from the gradient along the tube but nevertheless maintain a Maxwellian distribution of velocities, which corresponds to a temperature greater than that of the entering electrons, and which is many times greater than the temperature of the gas. These electrons drift toward the anode under the influence of the longitudinal gradient and, because they move faster than the positive ions, constitute all but a negligible fraction of the discharge current. This drift velocity of the electrons is small compared with their random velocities. In moving through the gas, these electrons collide with atoms of the gas, and some of these collisions result in the formation of positive ions and additional electrons. The electrons and positive ions have essentially equal concentrations in the positive column, and both diffuse toward the wall. As a result of their smaller mass, the electrons have higher velocities and tend to diffuse faster than the positive ions. This difference in rate of diffusion produces a radial electric field which accelerates the motion of positive ions and slows up the motion of electrons to the wall, until the two rates are just equal. This condition is necessary, because obviously the net current to the wall must be zero. It is apparent that the greater the electron velocities, the greater must be this radial field, and hence the greater must be the rate at which positive ions are lost to the wall. Tonks and Langmuir[8] have developed a relation called the plasma balance equation which expresses the rate at which ions are lost to the tube wall as a function of the electron temperature.

Not only is the rate at which ions are lost to the tube wall dependent on electron temperature, but also the rate at which they are generated is a function of the electron temperature. This follows from the fact that the probability that an electron will produce ionization by collision increases with the energy of the electron in excess of the minimum ionizing energy. (For very high energy electrons this probability decreases again.) This probability can be measured and is known for mercury vapor.[5] Thus it is possible to calculate the rate at which ions will be produced in a gas by electrons having any given electron temperature.[9]

In the steady state, for the type of discharge used in the fluorescent lamp, the rate at which ions are produced must equal the rate at which they are lost to the tube wall. Therefore the equations expressing the rate of ion generation and the rate of ion loss can be equated as shown by Found.[10]

$$(2V_T+V_i)e^{-V_i/V_T}=\frac{0.9}{B}\cdot\frac{s_0}{a}\cdot\frac{T_g}{P_g}\cdot\left(\frac{m_e}{m_p}\right)^{1/2} \qquad (2)$$

where V_T is the voltage equivalent of the electron temperature, V_i the minimum ionizing potential of the gas, s_0 is a factor dependent on the geometry of the container and the gas pressure, a is the tube radius, B is a constant relating to the probability of ionization which can be measured for any given kind of gas, T_g and P_g are respectively the temperature and pressure of the gas within the tube, and m_e and m_p are respectively the mass of an electron and the mass of a positive ion of the gas. From this equation it is possible to calculate the electron temperature for a tube containing a given gas at a known pressure by means of known constants. It can be seen that for any given gas the electron temperature (electron energy) decreases with an increase in tube radius and gas density and that the electron temperature remains constant as long as the product of gas density and tube radius remains constant.

This equation does not describe the experimental fact that the tube gradient decreases with an increase in arc current. This property of the positive column of the fluorescent lamp is accounted for partly by an increase in mercury vapor pressure with an increase in input watts. In addition, Tonks and Langmuir[8] propose the explanation that two-stage ionization processes would permit lower energy electrons to produce ions and would therefore require a lower electron temperature than is needed for single-stage ionization conditions.

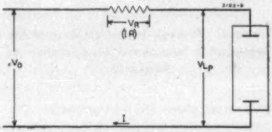
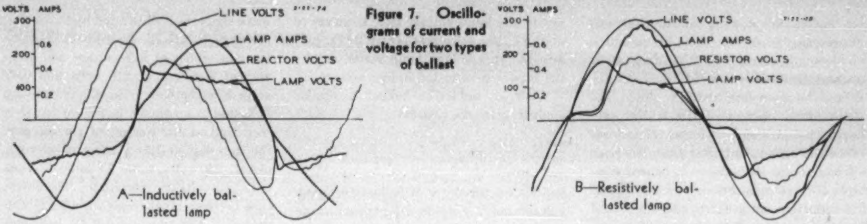


Figure 6. Simple resistively ballasted operating circuit

As was stated above, the positive ions are produced in the positive column only by the higher-velocity electrons in the Maxwellian distribution. This fact is of great importance in the production of radiation from a gas discharge lamp.

The Positive Column—Radiation Generation

In order to obtain a sufficient number of high-velocity electrons to satisfy the need for positive ions in the positive column, it is necessary (because of the Maxwellian distribution of velocities) to create a much larger number of lower-velocity



A—Inductively ballasted lamp

B—Resistively ballasted lamp

Figure 7. Oscillograms of current and voltage for two types of ballast

electrons. For example, if the average energy of the electrons is two electron volts, only 1.5 per cent of the total number have energies above the minimum ionizing potential of mercury, while 18 per cent have energies greater than the minimum excitation potential of mercury. Thus the process of radiation generation is only indirectly the result of the passage of current through the discharge tube. It follows that in order to increase the amount of radiation at any given frequency it is necessary to adjust the conduction processes so that the resulting electron energy approaches an optimum value for exciting the gas atoms to the desired energy level. In the practical lamp ideal conditions cannot be reached, because of considerations regarding total light output for a given size of lamp and other factors which cannot be considered here, relating to the lamp as a light source rather than as an electronic device.

THE ANODE REGION

The anode region serves to connect the positive end of the positive column to the external circuit. Conditions at the anode are similar to those at the tube wall, except that the electron current reaching the anode must exceed the positive ion current by an amount equal to the current through the tube. If the anode area is large enough, so that the rate at which the electrons strike it as a result of their random motions is equal to the discharge current, no anode drop is necessary. If the anode area is larger than this, it developes a negative voltage with respect to the gas and repels electrons, until the net current reaching it just equals the tube current. If the anode area is too small, it develops a positive voltage with respect to the gas and attracts electrons until the net current collected is equal to the drift current. In this case a sheath builds up around the anode, until the electron current entering the sheath is equal to the discharge current. The area of this sheath is then the effective anode area. By convention, a positive anode drop means an increase in tube volts. In the fluorescent lamp, the anode area is such that the average anode drop has a positive value of approximately seven volts. This value is primarily determined by the requirements of cathode heating.

ELECTRODE HEATING

No mention has been made of the manner in which cathode is heated, except to state that this is done by the discharge. This process will now be discussed more in detail. On alternating current, each electrode functions alternately as anode and as cathode. During both half cycles, heat is removed by conduction and radiation, and a small amount of heat is produced by the passage of current through the cathode coil. During the cathode half cycle an additional loss of heat occurs as a result of the evaporation of electrons from the cathode, and at the same time heat is gained from the positive ions which reach the cathode. On the anode half cycle heat is supplied by the electrons, which are accelerated through the voltage drop in the anode sheath and deliver this energy—in addition to their thermal energy and the heat of condensation—to the electrode. This is the most important source of electrode heating in a-c operation. The total net heating is the average of that produced on the two half cycles, and in the usual fluorescent lamp this is more than sufficient to raise the cathode to an operating temperature. In fact, it is usually necessary to attach projections to the electrode to reduce the amount of heat gained on the anode half cycle, in order to prevent evaporation of the cathode material.

Over-All Characteristics of the Fluorescent Lamp

Like most electronic devices the operation of the fluorescent lamp is greatly influenced by the characteristics of the circuit to which it is connected. Consequently, any description of lamp characteristics must be made with reference to a particular set of circuit conditions.

For reasons to be described in this section, the fluorescent lamp cannot be satisfactorily started and operated unless the line voltage is greater than the lamp voltage. Consequently a voltage-absorbing device must be connected in series with the lamp to absorb the difference between the two. This necessary difference between lamp voltage and line voltage is the determining factor in selecting the amount of ballast required. Commercially this ballast consists of a resistance for d-c operation, while on alternating current it may be a resistance, an inductance, or a combination of inductance and capacitance. The commonly made statement that a ballast is required because of the negative volt-ampere characteristic of the lamp is true, but there are other equally important reasons for its use. These reasons are, as will now be described, the need for lamp starting, the need for good regulation with changes in line voltage, and the need for good current wave shape on a-c circuits.

LAMP STARTING

In order to start a fluorescent lamp, it is necessary to ionize the gas between the electrodes and to heat the cathodes to an electron-emitting temperature. Perhaps the simplest method of accomplishing this is to apply a voltage several hundred volts in excess of the normal lamp-operating voltage across the lamp terminals. With this method of starting, however, the cathodes are required to emit electrons before the discharge has time to heat them to an emitting temperature, and the resulting high cathode drop may produce destructive sputtering of active material from the cathode surface. Also a disadvantage of this method of starting is the fact that the size and wattage loss of the ballast are greater than would otherwise be required. Conventional starting circuits avoid these difficulties by the use of a switch which connects the cathodes in series with the ballast, so that they are adequately preheated, and then opens the circuit to provide an inductive surge to start the lamp. The circuit of Figure 5 shows a thermal switch arrangement used for automatic starting of the 100-watt fluorescent lamp. Contacts are mounted on bimetallic elements, so that they can be opened by heat produced in heater *H*. When the main line switch is closed, the short-circuit current of the ballast preheats the cathodes. Heater *H* is connected in series, and after time has been allowed for the cathodes to be heated, contacts *C-C* are caused to open. The resulting inductive surge starts the lamp, and lamp current then flows through heater *H*, keeping the contacts open. This starting action may also be accomplished by means of a glow switch.[11] In the case of direct current, if the ballast consists of a resistor, no inductive surge is produced when the switch opens. This difficulty is overcome by using a series inductance which does not contribute to the ballasting, but which supplies the necessary inductive starting surge.

OPERATING STABILITY

On a perfectly regulated line, a fluorescent lamp would theoretically require a ballast just sufficient to offset the negative volt-ampere characteristic of the arc, and the lamp voltage could be made nearly equal to line voltage. In a practical circuit, however, the ballast must be greater than this value, in order to take care of normal variations in line voltage.

This fact can be demonstrated by the following approximate analysis of a resistively ballasted lamp. Over the normal range of operating currents, the lamp voltage changes by only a small amount and consequently can be treated as a constant. Referring to the circuit of Figure 6

$$V_o = V_{Lp} + IR \text{ and } IV_{Lp} = \text{lamp watts} = W_{Lp}$$

$$dV_o = RdI \text{ and } dW_{Lp} = V_{Lp}dI$$

Expressing the change in watts as a fraction of the total lamp watts

$$\frac{dW_{Lp}}{W_{Lp}} = \frac{V_{Lp}dI}{V_{Lp}I} = \frac{dI}{I} = \frac{dV_o}{IR}$$

$$= \frac{dV_o}{V_o} \cdot \frac{V_o}{IR} = \frac{dV_o}{V_o} \cdot \frac{V_o}{(V_o - V_{Lp})}$$

From the above equations we see that, if the lamp voltage were half the line voltage five per cent change in line voltage would produce a ten per cent change in lamp wattage, while if the lamp voltage were two-thirds the line voltage, a five per cent change in line voltage would produce a 15 per cent change in lamp wattage. How nearly the lamp voltage may be made to approach line voltage is limited by the permissible variation in lamp watts and by the expected variation in line voltage.

With an inductive ballast a similar analysis gives the relation

$$\frac{dW_{Lp}}{W_{Lp}} = \frac{dV_o}{V_o} \cdot \frac{V_o^2}{(V_o^2 - V_{Lp}^2)}$$

so that on this type of ballast the lamp voltage may be made to approach more nearly the line voltage for the same regulation than is the case with resistive ballast. In practice the wattage changes are greater than shown here, because the lamp voltage decreases with an increase in current. For most commercial ballasts the ratio of line volts to lamp volts is approximately 2 to 1.

Table I. Phosphors Commonly Used in Fluorescent Lamps[4]

Phosphor	Frequency, in 10^{12} Cycles Per Second of Maximum Radiation Output	Wavelength, in Angstrom Units, of Maximum Radiation Output	General Color
Calcium tungstate	680	4,400	Blue
Magnesium tungstate	625	4,800	Blue-white
Zinc silicate	570	5,250	Green
Zinc beryllium silicate	505	5,950	Yellow-white
Cadmium borate	484	6,150	Red

In addition to resistive and inductive ballast, a third type of ballast is used in commercial circuits. This consists of a combination of an inductance and a capacitance connected in series. This type of circuit has a more nearly constant current characteristic, and it is possible to operate the lamp more nearly equal to line voltage. Such circuits are usually used in conjunction with inductive ballasts, and the regulation of the inductive ballast controls the choice of line voltage as explained above.

WAVE SHAPE

On alternating current it is necessary also to consider the requirements of wave shape of current and voltage. This results from the fact that on alternating current the lamp must be reignited on each succeeding half cycle. The type of ballast has an important influence upon the wave shape, as is illustrated by the oscillograms of Figure 7. In both oscillograms the rms values of line voltage and lamp current were set at 210 volts and 0.42 ampere respectively. For the inductively ballasted lamp shown in Figure 7A, it is seen that at the instant of current zero the line voltage has a value of approximately 250 volts, while the lamp requires only 150 volts for reignition. The difference between these two is absorbed by the reactor. For the resistively ballasted lamp of Figure 7B the lamp current is inphase with the voltage, and consequently zero voltage is available for reigniting the arc at the instant of current zero. The current therefore remains at a low value for a period of approximately 20 degrees before reignition takes place. Due to this long wait, deionization of the positive column has begun to take place, so that 175 volts are required to reignite the arc.

A similar lag in reignition may occur on inductive ballast, if the line voltage is not sufficiently greater than the lamp voltage. The type of current wave shape shown in Figure 7B is not so desirable as that of Figure 7A, because of increased flicker, or stroboscopic effect, due to the extended period of zero current.

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Regulated Rectifiers in Telephone Offices

D. E. TRUCKSESS
MEMBER AIEE

FOR many years rectifiers of the garage type were used in converting alternating current to direct current for charging batteries used for communication purposes. These batteries furnish power for relay operation, for talking, and filament and plate supplies for repeaters. The rectifiers were of the manual-control type where the operator selected the charging current by means of tap switches or rheostats.

With the development of the thyratron type of tube, a rectifying means was made available in which the grid of the rectifier tube could be used to control its own output current by an electronic circuit. Rectifier circuits were designed to maintain a constant output voltage. If a regulated rectifier is connected to a battery and the constant rectifier voltage is 2.15 volts per cell, the load current will automatically come from the rectifier and not from the battery. Also the battery will draw from the rectifier sufficient additional current to maintain its charge. If the circuit voltage is held within limits of less than plus or minus one per cent, the maintenance of the battery is reduced and its life is extended.

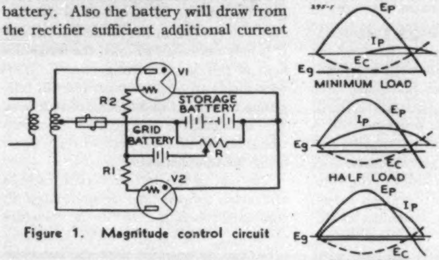

Figure 1. Magnitude control circuit

The thyratron tube differs from the vacuum tube in that the grid does not have a continuous control of the plate current.[1] When a positive potential is applied to the plate, current does not flow until the magnitude of the negative grid voltage is reduced to the critical value, at which time the plate current flows, and the magnitude of the plate current depends upon the voltages and impedances in the circuit. The grid has no further control, and plate current flows until it is stopped by reducing the plate voltage to zero.

Thyratron tubes use various gases and mixtures of gases. The earliest type used mercury vapor, but this type of tube is quite sensitive to temperature changes. The grid characteristics are shifted materially by changes in the room temperature in which it is operated, and in low temperatures it is almost a vacuum tube. Thyratron tubes using argon gas are not affected by temperature changes, but high-pressure argon tubes have a low inverse voltage which limits their application to low-voltage rectifiers. Tubes using low-pressure argon have a higher inverse voltage, but are accompanied by a high arc drop which makes their efficiency low. A mixture[2] of mercury vapor and argon has been found which provides the temperature-stable grid characteristic of the argon tube and the low arc drop of the mercury-vapor tube. This type of tube has been very successful with certain regulating circuits, particularly at voltages less than 60 volts.

Five kinds of regulating circuits are used in telephone offices to hold the output voltage of rectifiers constant. The selection of the circuit to be used depends upon the magnitude of the current, d-c voltage, and type of rectifying means to be used. Two forms of regulating circuits using thyratron tubes and one using two-element high-pressure tubes were developed. A fourth circuit using all vacuum tubes was adapted for telephone use. The fifth kind uses a negative resistance.

Magnitude Control

The automatic regulating circuit using the temperature-stable tubes has been called the magnitude method[3] of control, since the voltage applied to the grid is a d-c voltage, and its magnitude is varied to control the plate current of the tube. Figure 1 shows a schematic circuit and diagrams of the voltages applied to the rectifier tube. The rectifier circuit is the conventional full-wave circuit with a filter coil in the negative output lead. A negative voltage, with respect to the tube cathodes, is taken off a potentiometer connected across the battery to be floated. This negative voltage is opposed by a slightly smaller positive voltage of a grid battery. The net negative voltage of a few volts is applied to the grids of the tubes through grid resistances. The top diagram in Figure 1 shows the positive half of the a-c voltage applied to the tube. The dotted line represents the critical grid voltage of the tube, *Ip* the plate current that flows, and *Eg* the d-c grid voltage applied to the grids. If the applied grid voltage is more negative than the critical value, the tube does not fire and no plate current flows. If the magnitude of the grid voltage is decreased until it reaches the critical value, the tube fires and the rectifier delivers output. A further decrease in the grid voltage will result in the critical value being equaled earlier in the cycle, and the average value of the plate current will increase. Finally, with the grid voltage zero, the tube fires at the beginning of the cycle and the maximum average value of current is obtained.

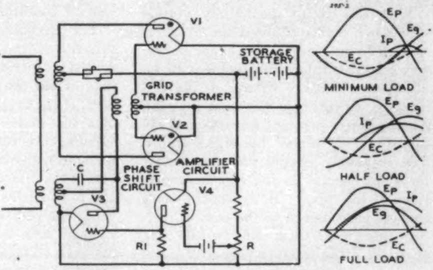

Figure 2. Phase-shift control circuit

If this circuit is operating without a filter coil, the current could be reduced by the variable grid voltage to only one half its maximum value, and then it would drop to zero due to its having control only during the first 90 electrical degrees. A smooth control from full load to light load can be secured by using a filter coil

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D. E. TRUCKSESS is on the technical staff, power apparatus development department, Bell Telephone Laboratories, New York, N. Y.

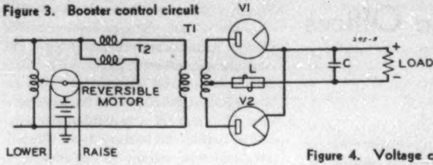


Figure 3. Booster control circuit

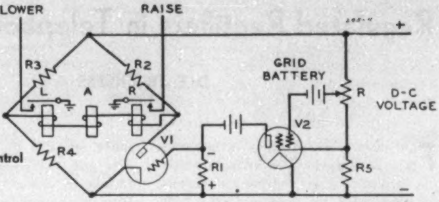


Figure 4. Voltage control for booster circuit

that has a high inductance with small currents, so that even though the tube fires near 90 degrees the current that flows is limited by the reactance of the coil. At full load its inductance is reduced by saturation of the iron to the value required for filtering. With conventional coil design where the no-load inductance can be made approximately five times the full-load inductance, the rectifier output current can be reduced by grid control to five per cent of the full-load value. When a telephone office load is applied to the battery, its external voltage decreases a fraction of a volt. This change is reflected over the potentiometer and grid battery to the grids to reduce the grid bias and increase the plate current of the tubes sufficiently to carry the office load.

With this circuit the battery voltage is held to a change of about one volt from no load to full load. This change in battery voltage is approximately the same for all values of battery voltage, as the grid must be varied about the same amount. Thus a better per cent regulation is obtained at the higher battery voltages. The regulating circuit will compensate for slight changes in the grid characteristics of the tubes. However, the large changes encountered in the case of mercury tubes make them unfit for this type of circuit. The mixture of mercury and argon has been found quite satisfactory when used in rectifiers covering a range of 24 to 60 volts. These tubes, however, have a low inverse-voltage rating and cannot be used in charging the higher-voltage storage batteries. The mercury tube has the necessary high inverse-voltage rating, so that a circuit was developed which has been called the phase-shift type of control; this enables the mercury tubes to be used, in spite of their changes of grid characteristic with temperature.

Phase-Shift Control

Figure 2 shows a schematic diagram of the phase-shift type[4] of control circuit. This circuit differs primarily from the magnitude circuit in that the grid voltage is an a-c voltage of a fixed magnitude, but its phase relation with respect to the a-c plate voltage is varied to control the output of the rectifier. When the grid voltage is out of phase with the plate voltage, the grid is negative all during the cycle that the plate is positive, so that the tube does not fire. As the grid voltage is shifted in a direction to be inphase with the plate voltage, the instantaneous grid voltage reaches a magnitude which is less negative than the critical value so that the tube will fire and deliver current for the remainder of that cycle. When the grid voltage is shifted to be practically inphase with the plate voltage, the tube fires at the beginning of the cycle and carries current during the remainder of that cycle. In this way the output current of the rectifier is controlled by shifting the phase from the out-of-phase to the inphase condition.

The grid voltage of varying phase is obtained from the phase-shift circuit which consists of a bridge circuit with two windings of the transformer furnishing two arms of the bridge, a capacitor the third, and the plate-cathode resistance of a vacuum tube the fourth. A transformer is connected across the galvanometer corners of the bridge which supplies the voltage to the grids of the thyratron tubes. When the plate-cathode resistance of the vacuum tube is at its minimum value, the grid voltage is nearly inphase with the plate voltage, the output current to the rectifier being maximum. When the vacuum tube is biased out (providing an infinite resistance) the grid voltage is practically out of phase with the plate voltage.

The plate-cathode resistance of the vacuum tube is controlled by the d-c bias applied to the tube. This varying grid bias of 2 to 20 volts is obtained from a vacuum-tube d-c amplifier circuit. When a telephone-office load is applied to the storage battery it decreases the battery voltage a fraction of a volt. This small voltage is amplified by the d-c vacuum-tube amplifier and applied to the phase-shift vacuum tube in a direction to shift the grid voltage more nearly into phase and thus increase the output of the rectifier. In this way the storage-battery voltage is held to the regulated value. The accuracy of regulation depends on the amount of gain in the d-c amplifier circuit. This circuit has been built to provide regulation in the order of a few hundredths of a volt, but in most applications one per cent regulation is all that is required. The filter coil is not essential for the operation of this circuit, but a swinging choke whose inductance changes four to one will assist in regulation.

In a thyratron with an oxide-coated filament, operating filament temperature must be reached before output current can be drawn from the rectifier. An inexpensive way to prevent output current is to apply a large negative grid bias to the tubes, which biases them out even though the a-c voltage is applied to the plates. With the magnitude method, the grid circuit can be switched to the battery voltage and in the phase-shift circuit it is only necessary to open the resistance arm of the phase-shifting circuit. This switching can be done with a small telephone-type relay on all sizes of rectifiers. The time delay is provided by a conventional bimetallic-strip type of relay operating the telephone relay.

The cost of the thyratron tube, in sizes above ten amperes per tube, is large in comparison with the cost of the complete rectifier. High-gas-pressure rectifying tubes can be operated with the tube elements at high temperatures if the grid is omitted. This permits a two-element tube to be built at about one fifth the cost of the thyratron, thus making it practical to build low-voltage rectifiers in sizes of 30 to 100 amperes.

Booster Control

In order to regulate the output of a rectifier using two-element tubes, a circuit has been developed which has been called the booster type of control. Figure 3 shows a schematic diagram of this type of circuit. The output current of the tubes is controlled by changing the plate voltage applied to the tubes. The plate voltage is varied by means of a circuit consisting of a continuously tapped autotransformer which changes the voltage applied to the primary of a booster transformer. When the continuously tapped

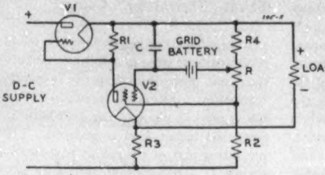
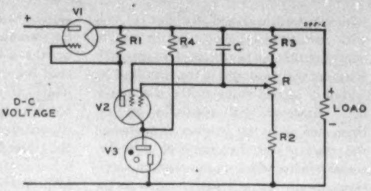


Figure 5 (left). Series-tube control with grid-battery voltage standard

Figure 6 (right). Series-tube control with cold-cathode tube as voltage standard



autotransformer is at its zero end, no voltage is applied to the booster transformer and the ratio of the plate transformer is selected so that the rectifier delivers minimum current. The output current of the rectifier is increased by driving the continuously tapped autotransformer to its maximum boost position which raises the plate voltage until the rectifier delivers its maximum output current. The continuously tapped autotransformer is controlled by means of a small reversible motor so that closing the circuit of the "raise" lead will increase the rectifier output current and closing the circuit of the "lower" lead will drive the motor in the direction to decrease the rectifier output current. The disadvantage of this circuit is that a second transformer 40 per cent as big as the plate transformer is required in addition to the autotransformer. The advantage is that all of the equipment in this circuit operates at a high efficiency, and with the boost type of connection the no-load loss is small. Even the core loss of T_1 is reduced, since it operates at a lowered voltage at light loads. Light-load efficiency is important in telephone applications, as the rectifier must operate many hours at night with practically no load. The input power factor is the same as that of the unregulated tube-rectifier circuit and is not reduced materially by the control circuit.

Various combinations of this rectifier circuit are possible in that the plate voltage, in small sizes of rectifiers, may be connected directly to the continuously tapped autotransformer or the auxiliary transformer may be connected to buck the line voltage. This circuit can be applied to more than one phase with the appropriate tapped autotransformers, booster transformers, and plate transformer to control each phase.

A voltage controller to operate with the booster type of regulating circuit is shown in Figure 4. This circuit is to translate variations in the magnitude of the d-c voltage to be regulated into a raise and lower signal to the reversible motor in the rectifier. The circuit consists of a d-c amplifier to increase the small changes in the load voltage from a fraction of a volt to several volts. This amplified voltage is applied to the grid of a vacuum tube which constitutes one arm of a resistance bridge circuit. Across the galvanometer corners of the bridge are connected two polarized relays in series with a nonpolarized relay.

When the load voltage is at the correct value, the bridge is balanced and no voltage appears across the galvanometer corners of the bridge, so that all relays are released. The change in the load voltage will be reflected through the amplifier and bridge circuit in a direction to unbalance the bridge and cause either the *R* or *L* relay to operate, depending on which way the voltage changed, which in turn operates the motor in the rectifier circuit. Plate supply for the bridge tube V_1 is obtained from the d-c voltage being regulated, but a separate source of direct current is required for the plate of the amplifier tube V_2. This may be a battery or a small half-wave disk rectifier with a filter capacitor to provide 50 volts, one milliampere for the plate of V_2. The standard voltage can be either a grid battery or the negative resistance regulating circuit described below. The nonpolarized relay *A* operates on twice the current of the *R* and *L* relays, and is connected in series with them to give an indication when the regulated voltage is approximately twice the variation that causes the *R* and *L* relays to operate. In this way, an alarm may be provided to indicate when the regulated voltage goes out of the regulating range.

The accuracy of this control is entirely a function of the amount of d-c amplification provided by tube V_2. A single-tube amplifier operating with a load voltage of 24 or 48 volts will provide regulation in the order of 0.1 to 0.2 volt. An ordinary voltage relay may be used instead of this circuit, but its accuracy is limited by the difficulty of adjusting the span between the high and low contact of the relay to less than one volt. An additional advantage of this circuit over the voltage relay is that the span between the "lower" and "raise" signal is a value fixed by the design of the circuit, but the average value of the regulated voltage may be shifted simply by adjusting the rheostat *R*.

Series Tube Control

Where a small amount of closely regulated power is required, the series vacuum-tube type of regulated rectifier is used.[5] The conventional full-wave vacuum-tube rectifier and filter is used to provide about 100 volts more than the regulated value. A vacuum tube is connected in series with the output and its plate-cathode resistance acts as a dropping resistance. The amount of resistance it introduces into the circuit depends upon the magnitude of the grid voltage. To reduce the rectifier d-c voltage automatically to the regulated value the circuit shown in Figure 5 is used. The output voltage is applied by means of the voltage drop over the potentiometer *R*, which is opposed to a constant-voltage source such as a grid battery to the grid of V_2. If the grid-battery voltage is of the same magnitude as the load voltage, the maximum accuracy of regulation is obtained. However, with a screen grid or pentode tube sufficient amplification is obtained with lower-voltage grid batteries. By adding the R_3 resistance which is in series with the load current, its voltage drop is applied to the grid of V_2 to compensate for load changes.

With a circuit of this type, plus or minus ten per cent variations of d-c supply can be reduced to the order of 0.05 volt

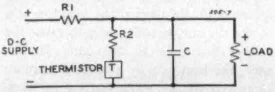


Figure 7. Shunt thermistor circuit

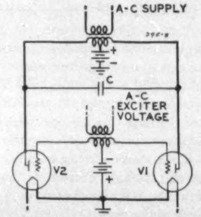


Figure 8. Fundamental inverter circuit

with constant load and to about 0.1 volt with variations in load. Better regulation than this can be obtained by carefully selecting the constants of the circuit or by adding a second stage to the d-c amplifier. However, with regulation of less than 0.05 volt, the contact potential in the grids of the d-c amplifier tube becomes a factor which causes random variations due to temperature changes in the cathode, produced mainly by variations in the heater voltage.

The circuit shown in Figure 6 is a variation of that shown in Figure 5 in that the voltage drop over a cold-cathode gas-filled tube is used as the standard for voltage regulation where accuracy of plus or minus one per cent to plus or minus three per cent is permissible. The arc drop of the tube V_3 is connected in the cathode circuit of the d-c amplifier circuit, and its grid voltage is picked off the potentiometer circuit, consisting of resistances R_2, R, and R_3, at a point which makes the grid of V_2 slightly negative with respect to its cathode. A variation in the load voltage in this way varies the grid of the amplifier tube V_2 whose amplified output appears as a voltage drop across R_1, to control the grid of the series tube V_1. It is desirable to add the R_4 resistance so that the V_3 tube can be operated at its most efficient current value, and the value of the R_1 resistance should be quite large so that the cathode current is small in proportion to the tube current in V_3.

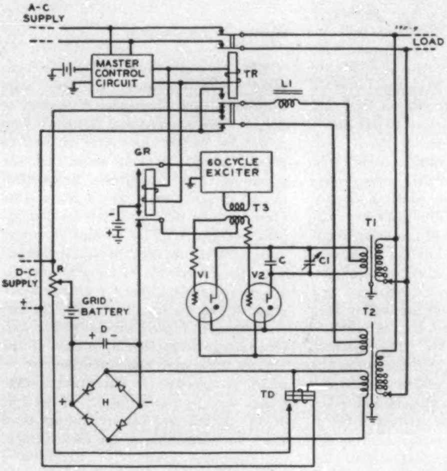


Figure 9. Combined rectifier–inverter circuit

In both of the circuits shown in Figures 5 and 6, feed-back filtering is obtained because the regulating circuit compensates for variations in voltage due to the ripple on the d-c supply in the same manner that it compensates for variations due to load. If the grid is not connected directly to the positive side of the load, the capacitor C should be added to conduct the ripple directly to the grid. Otherwise, a loss in ripple voltage would be obtained in proportion to the potentiometer ratio.

In this type of circuit the amplifier tube V_2 is usually of the heater type so that during starting, or in the event of a failure of the V_2 tube, the output voltage is unregulated and reaches a value approaching that of the d-c supply, except for the zero grid drop of the series tube V_1. This may be reduced by connecting a cold-cathode tube from the plate of V_2 to the potentiometer circuit at a voltage point with respect to the positive output terminal, which is slightly less than the breakdown voltage of the tube. Then with the circuit in normal condition, the tube will not pick up and will be an open circuit. If, however, the output voltage increases above the regulated value, the tube picks up and draws a current through the R_1 resistor which biases the series tube negative, reducing the output voltage. As soon as the V_2 tube is operating normally, the voltage of its plate terminal is drawn toward the negative end of the d-c output sufficiently to cause the cold-cathode tube to drop out. In this way, the load may be protected from an excessive voltage while starting or when trouble occurs in the amplifier tube.

Shunt-"Thermistor" Control

In regulated rectifiers where the use of a cold-cathode tube as a voltage standard does not provide sufficiently accurate regulation, a miniature regulated rectifier is used in place of the grid battery, where its higher cost is justified. The regulating part of the circuit is shown in Figure 7. The negative resistance is a "thermistor" made of a semiconductor. The value of R_2 is selected so that a change in d-c supply voltage causes a positive voltage drop in R_2 equal to the negative voltage drop in the thermistor with the net result that the voltage across the load is approximately constant. A resistance R_1 must be connected in series with the d-c supply to take up the variations in d-c supply voltage. This circuit, in general, is adaptable to voltages less than 100 volts and currents of a few milliamperes. Since most thermistors are sluggish in operation, a large capacitor C is connected across the circuit to smooth out rapid variations in d-c supply. The circuit is compensated for ambient temperature variations, as the resistance of thermistors is affected by temperature. This circuit has been used as a grid supply for amplifiers.

Rectifier–Inverter

With the increase in use of a-c voltages for filament and plate supply for repeaters, it became necessary to provide some means of providing power to operate these circuits when the commercial power supply had failed.[6] The electronic inverter using thyratron tubes provides a means to take energy from a storage battery and convert it into 60-cycle alternating current to keep the circuits functioning until the commercial power is restored. The elements of an electronic inverter are essentially the same as a rectifier, so that with the addition of a pilot source of 60 cycles and suitable switching means, one unit can serve as a regulated rectifier to float the storage battery normally, and during the time that the power has failed the circuit can take power from the battery and deliver 60-cycle power for use in the telephone office.

Figure 8 shows a schematic diagram of the fundamental inverter circuit. The battery is applied to the center tap of the plate transformer, and the grids of the tubes are held negative with a d-c bias sufficient to prevent their carrying current with the positive voltage applied to the plates by the battery. A 60-cycle excitation voltage is applied to the grid through the grid transformer. This excitation voltage has a greater magnitude than the negative d-c bias. When the excitation voltage drives the tube grid toward zero until it reaches a value less than the critical grid voltage, the tube fires, passing current through one half of the plate transformer. When the other tube grid is fired in the same way it passes current in the opposite direction through the other half of the plate transformer. The charge on the capacitor which has accumulated while one tube is firing is connected across the opposite tube by virtue of the firing, which drives the plate negative to interrupt the plate current. The a-c excitation voltage is supplied by a mechanical vibrator of the type that is used in automobile radios or by a vacuum-tube oscillator.

A particularly useful type of power supply has been built which functions normally as a regulated rectifier, taking power from the a-c commercial supply and floating the telephone-office battery. The instant that the power fails, relays convert the circuit from rectifier to inverter operation, and energy is taken out of the storage battery and supplied to the telephone circuits in the form of 60-cycle alternating current.

The control circuit for the combined rectifier and inverter is shown in Figure 9. A master control circuit is provided to measure the a-c supply voltage continually so that when its magnitude reduces to less than 85 per cent of its rated value the *GR* and *TR* relays are operated. The *TR* relay disconnects the rectifier and load from the a-c supply circuit and also reverses the polarity of the d-c supply to the plate transformer. The *GR* relay starts the 60-cycle exciter and switches the grid circuit from the regulated rectifier circuit to the fixed negative bias. The circuit then draws power from the d-c supply and generates a 60-cycle voltage which supplies the load and also supplies its own filament voltage through transformer T_2. When the commercial a-c voltage is restored, the master control circuit releases the *GR* and *TR* relays to restore the equipment to the rectifier operation.

The control of the rectifier output current is by the magnitude method, which has been described. The regulating circuit consists of the potentiometer *R*, grid battery, capacitor *D* and "varistor" *H*.

Table I. Regulated Rectifiers Used in Telephone Offices

Rectifier Output		Type of Regulation	Rectifying Means
Volts	Amperes		
48	0.001	Shunt thermistor	Disk type
20	0.006	Shunt thermistor	Disk type
85	0.23	Series vacuum tube	Vacuum tube
120–130	0.10	Series vacuum tube	Vacuum tube
95–180	0.10	Series vacuum tube	Vacuum tube
150–180	0.15	Series vacuum tube	Vacuum tube
17–45	0.60	Magnitude	Thyratron
48–74	0.6	Magnitude	Thyratron
120–130	0.6	Magnitude	Thyratron
34–48	3	Magnitude	Thryatron
24–34	10	Magnitude	Thyratron
24	30	Booster	2-element tube
48	10	Magnitude	Thryatron
48	30	Booster	2-element tube
130	2	Phase shift	Thyratron
130	8	Phase shift	Thyratron
130	3	Magnitude	Thyratron
130	10	Phase shift	Thyratron

Application of load to the battery reduces its voltage, and this change in voltage is reflected to the grids of the thyratron tubes to reduce their negative bias, which increases the charging current of the rectifier to bring the voltage back to the regulated value. The varistor *H* and capacitor *D* provide a voltage which varies directly in proportion to the a-c line voltage. This change is introduced into the grid circuit to compensate for line voltage changes.

In order to use the same ratio in the plate transformer for both rectifier and inverter operation, it was necessary to have different methods of adjusting the output voltage of the rectifier and the inverter. The output voltage of the rectifier is adjusted by means of taps on the primary of the plate transformer T_1 for charging different numbers of cells in the storage battery. This tap is selected at the time of installation. The output voltage of the inverter is adjusted for various a-c loads by means of the commutating capacitor C_1. The above rectifier then can be adjusted for charging a 132- or 142-volt battery and to provide any a-c output voltage between 210 and 250 volts with current loads of 2 to 5 amperes.

The time required to convert this circuit from a rectifier to an inverter is limited primarily by the operating time of the relays. The master control circuit requires about one cycle to detect a low voltage, and the power relays require about two cycles to make the transfer. Therefore the minimum practical time of transfer is such that approximately three cycles is lost between the time the commercial power fails and alternating current is available from the inverter to carry the load. The transfer back to the commercial power supply will be less because the master control circuit has time to function while the load is carried by the inverter, and the release time of the relays is smaller than the operate time. The practical loss in load voltage is approximately two cycles.

Rectifier Sizes

Table I shows the voltage and current output, type of control of the rectifiers, and the rectifying means that have found widespread use in the Bell System.

The regulated rectifier finds its applications in telephone offices where constant voltage, independent of load and a-c line-voltage variations, is required to supply filament grid bias and plate voltage to telephone repeaters. Certain measuring circuits require a regulated rectifier to supply a stabilized voltage. Regulated rectifiers also find applications where constant voltage is of secondary importance but an automatic power plant is desired for maintaining storage batteries in a fully charged condition to be ready to supply the power for telephone offices if the a-c power fails. A further compensation of regulating the voltage is the increase in life obtained from storage batteries if they are not continually being charged and discharged but are fully floated.

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The Dielectric Strength and Life of Impregnated-Paper Insulation — III

J. B. WHITEHEAD
FELLOW AIEE

IMPREGNATED-paper insulation, as used in high-voltage power cables, is subject, during the ordinary processes of manufacture, transportation, installation, and operation, to a wide variety of conditions, nearly all of which militate against the conservation of the original inherent properties of the insulation. Among these conditions are bending and other mechanical stresses, expansion and contraction due to temperature changes, original variations in manufacturing processes of drying and evacuation, and so forth. As a consequence, the electrical characteristics of the insulation of high-voltage cables, as for example power factor, dielectric loss, dielectric strength, and stability, all fall noticeably below those of samples prepared in the laboratory and generally are not subject to all the disturbing conditions mentioned above.

The uncertainties arising in the presence of these disturbing conditions and the inability to determine their relative importance has meant that most of the improvement in the quality of cables in recent years has resulted from empirical development and experiments on the finished cable. Cable engineers have been slow to adopt the results of laboratory research on controlled specimens, except perhaps in the clear-cut results of studies of such fundamental matters as drying and evacuation as related to the impregnation process, the purification of oil and paper, and the like. They have feared the effect of a change in a known variable on those which are still unknown.

The experimental studies described in this series of papers fall definitely in the controlled laboratory class. Effort has been made to eliminate the influence of all those disturbing elements in cable insulation except a particular variable under study. For example, the specimens tested are assembled and impregnated, cable fashion on rigid smooth conductors; they suffer no mechanical deformation, they are tested at one temperature, and in particular, they contain no free gas. On the other hand, the type of specimen and mode of testing permit the study of many variables in paper, oil, and mode of assembly of the usual type of cable insulation.

It will be evident that great care and perhaps reserve should be exercised in applying the results of these studies to the design and construction of finished cables. From what has been said, for example, there is an important condition obtaining in the usual solid type of cable, namely, free gas and gaseous ionization, which has been carefully eliminated in the present tests. It is most important, therefore, in such cables to consider the possible influence of the variations shown in these experiments in a number of other variables, on the volume and extent of gaseous ionization. On the other hand, it appears that many of the results of the present studies should be immediately applicable to the so-called oil-filled type of cable in that both in finished cable and laboratory specimens the conditions are closely the same. Even in this case, however, it is necessary to bear in mind the difference between a relatively short-life test in the laboratory and the long-time performance of finished cable.

Cable engineers are nevertheless deeply interested in these research programs and the results therefrom. They have shown this interest by suggestion, discussion, and other cordial support and co-operation. The researches have been supported by grants from The Engineering Foundation with the sponsorship of the American Institute of Electrical Engineers and have enjoyed the hearty co-operation of an advisory committee of manufacturers' and utilities' engineers as appointed by the latter body.

Scope of the Work

The first paper of this series is devoted to a study of the influence of the density of the paper on dielectric strength and life, the outstanding result being a noticeable decrease of dielectric strength with increasing value of paper density. The second paper reports studies of the influence of the thickness of paper on dielectric strength. The earlier studies on density occasioned some surprise and were contrary to the expectations of a number of cable engineers. Consequently the present paper describes primarily further experiments on the influence of the density of the paper, particularly in the use of a different oil, and also studies on the influence of oil viscosity, all directed to a possible explanation of the unexpected results on paper density. In addition, results of a study of the influence of the width of oil channels between convolutions of paper tape on dielectric strength are described. In all these experiments a very sensitive and rapid method of circuit interruption has been used, resulting in the great majority of cases in partial failures without burning. Followed by careful examination, dissection, oil extraction, and magenta staining, this has resulted in a very intimate picture as to the probable causes of failure in each case.

The Test Specimens, the Paper, and the Oils

THE TEST SPECIMENS

The specimens were wrapped on brass tubes 48 inches long and one inch in diameter. The central or measuring electrode was of lead foil 16.5 inches long. At each end it was overlapped by a lead foil guard electrode 9.5 inches long, separated therefrom by one layer of paper. Beyond the guards the specimen was equipped with narrow angle conical ends of impregnated paper designed for uniform stress distribution.

THE PAPER

The cellulose wood-pulp paper was supplied by a well-known manufacturer in four values of specific gravity 0.703, 0.896, 1.136, 1.144, all of approximately 0.005 inch in thickness, and here designated as papers *A*, *B*, *C*, and *D* in the order of increasing density. The paper tapes were in all cases one inch wide.

Certain observations were also made on a fifth grade of paper (*E*), of 0.004 inch in thickness remaining from foregoing studies and manufactured five years earlier.

The test specimens were made by wrapping the paper tapes on one-inch-diameter smooth brass tubes, normally with $33^{1}/_{3}$ per cent and $66^{2}/_{3}$ per cent

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J. B. WHITEHEAD is director of the school of engineering, The Johns Hopkins University, Baltimore, Md.

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overlay with a butt spacing of tapes of between $^1/_{64}$ inch and $^3/_{64}$ inch. The normal thickness of the insulation wall was 0.080 inch, that is, 16 layers of 0.005 inch paper. Studies of different channel widths up to $^1/_8$ inch were made with 0.005 inch *B* paper.

Nine identical specimens were constructed for each value of density, thickness of paper, for each value of channel width, or other variable studied. In a few cases the number of identical specimens tested was reduced to six.

For details as to the construction of the specimen, methods of test, experimental data, accuracy, and so forth, the earlier papers may be consulted.[1]

THE OILS

Two high-grade insulating oils having commercial numbers 5,314 and 5,317 were each tested over the entire range of paper variables as described above. Number 5,314 is a thin oil as used in oil-filled cables. Its values of viscosity at 40 degrees, 60 degrees, and 100 degrees centigrade are 17, 8.2 and 3 centipoises respectively. Oil 5,317 is a heavier oil somewhat darker in color and prepared for use in solid-type cables. Its values of viscosity at 40 degrees, 60 degrees, and 100 degrees are 480, 120, 18.3 centipoises respectively.

The specimens were impregnated in groups of three, normally at 60 degrees centigrade. In certain studies of the influence of viscosity on impregnation, the impregnating temperature of 5,314 was reduced to 40 degrees and that of 5,317 raised to 100 degrees, these temperatures giving the same value of viscosity for the two oils.

Breakdown and Stability Tests

The accelerated step-up life tests started at 400 volts per mil. The voltage was increased by 3.12 per cent every four hours, thus giving a geometric increase to 700 volts per mil in three days. The continuous application of voltage was interrupted at intervals (usually every four hours) for tests of power factor, capacitance, and internal gaseous ionization.[2] In this program the average life of a specimen was from two to five days. It has been assumed that this brief duration eliminates the influence of chemical change. Long experience has shown that there is no free gas in these specimens. Consequently the results appear to isolate definitely the influence of each of the several variables studied, that is, paper density, paper thickness, oil viscosity, and so forth. The design of the specimen and close attention to uniformity of construction have ensured that the failures have occurred almost invariably under the central electrode of the specimen.

Results of Tests

DENSITY OF PAPER

The results of the tests on four values of paper density using 5,314 oil have been published in an earlier paper.[3] For convenience some of them are reproduced in Figures 1 and 2. In the same figures will be found the results of later studies on the same papers as impregnated with 5,317 oil, and under the same conditions as to impregnation, test program, degree of accuracy, and so forth.

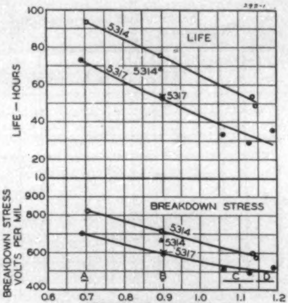

Figure 1. Stability of oil-impregnated-paper insulation—life and breakdown stress

Impregnation at a pressure of 2.0 millimeters of mercury

Δ—40 degrees centigrade
x—100 degrees centigrade
All others—60 degrees centigrade

Two noteworthy features are indicated:

1. The decrease of dielectric strength and life with increasing paper density, already reported for 5,314 oil, is also found to obtain with close parallelism for 5,317 oil.
2. Values of dielectric strength and of life using 5,317 are uniformly lower than those for 5,314.

The impregnating oil content is somewhat higher for 5,314, particularly at the low values of paper density. The power factor for 5,314 is also higher. The trend of these curves has been discussed in the earlier papers.

PAPER THICKNESS

The studies of the influence of paper thickness with 5,314 oil only have been reported in an earlier paper.[4] Figure 3 is reproduced therefrom. It will be noted that there is a pronounced and uniform decrease of breakdown strength with increasing thickness of paper ($21^1/_2$ per cent decrease from 0.003 inch to 0.008 inch thickness).

SATURATION STUDIES

For each group of tests the percentage of oil content by volume was determined for one or more specimens. Thus in Figure 2 the decrease of percentage of oil with increasing paper density is readily noted It is also noticed that specimens impregnated with 5,314 oil contain a higher percentage of oil, other things being equal, and that this is accompanied by higher dielectric strength. The same parallelism has been observed in other tests. In order to determine whether viscosity of the oil had any bearing on these differences, six specimens of *B* paper were impregnated with 5,314 oil at 40 degrees, and six others with 5,317 oil at 100 degrees, the viscosities of the two oils having the same value at these temperatures. All twelve specimens were subjected to life test at 40 degrees centigrade, in accordance with the standard program.

Results of these comparative studies are indicated as single points giving the average values in Figures 1 and 2. The order of accuracy is about the same as that in the paper-density studies (plus or minus deviation five to ten per cent). It will be noticed that the change of viscosity of 5,317 oil results in no appreciable change in either dielectric strength or life, notwithstanding a rather wide variation

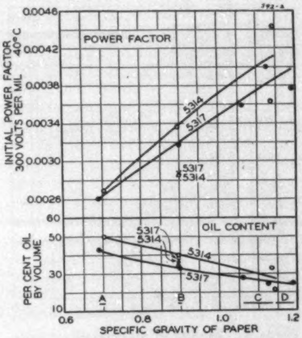

Figure 2. Stability of oil-impregnated-paper insulation—power factor and oil content

Impregnation at a pressure of 2.0 millimeters of mercury

Δ—40 degrees centigrade
x—100 degrees centigrade
All others—60 degrees centigrade

in the value of viscosity (480–120 centipoises). On the other hand, impregnation with 5,314 oil with a considerably smaller variation of viscosity results in a slight lowering of both dielectric strength and life, although the values are still substantially higher than those for 5,317.

As to oil content, in the case of number 5,317, large decrease in viscosity between 60 degrees and 100 degrees results in the small increase of oil content of from 33 per cent to 37 per cent. In the case of 5,314 oil the increase of viscosity between 60 degrees and 40 degrees in these comparative studies results in a reduction from 40 per cent to 34 per cent. When the viscosities are the same, the oil content and power factor are about the same.

WIDTH OF CHANNEL

The influence of the width of channel on breakdown strength and accelerated life was studied between the limits of a close butt joint and a maximum channel width of $^1/_8$ inch, observations being taken at one intermediate thickness, $^3/_{64}$ inch. Observations were also made on six specimens having a $^1/_{64}$-inch overlap, for one of the oils. Otherwise nine specimens were tested at each channel width and for each of the two oils, 5,314 and 5,317.

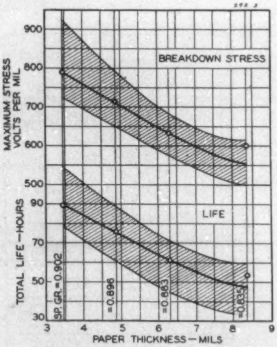

Figure 3. Stability of oil-impregnated-paper insulation—breakdown stress and life

The results of these studies are plotted in Figure 4, in which the average spread of the nine specimens represented by each point is indicated in vertical lines.

It will be noted that the decrease of dielectric strength with increasing width of oil channel is practically linear for oil 5,314, the percentage decrease from a close butt joint to a channel width of $^1/_8$ inch being 9.5 per cent. A similar over-all decrease is seen for oil 5,317, although the rate of decrease with channel width is less regular. The over-all decrease in this case is roughly six per cent. No great accuracy may be claimed for numerical values in these results, principally because it is difficult to maintain a uniform channel width of low value. Thus in the close butt experiments, the results on two specimens have been omitted, because on dissection it was found that the joints between tapes were slightly open in the region of failure. The closest butt joint that can be wound seems to have here and there a slight opening. With this in mind a set of specimens was wound with a $^1/_{64}$-inch overlap, and it may be seen from the curve for oil 5,317 that this resulted in a still further slight increase of dielectric strength.

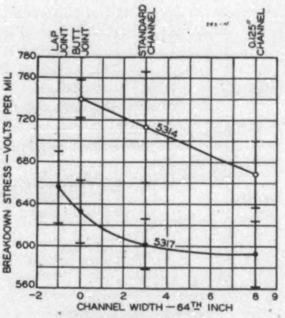

Figure 4. Stability of oil-impregnated-paper insulation—breakdown stress versus channel width

It is to be noted that the higher dielectric strength of oil 5,314 over 5,317 noted in the other experiments, appears also in the experiments on channel width.

Types of Failure

INITIAL AND PARTIAL FAILURES

An early method of interrupting the circuit following breakdown of the specimens, using a very light fuse in the primary circuit of the testing transformer, resulted uniformly in through failures with considerable burning. Later, use was made of a thyratron tube, operated from the voltage over a resistance in series with the connection of the low-voltage electrode to ground, and operating a fast-moving relay and circuit breaker in the primary circuit. This resulted in early interruption of failure and indeed enabled us to remove and examine a great many specimens in which partial failures only had occurred. These partial failures consisted of the burning, or break through, or other evidence of trouble, in from one layer of paper or channel of oil, to an increased number of layers up to but short of the full insulation wall. The amount of burning in a failed specimen of this type is very greatly reduced, and a much better idea of the origin, causes, and succeeding progress of the failure has been obtained.

The failures may be roughly divided into four groups based on careful dissection followed in many cases by oil extraction and staining with magenta:

A. Through radial failiures.

B. Partial failures involving one half of the total thickness of the insulating wall or less, and all terminating on the high-voltage conductor, or on the outer lead sheath.

C. Partial failures within the insulating wall, but not reaching either conductor or sheath.

D. Failures with no outward evidence of burning, but with subsequent evidence of wax formation.

Out of a total of 117 specimens tested, 46 had complete through failures and 66 had partial failures of one type or another.

Class A—Through Radial Failures. In these cases the burning is greater than in the partial failures, but in general the evidence is that there is little departure from an approximately straight radial failure ending in an oil channel near the outer layers and the first adjacent layers. Tree patterns and tracking are often evident, but only to the extent of reaching one adjacent oil channel. Puncturing through the tape of four consecutive layers was not uncommon.

Class B—Partial Failures Ending on Inner or on Outer Conductor. The characteristic of most of these failures is an area of blackened or burned oil film, never greater than one inch in diameter, towards the center of the insulating wall. Beyond the burned spot, evidence of trouble diminishes rapidly within one or two layers, then disappears. In the other direction burning also decreases, the puncture taking the form of short lengths of blackened oil in the channels and puncture through tapes, becoming smaller and cleaner towards the electrodes. The first, second, or third oil channel from the high-voltage conductor is usually involved. An exceptional case in this group consisted of four tiny punctures of four outside layers, originating in the oil adjacent to a corner of reinforcing paper at the end of the specimen and with no evidence of burning.

Of a total of 66 partial failures, 11 terminated on the outer electrode and 49 on the inner electrode. In one case there was a partial failure to each electrode, the two being well separated longitudinally, and apparently not related.

Class C—Partial Failures Not Reaching Either Electrode. Only three failures of this type were noticed, but they were quite definite. They were characterized by isolated black regions in an oil channel. Two were within four layers of an electrode. In one case the trouble was confined to only two layers.

In addition to these clearly marked local failures, there were ten cases in which this type of trouble was found to be coexistent with a failure either "through" or "partial" ending on an electrode, although definitely separated from it.

Class D—Failures With No Outward Evidence of Burning. In two cases first examination of the specimen failed to reveal any trouble other than a slight smell of burned oil. After subsequent oil extraction and staining with magenta dye, there was evidence in each case of initial wax formation at various places.

In considering these results, it should be borne in mind that in our specimens the insulation wall was relatively thin (0.08 inch), and the diameter of the smooth high-voltage conductor was relatively large (one inch). The excess of stress at the conductor surface over that at the outer electrode is of the order of 16 per cent, which is not much greater than the values of the average spread of our experimental breakdown results; thus partial failures originating on the outer electrode may be due to normal variations in the paper or oil. The smooth high-voltage conductor is perhaps an advantage in that it eliminates the common type of oil pocket in stranded conductors and confines the experimental results more definitely to the impregnated wall.

Throughout the careful examinations of the failed specimens, constant evidence has been found that the initial trouble arises in an oil space. In the cases of partial failures, either the first or the second oil channel was always involved. In all cases involving one, two, or three layers only, punctures were rarely found, the trouble being of the nature of oil blackening and surface tracking. Also in all cases without exception it was found that the oil in the channel next to the high-voltage conductor was more or less blackened, in many cases the blackening being limited to the edges of the paper and revealed by fine black lines on the polished conductor.

Discussion

It is believed that the outstanding results of this and the foregoing papers may be explained qualitatively in terms of the characteristics of the impregnating oil.

All refined degassed insulating oils apparently have the following characteristics in greater or less degree:

(*a*). A relatively low long-time conductivity, that is, as measured after a long application of continuous electric stress. This conductivity is partly electronic, but largely due to an inherent electrolytic dissociation arising in the complex molecular structure.

(*b*). An initial or short-time conductivity persisting for, say from one-tenth to one second, which may be many times as great as the short-time conductivity. This conductivity is due to an accumulation of relatively large and heavy ions or clusters of neutral molecules about a central ion. It is the motion of these large ions back and forth under alternating stress which causes the greater part of the dielectric loss in such oils.

(*c*). Both types of conductivity are increased by contact with paper and metals, due to added electrolytic impurities, and also to increased electrolytic dissociation.

(*d*). Application of continuous stress causes the large heavy ions to move to the electrodes or limits of an oil space, where, owing to their envelopes of neutral molecules, not all of them are discharged, and space charges accumulate close to the electrodes or other limiting surfaces, for example, paper tapes. Under alternating stress a similar action goes on resulting in maximum space charge accumulations at the crest of each half cycle. Studies of the mobility of these ions indicate that at 60 cycles there is sufficient time during a half cycle for some at least of these ions to pass completely across the oil channels occurring between paper tapes in impregnated-paper cable insulation.

The mass of evidence from the failures studied in this series of papers is that failure begins in an oil channel between paper tapes and probably at the contact surface between paper and oil. The failure is caused by a layer of high stress between the paper and a space charge layer immediately adjacent. Witness for example the spiral marking on the high-voltage conductor immediately under the edges of the paper tapes and over the full length of the specimen; also the isolated cases of partial failure in which the only evidence of failure is blackening in the channels, with carbonized channels through paper tapes spreading therefrom.

The ultimate cause of failure is a stress sufficiently high to ionize a molecule with the liberation of gas. The tiny bubble of gas may be reabsorbed before the next voltage crest in which case increasing carbonization may result before final failure. With further increase of stress, however, the gas bubble becomes larger, continues through the cycle, and is the seat of gaseous ionization. This latter attacks the adjacent paper much more aggressively, leading ultimately to puncture, to the extension of the oil breakdown in the next oil film, and to surface spreading to the adjacent oil channel of the next layer.

Failures here described, in outward appearance are often similar to those described by Robinson.[5] However, there is much indication in this work that the "coring" in the paper tapes occurs at the end rather than at the beginning of the process of failure. Coring has been noticed frequently at both conductor and sheath, but in these cases the complete absence of burning, as compared with conditions in the oil channels of deeper lying layers indicates strongly that the initial trouble originates in the channels then extending to puncture of the paper by the coring process. In our case we picture the oil as breaking down with the liberation of gas, subsequent gaseous ionization attacking the paper wall, perhaps in the manner suggested by Robinson. However, Robinson postulates a free gas bubble originally present near the high-voltage conductor, gaseous ionization in this bubble, and coring as the first stage of failure in the insulation wall itself. We believe our specimens to be originally entirely free of such gas bubbles and that the first gas that appears is due to oil failures. We do not find that coring is limited to the inside layers, due to the small difference in radial stress between outer and inner layers in our specimen. However, in the cases discussed by Robinson the higher stresses on the inside layers would tend to give first oil failures and subsequent coring in those layers.

The decrease of dielectric strength with increasing density of the paper tapes has already been ascribed to the increasing value of the stress in the oil channels due to the increasing value of dielectric constant of the impregnated paper. Approximate computation of the breakdown stress in the oil indicates a slight decrease in the critical stress with increasing paper density.[3] It is suggested that this is explained by the fact that the oil in contact with papers of increasing density takes on a greater number of adsorbed ions from the paper. There is a resulting increase in the density of the space charge layers, and an increase in stress between this layer and the adjacent surface. Thus the critical or breakdown stress in the oil is reached at a lower voltage or lower average stress.

Some cable engineers have been loth to accept this evidence that the dielectric strength of impregnated paper decreases with increasing paper density. It is stated that in manufacture, cables containing high-density paper give a higher dielectric strength than those using paper of a lower density. In one case it is stated that it has certain other advantageous physical properties. There are, however, no published data clearly showing these advantages of high-density paper in comparative life tests under sustained stress.

One possible explanation is that there

may be a difference in behavior under impulse or short-time tests and under sustained voltage tests. Here too experimental data is very meager. Del Mar and Works have recently presented data indicating that under short-time tests for certain values of overlap of paper tape, the breakdown strength of high-density paper may rise above that of low-density paper. However, as emphasized elsewhere in this paper, a time element undoubtedly is present in failures under sustained high stress. It is possible, therefore, that under impulse or short-time tests the inherent high dielectric strength of the high-density paper might play a more important part than the delayed breakdown of oil in the oil channels, in which the stress is much higher than the over-all average stress, due to the difference in the dielectric constants of the impregnated paper and the oil in the channels.

A pronounced decrease in dielectric strength with increasing paper thickness has been found.[4] This result may be attributed to the well-known fact that the breakdown strength of thin oil films decreases with increasing thickness. However, in this case also, approximate computations of the values of stress in the oil films at breakdown decrease with increasing thickness, again giving a first indication that the breakdown stress of the oil does not have a constant value. The same explanation may be offered here, namely, that with increasing paper and channel thickness, more ions are present in the oil film, greater values of space charge accumulate, and a critical breakdown strength of the oil is reached at a lower value of over-all stress.

Conclusions

1. Laboratory studies in accelerated life tests of the influence of the density of the paper on the breakdown strength of impregnated-paper insulation have been continued. When impregnated with a heavier oil, as used in solid-type cables, there is a marked decrease of dielectric strength with increasing paper density. Results are closely similar to those already found using a thin oil as impregnant.

2. Raising the viscosity of the thin oil and lowering the viscosity of the heavy oil so that both have the same value during impregnation has little effect on the breakdown strength. Specimens impregnated with the heavy oil show no change in behavior. Raising the viscosity of the thin oil results in a slightly lower dielectric strength. When the two oils have the same viscosity, the values of power factor and of oil content are approximately the same.

3. The dielectric strength of impregnated paper decreases with increasing oil-channel width between successive convolutions of tape. When the thin oil is used the decrease is about 9 per cent; when the heavy oil is used, the decrease is about $6^{1}/_{2}$ per cent, as the channel width increases from zero (close butt joint) to $^{1}/_{8}$ inch.

4. In all the foregoing experiments the dielectric strength and stability when using the thin oil as impregnant were noticeably higher than when using the heavy oil.

5. Close examination of all failures leads to the conclusion that in the accelerated life or stability tests lasting two days or more, failure always begins in an oil space.

6. Qualitative explanations are offered for results in this and two preceding papers of this series.

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Frequency Control of Load Swings

J. E. McCORMACK
MEMBER AIEE

R. J. LOMBARD
NONMEMBER AIEE

Synopsis: The Consolidated Edison Company made a series of tests to determine the cause and extent of system load swings on generators and tie feeders. The results were reported in detail in a previous paper.[1] An analysis of these tests indicated ways in which our load dispatching methods could be improved. This paper reports the effect of the changes in the method. Our experience indicates that the operators may reduce tie and generator load swings to negligible values by using frequency to control manual adjustments of generator output, provided that an accurate frequency indication is available at each generating station.

SEVERAL of the principles reported in the previous paper[1] have been applied to our method of load dispatching to reduce the load swings on the tie feeders and generators. These principles are:

1. Major load swings are caused by manual operation of governor valves in one or more stations during periods of load change.

2. Governors or supplementary controllers do not contribute to load swings but operate continuously to check them.

3. A change in voltage will result in a change in both real and reactive load demand, but a change in frequency within reasonable limits and with constant voltage does not result in an appreciable change in system demand.

4. The instantaneous relations between load, voltage, and frequency serve to indicate the initiating cause of a load change. An initial change in any one of these quantities will affect the other as shown in Table I.

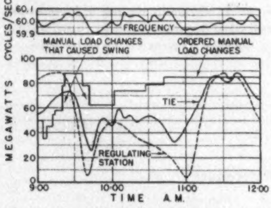

Figure 1. Load swing caused by load changes ordered by the system operator

The load changes were made when they drove the frequency away from normal

Table I

Initiating Change	Effect on Voltage	Effect on Load	Effect on Frequency
Voltage			
Increase	—	Increase	Decrease
Decrease	—	Decrease	Increase
Load			
Increase	Decrease	—	Decrease
Decrease	Increase	—	Increase
Frequency			
Increase	Increase	Increase	—
Decrease	Decrease	Decrease	—

Effect of Manual Adjustments of Generator Output

On a system supplied from one turbogenerator, a manual adjustment of the steam input would be readily discernible in the speed and voltage. This effect is not so apparent on a large system, because there are many governors and controllers operating to compensate the change. A change in steam input in excess of the load requirement on the one unit system results in a relatively large speed change, while on the large system, it results in a load swing over the connections between generators. On a large system the generator on which a manual load adjustment is made will have a simultaneous change of speed and load in the same direction, while all other units, under the influence of their respective governors, will change load in a direction opposite to the frequency.

If the generator ouput at one station is increased by manual adjustment when the frequency is at or above normal, the governors at the other stations will reduce the generated load by an equal amount. If these other stations are operating on scheduled loads the station operators will make manual adjustments to restore their stations to schedule, thus driving frequency still higher. This jockeying of load between stations results in load swings on the tie feeders, as well as on the stations, and imposes an unnecessary burden on a station assigned to regulation of frequency.

An example of load swing caused by manual adjustments without regard to instantaneous frequency is shown in Figure 1. This type of load swing can be avoided if the system frequency is used as a control in making manual adjustments.

Load-Dispatching Methods

It was the practice of the system operator to assign blocks of load to each station from time to time on the basis of an advanced estimate of the load requirements. Past experience with the hour-to-hour changes in total system demand was used in making the estimate. The station operators made the load changes when ordered, regardless of the frequency at that time. If the system demand did not change exactly as anticipated, the ordered changes would cause the frequency to depart from normal, and a

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J. E. McCORMACK and R. J. LOMBARD are both with Consolidated Edison Company of New York, Inc., where Mr. McCormack is assistant outside plant engineer.

system-wide load swing would result as the governors and controllers operated to compensate for the excess change. Where the tie feeders are relatively small in capacity, or where for contractual reasons the tie feeder interchange is to be maintained at a fixed value, it is then necessary to make additional adjustments to restore the tie-feeder load to normal.

In order to minimize these load swings, the following changes were made in our load-dispatching methods. The system operator now estimates the load in advance but assigns load to each station in larger blocks and at less frequent intervals. When the station operator receives a load-change order, he executes the order only at an instant when the change will tend to restore the frequency to normal. In other words, the station operator increases load only with low or dropping frequency and decreases load only with high or rising frequency. Thus, errors in load anticipation which could cause load swings are automatically eliminated.

The system operator has authority to make load changes, regardless of frequency, should changes be necessary, but he must specifically state that this shall be done.

We would like to point out that these measures are not intended to maintain closer frequency or to keep the accumulated time error within narrow limits, but are for the purpose of reducing, by the use of indicated frequency, the unnecessary load swings on generating facilities and tie feeders.

Figure 2 shows the extent that load swings have been reduced by the present method of load dispatching.

Need for Accurate Frequency Indication

Frequency indication as a control in making manual load adjustments requires that each station have a suitable frequency indicator. Since the normal frequency band on the system is about 0.1 cycle per second, an instrument for this purpose would have to be accurate within a fractional part of this normal band. This accuracy should be in the neighborhood of one part in 50,000, which is beyond the capability of the conventional frequency meter. A meter of normal accuracy could be used if one meter sent impulses to all stations indicating whether the frequency was high or low. Another way would be by transmitting a standard frequency voltage and comparing it differentially with another voltage at system frequency at each station.

On our system a standard frequency accurate to better than one part in a million is available.[2] This standard frequency is transmitted to each major station and compared with the system frequency by means of a synchroscope. The synchroscope gives one revolution for each cycle of slip frequency between the standard and system frequencies. The direction of rotation indicates whether the system frequency is high or low, and the rate of rotation is a measure of the amount of departures from standard frequency. Each station operator therefore has the same indication of system frequency.

This method of transmitting a frequency standard has been practical on the Consolidated Edison Company system because of the relatively few large stations and the short distances between them. In order to use a similar scheme over a widespread system, we have developed a frequency indicator operating from a frequency standard received by radio. Figure 3 shows the schematic diagram of this device. This scheme is based upon sending a standard frequency by a radio transmitter which has sufficient signal strength to cover the area of the system.

The radio signal is amplified by a radio receiver at each station, and the amplified standard-frequency voltage output is impressed on a step-up transformer. The transformer supplies a neon tube which is circular in shape and is covered by a disc having a single slot. The disc is driven by a synchronous motor from the power system. The standard frequency in cycles per second must be exactly divisible by the motor synchronous speed in revolutions per second. When the device is in operation, and the neon tube is viewed through the slotted disc, a series of light spots evenly spaced in a circle will be seen. If these spots remain stationary, the system frequency at that time is correct. If the spots rotate, the system frequency is incorrect. The direction of rotation indicates whether the system frequency is above or below the standard frequency.

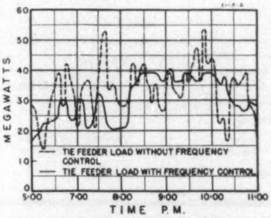

Figure 2. Effect of frequency control of manual load changes on tie-feeder loads

When load changes are made independent of system frequency, the tie-line loads vary considerably. When the load changes are made so that they restore frequency to normal, the large rapid load changes are not experienced

A few trial installations of this device are in service using an existing standard-frequency broadcast by the Bureau of Standards in Washington. This signal is broadcast on a carrier frequency of five megacycles and modulated by a 440-cycle tone. A 1,200-rpm synchronous motor is used to drive the slotted disc. The experience with this device has been very satisfactory.

Conclusion

The results of a year's experience using frequency as a control in making manual load changes on the Consolidated Edison Company system have led us to the following conclusions:

1. Generating-station and tie-feeder load swings can be reduced to a point where load swings are no longer a problem.

2. The need for accurate load anticipation becomes less important.

3. Tie feeders can be loaded safely to their thermal or stability limits.

4. Interchange energy between parts of the system may be controlled within closer limits. For contractual reasons this is often important.

5. Boiler-room maintenance costs can be decreased by reducing the load swings on the boilers.

6. Manual load adjustments by the station operator can be normally made in a direction to aid the automatic governors or controllers.

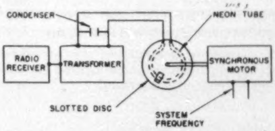

Figure 3. Schematic diagram of stroboscopic method of frequency indication based upon a standard frequency received by radio

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The Effect of Initial Conditions on Subharmonic Currents in a Nonlinear Series Circuit

STEPHEN J. ANGELLO
ASSOCIATE AIEE

Synopsis: This paper shows the importance of switching time as an initial condition for the current in a simple nonlinear series circuit with a saturable inductor. It is also shown that initial flux linkages in the inductor have an important effect on the current response during the first few cycles after closing the switch. It is emphasized that all experimental work was done with voltages below the critical ferroresonant voltage.

IT is the purpose of this paper to report the results of a study of the effect of switching time on the current response in a series *R*, *L*, *C* circuit containing a saturable inductor. In previous experimental work no attempt has been made to control the initial angle of the sinusoidal applied voltage.

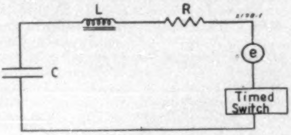
This series nonlinear circuit has been the subject of considerable investigation. Its importance was recognized in this country when outages caused by abnormally high currents were experienced in transmission lines equipped with series capacitors. It was found that the observed abnormal currents had a frequency lower than the system frequency. The frequency was a submultiple of the fundamental; hence, they were called "subharmonic" currents. Concordia[1] and Butler solved the equations of the circuit for the parameters of interest in their transmission-line problem by employing a differential analyzer. Following this work Travis[2] and Weygandt approached the problem analytically with the method of matching boundary conditions. They were forced to make many simplifying assumptions, but the solutions they obtained give an insight into the mechanism of the production of subharmonic currents in this circuit. One of the most serious assumptions, namely, that of neglecting capacitance in the circuit when the inductor is saturated, was removed by I. Travis[3] in a later paper. The theoretical investigation has reached a point where a great amount of computation must be done to obtain a small amount of information. To study the performance of this circuit further J. D. McCrumm[4] has employed an experimental method which consists of a systematic variation of circuit parameters, and recording of wave forms on oscillograms. The currents in his circuit were initiated by the random closing of a knife switch.

Experimental Procedure

The circuit studied is shown in Figure 1. The timed switch is constructed so that it can be closed at any point of the 60-cycle sine wave. After closure, it remains closed for the duration of four cycles, and then opens. Two cycles later it closes again at the same switching angle as before. This repetitive closing and opening enables the experimenter to view the first four cycles of the current as a steady image on an oscilloscope screen. During the two cycles that no voltage is applied, the capacitors are short-circuited to insure that the initial charge is zero. With this timed switch it is possible to watch the change of wave form as the switching angle is varied and also while various circuit parameters are varied. Provision was made to keep the switch closed after one of its periodic closures. This enables the experimenter to make oscillograms of the current from the time of initiation. Details of the timed switch are given in the appendix.

Results

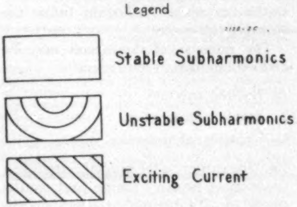
The effect of initial voltage phase angle on the circuit response is best shown by

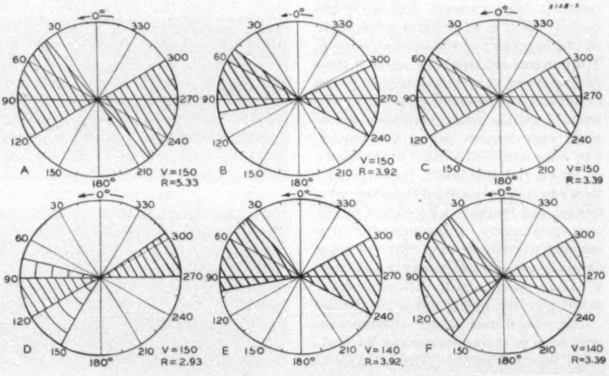


L = G.E. Transformer 9TM754A1
C = 355 Mfds. (Leakage 2000 Ohms)
R = Varied
e = 12 E sin (ωt + φ)

Figure 1. Circuit diagram

Figure 2. Polar diagrams showing the type of circuit response for the full range of initial voltage phase angle





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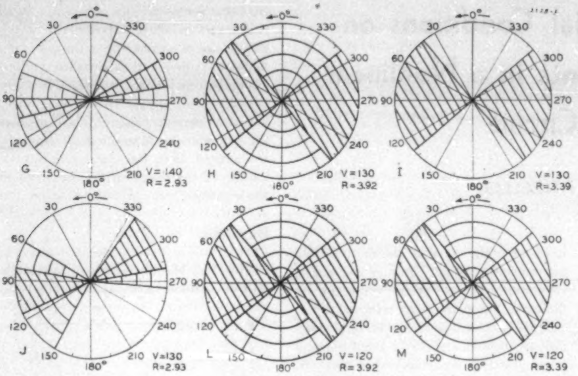
STEPHEN J. ANGELLO is with the research laboratories of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

The author wishes to express appreciation to his adviser, C. N. Weygandt, for help, and to the General Electric Company for plans of the relay employed in the experiment. This work was done at the Moore School of electrical engineering, University of Pennsylvania, Philadelphia, Pa., during the term 1939-40. Financial assistance was given by a Moore School fellowship.



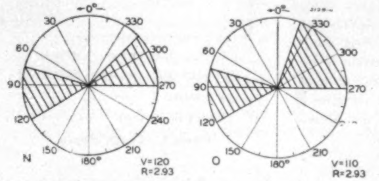


Figure 3 (above)

Figure 4 (left)

the polar diagrams of Figures 2, 3, and 4. The zero of angle was arbitrarily chosen as the zero on an oscillogram before the negative half-cycle.

The response of the circuit may be divided into three types, namely:

1. Exciting current.
2. Stable subharmonics.
3. Unstable subharmonics.

Exciting current is the normal current in a transformer primary for no load on the secondary. Stable subharmonics are abnormally high currents having a frequency which is an integral submultiple of the frequency of the applied voltage. The currents are said to be stable if they persist as long as voltage is applied to the circuit and they maintain a repetitive wave form. Unstable subharmonics are abnormally high currents having a nonrepetitive wave form. According to the theory of Travis and Weygandt, these currents have imperfectly matched boundary conditions, and therefore they cannot repeat each cycle exactly. It is common for such currents to continue in a "jittery" state for many minutes and suddenly disappear not to reappear until the circuit is again closed with the proper initial conditions.

The wave forms of the subharmonics are not given here, since many have already been published.[4] The initial voltage phase angle has no effect on the wave form of the subharmonic current in the region where these currents are possible. Switching angles of 90 degrees and 270 degrees generally give an exciting-current response, and the unstable subharmonics are situated on the border line between exciting current and stable subharmonics.

The diagrams of Figures 2, 3, and 4 show also that the subharmonic region is smaller for high values of circuit resistance. In the circuit of Figure 1, subharmonics could not be initiated at any angle when the resistance R was greater than approximately seven ohms. The extent of the subharmonic region also decreases as the applied voltage decreases. This is to be expected, because the lower the voltage, the more difficult it is to saturate the core of the inductor.

The boundary lines between current regions are drawn sharply but actually it is difficult to tell within plus or minus five degrees where one region ends and another begins. These graphs then have only a semiquantitative value. Each diagram is the result of observations taken at least two complete cycles of 360 degrees to be certain that the results are reproducible.

Another possible initial condition which has not been previously investigated either theoretically or experimentally is the initial flux in the inductor core. Oscillograms were taken with the transformer winding flashed with d-c before the switch was closed. Figure 5 shows two oscillograms taken of current initiated with identical initial conditions, except that the inductor was flashed in one direction for one oscillogram, and in the reverse direction for the other. It can be seen that the first few cycles are very different, but the wave forms after a short time has elapsed are identical. Figure 6 shows a repetition of the experiment for a different set of initial conditions.

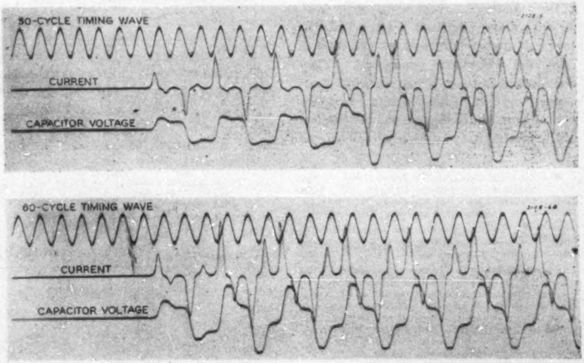
Conclusions

The results of this study may be summarized as follows:

(*a*). The current response in this circuit is dependent on the initial voltage phase angle.

Figure 5. Oscillograms showing the current response of the circuit for identical initial conditions except that the initial flux in the inductor core was reversed

The initial voltage angle was 31 degrees. Other data are $R=2.93$ ohms, $V=120$ volts, and $q_0=\text{zero}$



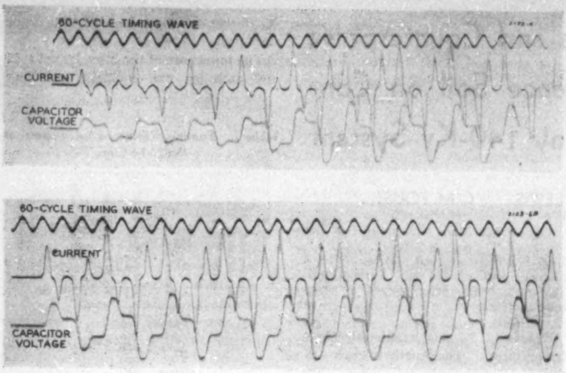


Figure 6. Oscillograms made in a manner similar to those of Figure 5

In this case the initial phase angle was 266 degrees. Other data are $R=2.93$ ohms, $V=140$ volts, and $q_0=$ zero

(*b*). The wave form of stable subharmonics is independent of initial phase angle.

(*c*). The wave form of the first few cycles of a subharmonic current is dependent on the value of initial flux linkages in the inductor.

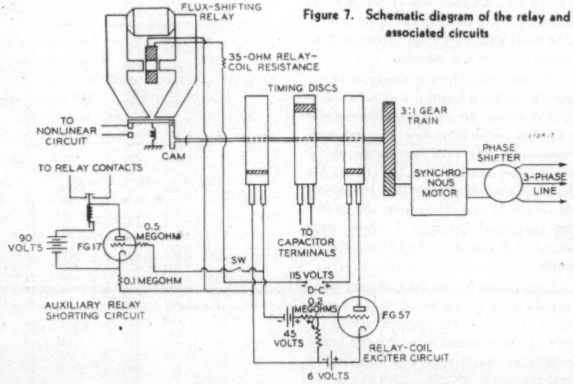
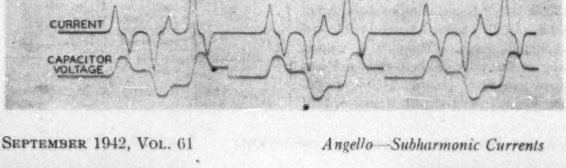


Figure 7. Schematic diagram of the relay and associated circuits

It should be noted that nothing has yet been said about ferroresonance. The impressed voltages employed in this work were lower than the critical voltage for ferroresonance in all cases. All statements concerning initial conditions, therefore, apply only to subharmonic currents. It is certain that the currents of ferroresonance are abnormally high, but the frequency is that of the applied voltage. This point is stressed, because some confusion is apparent in the discussions of other papers.

Figure 8. An oscillogram illustrating the performance of the timed switch

Note that the wave forms are repeated in detail. Note also that the capacitor voltage is reduced to zero after the switch opens so that $q_0=$ zero for the next closure



Appendix

The timed switch discussed under experimental procedure was a mechanical flux-shifting relay. A mechanical relay was employed, because it is necessary to have the circuit resistance very low. This eliminates the possibility of using electronic switches, because they have a high internal resistance, and furthermore this resistance is a function of the current.

Figure 7 shows a schematic diagram of the relay and the associated circuits. The purpose of the circuits is to excite the flux-shifting coil on the relay at the proper time. The timing is effected as follows:

(*a*). A synchronous motor running at 1,800 rpm turns three coaxial disks at 600 rpm through a 3 to 1 gear train.

(*b*). One disk has a shorting bar on the periphery which closes the bias circuit of the *FG* 17 thyratron. The thyratron plate current excites the flux-shifting coil on the relay, causing it to close. This coil excitation occurs once every six cycles. The part of the 60-cycle wave at which it occurs is determined by the space angle of the synchronous motor rotor. This space angle can be varied through 360 degrees by means of the phase shifter on the input.

(*c*). A cam is set on the end of the synchronous motor shaft so that it opens the relay about four cycles ($^1/_{15}$ second) after the closing instant.

(*d*). Another disk on the motor shaft is arranged to break the thyratron plate circuit after the arc in the tube has been initiated. This is necessary to make the tube ready for the next closure.

(*e*). A third disk on the motor shaft has a shorting bar on the periphery. This is oriented so that during the two cycles after the cam opens the relay, the charge on the capacitors in the circuit may be reduced to zero.

The oscillogram, Figure 8, shows the performance of the timed switch. Note that each cycle of operation is repeated in every detail. The result is a steady image of the first four cycles of current in the circuit on an oscilloscope screen.

The *FG* 17 circuit is switched in if it is desired to keep the circuit closed after a given closure. This circuit is used in conjunction with an oscillograph to get photographs of the current, and capacitor voltage wave forms from the time of closure until the steady state is reached.

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Study of Driven Rods and Counterpoise Wires in High-Resistance Soil on Consumers Power Company 140-Kv System

J. G. HEMSTREET
ASSOCIATE AIEE

W. W. LEWIS
FELLOW AIEE

C. M. FOUST
MEMBER AIEE

THE Consumers Power Company operates an extensive 140-kv system in eastern, southern, and western Michigan. In parts the top soil is sand, sometimes extending to a considerable depth, and tower footings in this soil have very high resistance compared with resistance encountered in other soils.

In 1936 the Consumers Power Company and the General Electric Company began a joint investigation of the ground-circuit arrangement best suited to carry the lightning current with a minimum potential across the line insulators. The test setup was arranged to study the relative merits of the driven rod, the continuous-parallel counterpoise, and the radial counterpoise, as well as to investigate the current and voltage conditions near the surface of the earth in the vicinity of the lightning stroke.

Methods of Grounding

To study various methods of grounding, a portion of the *N*-14 line between Croton and Muskegon was divided into three sections, and each section was equipped with special earth connections as follows:

Section A. Towers 7,659 to 7,682, parallel continuous counterpoise and right-angle counterpoise.

Section B. Towers 7,683 to 7,708, parallel continuous counterpoise and driven rods.

Section C. Towers 7,711 to 7,723, right-angle counterpoise and driven rods. (Tower 7,709 has been removed. Tower 7,710 is equipped with ground rods, but not with right-angle counterpoise because of deep gully and road.)

Figures 1a, 1b, and 1c show the three arrangements. The length of each section is approximately two miles. The counterpoise wire consists of seven-strand $^3/_8$-inch galvanized-steel cable buried to a depth of approximately 18 inches. The parallel wire extends continuously between towers, passing directly beneath the towers and connecting to the middle leg. The right-angle counterpoise wires extend 250 feet each side of the line and are attached to the side legs of the towers. The total length for each tower approximates the length of one span.

The rods were driven as deep as necessary to secure a resistance of between 10 and 20 ohms. In some cases one rod was sufficient and in other cases two rods were necessary. The rods were made of $^5/_8$-inch copper bearing steel, 8 feet long, the lengths being joined together by pressed fit couplings and driven into the ground by gasoline hammers.[1,2] Rods were driven to all depths, up to 150 feet in some cases.

In addition to the counterpoise wires and driven rods connected to the towers, 10 isolated probe wires approximately 100 feet long were buried in the ground, beginning approximately 300 feet from the line and extending at right angles to the line. These were spaced approximately uniformly throughout the experimental region between towers 7,659 and 7,723.

The *N*-14 line for the greater part of its length is a single-circuit line on steel towers. The conductors are arranged in a triangle, two on one side of the tower and one on the other, the equivalent delta spacing being approximately 15.6 feet. The total length of the line between Croton and Muskegon is 37.4 miles, of which 35.4 miles is 3/0 steel-reinforced aluminum cable and two miles 4/0 steel-reinforced aluminum cable. There is a ground wire at the peak of the towers made of three strands of number 7 Copperweld. The line is insulated with 9 disks spaced $5^3/_4$ inches on suspension and 11 disks on dead ends.

The towers are of the three-legged type and each leg was equipped with one

Table I. Footing Resistance of Towers of the *N*-14 Line

Section and Arrangement	Tower No.	Footing Resistance (Ohms)	No. of Driven Rods	Depth of Driven Rods (Feet)	Combined Resistance, Footing and Rods (Ohms)
Section *A*, parallel and right-angle counterpoise	7,659	460			
	7,660	510			
	7,661	660			
	7,662	750			
	7,663	680			
	7,664	750			
	7,665	640			
	7,666	600			
	7,667	840			
	7,668	360			
	7,669	420			
	7,670	700			
	7,671	730			
	7,672	820			
	7,673	740			
	7,674	750			
	7,675	360			
	7,676	920			
	7,677	860			
	7,678	760			
	7,679	740			
	7,680	920			
	7,681	620			
	7,682	610			
Section *B*, parallel counterpoise and driven rods	7,683	620	1	100	14
	7,684	840	1	82	15
	7,685	970	1	92	14
	7,686	520	1	76	9.5
	7,687	720	2	48–52	24
	7,688	590	2	32–36	19
	7,689	810	1	53	15
	7,690	770	2	42–41	14
	7,691	490	1	56	14
	7,692	440	1	58	10.5
	7,693	800	1	100	8.5
	7,694	675	1	100	8.5
	7,695	910	1	124	11
	7,696	775	1	90	13
	7,697	700	1	94	16
	7,698	775	1	116	11
	7,699	630	1	100	10.5
	7,700	640	1	108	10
	7,701	690	1	92	14
	7,702	680	2	58–57	14
	7,703	650	1	84	11.5
	7,704	640	1	84	10
	7,705	540	2	65–53	23
	7,706	825	1	108	10
	7,707	560	2	71–92	9
	7,708	1,400	1	92	11
Section *C*, right-angle counterpoise and driven rods	7,710*	850	2	121–92	13
	7,711	665	2	108–104	12
	7,712	910	2	98–108	13
	7,713	800	1	81	12.5
	7,714	1,030	1	100	8.5
	7,715	1,050	1	128	11
	7,716	850	1	132	14
	7,717	650	1	124	14
	7,718	810	1	156	12
	7,719	880	1	100	8
	7,720	610	2	84–100	7
	7,721	890	1	124	10
	7,722	700	1	116	7
	7,723	493	1	140	12

There is no tower 7,709.

* Tower 7,710 has two driven rods, but no counterpoise connected to it.

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J. G. HEMSTREET, is superintendent of operation, Consumer's Power Company, Jackson, Mich.; W. W. LEWIS is with the central-station engineering department, and C. M. FOUST with the general engineering laboratory, both of the General Electric Company, Schenectady, N. Y.

The authors acknowledge the assistance of many people in this investigation, especially J. R. Eaton, now of Purdue University, who had active charge for Consumers Power Company of the installation of the driven rods, counterpoise wires, and measuring equipment; Carl C. Tanner, who carried on the field work after Mr. Eaton left the company; and R. F. McAtee of the general engineering laboratory, General Electric Company, who helped in the calibration work and in analyzing the data.

bracket for magnetic links. After the 1937 season, brackets were installed on the overhead ground wire on each side of every tower, about 3 feet away from the tower. The continuous counterpoise wires were equipped with brackets on each side of each tower, about 4 feet from the center tower leg, and also at the middle of each span. The right-angle counterpoise wires on each side of the tower were equipped with one bracket at 4 feet from the tower leg, and on only the south side one at 125 feet and one at 225 feet. Each separate probe wire had a bracket at approximately the middle of its length. Each driven rod was equipped with one bracket. In all cases each bracket was equipped with two magnetic links in order to obtain a current range and to make use of the oscillatory calibration.[3]

The entire test portion of the *N*-14 line extends over a very uniform flat sandy plain. Table I gives the resistance measured by ground megger of the footing of each tower of the *N*-14 line included in this investigation. These resistances are of the footings alone and were measured before counterpoise wires and driven rods were installed. The table also gives for sections *B* and *C* the lengths of the driven rods and the final combined resistance of tower footing and driven rods.

During the years 1937 to 1941 inclusive, six strokes were experienced on the experimental part of the *N*-14 line. Of these one was in section *A*, two in section *B*, and three in section *C*. Following is a brief analysis of the important features of these strokes:

Table II. Footing Resistance of Towers on the T-20 Line

Tower No.	No. of Driven Rods	Combined Resistance Footing and Rods (Ohms)
4,387	4	125
4,388	4	21
4,389	4	28.5
4,390	2	25
4,391	2	29
4,392	2	22
4,393	2	33
4,394	2	35
4,395	1	51
4,396	1	56
4,397		760
4,398		807
4,399		729
4,400		1,299
4,401		942
4,402		1,240
4,403		1,260
4,404		816
4,405		1,187
4,406		784
4,407		266
4,408		75
4,409		138
4,410		15
4,411		14
4,412		13
4,413		158
4,414		434
4,415		131
4,416		44
4,417		—

STROKE 1

On October 19, 1937, there was a stroke to the overhead ground wire of the *N*-14 line, between towers 7,764 and 7,765, in section *A*, equipped with parallel counterpoise and right-angle counterpoise. Current records were obtained at 14 towers, 7,659 to 7,672 inclusive. The stroke was negative, that is, all tower currents flowed upward and all parallel counterpoise currents toward tower 7,665 and all right-angle counterpoise currents toward the towers. An idea of the comparative ability of the two types of counterpoise to pick up current may be gained from the following:

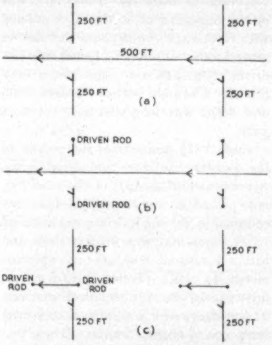

Figure 1. Experimental installation of parallel continuous counterpoise, right-angle counterpoise, and driven rods

For the parallel counterpoise, the difference between the current in a span approaching the tower and the current in the same span leaving the preceding tower is taken as the current collection or pickup in that span. For the right-angle counterpoise, the currents approaching the tower at each side at the magnet station nearest the tower are added up to give the pickup for that tower. Table III shows the parallel counterpoise pickup and Table IV the right-angle counterpoise pickup for this stroke. It will be noted that these currents are nearly equal, 23,000 amperes total in the parallel counterpoise and 25,700 amperes in the right-angle counterpoise.

At the first tower at which readings were obtained, tower 7,659, there were 2,800 amperes in the parallel counterpoise at the stroke side of the tower and no current was read in the counterpoise on the other side of the tower, in the tower legs, or in the right-angle counterpoise. Likewise, at the extreme end, tower 7,672, there were 1,800 amperes in the parallel counterpoise on the stroke side and no current was read elsewhere. The origin of this current is unknown.

It is interesting to note that current flowed in the parallel counterpoise for about six spans on each side of the point of stroke, while the right-angle counterpoise carried readable current at only three towers adjacent to the stroke, and the tower members themselves showed current in only two towers. This wide extent of current pickup on the parallel continuous counterpoise is common on this line, and also has been observed on other lines.[4,5]

Table III. Parallel-Counterpoise Current Pickup, Stroke 1

Toward Croton		Toward Muskegon	
Tower No.	Amperes	Tower No.	Amperes
Beyond 7,659	0	Beyond 7,672	0
7,659 to 7,660	700	7,672 to 7,671	200
7,660 to 7,661	500	6,671 to 7,670	500
7,661 to 7,662	1,000	7,670 to 7,669	600
7,662 to 7,663	2,200	7,669 to 7,668	500
7,663 to 7,664	3,100	7,668 to 7,667	2,100
7,664 to 7,665	4,300	7,667 to 7,666	2,300
		7,666 to 7,665	5,000
	11,800		
			11,200
			11,800
Total counterpoise pickup			23,000

Table IV. Right-Angle Counterpoise Current, Stroke 1

Tower No.	Direction of Counterpoise Wire	Amperes
7,664	North	2,500
	South	2,500
7,665	North	8,000
	South	8,300
7,666	North	2,200
	South	2,200
Total		25,700

Isolated probe conductors were installed in the ground opposite towers 7,660 and 7,667, but no current was measured in these wires. However, neither was any current measured in the right-angle counterpoise wires extending out toward the probe conductors at these towers.

It is interesting to obtain an idea of the comparative ability for collecting current of the shallow buried counterpoise wires and the short tower footings, extending perhaps eight feet below the ground surface. This may be done as follows:

Table V shows the total current picked up by the ground system, exclusive of the tower footings, which could not be measured in these tests.

Current was measured in only two towers, 7,664 and 7,665, as shown in Table VI. The difference between the total tower current of 58,400 amperes and the total counterpoise pickup current 53,300 amperes is 5,100 amperes, which is assumed to be the tower-footing pickup current.

If the current of 2,800 amperes in counterpoise at tower 7,659 and 1,800 amperes in counterpoise at tower 7,672 is prorated to the parallel and right-angle counterpoise in the ratio 23,000 to 25,700, we can sum up the distribution of currents between counterpoise and tower footings and evaluate the counterpoise resistance.

Table V. Measured Current Picked up by Counterpoise System, Stroke 1

	Amperes
Parallel counterpoise	23,000
Right-angle counterpoise	25,700
Current in counterpoise at tower 7,659	2,800
Current in counter poise at tower 7,672	1,800
Total	53,300

The multiple resistance of the 14 towers, 7,659 to 7,672, from Table I, is 42.7 ohms. The counterpoise resistance is then assumed to be inversely proportional to the current pickup, as shown in Table VII.

This comparison indicates that the counterpoise wires are very efficient compared with the natural tower footings which penetrate only a short distance below the ground in the same top soil occupied by the counterpoise wires.

STROKE 2

On August 20, 1938, there was a stroke to the overhead ground wire on the *N*-14 line between towers 7,708 and 7,710. Tower 7,709 had been removed prior to this time, so that towers 7,708 and 7,710 were adjacent. The stroke was negative, that is, cloud negative and earth positive, all tower currents flowing upward and all counterpoise currents flowing inward toward the point of stroke.

Tower 7,708 was at one end of section *B*, equipped with parallel counterpoise and driven ground rods. Tower 7,711 was at the beginning of section *C*, equipped with right-angle counterpoise and driven ground rods, but tower 7,710 had only two driven rods and no counterpoise connected. The span between towers 7,708 and 7,710 was long and ran across a gully.

Table VI. Tower Current, Stroke 1

Tower No.	Amperes			Total Current (Amperes)
	Leg 1	Leg 2	Leg 3	
7,664	8,000	1,600	1,600	11,200
7,665	11,700	8,500	4,000	24,200
				35,400
Estimated cross-member current 65 per cent				23,000
Total				58,400

Table VII. Counterpoise and Tower-Footing Current and Resistance, Stroke 1

	Current		Resistance (Ohms)
	Amperes	Per Cent	
Parallel counterpoise	25,170	43.2	8.66
Right-angle counterpoise	28,130	48.3	7.75
Tower footings	5,100	8.5	42.7
Total	58,400	100.0	

Table VIII summarizes the pickup in the counterpoise wires and driven rods. Approximately one half of the total current picked up by the driven rods was collected by the single driven rod at tower 7,708. This rod was 92 feet deep and had a measured resistance of approximately 11 ohms. Tower 7,710 had two driven rods, one 121 feet deep and one 92 feet deep, with a combined measured resistance of about 13 ohms. These two rods, however, picked up only 4,200 amperes total. It appears that ground currents flowing in the gully at right angles to the line were tapped more readily by the single rod at tower 7,708, possibly because of soil resistance in the vicinity and also possibly because the cloud may have been centered more to the north of the gully (toward tower 7,705).

The set of readings in the parallel-counterpoise and driven-rod section, towers 7,705 to 7,708 inclusive, lends itself to a determination of the equivalent resistance of the counterpoise, on the assumption that the resistances are inversely as the currents carried. The tower-footing resistance for towers 7,705 to 7,708 inclusive, in multiple, is found to be 179 ohms, from Table I, the combination tower and driven-rod resistance is found to be 2.89 ohms, and by calculation the driven-rod resistance alone is 2.93 ohms. Table IX shows that the driven rods carried about 81 per cent of the total current and the parallel continuous counterpoise about 17 per cent, with the tower-footing current negligible. The equivalent resistance of the parallel counterpoise is 13.7 ohms, compared with 2.9 ohms for the driven rods.

Table VIII. Counterpoise and Driven-Rod Current, Stroke 2

Tower No.	Counterpoise		Driven Rod (Amperes)
	Type	Amperes	
7,705	Parallel	1,000	1,000
7,706	Parallel	800	2,200
7,707	Parallel	200	4,400
7,708	Parallel	2,500	13,400
7,710*			4,200
7,711	Right angle	2,000	2,000
Total		6,500	27,200

* Not installed.

Table IX. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Stroke 2

	Current		Resistance (Ohms)
	Amperes	Per Cent	
Driven rods	21,000	81.1	2.93
Parallel counterpoise	4,500	17.3	13.7
Tower footings	340	1.6	179.0
Total	25,840	100.0	

A buried probe conductor opposite tower 7,707 carried no measurable current, while the driven rods at this tower picked up 4,400 amperes.

STROKE 3

This stroke took place on July 30, 1940, to the overhead ground wire between towers 7,714 and 7,715, in section *C*, equipped with right-angle counterpoise and driven rods. The stroke was negative. All tower currents flowed upward and all rod and counterpoise currents flowed toward the towers. Table X summarizes the current collection by right-angle counterpoise and driven rods.

The multiple footing resistance of the 12 towers, 7,712 to 7,723 inclusive, from

Table X. Right-Angle Counterpoise and Driven-Rod Current, Stroke 3

Tower No.	Counterpoise (Amperes)	Driven Rod (Amperes)
7,712	2,000	1,800
7,713	2,500	6,300
7,714	8,900	12,000
7,715	10,900	30,000
7,716	6,000	6,600
7,717	2,000	2,500
7,718	2,500	1,800
7,719	2,000	1,600
7,720	2,000	1,000
7,721	2,000	1,500
7,722	2,000	1,000
7,723	3,100	1,000
Total	45,900	67,100

Table XI. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Stroke 3

	Current		Resistance (Ohms)
	Amperes	Per Cent	
Driven rods	67,100	59.0	0.86
Right-angle counterpoise	45,900	40.3	1.26
Tower footings	900	0.7	64.0
Total	113,900	100.0	

Table I, is 64 ohms, while the multiple resistance of the combined footings and ground rods is 0.85 ohm. From these figures the driven-rod resistance alone calculates 0 86 ohm.

Table XI shows the division of current and the equivalent resistance of the driven rods, right-angle counterpoise, and tower footings, on the assumption that the resistances are inversely proportional to the currents. From Table XI it will be seen that the driven rods carry about 59 per cent of the current and the right-angle counterpoise wires about 40 per cent, the tower-footing current being negligible.

From Table XI the summation of the currents picked up by the driven rods, right-angle counterpoise wires, and tower footings is about 114,000 amperes. The sum of all tower currents involved in the stroke, towers 7,712 to 7,723 inclusive, is 60,700 amperes, which increased by 65 per cent for estimated cross member currents, would give 100,000 amperes. This should be increased by 3,300 amperes coming in on the overhead ground wire from beyond tower 7,712. Thus there is good agreement between the current of about 114,000 amperes collected from the ground and the tower current of about 103,000 amperes. However, the current in the overhead ground wire at the two ends of the span that was struck totaled only 57,300 amperes. This current is undoubtedly too low. A saturated inner link at the Croton side, at tower 7,715, permitted the use of the outer link only, with the unidirectional calibration curve. This value is therefore a minimum. This discrepancy might indicate that the counterpoise and ground-rod current crests are not coincident. The currents in the counterpoise in the ground surface might rise to crest value more quickly than the currents in the ground rods, which are draining a charge from 100 feet below the surface, including the ground charge extending well out beyond the surface counterpoise wires.

It will be noted from Table X that, in the towers near the lightning stroke, the driven-rod currents are much greater than the counterpoise currents, while at the more remote towers the pickup is more nearly equal, if not greater in the counterpoise. This might be expected if the major portion of the ground charge is located deeply and flows through a conducting layer radially to the driven rods near the lightning stroke.

Summation of the counterpoise currents to the north of the line gave 30,000 amperes, while those to the south of the line totaled 15,900 amperes. This would suggest that the cloud charge participating in the lightning stroke was more to the north of the line.

A reading of 5,000 amperes was obtained in a buried probe conductor opposite tower 7,715, while in the three stations on the right-angle counterpoise wire, extending in the same direction, readings were obtained of 1,000, 2,500, and 3,400 amperes, going progressively toward the tower. Also a reading of 1,800 amperes was obtained in a probe conductor opposite tower 7,722, while a reading of 1,000 amperes was obtained in the right-angle counterpoise extending in the same direction at the magnet station nearest the tower.

STROKE 4

This stroke took place July 30, 1940, to the overhead ground wire of the *N*-14 line between towers 7,702 and 7,703, in section *B*, with parallel continuous counterpoise and driven rods.

The current collection for the parallel counterpoise and driven rods is shown in Table XII. The stroke was negative.

The resistance of the footings of towers 7,700 to 7,706 inclusive, in multiple, is 93.7 ohms from Table I. The combined resistance of tower footings and driven rods is 1.74 ohms and the calculated resistance of the driven rods alone is 1.77 ohms.

Table XIII shows the distribution of current among the driven rods, parallel counterpoise, and tower footings, and the equivalent resistance on the basis that the current divides inversely as the resistance. It will be noted that the driven rods carry 88 per cent of the total current, as against 10 per cent for the parallel counterpoise wires.

Table XII. Parallel-Counterpoise and Driven-Rod Current, Stroke 4

Counterpoise		Driven Rods	
Tower No.	Amperes	Tower No.	Amperes
Beyond 7,700	1,600	7,700	1,900
7,700 to 7,701	0	7,701	2,300
7,701 to 7,702	1,100	7,702	10,100
7,702 to 7,703	2,300	7,703	17,800
		7,704	7,100
		7,705	1,000
		7,706	2,000
Total	5,000		42,200

Table XIII. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Stroke 4

	Current		Resistance (Ohms)
	Amperes	Per Cent	
Driven rods	42,200	88.0	1.77
Parallel counterpoise	5,000	10.4	15.0
Tower footings	800	1.6	93.7
Total	48,000	100.0	

Buried probe conductors opposite towers 7,700 and 7,707 showed no readable current. The driven rods at tower 7,700 picked up 1,900 amperes and there were readings in the parallel counterpoise wire and in the tower legs at this tower. However, at tower 7,707 no readings were obtained in the driven rods, counterpoise wire, or tower legs.

STROKES 5 AND 6

These strokes occurred on October 2 1941, to the overhead ground wire between towers 7,715 and 7,716, and between towers 7,720 and 7,721. This is the section of line equipped with right-angle counterpoise and driven rods (section *C*). Subsequent inspection showed flash marks on all three insulator assemblies at tower 7,721, but there was no tripout on this date. Both strokes were negative.

The current collection for the right-angle counterpoise and driven rods is shown in Table XIV.

The multiple resistance of the footings of the ten towers, 7,714 to 7,723 inclusive, from Table I is 76 ohms, and the multiple resistance of the combined driven rods and tower footings is 0.976 ohm. From these figures the resistance of the driven rods alone is 0.99 ohm. The distribution of current and the equivalent resistances are shown in Table XV. It will be noted that the driven rods carry

Table XIV. Right-Angle Counterpoise and Driven-Rod Current, Strokes 5 and 6

Tower No.	Counterpoise (Amperes)	Driven Rod (Amperes)
7,714	0	1,500
7,715	6,400	1,000
7,716	0	1,800
7,717	0	3,200
7,718	0	3,000
7,719	0	10,400
7,720	7,300	40,600
7,721	14,000	42,000
7,722	0	15,000
7,723	0	6,000
Total	27,700	124,500

Table XV. Driven-Rod, Counterpoise, and Tower-Footing Current and Resistance, Strokes 5 and 6

	Current		
	Amperes	Per Cent	Resistance (Ohms)
Driven rods	124,500	81	0.99
Right-angle counterpoise	27,700	18	4.45
Tower footings	1,620	1	76.0
Total	153,820	100	

about 81 per cent of the current, as against 18 per cent for the right-angle counterpoise.

No readable current was measured in buried probe conductors opposite towers 7,715 and 7,721. In the right-angle counterpoise wire, extending toward the probe wire at tower 7,715, 6,400 amperes were measured at the magnet station adjacent to the tower. In the right-angle counterpoise wire, extending toward the probe wire from tower 7,721, currents of 800, 3,600, and 5,800 amperes were read progressively toward the tower.

Voltage Gradient in Vicinity of Towers

For the purpose of indicating the voltage conditions in the vicinity of the tower during lightning strokes, a portion of the *T*-20 line was selected. This line runs between Croton and Grand Rapids, a distance of approximately 39.2 miles. The line has four-legged double-circuit steel towers, with the conductors arranged vertically on one side of the tower only for 32 of the 39.2 miles. The conductors are 110,000-circular-mil copper, with 15.1-feet equivalent delta spacing. One overhead ground wire, consisting of three strands of number 8 Copperweld, is installed at the peak. The line is insulated with ten disks, spaced $4^3/_4$ inches on suspension, and 12 units on dead ends. A section of this line from towers 4,387 to 4,417 inclusive, 31 towers, was selected for this investigation.

About two thirds of this test section traversed a sandy plain where tower-footing resistance was very high, while the remainder passed over low ground composed of muck and sand where footing resistances were comparatively low. Ten consecutive towers in the high-resistance section, 4,387 to 4,396 inclusive, were equipped with deep-driven rods to reduce the tower-footing resistance. In two cases there was one rod per tower, in five cases two rods per tower, and in three cases four rods per tower. The resistances of all the towers in the experimental section are given in Table II.

At one side of each tower, and about six feet from the tower legs, a probe rod was driven into the ground about five feet deep, and between the tower and the probe rod was introduced a lightning-stroke recorder.[6] The purpose of this recorder was to measure the voltage drop between the tower and the probe rod during lightning strokes.

Each tower leg was equipped with a bracket, and after 1937 a bracket was installed on the overhead ground wire on each side of each tower. Each deep-driven rod was also equipped with a bracket. In all cases each bracket was equipped with two magnetic links.

Eight strokes occurred on the *T*-20 line during the period of this investigation (1936 to 1941 inclusive). Six strokes in which there was good correlation between the tower current and the potential between tower and probe rod are summarized in Table XVI.

The stroke on September 15, 1936 (stroke 7) made contact with the overhead ground wire between towers 4,404 and 4,405. Measurements were obtained at 31 towers extending from 4,387 to 4,417 inclusive. At 29 towers, records were obtained on the lightning-stroke recorders connected between the tower leg and the five-foot probe rod. These readings ranged from 5 to about 50 kv, the external flashover value of the recorder. There were 3 cases of external flashover and 13 cases of film flashover. As a result of this experience, capacitance dividers with ratios of 2 to 1 were installed at all tower probe-rod stations.

On June 24, 1937, a stroke occurred to tower 4,408 (stroke 8). Records were obtained at 19 towers, 4,395 to 4,413 inclusive. In 7 cases the lightning-stroke recorder flashed over. For the other 12 cases, the readings ranged from 9 to about 100 kv, which was the flashover of the recorder and voltage-divider combination. It was therefore necessary again to increase the voltage-divider ratio and a 6 to 1 ratio was installed. This ratio proved sufficient, as no more flashovers were experienced.

All the strokes of which there were good records were negative. Stroke 12 is worthy of mention. This stroke (not listed in Table XVI) took place to the top of tower 4,389. About 6,300 amperes flowed upward in this tower, and a total of 6,300 amperes was measured in the two deep-driven rods connected to this tower, flowing upwards. In the adjacent tower 4,388, no current was measured, but in the second tower to the north, 4,387, about 13,200 amperes flowed downward and 6,000 amperes was measured flowing downward in the single deep-driven rod connected to this tower. Currents from the overhead ground wire flowed toward the stroke from beyond tower 4,387 and from beyond tower 4,391 to the south.

Surge Resistance of Tower Footings Under Natural Lightning Conditions

The potentials measured on the probe rods driven five feet deep at a distance six feet from the tower footings have been correlated with the tower-structure currents as shown in columns 4 and 6 of Table XVI. These tower-to-probe voltage measurements of column 6 are less than true tower potential by the earth potential differences beyond the probe rods. An approximation of the relation between measured tower-to-probe-rod voltage and actual tower potential is found as follows:

The current from the earth to the tower footing is assumed to be distributed uniformly in the earth, its density being the same at identical distances from the tower footing and inversely proportional to the square of the distance from the footing. The earth's resistance, being proportional to cross section, also is de-

Table XVI

(1) Stroke No.	(2) Tower No.	(3) R_N Tower-Footing Resistance (Ohms)	(4) I_T Total Tower Current† (Amperes)	(5) R_NI_T Tower Potential (3)×(4) (Kv)	(6) V_M Tower-to-Probe Potential (Kv)	(7) V_T Tower Potential (6)×2.5 (Kv)	(8) R_S Surge Resistance (7)/(4) (Ohms)	(9) R_S/R_N (8)/(3) Ratio
7	4,394	35	6,000	210	27	67.5	11.2	0.32
8	4,410	15	3,300	50	5	12.5	3.8	0.254
9	4,408	75	11,700	876	190	475	40.6	0.542
10	4,400*	1,299	13,700	17,800	280	700	51.0	0.039
	4,401	942	6,800	6,400	170	425	62.5	0.066
11	4,394	35	6,600	230	40	100	15.1	0.432
	4,395	51	16,000	816	170	425	26.4	0.517
	4,396	56	11,000	617	140	350	31.8	0.569
13	4,412	13	45,600	592	222	555	12.2	0.939

* Flashover all three phases.

† Total tower current (column 4) assumed 1.5 times measured current in tower legs, to take care of cross member current.

creasing as the square of the distance from the tower footing.

The voltage gradient at ground points of differing distances from the footing will be inversely as the square of the tower to point distance. The potential at a ground point is the integrated voltage gradient and is therefore inversely proportional to distance.

Accordingly, with an assumed tower-base spread of 18 feet and a probe point 6 feet farther out, the probe potential will be to the tower potential as 9 is to 15, or the tower potential will be 15/9 times the probe potential. The tower potential less the measured tower-to-probe voltage will be the probe potential. Therefore, the measured tower-to-probe potential must be multiplied by 2.5 to give tower potential. Values of true tower potential calculated on this basis are given in column 7.

To show the relation between the surge resistance and the normal footing resistance, the values of column 9 were calculated by dividing values in column 8 by those in column 3. These figures show the surge resistance to range from some 4 to 90 per cent of normal resistance values. These values are somewhat lower than those reported from artificial surge measurements.[7,8] In this connection, however, it should be observed that the range of normal resistances, from 13 to 1,300 ohms, is wider than is ordinarily investigated, and the range of currents, 6,000 to 45,000 amperes, is also very wide. In general in these measurements we are dealing with high resistances and large currents.

The resistance ratio of column 9 was shown, by plotting, to decrease very definitely with increasing currents and with increasing resistances. Accordingly, this ratio was plotted against a composite factor the product of up-tower current and normal footing resistance shown in column 5. In Figure 2 the ratio of surge resistance to normal resistance of column 9 is plotted as ordinate against the product of current and normal footing resistance of column 5. The points, while somewhat scattered, nevertheless show very clearly the trend indicated by the solid line. This is borne out by plotting on log-log paper, which shows a relation between ratio and product as follows:

$$\frac{R_S}{R_N} = \frac{14}{(R_N I_T + 80)^{0.6}}$$

in which R_S = surge resistance, R_N = normal or megger tower-footing resistance, and I_T = total tower current.

For practical purposes these curves might be utilized somewhat as follows: Assume that it is desired to evaluate the surge resistance of a certain tower under a lightning current of 10,000 amperes, where a normal tower-footing resistance of 100 ohms has been measured. The product of tower current and footing resistance is 1,000 × 10³, which referred to the curve gives a ratio of 0.22. This ratio applied to the 100-ohms footing resistance gives a surge resistance of 22 ohms. The resultant tower potential would be 220,000 volts, which is substantially below flashover.

The flashover of this line is taken as 700 kv on a 1.5x40-microsecond wave and on this basis it is interesting to examine actual field operating records as to insulator-assembly flashovers that might coordinate with the derived tower potential of column 7. Flashover occurred at tower 4,400 for stroke 10, for which the derived tower potential is just 700 kv. All three phases flashed over. No other flashovers are correlated with tower-to-probe measurements for the records shown in this table. Accordingly, on the basis of this flashover it is indicated that the ratio between true tower potential and measured tower-to-probe potential could not be lower than the 2.5 figure derived from the assumption outlined above. Also for tower 4,412, stroke 13, the tower potential is 555 kv, as the last item in column 7. In this case there was no flashover and the tower-to-probe potential was 222 kv. The ratio of 700 to 222 kv, or slightly over 3 to 1, is therefore indicated as the upper limit by these data. It is therefore concluded that the true ratio is between 2.5 and 3 to 1 and the use of the curve for interpreting true surge resistances from tower currents and normal ground-resistance measurements is seen to be reasonable.

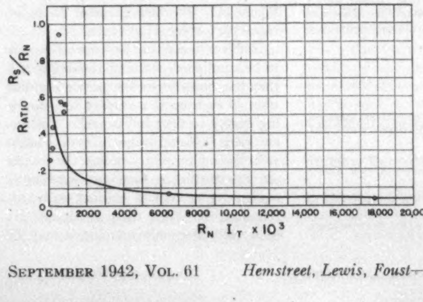

Figure 2. Ratio of surge resistance to normal tower-footing resistance, plotted against product of current and normal footing resistance during natural lightning strokes

Conclusions

In field lightning research, the investigators are often disappointed at the few direct strokes in the experimental section, even after a period of several years. The present investigation is no exception. However, it is believed that the preceding analysis and the following conclusions represent a fair interpretation of the limited data available. It should be understood that the conclusions apply specifically to the conditions prevailing on this particular transmission line.

1. Parallel continuous counterpoise wires and right-angle counterpoise wires of equal total length have approximately equal current-collecting ability. Direction of counterpoise apparently is not a governing factor, but only resistance to ground. In comparison with the normal tower footings the counterpoise wires are very effective in picking up lightning current.

2. In sandy high-resistance soil, rods driven deep enough to penetrate the clay or loam beneath the sand serve to reduce the tower-footing resistance from 500–1,400 ohms to 7–25 ohms. These rods, in comparison with counterpoise wires in the high-resistance surface soil, are much more effective in picking up lightning current.

3. There is some indication that the current pickup in the counterpoise wire near the ground surface and in the deep-driven rods is not coincident. Possibly the ground rod requires more time to discharge the surrounding earth on account of its deep penetration into the earth.

4. Data seem to indicate that the ratio of surge tower-footing resistance to normal tower-footing resistance decreases definitely with increasing currents and with increasing resistances. For example, at 10,000 amperes and 100 ohms the surge resistance decreases to approximately 22 ohms. This may account for frequent failure to flash over when the product of current and normal resistance indicated that flashover should take place.

5. The ratio of surge resistance to normal footing resistance is shown under natural lightning conditions to be as low as 0.04 for sandy soil, where normal resistance ranges up to 1,000 ohms or more and tower current up to approximately 50,000 amperes.

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Precision Speed Control for World's Largest Induction Motor

ROBERT R. LONGWELL
NONMEMBER AIEE

M. E. REAGAN
FELLOW AIEE

A RECENT paper by Dickey, Kilgore, and Laffoon described an installation consisting of a 40,000-horsepower wound rotor induction motor and the necessary auxiliary equipment required to provide for variable speed operation.[1]

To meet the many requirements of control, imposed by this application, it was necessary to devise new methods for insuring smooth acceleration of auxiliaries and main motor, for limiting the change in power required from the supply system to a definite maximum value, to provide precision regulation of speed, as well as means for control for the main motor from either of two points. In addition, power-factor regulation and means for emergency quick stopping was provided. These and many other features were incorporated in the automatic control, the functions of which are briefly outlined in the following paragraphs.

In order that the auxiliary motor generator sets and main motor may be supplied with adequate bearing lubrication and ventilation at all speeds, it is necessary to start the oil circulating pump and ventilating fans prior to placing those units in service. In addition, the seven-unit exciter set, which supplies d-c energy to the fields of the machines comprising the auxiliary motor-generator sets, must be started. After these preliminary operations are complete, the starting indication is given the automatic control by manual operation of the starting control switch which energizes the master relay. Starting and acceleration of the auxiliary motor-generator sets and connection of the main motor to the 6,900-volt bus is accomplished in the following sequence:

1. Closing of d-c machine field breakers.
2. Closing of high-voltage breaker for constant speed motor-generator set, energizing motor through starting reactor.
3. Closing of starting reactor short-circuiting breaker.
4. Energizing of fields of a-c machines.
5. Connection of speed matcher to bus and primary of main motor.
6. Acceleration of variable speed motor-generator set under control of constant current regulator for d-c machine fields and master speed-control rheostat operated by speed matcher contacts.
7. Energizing of voltage-balancing relay and automatic synchronizer.
8. Closing of main motor primary breaker.

At this point the main auxiliaries are operating at synchronous speed and the main motor is at rest but connected to the 6,900-volt three-phase bus. To start the main motor the operator, at one of the two suitably interlocked control points, turns the pointer on the speed-control dial to a definite speed position. Following that manual operation the automatic control completes the following operations:

1. Starts rotor-lift oil pumps for main motor.
2. Causes the variable speed set to slowly decrease in speed.
3. Main motor begins to rotate and gradually approach the desired speed.
4. Acceleration stops and main motor speed is maintained constant by means of the electronic speed regulator, controlled from a pilot generator on main motor. This regulator provides energy for speed-regulating field windings on d-c machines of constant speed motor-generator set.

In stopping the main and auxiliary units, the automatic operations take place in the reverse order to those performed when starting. The same smooth operation is afforded, as in starting, with power being fed back into the system as a means of decelerating the machines.

All of the recent developments in switching equipment were employed to provide utility, flexibility, and compactness of design. The 6,900-volt switchgear is of the metal-clad type, using oil circuit breakers having a suitable interrupting capacity; all low-voltage circuits are protected with metal-enclosed air circuit breakers, and the main control switchboard is of duplex construction. In addition to all those features, the latest innovation in relay construction was employed by the use of plug-in devices for both protection and automatic control.

Starting of Auxiliaries

All of the power and switching equipment, consisting of the following apparatus, is located in the power building (Figure 2).

1. 40,000-horsepower main motor.
2. 6,250-kva constant-speed motor-generator set.
3. 38,000-horsepower variable-speed motor-generator set.
4. 400-horsepower seven-unit exciter set.
5. 6,900-volt metal-clad high-voltage switchgear.
6. 460-volt metal-enclosed switchgear.
7. Seven-panel main duplex control switchboard.
8. Ventilation blowers for main motor and each motor-generator set.
9. Lubricating-oil circulating equipment.

Apparatus for remote control of the main-motor speed and for emergency quick stopping is installed on a control desk in the control room of the test chamber, Figure 3. The test chamber is an integral part of the wind tunnel.

If it is assumed that test operations are to be conducted, the first requirement is that the operator in the power building start all equipment necessary for supplying energy to the wind tunnel. This operation is initiated from the main control switchboard by first starting the exciter set, the ventilating fans, and lubricating equipment by remote manual push-button control. When these auxiliaries are in service, the main auxiliaries may be

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ROBERT R. LONGWELL and M. E. REAGAN are both with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., where Mr. Reagan is section engineer of the switchgear engineering department.

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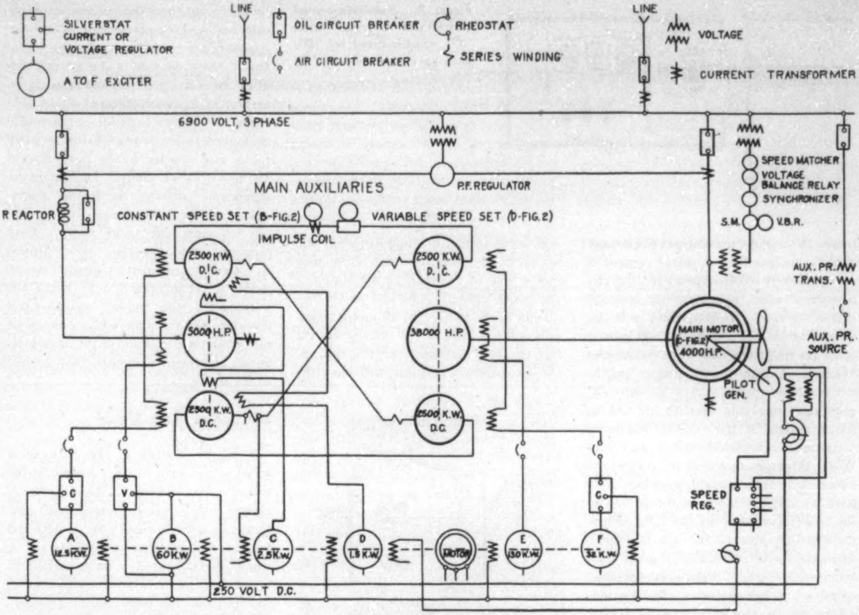
SILVERSTAT CURRENT OR VOLTAGE REGULATOR
A TO F EXCITER
LINE
OIL CIRCUIT BREAKER
AIR CIRCUIT BREAKER
RHEOSTAT
SERIES WINDING
LINE
VOLTAGE
CURRENT TRANSFORMER
6900 VOLT, 3 PHASE
P.F. REGULATOR
MAIN AUXILIARIES
REACTOR
CONSTANT SPEED SET (B-FIG.2)
IMPULSE COIL
VARIABLE SPEED SET (D-FIG.2)
SPEED MATCHER
VOLTAGE BALANCE RELAY
SYNCHRONIZER
S.M.
V.B.R.
2500 KW D.C.
5000 H.P.
2500 K.W. D.C.
2500 K.W. D.C.
38000 H.P.
2500 K.W. D.C.
AUX. PR. TRANS.
MAIN MOTOR (E-FIG.2) 4000 H.P.
AUX. PR. SOURCE
PILOT GEN.
SPEED REG.
C
V
C
A 12.5 K.W.
B 60 K.W.
C 2.5 K.W.
D 1.5 K.W.
MOTOR
E 150 K.W.
F 32 K.W.
250 VOLT D.C.

Figure 1. Schematic connection diagram for 40,000-horsepower motor and auxiliaries

started by means of a control switch on the main control switchboard. This switch energizes the master relay of the automatic control and all further starting operations are accomplished under the supervision of that equipment.

Starting of Constant-Speed Motor-Generator Set

The constant-speed motor-generator set consists of a 6,250-kva a-c machine and two 2,500-kw 750-volt d-c machines. During the starting operation of both main auxiliary sets, the a-c machine functions as a synchronous motor, and the d-c machines as generators. After the 40,000-horsepower motor is in service, the d-c machines operate as motors driving the a-c machine as a synchronous generator supplying power to the a-c system. Because of the dual function of these machines and to avoid confusion, they are referred to simply as a-c and d-c machines rather than as motors or generators.

Each operation, in the starting scheme, follows a planned automatic sequence which insures correct and positive functioning of the equipment being controlled. Following energizing of the master relay, the automatic control closes the field breakers for the fields of the d-c machines of both main auxiliary sets. Completion of this operation, as indicated by interlocks on the field breakers and a contact on the field relay for the d-c machines of the variable-speed set, the a-c machine of the constant-speed set is connected to the 6,900-volt bus through a starting reactor.

During the time that the constant-speed motor-generator set is accelerating, excitation on the exciter for the d-c machines of this set is held at zero. After sufficient time has elapsed for the set to reach approximately synchronous speed, a reactor short-circuiting breaker is closed connecting the a-c machine directly to the bus. This operation is followed by application of field energy, and the motor-generator set then pulls into synchronism and is ready to assume the load required for starting the variable-speed unit.

Master Speed-Control Rheostat

Because of the necessity for simultaneous control of all d-c-machine fields when initiating speed changes, it was necessary to combine all field rheostats in a motor-driven master speed-control assembly. This master rheostat is comprised not only of rheostats for exciter field control, but rheostats for recalibrating the Silverstat constant current regulators used in the fields of the d-c machines of the main auxiliary sets. In order to assure correct commutation on the d-c machines of the variable-speed set at all loads, shunt rheostats connected across those commutating fields are included in the master assembly. Also, this assembly contains a recalibrating rheostat for the electronic speed regulator, as well as one for controlling the field current to the driving motor of the master speed-control rheostat. This latter rheostat is used to provide for slow operation of the entire assembly when the variable-speed set or main motor is being accelerated, or decelerated, thereby assuring a slow and uniform change in power on the system.

Starting of Variable-Speed Motor-Generator Set

The variable-speed motor-generator set consists of a 38,000-horsepower synchronous machine and two 4,000-horse-

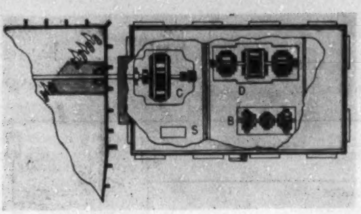

Figure 2. Automatic-control switchboard "S", main motor "C", variable-speed set "D", and constant-speed set "B"

power 750-volt d-c machines. The a-c machine is designed for a stator current of approximately 5,000 amperes at 5.5 cycles frequency when operating at approximately 48 rpm. This machine will also operate at a stator voltage of 4,800 volts at 60 cycles frequency and when rotating at 514 rpm. During the starting period the a-c machine operates as a synchronous generator supplying exciting current to the windings of the 40,000-horsepower main-motor rotor to which it's connected. When the main motor is in service, the a-c machine of the variable-speed set functions as a synchronous motor driven by the power generated in the rotor of the main motor—due to the slip frequency when the motor is rotating at other than synchronous speed. When the variable-speed set is being started, the d-c machines act as motors driven by energy from the d-c machines on the constant-speed set. After the main motor is in operation, these same d-c machines function as generators, driven mechanically by the a-c machine, and supply power to drive the constant-speed motor-generator set which feeds power into the a-c system. Had the conventional means of speed control been used, this power would have been dissipated as heat and been lost.

During the starting operation of the variable-speed set, the master speed-control rheostat is under control of a balanced torque-type speed matcher. The elements of the speed matcher are connected to potential transformers on either side of the open contacts of the main-motor primary breaker. Initially with the variable-speed set at rest, there is no voltage on the stator or primary terminals of the main motor, and the speed-matcher element connected to the bus side of the breaker operates to close a contact to cause the master speed-control rheostat to operate. The rheostat will function to permit excitation of the fields of the d-c machines of the constant-speed set, and those machines will deliver energy to the d-c machines on the variable-speed set. As this set begins to rotate, the a-c machine will generate a voltage and frequency proportional to the speed. An equivalent voltage at equal frequency will appear on the stator terminals of the main motor, due to the transformer action of the motor windings. So long as the frequency of the voltage on the motor terminals is below that of the voltage on the bus, the speed matcher, operating through the speed-control rheostat, will cause the variable-speed set to accelerate. Acceleration is accomplished first, by increasing the field strength on the d-c machines of the constant-speed set to a maximum and, finally, by decreasing the field current in the d-c machines of the variable-speed set until synchronous speed is reached.

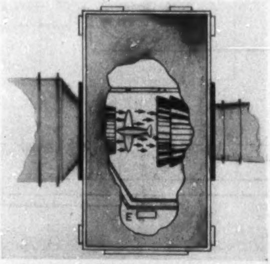

Figure 3. Remote-control desk "E"

Connection of Main, 40,000-Horsepower Motor to Bus

In order that power drawn from the power-supply system may be a minimum when the main motor is connected to the bus, the voltage on the stator windings is synchronized with the bus voltage before the oil circuit breaker is closed. This synchronizing operation is accomplished by means of an electronic-type automatic synchronizer operating in conjunction with the speed matcher and a voltage-balancing relay. At the instant that synchronism is accomplished, the oil circuit breaker is closed automatically.

All operations of the speed matcher, synchronizer, and voltage-balancing relay cease with closing of the main-motor breaker, and these devices are automatically removed from the circuit. However, a power-factor regulator takes over control of the excitation of the a-c machine of the variable-speed set, and through operation of a motor-operated shunt field rheostat functions to maintain practically unit power factor during the time the main motor is connected to the bus. Except for power-factor control the automatic equipment performs no further operations until the indication is given manually to start the main motor. Both auxiliary motor-generator sets continue to rotate at synchronous speed, since no device is yet in operation to change the position of the master speed-control rheostat, and the main motor remains at rest while the frequency of the rotor voltage is in synchronism with that applied to the stator.

Starting of Main Motor

Normally, control of the main motor speed is the duty of the operator in the test chamber, and for this reason, a speed-control dial, Figure 4, equipped with contacts and a synchro-tie motor, is mounted on the test-chamber control desk. A similar dial is located on the main switchboard, but it may be assumed that speed control from that point will be an emergency operation in the event that a fault occurs in the control cable between the power building and the test chamber. Control assignment switches are located beside each control dial for the purpose of assigning control to either, but not both, locations. Assignment of the point of control is accomplished by both assignment switches being in agreement, and, after once being assigned, cannot be changed by either one of the operators

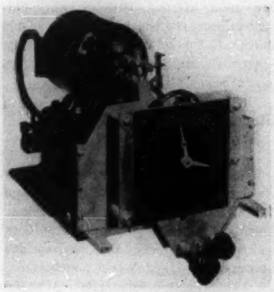

Figure 4. Speed-control dial, contact mechanism, and Synchro-tie

changing the position of his switch. A set of indicating lamp signals, coupled with the assignment switches, indicate the point to which the control has been assigned, and also indicate when the assignment switches are not in agreement.

With the main motor connected to the bus, but at rest, the operator to whom control is assigned may cause the motor to start and assume a certain definite speed by merely turning the knob on the control dial until the pointer reaches the desired speed position. It is well to note that the automatic control is provided with a position indicating relay, so that if the speed-control dial is not at the zero speed position when the main motor is connected to the bus, it will be necessary for the operator to return the pointer to zero and then to the desired speed setting before the motor will start. This insures that the motor will remain at reset after being connected to the bus during the starting operation, even though the control dial is at other than the zero speed position. Movement of the pointer off the "zero" speed position will cause the starting of rotor-lift oil pumps which will raise the main-motor rotor off the bearings, and when this is accomplished, the speed control equipment will operate to start the motor.

Acceleration of the main motor is accomplished by gradually slowing down the variable-speed set by first strengthening the field of the d-c machines on that set, and, as the speed decreases, by weakening the fields of the d-c machines on the constant-speed set. These field changes are accomplished by means of the master speed-control rheostat. This rheostat is equipped with a synchro-tie motor which is coupled electrically to a similar motor on the speed-control dial mechanism. When rheostat and dial mechanism are in the same relative position, with respect to main-motor speed setting, contacts on the dial mechanism are closed, short-circuiting the operating coils of the polarized "raise–lower" relay which is a part of the speed-control apparatus. Moving the dial mechanism to some fixed speed position opens these contacts, and the polarized control relay operates to cause the master rheostat to operate in a direction which will increase or decrease the main-motor speed, depending upon which way the dial pointer is turned. As the rheostat moves to accomplish the desired speed change, the synchro-tie moves a contact on the dial mechanism. When the rheostat reaches the position which will cause the motor to operate at the new speed setting, the control contacts will again close, and the rheostat will stop.

Control of Motor Speed

After the master speed-control rheostat comes to rest, the control for constant speed of the main motor is taken over by an electronic speed regulator which receives control energy from a pilot generator geared to the motor shaft. This speed regulator is recalibrated for any new speed setting by means of a rheostat on the master speed-control rheostat assembly. The regulator energizes the field of a small 1.5 kw speed-control exciter which supplies energy to field windings on the constant-speed machine. By increasing or decreasing the shunt field strength of this machine, the speed of the main motor is held constant by varying the torque through control of the power taken from the rotor and fed back into the a-c system.

By varying the speed of the speed-control rheostat automatically, smooth acceleration and deceleration of the main motor is possible. Also, by this means, the amount of power drawn from the bus during acceleration may be held to a steadily increasing value not exceeding a fixed increase per minute at any point on the speed range between 0 and 295 rpm. When the desired speed is reached, the electronic regulator maintains that speed constant within a range of 0.5 per cent for speeds from 37.5 rpm to 150 rpm and 0.3 per cent between 150 and 295 rpm.

Stopping of Main Motor and Motor-Generator Sets

The normal method for stopping the main motor and auxiliaries is first to reduce the main-motor speed to zero, then trip the motor breaker by turning the "start–stop" control switch to the "stop" position. Tripping of this breaker will disconnect the motor from the bus, and the automatic control will operate to decelerate the variable-speed set and cause it to come to rest. When this set is at rest, the high-voltage breaker for the power supply to the constant speed a-c machine will be tripped, and all automatic equipment will assume a normally de-energized position in readiness to again start the units at the will of the operator.

Emergency Quick Stopping

Means is provided for rapid deceleration of the main motor in case of emergency and, under such conditions, the motor is decelerated at the most rapid possible rate permitted by the power company. Rapid deceleration will cause power to be fed into the power company's system by regeneration through the main auxiliary motor-generator sets. Means is also provided for emergency shutdown and disconnection of all units from the bus should the necessity arise. Under this condition, all high-voltage and field circuit breakers are tripped simultaneously, and the main motor and motor-generator sets drift to standstill with friction and windage losses being the only decelerating forces.

Protective Devices

A complete complement of protective relays is provided with the automatic control. Those devices which prevent starting or operate an annunciator and sound an alarm are as follows:

1. Failure of bearing oil flow.
2. Failure of ventilation air.
3. Low lubricating oil pressure.
4. Low a-c voltage.

Those devices which merely annunciate and sound an alarm are:

1. High machine winding temperature; all machines.
2. High lubricating oil temperature.
3. Low lubricating oil temperature.
4. High bearing temperature.
5. Basement air temperature.
6. Low lubricating oil level.
7. Ground on auxiliary power circuits.

Devices which operate a lockout relay, drop an annunciator, sound an alarm, and cause all machines to be immediately removed from service are:

1. Overspeeding of either main auxiliary motor-generator set.
2. Unbalanced phase current on constant-speed a-c machine or main motor.
3. Differential protection on constant speed a-c machine or main motor.
4. A-c overcurrent in constant speed a-c machine or main motor.
5. Low a-c voltage on the bus.
6. Failure of unit to complete automatic starting sequence.
7. Overcurrent on d-c machines of main auxiliaries.
8. Short circuit on d-c machines of main auxiliaries.
9. Manual operation of emergency stopping switch.

The more serious of these faults, as indicated, cause immediate disconnection of the units from the bus while others merely sound an alarm and warn the operator that continued operation is undesirable, at which time provisions can be made to complete the test in as short a time as

Electrical Features of Design and Operation of the Plantation Pipe Line

M. A. HYDE
ASSOCIATE AIEE

H. B. BRITTON
NONMEMBER AIEE

PLANTATION Pipe Line Company's new 1,254-mile pipe line system is the largest ever built for the handling of refined petroleum products and is the largest all-electric-powered pipe line in existence. First full-length delivery was made early in February 1942, and immediately the line became an important factor in the transportation of gasoline and fuel oils to a large area in the southeastern states. Compared to the usual pipe line, this system operates under highly exacting conditions, and in its design the electrical and associated control features are of prime importance. The major electrification problems were:

1. To design for economical future increase in line capacity.
2. To provide flexibility in operation to meet varied schedule requirements.
3. To co-ordinate pumps, motors, and control, so as to accomplish the starting of units reliably and with reasonable demand on the station power service.
4. To design stations for safe handling of petroleum products.
5. To develop a station control system, co-ordinating the electric and hydraulic-control devices and meeting the requirements of single-attendant operation.

General Conditions of Operation

The statements made in this report regarding the capacities, pressures, and horsepowers, both for present operation and future installation possibilities, do not necessarily represent actual conditions, but do indicate the general principles involved.

The system comprises an unloading dock at the supply terminal with a short line of four parallel 12-inch pipes to a storage tank farm and the initial station of the pipe line proper, a section of 12-inch line, followed by a section of 10-inch line extending to the delivery terminal. Four branch lines serve areas to the north and south of the main line. The tankage at the supply terminal main line station is used to store incoming shipments, and to make up pipe-line shipments (known as tenders) according to the system schedule. Smaller storage facilities are located at the branch line origins.

Approximately 30 delivery terminals are situated at various points along the system, each with facilities for handling several products. The handling of multiple products to 30 delivery points creates a scheduling and dispatching problem probably far more complex than on any existing line.

The capacity of the line as initially equipped is 60,000 barrels per day of gasoline through the 12-inch section and 42,000 barrels per day through the 10-inch section. This requires two 600-horsepower motor-driven main-line pumps for each of the seven stations on the 12-inch line, and two 450-horsepower units in each of the seven stations on the 10-inch line. Each of three branch lines is served by a single 150-horsepower unit. A 450-horsepower unit is required at the origin of the fourth branch line, plus an additional booster station requiring a single 450-horsepower unit. The unloading station at the supply terminal dock contains five 600-horsepower units, providing ample capacity for rapidly unloading two tankers simultaneously. The initial mainline station at the supply terminal tank farm contains a complete spare pump unit to insure the availability of two units at all times. The system comprises 16 stations with a total of 39 centrifugal pumping units exclusive of tankage pumps and auxiliary pumps.

Provisions for Future Increase in Capacity

The present installation is designed as a long-term project to cover the commer-

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M. A. Hyde is industry engineer of the petroleum and chemical section, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. H. B. Britton is chief engineer of the Plantation Pipe Line Company, Atlanta, Ga.

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possible. All of the above protective features may be incorporated in the control of different types of machines but, never before, have all been included in the protection of a single assembly of machines.

Switchgear

The high-voltage metal-clad switchgear is equipped with lift-type oil circuit breakers which may be easily removed for inspection and service. Low-voltage metal-enclosed switchgear makes use of drawout-type air circuit breakers of suitable interrupting capacity for the circuits involved. The control switchboard is of the duplex type with meters and control switches on the front panels and all automatic and protective relays on the rear panels. Meters are semiflush, and all relays are semiflush of the plug-in type, equipped with stab contacts which permit easy removal from the panel for inspection and calibration. The entire assembly is easily accessible for maintenance and service, and all parts are of most advanced design with formed steel switchboard panels assembled in a complete self-supporting structure.

Conclusion

The automatic control provides reliable means, by use of proved apparatus, for performing all of the necessary starting, stopping, speed control, and protective functions for equipment of the type and size employed. The various switchgear assemblies are compact and well adapted to the service.

Many new switching problems were encountered in designing the complete control, and their solution opens up new fields of application to other apparatus. In addition, different types of standard equipment were employed to provide a complete and modern design which can be easily duplicated and one in which spare or renewal parts are readily obtainable.

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1. VARIABLE-SPEED DRIVE FOR UNITED STATES ARMY AIR CORPS WIND TUNNEL, **A. D. Dickey, C. M. Laffoon, L. A. Kilgore.** AIEE TRANSACTIONS, volume 61, 1942, March section, pages 126–30.

Figure 1. Pump room of typical main-line station on Plantation Pipe Line system

cial needs of the territory served and to operate economically on that basis. At the same time provision is made for possible future operation at increased capacity.

The maximum pressure at which the line will be operated is approximately 900 pounds per square inch in the 12-inch section and 975 pounds per square inch in the remaining sections. These values are determined by the allowable unit stress in the pipe walls, which in turn is a matter of economics in the design of the line. It is necessary in centrifugal pump operation that positive suction pressure be maintained at each pump, and if the minimum allowable suction pressure at the initial pump is established as, say, 40 pounds per square inch, then with a maximum allowable station discharge pressure of 900 pounds per square inch, the pressure increment introduced into the line at any station is 860 pounds per square inch, plus the pressure loss through the station piping. The stations are so spaced, then, that the pressure drop between stations due to fluid friction at rated line throughput, plus the static pressure differential totals approximately 860 pounds per square inch. Between the discharge of one station and the suction of the next, with uniform pipe, there is a uniform decrease in the component of total pressure due to line friction, this pressure drop being directly proportional to the distance from the upstream station. Referring to Figure 2, which represents a level section of 12-inch line, *SA* represents the suction pressure at initial station *A*, *DA* the discharge pressure at this station, and line *DA-SB* the pressure gradient along some 60 miles of line from station *A* to station *B*, for a flow rate of 60,000 barrels of gasoline per day. This is the flow that can be carried by the line under the conditions prescribed. If station *B* is at an elevation higher than station *A*, the distance between stations must be decreased by an amount that will reduce the friction drop sufficiently to provide the pressure differential necessary to balance the positive static head, and if station *B* is lower than station *A*, the distance between them will be increased to compensate for the negative static head.

To increase the capacity of the line without exceeding the maximum allowable working pressure, a second series of intermediate pressure increments may be added, one of which is represented by intermediate station *M*. If this station is placed at the hydraulic mid-point between stations *A* and *B*, the same pressure increment may be introduced at station *M* as at station *A*. The flow rate then will increase to a value such that the pressure drop between stations *M* and *B* will be the same as existed between stations *A* and *B* before adding the pressure increment at *M*. For the line in question this flow rate for gasoline is calculated at approximately 90,000 barrels per day.

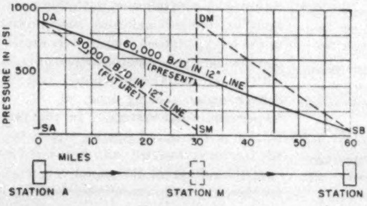


Figure 2. Pipe-line pressure gradients for flows of 60,000 barrels per day and 90,000 barrels per day

When this higher flow rate is established, obviously the horsepower input per station must be increased in both the initial and added stations. Whereas the initial stations are equipped with two 600-horsepower units for 60,000 barrels per day throughput, operation at 90,000 barrels per day will require two 900-horsepower units in all stations.

All stations on the main line are arranged for future change-over to higher capacity by initially installing pumps of such design that new impellers can be readily substituted, and motors designed for ultimate operation at the increased horsepower. These motors are of the totally enclosed fan-cooled type, and in order to secure efficient operation of the 12-inch line motors at 600 horsepower, they are equipped initially with a ventilating fan designed to deliver only the amount of ventilating air required by the motor operating at this capacity; when the change-over to 900 horsepower is made, no electrical or mechanical changes are involved except replacement of this one fan on the front shaft extension. Similarly, on the 10-inch section of the line, motors of ultimate 600 horsepower capacity are installed with initial fan capacity for operation at 450 horsepower. The motor-control and power-supply equipment in all the main-line booster stations are designed for operation at the ultimate ratings.

Operating Flexibility

All motors 75 horsepower and above are 2,300 volt. All motors above 150 horsepower are 3,600 rpm. All motors 450 horsepower and above are mechanically interchangeable, simplifying the problem of spare equipment, because a spare motor of 600/900-horsepower design can be installed on any line pump unit in the system except the 150-horsepower branch-line units. This feature also provides maximum utility of the equipment under unpredictable future alterations, due to changed operating conditions.

While the line is designed for a certain rated capacity, consideration must be given also to its flexible and economical operation at reduced throughputs. Contrary to practice on lines handling one uniform type of tender, the throughput of this line must be held above a certain minimum rate, to prevent excessive contamination between adjoining tenders passing through the line. Between this rate and the full capacity of the line it is desirable, from the standpoint of power consumption, to operate at a rate as low as will suffice to meet the transportation schedules.

Motors of the constant-speed type are preferred because of their simplicity and their adaptability to construction suitable for hazardous atmospheres. Installation of two units per station, operated at constant speed, with the pumps connected in series, provides the required degree of flexibility. With this arrangement, and with all stations along the line having piping installed to permit by-passing any unused unit or station, changes in throughput in steps of a few per cent can be made by operating 2, 1, or 0 units in properly selected stations.

Motor-Starting Conditions

With the station locations determined solely by the hydraulics of the line and, therefore, without regard to existing power facilities, it was necessary to consider carefully the method of starting the units, from the standpoint of voltage conditions on the electric service.

Most stations contain two main pumps comprising 95 per cent of the station load. From the standpoint of starting conditions the 12-inch line motors are 900 horsepower, and the 10-inch line motors 600 horsepower. The motors are of low-starting torque design in order to minimize rotor resistance and attain high operating efficiency, important in view of the high load factor at which the system will operate. To minimize current peaks during starting, motor starters for all units 450 horsepower and above are of the autotransformer type, with closed-circuit transition. During the brief interval when the motor is transferred to full voltage with its terminals still connected to the starting tap, a reactor is inserted between the autotransformer tap and the motor to limit the current in the autotransformer circuit. By suitable control this reactor also provides an additional reduced-voltage starting step when power is first applied to the motor.

During starting, the discharge valve of the centrifugal pump is closed which minimizes the required motor torque. After the unit is up to speed the discharge valve is opened slowly so that the change in direction of flow through the station piping will be gradual, for well-known hydraulic reasons. Speed-torque curves for the pump with closed discharge, and for the motor with various applied voltages, are shown in Figure 3. These curves apply to the 12-inch line pumps when equipped with impellers for 900-horsepower operation, representing the most difficult conditions that must be met. The speed-torque requirements of the pumps are shown as a band curve, the upper limit applying to the pump when operating on fuel oil of specific gravity 0.83 and the lower limit for gasoline of 0.73 specific gravity.

Motor speed-torque curves and line current-torque curves are shown for full voltage, also for 80 per cent voltage applied by autotransformer. Also shown are speed-current curves for three-step starting with the reactor initially connected between the autotransformer tap and the motor. With the reactor in circuit approximately 85 per cent of the tap voltage appears at the motor terminals during the first portion of the initial starting step.

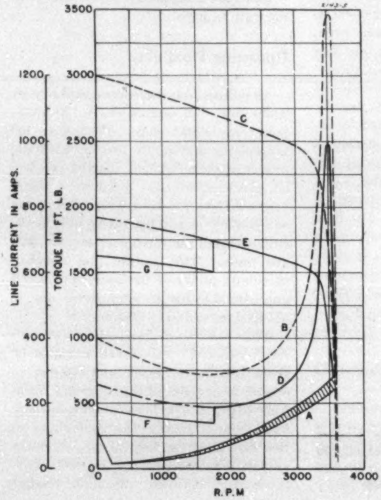


Figure 3. Starting characteristics of motor-driven pump on 12-inch line

A—Speed–torque for pump starting with closed discharge

B—Speed–torque for motor starting on 100 per cent voltage

C—Speed–line current for motor starting on 100 per cent voltage

D—Speed–torque for motor starting on 80 per cent voltage autotransformer tap.

E—Speed–line current for motor starting on 80 per cent voltage autotransformer tap

F—Speed–torque for motor starting on 80 per cent voltage autotransformer tap with series reactor

G—Speed–line current for motor starting on 80 per cent voltage autotransformer tap with series reactor

During starting, the voltage actually applied to the motor terminals should be of sufficient value to enable the motor to develop the torque necessary to reliably accelerate the unit to a speed above pull-out on the starting tap; when this speed is exceeded, the current decreases rapidly, and transfer to full voltage can be made on a low-current portion of the motor current-torque curve. Acceleration occurs directly as the excess of motor torque beyond pump input torque and inversely as the total inertia of the unit. Starting must be accomplished within a reasonable time, a limit in the order of 30 seconds being established as desirable by considerations of heating of the fluid within the pump casing. To perform the accelerating duty within this time limit it is found that the speed-torque curve of the motor when operating from the autotransformer tap should not fall below the curve corresponding to an applied voltage of about 1,630 volts. This means, when accelerating with the motor on tap giving a voltage of 80 per cent of line voltage, and with the reactor shunted, the voltage supply to high-voltage side of the starting autotransformer must be not less than:

1,630 volts ÷ 0.80 = 2,038 volts, or 88.6 per cent of normal 2,300 volts

Using a starting tap giving 85 per cent of line voltage, the voltage supply to the starting equipment must be not less than:

1,630 volts ÷ 0.85 = 1,918 volts or 83.4 per cent of normal 2,300 volts

To accommodate both present and future pump requirements, as well as variations in voltage conditions encountered at the several stations, the starters for the 600/900-horsepower motors were equipped with autotransformers having taps to deliver, under starting load, 80, 85, or 90 per cent of line voltage. In the first portion of the initial starting step, when the reactor is inserted into the tap circuit, the voltage at the motor terminals for these three tap connections is respectively, about 68, 72, or 76 per cent of normal line voltage.

Station Design as Affected by Hazardous Nature of Tenders

Safety problems introduced by the handling of petroleum hydrocarbon products of volatile and inflammable nature required special consideration throughout the design of the stations.

The areas subject to the occurrence of hazardous atmospheres (class *I*, group *D* as defined in the National Electrical Code) are the pump room and adjoining piping platform. The pump room contains the pumps with associated valves, hydraulic control devices, and piping. On the piping platform are situated the incoming and outgoing line piping and all necessary station and auxiliary piping with associated scraper traps, strainers, filters, valves, a sump tank with motor-driven pump for its service, and in many stations pressure-reducing valves and fluid meter and meter calibration facilities for delivery of product from the line to the distribution terminals.

The pump room is ventilated by a flow of incoming fresh air through glass-wool filters below the windows in the front wall of the station and directly opposite the motor air intakes. This air is forced through the motors by the motor blowers and discharged toward the rear wall of the pump room. In the rear wall just below the ceiling are mounted one or more motor-driven exhaust fans which discharge the air from the pump room and onto the piping platform. The piping platform is open on three sides with a flat roof for weather shelter, a construction affording maximum natural ventilation. The piping platform and pump room are at ground level, and in their design trenches and other depressions favorable to accumulating hydrocarbon vapors were avoided.

Figure 5. Outdoor substation for pumping station serving main and two branch lines

44- to 2.3-kv transformer bank consists of three 833-kva units. At right is the outdoor breaker controlling the 2,300-volt underground circuit to the pumping station bus. Structure at right mounts transformers for lighting, outside auxiliaries, and metering, as well as low-voltage breakers for lighting and outside auxiliary circuits

All electric motors, control, signal devices, motor-operated valves, lighting fixtures, and wiring installation in this area are specifically designed for service in class *I* group *D* hazardous atmospheres.

The 600/900-horsepower motors on the 12-inch line pumps are believed to be the largest, explosion-resisting motors ever built. Attainment of this rating in a relatively compact construction—an important consideration in explosion-resisting design—is accomplished by the circulation of large volumes of cooling air through the motor frame and by the use of class *B* insulation with the motor rating based on 75 degrees centigrade rise. All main-line pump motors have sleeve bearings, oil-ring lubricated and cooled by air blast from the motor ventilating fan. These bearings require no oil circulating system; consequently each motor is an entirely self-contained unit without external accessories.

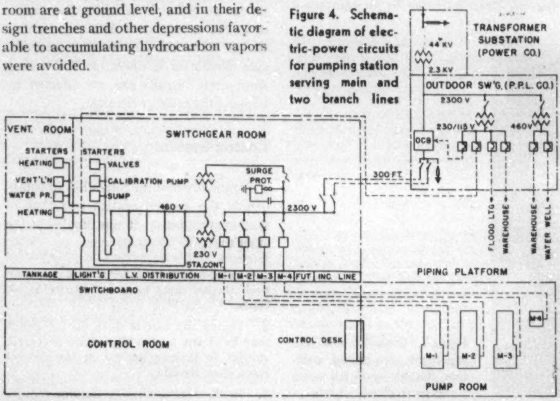


Figure 4. Schematic diagram of electric-power circuits for pumping station serving main and two branch lines

Control apparatus is of dead-front construction. With appropriate measures taken for safety, experience in products pipe-line operation indicates that provision of a nonhazardous location and installation therein of switchgear and control apparatus of open design are preferable to the use of special forms of switchgear and control in explosion-resisting or oil-immersed enclosures designed for class *I* group *D* location. This is true from both cost and operating standpoints. The importance of accessibility hardly can be overemphasized; since the several stations on a pipe line operate in series and interdependently, it is imperative that during operation of the line any inspection and maintenance work on electrical equipment be performed without shutdown or in emergency with the very minimum of shutdown time.

Nonhazardous conditions are maintained in all parts of the pumping station

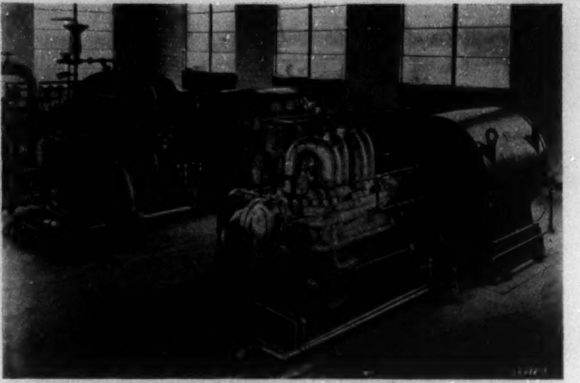


Figure 6. General view of pump room showing two main-line pumps (left) and branch-line pump (right) driven by 450/600-horsepower explosion-resisting motors

building, except the pump room and piping platform. The nonhazardous portion of the building, designated as the operating section, has a floor level about three feet higher than the pump-room floor and is completely separated by wall from the pump room and piping platform. No direct connection is provided between these areas, it being necessary to pass through outside entrances in going from one to the other. The atmosphere in the operating section is maintained in safe condition by outside air drawn into the building at the end opposite the pump room, maintained at a pressure a few inches above the outside air and above the atmosphere in the pump room. This insures that any air seepage through the wall, separating the operation section from the pump room or piping platform, will be in the direction to prevent entrance of hazardous atmosphere to the operating section.

The operating section of the station comprises a control room, switchgear room, ventilating and auxiliary room, lavatory, and in three of the stations a division superintendent's office.

The wall between the control room and the pump room consists of a vapor-tight, wire-glassed observation window. Into the center section of this window is sealed a vapor-tight steel gauge board, on which are mounted hydraulic recording and control devices and indicating pressure gauges as will be discussed later. These devices are gasketed to prevent passage of vapor through the mounting openings.

To provide for safely shutting down the station in case the control room should become hazardous due to an equipment failure, an explosion-resisting "emergency" pushbutton is mounted on the control desk contiguous to the gauge board. By pushing this button the attendant will trip the circuit breaker at the origin of the incoming power feeder.

This breaker, normally controlled from the incoming line panel in the station, is located at the transformer substation approximately 300 feet from the pumping station. From the outdoor breaker, an underground circuit runs to a 2,300-volt bus in the switchgear room, which in turn feeds the main pump starters as well as a 2,300-to-460-volt transformer bank serving all the station auxiliaries. Energy for the control circuits of the motor starters and for signal lights and alarms is furnished at 230 volts through a transformer from the station auxiliary supply. A single-phase transformer connected to the 2,300-volt service ahead of the station power breaker supplies station lighting and outdoor lighting at 115/230 volts through separate low-voltage outdoor breakers. At some of the stations there are loads outside the station proper, such as fire pumps, water well pumps, and warehouse, which must be operable even when the pumping station is shut down. To carry these loads, as well as the lighting load, a 2,300-volt bus is provided on the outdoor structure, connected to the power service ahead of the station power breaker. To this outdoor 2,300-volt bus are connected the station bus breaker, the lighting transformer, a 2,300-to-460-volt transformer bank for outside auxiliaries and a 2,300-volt fire pump circuit.

With this arrangement of circuits, shown schematically in Figure 4, the station operator can clear all power circuits within the station proper by opening the 2,300-volt outdoor circuit breaker. This he can do either from within the station or at the substation. At the same time he can leave the station lights and outside auxiliaries in operation if required, since these circuits are not affected by tripping the outdoor breaker.

Control System

A fundamental consideration in establishing the control system was that of station attendance; it was decided at the start that:

1. In the interest of safeguarding property, each station must have attendance at all times.

2. During the tour of duty the attendant may be at any part of the station property, subject to prompt arrival at the control room upon signal.

3. In the simple booster stations the at-

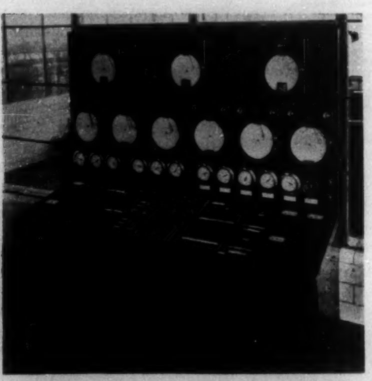


Figure 7. Control desk set into vaportight wire-glassed partition, through which the pump room is clearly visible

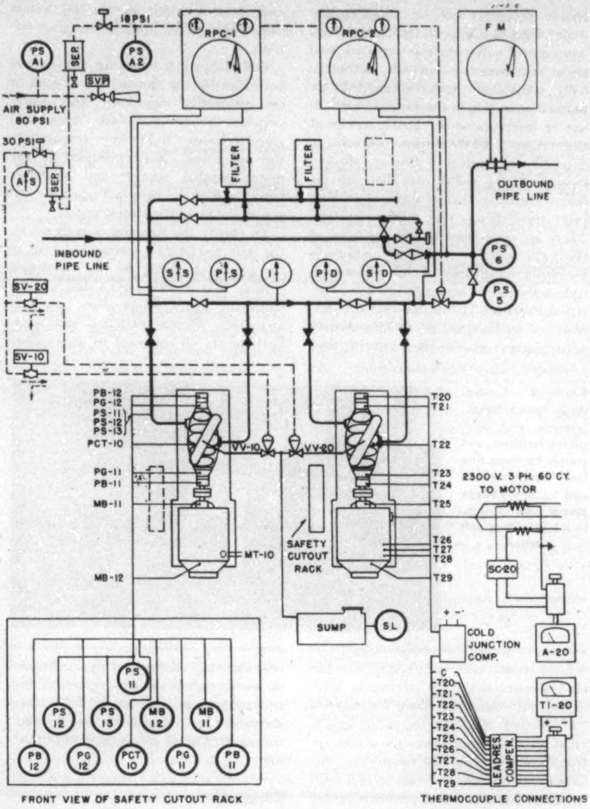

18PSI
RPC-1
RPC-2
F M
PS A1
SER
PS A2
SVP
AIR SUPPLY 80 PSI
30 PSI
A S
SER
FILTER
FILTER
OUTBOUND PIPE LINE
INBOUND PIPE LINE
S S
P S
I
P D
S D
PS 6
PS 5
SV-20
SV-10
PB-12
PG-12
PS-11
PS-12
PS-13
PCT-10
VV-10
VV-20
PG-11
PB-11
MB-11
MT-10
SAFETY CUTOUT RACK
MB-12
T20
T21
T22
T23
T24
T25
T26
T27
T28
T29
2300 V. 3 PH. 60 CY. TO MOTOR
SC-20
SUMP
S.L
COLD JUNCTION COMP.
A-20
TI-20
C
LEAD RES. COMPEN.
PS 11
PS 12
PS 13
MB 12
MB 11
PB 12
PG 12
PCT 10
PG 11
PB 11
FRONT VIEW OF SAFETY CUTOUT RACK
THERMOCOUPLE CONNECTIONS

Figure 8. Schematic diagram of station control system for typical two-unit station

tendant will perform singly all operating duties incidental to starting, running, and stopping units, as required by the central dispatching office.

4. Maintenance work except of minor nature is to be handled by established maintenance personnel, progressing from station to station. In case of difficulty or failure in the operation of equipment, specialized repair personnel will be available on call to the superintendent of the division.

These operating conditions determine the following essential features of the system of control:

(*a*). Starting and normal stopping of the stations and of units within each station shall be controlled by the station attendant.

(*b*). Protective shutdown of units and stations must be instituted automatically when conditions arise that exceed predetermined limits of safe operation.

(*c*). In case of automatic protective shutdown, a signal must be given to inform the attendant that shutdown has occurred, and indication must be given of the specific protective function that caused shutdown.

(*d*). In the starting and stopping of main-line units, it is desirable to simplify the duties of the attendant by providing automatic operation in proper sequence for the valves, motor control, and pressure devices concerned. The operator, thus relieved from performing a routine of manual operations at different points in the station, can devote his attention to the over-all process and to the results obtained.

(*e*). For continuous operation of the line it is, of course, necessary to maintain pressure conditions within the limits predetermined for protective shutdown, and this should be accomplished by automatic means.

Design of Station Control

The control system developed to meet these requirements embodies the co-ordinated application of pressure, temperature, and electrical control and indicating devices.

Each pump unit normally is controlled from an individual "start–stop" push-button, the control system providing a predetermined sequence of operations. Considering a typical two-unit booster station, this control system provides automatic means for:

1. Venting the pump casings during shutdown to prevent building up of pressure within them by leakage past suction or discharge valves.
2. Permitting the starting of a unit only upon existence of a predetermined minimum suction pressure.
3. Operating in correct sequence a motor-driven suction valve and a motor-driven discharge valve for each unit.
4. Venting vapor from the pump casing and circulating liquid through the pump during starting.
5. Adjusting the station capacity to the line throughput while the station is on the line.
6. Limiting the minimum value of station suction pressure to a value safe for pump operation.
7. Limiting the maximum value of the station discharge pressure to a value safe from the standpoint of pipe-line stress.
8. Safety shutdown, sounding the station alarm and lighting an individual indicating light, in case of:

	See Figure 8 Symbol
Excessive duration of high motor starting current	*SC*-20
High motor-winding temperature (alarm and light only)	*MT*-10
Power-supply failure (alarm only—air operated)	*SV-P*
High motor-bearing temperature (front or rear bearing)	*MB*-11, *MB*-12
High pump-bearing temperature (front or rear bearing)	*PB*-11, *PB*-12
High pump-gland temperature (front or rear gland)	*PG*-11, *PG*-12
High pump-casing temperature	*PCT*-10
Low suction pressure pumps 1 and 2	*PS*-11, *PS*-21
High suction pressure pump 1 and 2	*PS*-12, *PS*-22
High station discharge pressure	*PS*-5
Low station discharge pressure	*PS*-6
Low air-supply pressure to hydraulic controllers (alarm and light only)	*PS*-*A*2
Low station air-supply pressure (alarm and light only)	*PS*-*A*1
High sump-tank level (alarm and light only)	*SL*

An ammeter (*A*-20) and ammeter switch are provided for each unit, also a common temperature indicator (*TI*-20) for both units with a selector switch for each unit whereby the temperature indicator may be connected, per unit, to any one of:

	See Figure 8 Symbol
2 Pump-bearing thermocouples	*T*-21, *T*-25
2 Pump-gland thermocouples	*T*-22, *T*-24
1 Pump-casing thermocouple	*T*-23
2 Motor-bearing thermocouples	*T*-20, *T*-26
3 Motor-winding thermocouples	*T*-27,-28,-29

The gauge panel mounts a flowmeter (*FM*) for indicating and recording the rate of flow at or by the station, also two pressure controllers (*RPC*-1, *RPC*-2) whose function will be discussed below, and six gauges indicating pressure at the following points:

	See Figure 8 Symbol
Station suction	*SS*
Pump suction	*PS*
Interpump	*IP*
Pump discharge	*PD*
Station discharge	*SD*
Air supply to air operated vent valves	*AS*

Pump Venting

Each pump has a connection from the top of its casing through a vent valve to the station sump. The vent valve (*VV*-10, *VV*-20) is spring-opened, air-closed, the air supply being controlled by a three-way solenoid valve (*SV*-10, *SV*-20). This valve is so connected in the motor-control sequence that the vent valve is open while the pump unit is down and remains open during the starting cycle until the pump discharge valve is fully open, whereupon the solenoid valve is energized to close the vent valve. In a shutting-down cycle, when the pump discharge valve leaves its open position, the solenoid valve is de-energized to open the vent valve. At the conclusion of the shutting-down cycle both the discharge valve and the suction valve are closed, and flow through the pump-venting system ceases.

Station Pressure Control

The regulation of station pressure is accomplished by air-actuated instruments such that the station suction and discharge pressures can be automatically maintained at any predetermined values. The control valve which is actuated from these instruments is normally automatically controlled, but is equipped for manual operation in the event of air failure or interruption of electric service at the stations. All the stations operating in a continuous section of line are affected by any change in flow rate that may occur. There are many changes in flow rate throughout the entire system. These are occasioned by the existence of the large number of delivery points or terminals, which may be utilized for multiple deliveries or for single full-line capacity deliveries. The station pressure controls are so designed as to immediately adjust each station to these varying flow conditions and thereby maintain uninterrupted operation. The previously mentioned automatic shutdown features are co-ordinated with the regulating equipment, so as to permit operation of the control system over its full range, and only to prevent operation of one or two units in the station in case the pressures exceed pre-established limits.

Figure 9. Control desk, gauge board, teletype and telephone facilities, and panels for incoming line, motor starters, and low-voltage power distribution, in a typical two-unit station

Control Desk

Physically, the control for each station centers in a control desk in the control room, where also is situated the gauge board previously mentioned, the attendant's working desk, and a teletype operating over a circuit serving the system dispatcher's office and all pumping stations.

Red indicating lights on the control desk identify the functioning of each of the protective shutdown and alarm switches already mentioned. Amber and green indicating lights show respectively the open and closed positions of the motor-operated suction and discharge valves and of the principal manually operated valves in the station piping.

To clarify the function of each device the desk top bears a schematic layout of the principal piping for the particular station, and in this piping diagram the indicating lights, temperature indicator, ammeters, selector switches, and push-buttons are all mounted in appropriate position. An accompanying illustration shows how conveniently the control desk and gauge board in one of the stations serves as the central point for indication and control of all principal station functions.

Conclusion

As evident from this discussion, the electrification of the Plantation system was designed to meet the practical requirements of products-line operation. That it adequately serves these needs is indicated by operating experience with the system to date, which has verified all fundamental features of its design.

Protection of Pilot-Wire Circuits

E. L. HARDER
MEMBER AIEE

M. A. BOSTWICK
ASSOCIATE AIEE

THIS paper reviews the problem of protecting pilot-wire circuits, briefly discusses the sources of disturbing voltages, describes two types of neutralizing transformers, and illustrates how they are used to distribute the disturbing voltage most favorably throughout the pilot-wire circuit. The recent increase in use of high-speed pilot-wire relays for the protection of important power circuits has accentuated the need of better understanding of the pilot-wire protection problem. Therefore, a review of this general subject and a description of a new type of neutralizing transformer with mathematical formulas to determine its effectiveness, is considered timely.

Three principal objectives to be attained when applying pilot-wire protective apparatus are:

1. To assure correct operation of the protective relays or other apparatus at all times.
2. To protect operating personnel.
3. To protect the apparatus.

Pilot circuits for protective relaying must be operative at all times, especially at times of system disturbances. Thus, simple protective gaps which provide adequate protection for many other pilot services cannot be used to protect against voltages induced by the power-system currents, as this would render the circuit inoperative when most needed. Certain supervisory control and other functions are also required to be operative during power-system faults, and this discussion applies equally well to pilot circuits for these purposes. The principles and methods will also be found useful in applying protection to those telemetering and supervisory control circuits which are not required to be operative during power-system faults.

A pilot circuit may be exposed to disturbing voltages of several sorts, which will be discussed subsequently. Briefly, these are:

(*a*). Lightning and other surges.

(*b*). Induction from power lines.

(*c*). Elevation of station ground potential.

(*d*). Direct crosses with power circuits.

If these voltages are not to disrupt the proper operation of the circuit, they must be disposed of by some combination of the following measures.

(*a*). By insulating the circuit to stand them.

(*b*). By distributing them through design of the circuit impedances and grounds.

(*c*). By the use of gaps or arresters to prevent surge voltages from reaching excessive values.

(*d*). By drainage—dissipating the disturbing voltages as impedance drops in the circuits.

(*e*). By shielding the wires or so locating them that the disturbing voltages are lessened.

(*f*). By using insulating transformers or neutralizing transformers to take up longitudinal voltages as will be explained.

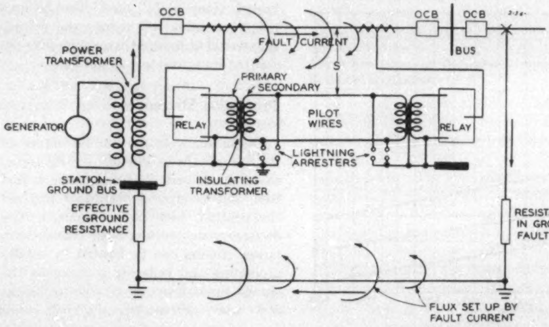
In many cases no specific protective measures are required. This is frequently the case when rubber-lead-covered control cable is used in relatively unexposed locations. However, a consideration of the magnitudes of disturbing voltages is worth-while in any case, to insure that the pilot circuit, through its insulation, arrangement, and protective features, provides adequately for the voltages to be encountered.

Pilot-wire circuits that are totally enclosed in a frequently grounded sheath are inherently provided with a high degree of protection against lightning. Circuits of this kind, that are not exposed to induction from transmission lines and that enter stations in which only small ground potentials can occur, require no special protective measures. The auxiliary equipment that will be discussed is needed only when calculation or measurements indicate voltages in excess of the normal insulation strength.

The general problem is very much as follows: Assume that lightning strikes the power line and causes a phase-to-ground fault at the far end of the line section that is protected with pilot-wire relays. Assume also that the pilot circuit is exposed to the effects of the same lightning stroke. Arresters on the pilot wires must operate to protect the relays and associated equipment. However, the arresters must immediately seal off and interrupt any power follow, so as to make the pilot circuit operable. The seal-off voltage of the arresters must be above the maximum voltage that can be impressed on them by induction in the pilot wires or by a combination of a rise in ground potential and induction which can be caused by the flow of transmission-line fault currents. If the arresters should fail to seal off, they would effectively short-circuit the pilot wires and block operation of the protective relays. Similarly, provision must be made to guard against the accidental operation of pilot-wire relays which may be caused by unsymmetrical operation of the arresters. This may be accomplished, for example, by connecting a low-voltage protector tube between the pilot wires, as shown in Figure 4 and by distributing fundamental frequency-disturbing voltages so

Figure 1. Schematic diagram of faulted transmission line and pilot-wire relay circuit

S = Separation between pilot wire and power line



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E. L. HARDER is central station engineer, Middle Atlantic district, industry engineering department, with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. M. A. BOSTWICK is protective-relay application engineer with Westinghouse Electric and Manufacturing Company, Newark, N. J.

The authors acknowledge the assistance of J. E. Barkle in preparing many of the potential-gradient diagrams.

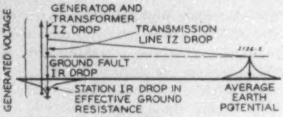

GENERATED VOLTAGE
GENERATOR AND TRANSFORMER IZ DROP
TRANSMISSION LINE IZ DROP
GROUND FAULT IR DROP
STATION IR DROP IN EFFECTIVE GROUND RESISTANCE
AVERAGE EARTH POTENTIAL

Figure 2. Distribution of voltages in transmission-line and ground circuits

IX_L component of line drop equals maximum possible voltage that can be induced in pilot wires

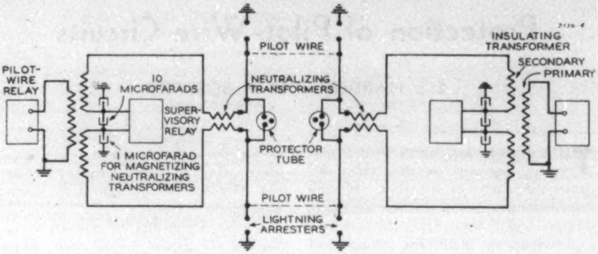


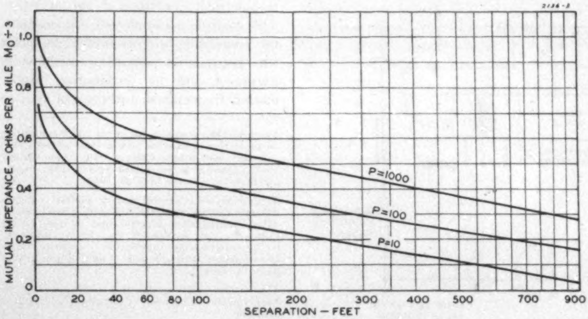
Figure 4. Protective apparatus

as to avoid operating the lightning arresters.

To aid in visualizing the manner in which these disturbing voltages may be impressed on the pilot-wire circuits by transmission line faults, consider the circuit that is illustrated in Figure 1. Inasmuch as these disturbances are most severe when one phase of the transmission line is grounded, only a single-phase system is drawn. As shown in Figure 1, assume that the faulted power system consists of a single-phase generator, a grounded transformer, a section of line, and equivalent ground-fault resistance. The pilot-wire circuit is spaced from the power-line conductor, a distance of *S* feet.

When the ground fault occurs, current will flow out through the transmission line and return through the ground. The generated voltage is used up in a series of voltage drops in the generator and transformer, line, ground-fault resistance, and the effective resistance that can be measured between the station ground bus and true ground. This resistance, indicated in Figures 1 and 2, varies from a fraction of an ohm in well-grounded stations to several ohms in stations that are not so well grounded. The value may be high or low, depending upon the earth resistivity in that vicinity. Ground potential rises of 1,000 to 3,000 volts are not uncommon, while values up to 15,000 volts are sometimes encountered.

Figure 3. Approximate variation in mutual impedance between transmission circuit and pilot wires



The manner in which voltage is induced in the pilot-wire circuit is indicated in Figure 1. Fields of flux are set up by the flow of current in the transmission line and in the earth return. These fluxes link both pilot wires and induce voltages along the length of the exposed portion of the pilot-wire circuit in proportion to the magnitude of the flux linkages. If a twisted or spiraled pair of wires is used for the pilot circuit, no appreciable voltage difference will appear between the pilot wires. This is important, for the voltage difference that appears between the pilot wires must be limited to avoid operating the protective relay, or associated apparatus that is connected to the pilot circuit. Because of this, only longitudinally induced voltages, which are equal and in the same direction in both wires, will be discussed.

It will be noted that the flux which links the pilot wires from the ground current is 180 degrees out of phase with respect to that set up by the transmission-line current. Consequently, these two fields tend to cancel, or it may be said that the ground current tends to "shield" the pilot wires from induction. This effect becomes quite appreciable when ground wires carry a large portion of the ground-fault current.[1]

Figure 3 illustrates how the mutual impedance between the transmission line and pilot wires varies with the separation between the two circuits. Three different curves are drawn to illustrate how this quantity is affected by extreme variations in the resistance of the earth-return circuit or how the earth currents "shield" the pilot circuit from induction. When the earth resistivity is high, current returns deep in the ground and produces less shielding. The ground-fault current multiplied by the proper mutual impedance equals the magnitude of voltage that will be induced in the pilot-wire circuit. Note that this quantity can never exceed the magnitude of the transmission-line reactance drop. A voltage equal to that magnitude can be obtained only when the transmission line and pilot wires occupy the same space or have 100 per cent coupling. It should also be remembered that ground wires and return circuits that are provided by cable sheaths all tend to reduce the magnitude of the induced voltage. In general, the shielding that is obtained from a full-sized, lead-covered telephone cable will reduce the voltage that would be induced in an open-wire circuit by approximately 50 per cent.[1]

Protective Measures

Disturbing voltages can be impressed on the pilot-wire circuits by lightning, electrostatic and electromagnetic induction, station ground potentials, and actual contact with power circuits. The damage which results from crosses with power circuits can be limited by solidly grounding and isolating portions of the circuit through action of arrester blocks and fuses. Frequently this will cause relay operations that cannot be prevented. Voltages impressed on the circuit by lightning, induction, and ground potentials can be dissipated, distributed, or isolated with insulation in a manner that will not cause relay operation. This is accomplished by use of lightning arresters (which must be chosen so that they cannot be operated by system disturbances), by draining appreciable amounts of currents from the circuit through mid-tapped transformers or similar balanced impedances, by altering the natural distribution of voltage stresses with neutralizing transformers or grounding impedances, and by insulating the apparatus or isolating it with insulating transformers so that it will withstand the applied voltage.

Insulating transformers have long been used to insulate the terminal equipment from disturbing voltages in pilot-wire circuits. They are also used to keep station ground potentials off the pilot wires. Insulation built directly into the relay can be used in a similar manner. This action is illustrated with typical graphical solutions in the latter part of this paper. The effects of electrostatic and electromagnetic induction can be counteracted directly in the pilot wires by current drainage, as by grounding the mid-points of the insulating transformers at both ends of the line. When this is done, it is necessary to consider thermal capacities of the equipment and inequalities in voltage drop in the individual pilot wires as any lack of symmetry in the circuit tends to insert an extraneous (unbalanced) voltage in the pilot loop and may affect the associated relay operation. Neutralizing transformers are used in circuits of this kind to reduce the circulating current to an absolute minimum and to provide a control means that can be used in d-c circuits.

To study the action of neutralizing transformers and determine the distribution of the disturbing voltages, pilot-wire equipment should be classified into groups at different potentials somewhat as follows:

1. Terminal equipment at station ground potential. In Figure 1 this includes the current transformers and relay parts up to and including the primary of the insulating transformer.

2. Terminal equipment on the station side of a neutralizing transformer. As neutralization is not perfect, this equipment operates at the remnant or unneutralized voltage to ground. In Figure 4 this includes the supervisory relay, secondary winding of the insulating transformer, and the capacitor between the two center taps of the insulating transformer.

3. The incoming pilot wires, any equipment connected thereto, and their potential with respect to ground.

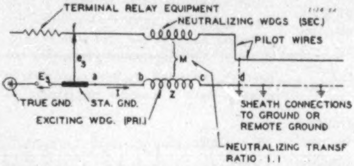


Figure 5a. Three-winding neutralizing transformer neutralizing station ground potential

I = Neutralizing - transformer exciting current

r = Resistance of exciting-winding connections a-b and c-d

Z = Self-impedance of exciting winding

M = Mutual impedance between exciting winding and secondary windings

e_s = Remnant or unneutralized voltage which appears between relays and station ground

Z_p = Primary leakage impedance = $Z-M$

$$e_s = E_s\frac{Z_p+r}{Z+r} \qquad (1)$$

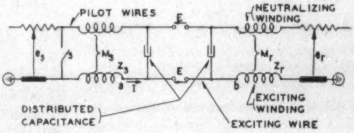


Figure 5b. Three-winding neutralizing transformer neutralizing longitudinally induced voltage

Z = Impedance of ground return circuit, a to b

E = Longitudinal voltage induced equally in pilot and neutralizing wires

m = Mutual impedance between exciting wire-ground return circuits, from a to b

$Z_{sp} = Z_s - M_s$, $Z_{rp} = Z_r - M_r$, primary leakage impedances

Total remnant voltage:

$$e_s - e_r = E\,\frac{Z_{sp}+Z_{rp}+z-m}{Z_s+Z_r-z} \qquad (2)$$

4. The cable sheath, usually at true ground potential if insulated, or at station ground if connected thereto.

5. Other wires in the cables, and their potential to ground.

Some of these items span from one potential to the other. The neutralizing transformers span from (3) to (2); that is, from the pilot wires to the remnant voltage point. The grounding capacitors span from (2) to (1); that is, from remnant voltage point to station ground. The insulating transformers span from (2) to (1).

Cable sheaths, if not connected to station ground, may usually be considered as at true ground throughout their lengths. They are usually grounded by leakage if not intentionally. Aerial cables with sheaths purposely insulated require a study of the various capacitive impedances, between wires and sheath, and sheath to ground, to determine the voltage with which their insulation is stressed. If a sheath is grounded along the line and also grounded to station ground, a heavy current will flow over it when there is a rise of station ground potential. The steepness of gradient along the cable sheath from station ground to true ground will depend on the resistance of the "pole grounds." Possible danger of burning the sheath should be checked, especially if the cable is buried under the power line so that it may carry a considerable part of the return fault current.

With given voltages, impressed on the pilot-wire circuit by longitudinal induction or a rise in station ground potential, the determination of voltage distribution as indicated above is a problem in conventional circuit theory. After determining each potential with respect to any convenient reference, such as true ground, the potential differences are obtained for verifying or designing the insulation coordination. Graphic examples of this are given after operation of the neutralizing transformers is explained.

Three-Winding* Neutralizing Transformer

When used to protect against a difference in ground potential, the exciting winding of the three-winding neutralizing transformer is connected between true ground (remote pole ground) and the station ground as shown in Figure 5a. The pilot wires are connected in series with the secondary windings of the neutralizing transformer and thereby have a voltage almost equal and opposite to the station ground-potential rise impressed directly on them. Thus, with the pilot wires at true ground potential (sheath potential usually), the terminal relay equipment will be held close to station ground potential.

The connection from the exciting wind-

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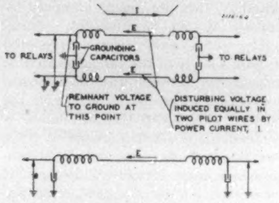
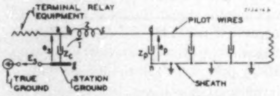
* Three winding is used to distinguish the type having a separate exciting winding. There may be any number of secondaries. Usually, there are two, through which a pair of pilot wires is carried.



(a) Actual and single-line representation of circuit with neutralizing transformers

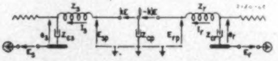


(b) Neutralizing station ground potential

r = Resistance of pilot wires in parallel from a to b and c to d

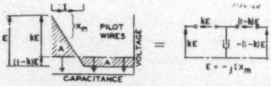
Z_p = Impedance of pilot wires to ground (usually sheath) looking into terminal d-n, including distributed capacity and terminating impedances

Z_c = Parallel impedance of grounding capacitors from a to g



(c) General case—neutralizing station ground potential and longitudinal induction

E = Total longitudinal induced voltage



(d) Equivalent circuit for pilot wires having induced voltage and distributed and lumped capacitance

Figure 6. Two-winding neutralizing transformer

ing to ground may be a wire carried out a few hundred feet to a pole ground. Or the cable sheath may be insulated for the last few hundred feet into the station and the exciting winding of the neutralizing transformer connected between it and station ground.

Assuming the pilot wires to be at true ground potential as indicated schematically by the dotted connection at d, Figure 5a, the remnant or unneutralized voltages between the terminal relay equipment and station ground can be expressed as a function of the station ground-potential rise, E_s, and the circuit constants shown. The voltage e_s, is the difference between the voltage drops from f to d, and a to d, due to I. Thus:

$$e_s = E_s \frac{Z_p + r}{Z + r} \tag{1}$$

Commercial designs have been described[2] having 0.35 ampere exciting current with 4,000 volts applied to the primary winding. This results in an impedance $Z = 4{,}000/0.35$ or 11,400 ohms. The primary leakage impedance, Z_p, has been held to a value such that with a disturbing voltage, E_s, of 4,000 volts, the remnant voltage e_s, is 70 volts for $r = 0$, and 81 volts for $r = 35$ ohms. (r is the resistance of the exciting winding connections.)

The three-winding type of neutralizing transformer has also been used for neutralizing induced voltages.[3] In applications of this kind an exciting wire is run the full length of the pilot wires and has induced in it the same voltage as the pilot wires. However, the cost of the exciting pilot wire, which must be held to a low value of resistance, usually eliminates this scheme from consideration.

Two-Winding Neutralizing Transformer

The two-winding neutralizing transformer (or longitudinal choke) is a new form of neutralizing transformer in which the exciting current is conducted through the windings that are connected in series with the pilot wires and otherwise operates in the same manner as the three-winding neutralizing transformer. This design was introduced in 1935. The first application was for protection of extensive pilot wires along an a-c railway electrification.

The two-winding neutralizing transformer is designed to protect pilot wires and their terminal equipment from station ground potentials and longitudinally induced voltages. Its self-exciting feature is particularly valuable for applications which involve longitudinal induction, as it eliminates the need of a third pilot wire. Furthermore the device is more effective than the three-winding transformer, when used to counteract induced voltages, as the magnetizing currents flowing through the pilot circuit actually aid the neutralizing action.

By providing grounding capacitors as shown in Figure 6a, a complete excitation path is secured over the pilot wires. The resulting circuit which includes the induced voltage and the neutralizing transformers is termed a "self-exciting neutralizing-transformer circuit," since the exciting current is carried over the pilot wires which are also used for relaying.

The two windings of the neutralizing transformer are wound in the same direction on the core, so that currents which flow in the same direction in the pilot wires encounter exciting impedance, whereas relay loop currents, in opposite directions in the two pilot wires, encounter only the leakage impedance. The exciting impedance of such neutralizing transformers can readily be made as high as 100,000 ohms at 60 cycles with only 160 ohms added to the pilot-wire loop resistance. One-microfarad grounding capacitors, connected one from each wire to ground, have a parallel impedance of 1,325 ohms at 60 cycles and limit the remnant voltage to about 1.3 per cent of one half of the induced voltage. This assumes balanced conditions in which one half of the voltage is impressed across neutralizing transformers at each end of the pilot wires.

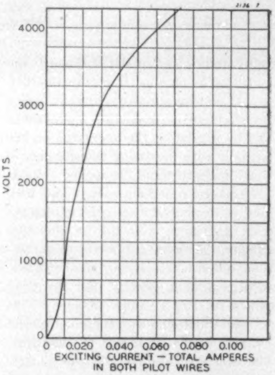


Figure 7. Saturation curve of typical two-winding neutralizing transformer (longitudinal choke coil)

STATION GROUND POTENTIAL

The theory of the two-winding type of neutralizing transformer, as used for neutralization of station ground potentials, may be understood from a consideration of Figure 6. The two pilot wires are considered paralleled, so that the parallel impedances are used. The remnant voltage, e_s, of the terminal relay equipment from station ground is given by

$$e_s = -IZ_c = -E_s \frac{Z_c}{Z_c + Z + Z_p} \tag{2}$$

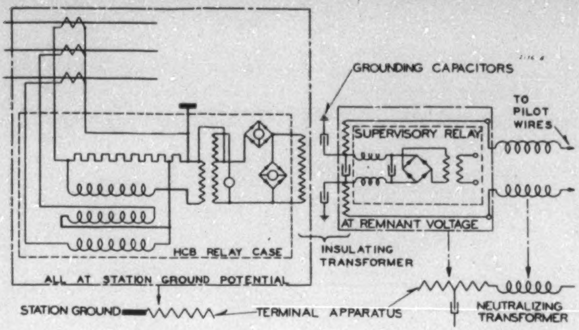


Figure 8. Actual connections

Arrows show parts included in each symbol of the potential-gradient diagrams

The voltage from the pilot wires to the cable sheath is

$$e_p = -IZ_p = -E_s \frac{Z_p}{Z_c+Z+Z_p} \tag{3}$$

In practice, the neutralizing-transformer exciting impedance Z is very large, compared with the other impedances in the circuit. Therefore, the neutralizing transformer has impressed on it approximately the full disturbing voltage, E_s. The corresponding exciting current can be determined by reference to its saturation curve, see Figure 7. The remnant voltage, e_s, is then numerically the product of the exciting current, I, and the impedance, Z_c of the grounding capacitors (see equation 2). Similarly, the voltage from pilot wires to sheath is the exciting current I multiplied by Z_p, defined in Figure 6b (see equation 3). If the distributed capacitance is small, the voltage drop from the pilot wires to the sheath may be high. This can be reduced by adding lumped capacitance ground at d or terminating impedance to ground at the other end of the line.

Figure 7 shows the excitation characteristics of a commercial design of a two-winding neutralizing transformer. At 3,000 volts the exciting current is 30 mils (15 mils in each winding), or the exciting impedance, Z is 100,000 ohms. At 4,000 volts the exciting current is 0.062 ampere. The impedance inserted in the relay loop circuit by this design is $160+j\,130$ ohms, per transformer, at 60-cycle frequency.

LONGITUDINAL INDUCTION

The theory of the two-winding neutralizing transformer, used for protection against longitudinally induced voltages, is given in the appendix. This treatment covers the general case in which both longitudinal induction and station ground-potentials are present. Remnant voltages for the simpler case of longitudinal induction only are obtained using the same equations with the station ground potentials set equal to zero.

The potential distribution is influenced by the relative values of terminating impedances to ground and distributed impedances to ground as well as the locations and amounts of induced voltages. These factors are taken into account in the exact theoretical treatment of the appendix. The important case in which a station ground-potential rise and longitudinal induction are both caused by the same primary fault current is fully developed, the voltage distribution being expressed by equations 5a to 9a of the appendix.

Potential Distribution for Typical Conditions With and Without Neutralizing Transformers

A reasonably accurate conception of the potentials existing in various parts of a pilot-wire circuit that is exposed to station ground potential or longitudinal induced voltage may be obtained from the typical diagrams in Figure 9. The potentials shown are based on 2,000 volts station ground potential or 4,000 volts longitudinal induction as indicated, but can be ratioed for other values of disturbing voltage. Various parts of the circuit are represented by symbols drawn in position to show the voltages to ground at which those parts operate under the abnormal conditions. In this discussion, operating voltages between wires are taken as zero, and the two wires are represented as one for determining voltages to ground. The operating voltages are usually negligible in comparison but can be superposed if appreciable.

Figure 8 illustrates which parts of a typical pilot-wire relay are included in terminal apparatus at "station ground potential," and which part operates at the "remnant voltage" to ground. The insulating transformer presents negligible impedance to pilot-wire currents in the same direction (two-winding neutralizing-transformer exciting currents) which flow to ground. It may, therefore, be considered as operating at the remnant voltage, although physically it is in the circuit between the terminating capacitances and the neutralizing transformer.

Parts (a) to (d) of Figure 9 show the distribution of 2,000 volts station ground potentials. Part (a) is without neutralizing transformers; it illustrates the distribution of voltage that is obtained when an insulating transformer is connected between the relay and pilot wires. Note that the relay is held at station ground potential, and the entire voltage difference is impressed across the transformer insulation. If a supervisory relay were connected directly to the pilot wires, with its base connected to station ground, its insulation would be stressed with the full 2,000 volts rise in ground potential.

If the cable sheath is tied to station ground, the rise in potential of the pilot wires depends on the relative lengths of pilot wires affected by sheath sections at true ground and higher potentials. For example, if one tenth of the length of cable sheath is elevated 2,000 volts and nine tenths is at true ground, the wires will rise to ten per cent of 2,000 volts, or 200 volts above true ground. If only one per cent of the sheath is elevated, the potential rise will be a corresponding proportion of the rise in the short length of sheath. A wire-to-sheath potential approaching the full station ground potential must then be provided for in the cable insulation. This illustrates that voltage stress on the cable can be reduced by insulating the cable sheath from station ground.

The use of two-winding neutralizing transformers is illustrated in parts (b) and (c). This transformer holds the relay parts that are metallically connected to the pilot wires at station ground potential, except for the small remnant voltage, caused by the neutralizing transformer exciting current passing through the capacitance to ground. These remnant voltages are usually under 100 volts. The effects of predominant and negligible pilot-wire distributed capacity are also shown in (b) and (c). The rise in pilot-wire potential indicated in (c), can be reduced by connecting small fixed capacitors between the pilot wires and ground. The circuit then becomes equivalent to that shown in (b). In each case, the effect of connecting the sheath to station ground is shown. The resulting condition is tolerable, if the cable has ample insulation, and its sheath has ample current-carrying capacity. However, the sheath must be insulated from station ground if the cable is not insulated to stand 50 per cent to 100 per cent of the ground potential rise, with a suitable margin.

As shown in part (d), the three-winding neutralizing transformer produces a potential distribution under conditions of station ground voltage rise, substantially the same as for the two-winding neutralizing transformer with predominant distributed capacity.

The distribution of potentials resulting from 4,000 volts of longitudinal induction is illustrated in parts (e), (f), (g), and (h), taking into account the following variables:

With and without neutralizing transformers (two-winding type).

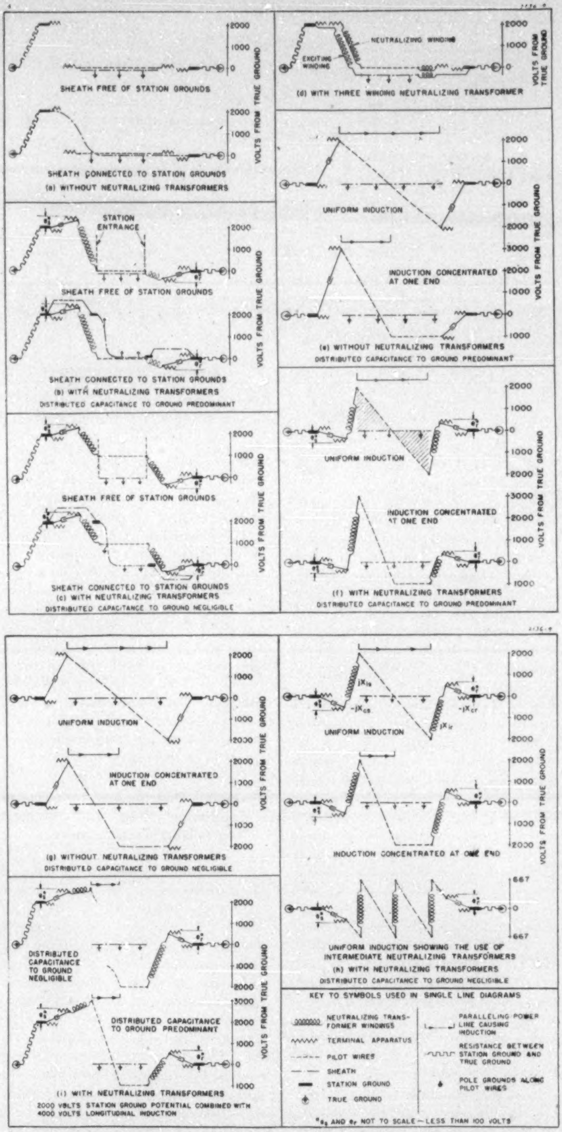
Pilot-wire capacitance predominant or negligible.

Different exposure conditions.

Parts (e) and (f) show that with predominant distributed capacity, introduction of neutralizing transformers leaves the pilot-wire voltages unchanged and simply lowers the potential of any relay equipment metallically joined to the pilot wires, to substantially station ground potential.

The natural distribution of pilot-wire potential determined by distributed capacitance, as explained in the appendix, can be modified by terminating capacitances to ground indicated in Figure 9(c) and (g). The distributions are shown, in part (g), for the case of distributed capacitance negligible compared with these terminating capacitors, and in part (c) for the case of distributed capacitance predominant. Other cases fall in between. The unbalanced voltage distribution which results from heavy induction near one end, when distributed capacitance is predominant, part (c), can be equalized by the addition of terminating capacitances.

As shown in parts (f) and (h), the neutralizing transformers operate to hold the relay equipment substantially at station ground potential except for the remnant voltage. Part (h) of Figure 9 also illustrates the use of a number of neutralizing transformers, connected at intervals in the pilot-wire circuit. This expedient is used to reduce voltages on the cable itself, as neutralizing transformers at the



SHEATH FREE OF STATION GROUNDS
SHEATH CONNECTED TO STATION GROUNDS
(a) WITHOUT NEUTRALIZING TRANSFORMERS
STATION ENTRANCE
(b) WITH NEUTRALIZING TRANSFORMERS
DISTRIBUTED CAPACITANCE TO GROUND PREDOMINANT
(c) WITH NEUTRALIZING TRANSFORMERS
DISTRIBUTED CAPACITANCE TO GROUND NEGLIGIBLE
NEUTRALIZING WINDING
EXCITING WINDING
(d) WITH THREE WINDING NEUTRALIZING TRANSFORMER
UNIFORM INDUCTION
INDUCTION CONCENTRATED AT ONE END
(e) WITHOUT NEUTRALIZING TRANSFORMERS
DISTRIBUTED CAPACITANCE TO GROUND PREDOMINANT
(f) WITH NEUTRALIZING TRANSFORMERS
DISTRIBUTED CAPACITANCE TO GROUND PREDOMINANT
(g) WITHOUT NEUTRALIZING TRANSFORMERS
DISTRIBUTED CAPACITANCE TO GROUND NEGLIGIBLE
UNIFORM INDUCTION SHOWING THE USE OF INTERMEDIATE NEUTRALIZING TRANSFORMERS
(h) WITH NEUTRALIZING TRANSFORMERS
DISTRIBUTED CAPACITANCE TO GROUND NEGLIGIBLE
DISTRIBUTED CAPACITANCE TO GROUND NEGLIGIBLE
DISTRIBUTED CAPACITANCE TO GROUND PREDOMINANT
(i) WITH NEUTRALIZING TRANSFORMERS
2000 VOLTS STATION GROUND POTENTIAL COMBINED WITH 4000 VOLTS LONGITUDINAL INDUCTION
VOLTS FROM TRUE GROUND
2000
1000
0
3000
667
KEY TO SYMBOLS USED IN SINGLE LINE DIAGRAMS
NEUTRALIZING TRANSFORMER WINDINGS
TERMINAL APPARATUS
PILOT WIRES
SHEATH
STATION GROUND
TRUE GROUND
PARALLELING POWER LINE CAUSING INDUCTION
RESISTANCE BETWEEN STATION GROUND AND TRUE GROUND
POLE GROUNDS ALONG PILOT WIRES
e_s AND e_r NOT TO SCALE—LESS THAN 100 VOLTS

Figure 9. Distribution of wire-to-ground voltages caused by station ground potential and longitudinal induction

(a) to (d)—2,000 volts station ground potential

(e) to (h)—4,000 volts longitudinal induction

(i)—Combination of 2,000 volts station ground potential and 4,000 volts longitudinal induction terminals provide adequate protection for the relay equipment.

Part (i) illustrates the potential gradient that is obtained when a combination of station ground potential and longitudinal induction exists. As one of these voltages is produced by resistance coupling and the other by mutual reactance coupling from the same current, the two are 90 degrees out of phase with each other. (The voltage produced by mutual coupling is ahead, if the reference directions are used as indicated in Figure 6, and the pilot wires are more closely coupled to a power phase wire than to the ground return.) As a result the voltage along the pilot wires varies not only in magnitude but also in phase position. Figure 9(i), shows the voltage magnitude to ground, of the principal parts of the circuit, but does not indicate the magnitude of voltage across the neutralizing transformers. The vectorial shift of the voltages along the pilot wires is illustrated in Figure 10.

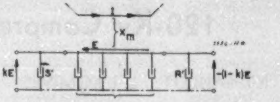
The distribution of voltages that are imposed on pilot-wire circuits and their terminal equipments, by induction and station ground potentials, can be determined by use of the equations and diagrams presented herein. It will be noted that in many instances this can readily be accomplished by first determining the distribution of induced voltages, per Figure 6d, and then adding the rise in ground potential at right angles to it at the correct location in the circuit. The maximum stress on the pilot-wire insulation can be determined in this manner. If relay equipment is connected directly to the pilot-wire circuit, its insulation (and operating characteristics) must be able to withstand such a voltage, or neutralizing transformers must be installed to dissipate it and reduce the voltage stress at the relay to meet operating limitations. After the voltage distribution has been determined, as mentioned above, the effect of neutralizing transformers, or the use of grounding impedances, or of insulating transformers can be determined by simply subtracting the impedance drops from the voltages that are impressed on the circuit.

In general, all parallel wires that enter a cable at a given point should be protected in a similar manner to make certain that all wires within the cable are held at the same potential. A circuit of this kind should be studied, and the necessary protective measures must be taken to avoid the transfer of disturbing voltages from one set of wires to the other.

The paper has reviewed the problem of protecting pilot-wire circuits with emphasis on high-speed relay requirements and has presented mathematical and graphic aids to help the reader visualize it. It has also presented a mathematical analysis of a new tool, the two-winding neutralizing transformer, that is well suited to use in dissipating disturbing voltages that are frequently impressed on pilot-wire circuits. Use of this "tool" and the methods of analysis described should be of value to the users of pilot-wire circuits in avoiding difficulties that have been previously overlooked. The important points to be checked are the adequacy of insulation provided at different points in the circuit, and the voltage that is impressed on the lightning arresters. This latter point is of primary importance on leased circuits that are used with high-speed protective relays. The voltage distribution must be such that lightning protection is never operated by power-system disturbances.

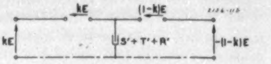
Appendix. General Case of Longitudinal Induction and Ground Potential

In the general case where both station ground potentials and longitudinal induced voltages are present, the voltage distribution can be determined in the following manner: First determine the distribution of the induced voltage in the pilot wires to obtain the value k; this is done with the terminal equipment disconnected. Then complete the circuit to calculate the distribution of voltages in the entire circuit as will be explained.



(a) Actual circuit

Uniform section of distributed capacitance and induced voltage
Total shunt capacitance in this section $= T'$
Total induced voltage $= E$



(b) Circuit equivalent to (a) as far as terminal conditions are concerned

Figure 11. Potential gradient for induction in part of a section

$$k = \frac{2R'+T'}{2(R'+T'+S')}$$

Z_{cp} = Impedance corresponding to $S'+T'+R'$
All capacitances in microfarads

As shown in Figure 6d, the pilot-wire circuit can usually be replaced by a T section composed of two parts of the induced voltage, kE and $(I-k)E$, and the total shunt capacitive impedance of the two wires to ground, Z_{cp}. This assumes that voltage drops in the series impedance of the pilot wires, caused by the shunt capacitive current, are negligible. The value of k can be determined by considering the voltage distribution in the pilot wires with the neutralizing transformers and terminal equipment disconnected. If shunt impedances are all capacitive, either distributed or lumped, the voltage distribution diagram can be constructed as shown. The positive and negative areas A are made equal, since they are proportional to the charging currents entering and leaving the pilot wires, which must be equal. This is most easily visualized by referring to Figure 6d. Note that the abscissa is capacitance, rather than distance to allow for cable or open wire, and lumped capacitance effects. Thus the potential distribution diagram can be readily drawn from the known capacitances and induced voltages by making the areas equal.

One of the most common cases is shown in Figure 11. Uniform induction, of total voltage E, occurs over a section having uniformly distributed capacitance, totaling T' microfarads. The sum of remaining distributed capacitance plus lumped capacitance at one end is S' and at the other end, R'. For this case

$$k = \frac{2R'+T'}{2(R'+T'+S')} \tag{4}$$

After the potential distribution in the pilot wire above has been determined, that is, the value of k fixed, the equivalent diagram can be connected to the terminal equipment as in Figure 6c. A vector solution of the resulting two-mesh network gives the various voltages and exciting currents.

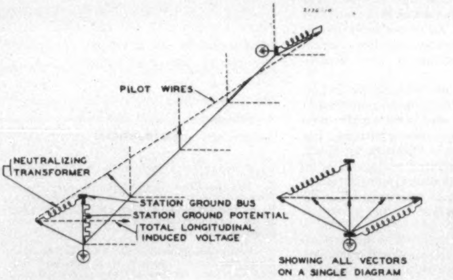


Figure 10. Vector voltage distribution along pilot wire exposed to both station ground potential and longitudinal induction

These are

$$I_s=\frac{-E_rZ_{cp}+Z_{cp}(1-k)E+(Z_{cr}+Z_r+Z_{cp})(kE+E_s)}{(Z_{cs}+Z_s+Z_{cp})(Z_{cr}+Z_{cp}+Z_r)-Z_{cp}^2} \quad (5)$$

$$e_s=Z_{cs}I_s \quad (6)$$

$$e_r=e_s-E-E_s+E_r \quad (7)$$

$$E_{sp}=-E_s+I_s(Z_s+Z_{cs}) \quad (8)$$

$$E_{rp}=E_{sp}-E \quad (9)$$

E_r and E_s = station ground potentials at r and s

E = voltage induced in the pilot wires

Frequently, the shunt capacitance to ground of the pilot wire will be so large that the potential distribution of the pilot wires will be relatively unaffected by the connection of the neutralizing transformers and terminating capacitors, Z_{cs} and Z_{cr}. Test of this condition is whether the neutralizing transformer impedance (approximately 100,000 ohms for a commercial design) is large compared with the capacitive reactance to ground (sheath) of the pilot wires. In this case corresponding to $Z_{cp}=0$, the disturbing voltages can be taken as E_s+kE at the "s" end and $(1-k)E$ at the "r" end. The voltages e_s and e_r can then be determined by multiplying Z_{cs} and Z_{cr} respectively by the neutralizing transformer exciting current (Figure 7) corresponding to these disturbing voltages.

A practical case of considerable importance is that of station ground potential at only one end, combined with longitudinal induction from the same power current. For this case, represented in Figure 6c by $E_r=0$, the longitudinally induced voltage E is 90 degrees ahead of the station ground potential E_s. Thus taking E_s as reference, $(\bar{E}_s=\bar{E}_s)$ equations 5 to 9 reduce to 5a, to 9a, as shown below:

When $E_r=0$ E_s = reference = $\bar{E}_s$ $E=90°$ ahead = $j\bar{E}$

Substituting these quantities in equations 5 to 9.

$$I_s=\frac{jZ_{cp}(1-k)\bar{E}+(Z_{cr}+Z_r+Z_{cp})(jk\bar{E}+E_s)}{(Z_{cp}+Z_s+Z_{cp})(Z_{cr}+Z_{cp}+Z_r)-Z_{cp}^2} \quad (5a)$$

$$e_s=Z_{cs}I_s \quad (6a)$$

$$e_r=e_s-j\bar{E}-\bar{E}_s \quad (7a)$$

$$E_{sp}=\bar{E}_s+I_s(Z_s+Z_{cs}) \quad (8a)$$

$$E_{rp}=E_{sp}-j\bar{E} \quad (9a)$$

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120-Kv Compression-Type Cable

I. T. FAUCETT
FELLOW AIEE

L. I. KOMIVES
ASSOCIATE AIEE

H. W. COLLINS
ASSOCIATE AIEE

R. W. ATKINSON
FELLOW AIEE

THE oil-filled type of high-voltage paper-insulated cable has been used on all extra high-voltage underground installations now in service in this country. While the major portion of these installations operates at oil pressures under 30 pounds per square inch, several of the shorter ones utilize oil pressures up to 200 pounds per square inch ("oilostatic" type). Although an alternate system[1] in which a high gas pressure is applied externally to the lead sheath has been in commercial service abroad since 1932, it was not until recently that this type of cable, known as compression cable, received serious study here. In 1939 the Detroit Edison Company found that a system employing a welded steel pipe line offered a number of advantages over the conventional duct installation under the conditions existing over the route of a projected 120-kv underground circuit. For this reason the companies with which the authors are associated initiated an intensive laboratory and field investigation of the technical characteristics of several systems employing a steel pipe. The present paper describes some of the more interesting results of the technical investigation on the compression cable system.

Essentially the compression-cable system comprises a cable that is in most respects an ordinary solid-type cable encased in a pressure chamber in which a gas pressure of approximately 200 pounds per square inch is applied externally to the lead sheath. In any high stress design it is necessary to eliminate entirely or nullify effectively the two important inherent defects of ordinary solid-type cable:

1. The expansion and contraction of the impregnating compound with the load cycle must be provided for.

2. The small but inevitable gas occlusions within the insulation must be rendered innocuous.

In the low-pressure oil-filled cable the first requirement is taken care of by incorporating an oil-flow channel or channels within the cable and by locating along the route reservoirs having expansible cells and an external gas supply to maintain positive pressure. The second prerequisite is met by employing a design which allows more complete degassing of the oil and saturation of the paper than obtained in ordinary solid-type cable. Several investigators have patented designs in which impermeable expansible members and the associated gas supply are incorporated in the conductor or sheath of the cable itself. Because of economic considerations none of these latter designs has attained commercial recognition. In the compression cable system the cable is made noncircular so that the lead sheath by change of shape may serve as an impermeable expansible member, the action of which is made reversible by external gas pressure. Thus the cable becomes self-compensating in that on increasing load the volumetric increase is accommodated by an increase in the cross-sectional area, while on decreasing load the external gas pressure causes the reverse action to take place, thereby preventing void formation as a result of load changes.

In actual practice the compression cable is made oval in cross section so that the shape is free to change in such a direction as to alter the minor axis at the expense of the major axis and hence alter the cross-sectional area without necessitating any large change in the periphery of the cable. A thin metal binder tape with paper tape over and under it is applied over the lead sheath in order to insure uniform diaphragm action along the sheath. The external gas pressure is secured by placing the cable in a welded steel pipe line filled with nitrogen gas under pressure. By actual field and laboratory experience it has been found that a gas pressure of about 200 pounds per square inch is adequate to maintain a positive internal oil pressure sufficiently high to prevent ionization in those voids

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I. T. FAUCETT is chief engineer, General Cable Corporation, New York, N. Y.; L. I. KOMIVES is engineer, underground lines department, and H. W. COLLINS is assistant to the superintendent of the electrical system, both with Detroit Edison Company, Detroit, Mich.; and R. W. ATKINSON is director of the high-voltage research department, General Cable Corporation, Bayonne, N. J.

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which are inherent in solid-type cable when operated in the usual manner. In consequence of the application of this principle of using high gas pressure in conjunction with an oval-shaped cable and the lead-sheath separator, it is possible to operate ordinary solid-type insulation at voltage stresses and conductor temperatures comparable with those used on the oil-filled type of cable.

Experience Abroad

The first commercial circuit of compression cable was installed in England in 1932. The cable used in this initial 66-kv installation was a three-conductor type *HSO* design, that is, a conventional three-conductor type *H* cable with round conductors and binder tape and a triangular-shaped sheath, the latter being obtained by omitting the major portion of the filler material. In order to distribute the diaphragm action as uniformly as possible over the flat sides of the triangular-shaped sheath, a metal binder tape with an asphalted paper tape over and under it, was applied over the sheath. A rectangular steel wire armor applied with a long lay completed the makeup. The armor wires were considered necessary in order to permit the pulling in of a long length of cable without injury. It is the practice abroad to allow these armor wires to take the entire strain of the pull.

While from an electrical point of view this installation has operated satisfactorily since being placed in service, yet on the basis of subsequent experience and laboratory tests the use of the type *HSO* construction has been discontinued abroad and another construction known as type *HSL* has been adopted, because it is easier to manufacture and lends itself better to diaphragm action of the lead sheath. In the latter design three oval-shaped conductors, each separately insulated, leaded and reinforced with several metal tapes, are cabled together with a short lay and armored with steel tape.

In addition to the two designs mentioned, both of which are installed in a steel pipe, a third type known as the self-contained compression cable has been developed abroad. In this design the function of the steel pipe is accomplished by an outer lead sheath which is separated from the diaphragm sheath by a spacer wire to provide a gas channel and is suitably reinforced to withstand high gas pressure. It was at first considered that this design might find some application for duct systems in this country. However, to protect the metallic reinforcing tapes against mechanical damage and corrosion, a third lead sheath would be desirable. With the large conductor sizes commonly employed here, the resulting sheath losses, which cannot easily be prevented, would be excessive. In view of these facts, this type cannot be justified economically.

At the end of 1940 there were over 55 conductor miles of compression cable in service abroad operating at voltages ranging from 50 to 120 kv. Operating experience on these installations has been entirely satisfactory from an electrical standpoint. Electrolytic corrosion of the pipe was experienced on the first installation in England as the result of poor pipe coating, and mechanical trouble near the joint wipes was encountered on a 50-kv installation in Copenhagen because of inadequate provisions for the differential longitudinal movement between cable and pipe.

Because of the difficulty of obtaining precise information on all phases of the foreign installations, and the hazard involved in extrapolating foreign experience to relatively large loads and consequently large conductor sizes normally used in this country, it seemed advisable to recheck fully the mechanical features of the system in the laboratory and in the field by means of an experimental installation.

Preliminary Studies

DIAPHRAGM ACTION

It was first essential to determine whether the lead sheath could withstand indefinitely the diaphragm action resulting from temperature changes occurring during normal operation of cable. Accordingly extensive laboratory tests were made on pure lead sheath simulating the normal conditions of operation.

It is reasonable to assume that the maximum daily temperature change would be approximately 20 degrees centigrade, which would correspond to a volume change of one-half of one per cent. This about equals the volume difference between an oval-shaped sheath in which the ratio of the minor to major axis is 0.9 and a circular sheath of the same perimeter. Assuming the maximum seasonal change to be 80 degrees centigrade, a volume change of two per cent would occur. To obtain the desired information on the mechanical performance of the lead sheath it was necessary to subject the samples to changes in volume corresponding to both these daily and annual changes. The two per cent volume change would occur once for every 365 of the one-half of one per cent volume change cycles. The test program adopted for most of the measurements consisted in subjecting the samples to one two per cent volume change and 300 one-half of one per cent volume changes in a 24-hour period. This accelerated laboratory test program subjected samples in a single day to the equivalent of approximately one year's diaphram action under normal operation.

Samples of compression cable 32 inches long were used for the tests. The diaphragm action or change of volume was

Table I. Tests of Diaphragm Action of Lead Sheath

Sample Number	External Pressure Medium	Internal Pressure Medium	Type of Cycle	Temp.	Lead Thick. Mils	Origin of Cracks	No. of Cycles to Failure
M-8	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,900
O-9	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	5,800
O-10	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,500
M-9	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	5,000
M-10	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,100
N-*a*	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,460
P-1*a*	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,160
O-11	*C*	*A*	½–2% 4.8 min.	Room	90	Outside	4,460
O-12	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	4,100
M-11	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	3,100
N-1	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	3,450
P-2*a*	*C*	*A*	½–2% 4.8 min.	60 C	90	Outside	3,500
P-4	*C*	*A*	½–2% 20 min.	Room	90	Outside	3,100
Q-1	*C*	*A*	½–2% 4.8 min.	Room	80	Outside	3,000
Q-2	*C*	*A*	½–2% 4.8 min.	Room	80	Outside	3,200
Q-3	*C*	*A*	½–2% 4.8 min.	Room	80	Outside	3,250
O-13	*B*	*A*	½–2% 4.8 min.	Room	90	Inside	11,500
O-14	*B*	*A*	½–2% 4.8 min.	Room	90	Inside	11,800
O-15	*B*	*A*	½–2% 4.8 min.	Room	90	Inside	13,000
O-18	*B*	*B*	½–2% 4.8 min.	Room	90	Both sides	24,000
O-19	*B*	*B*	½–2% 4.0 min.	Room	90	Both sides	15,240
P-4*a*	*D*	*B*	½–2% 4.0 min.	Room	90	Both sides	17,000

A—Oil initially degassed, but becoming slowly contaminated with air.

B—Degassed oil protected by Sylphon diaphragms from air contamination.

C—Compressed air.

D—Nitrogen (water-pumped).

produced by causing pressure differentials between the inside and outside of the cable sheath. At the start of the tests oil, not highly degassed, was used as a pressure medium on the inside of the sheath and compressed air was used directly on the outside. The first eight samples tested on the cycle described gave a life ranging from 4,100 to 5,800 cycles (number of one-half of one per cent volume cycles) at room temperature. Examination of these samples after failure showed evidence that the incipient cracks which occurred had originated on the outside surface of the sheath. Since that surface was in direct contact with the compressed air it seemed likely that oxidation may have influenced the results. Subsequent tests were made in which carefully degassed oil was used as an external pressure medium. This change immediately resulted in a life ranging from 11,500 to 13,000 cycles. Further precautions to insure carefully degassed oil on the inside as well as the outside of the sheath resulted in a life ranging from 15,240 to 24,000 cycles, that is, a life approximately four times as great as that obtained when no precautions against oxidation of the sheath had been taken.

Table I gives the results of laboratory tests on five different kinds of samples (*M*, *N*, *O*, *P*, *Q*) each representing some change in either sheath thickness, tension of paper tapes during manufacture, or in degree of looseness of armor tapes. In general these structural differences had no significant effect on the life of the sheath.

Samples tested with air as a pressure medium show that thinner sheaths and higher temperatures decrease the ability to withstand diaphragm action. This may be attributed to the effect of oxidation.

The test conditions differed from service conditions in two important respects, namely:

(*a*). That each cycle is carried out in about $^1/_{300}$ the time that would occur in practice.

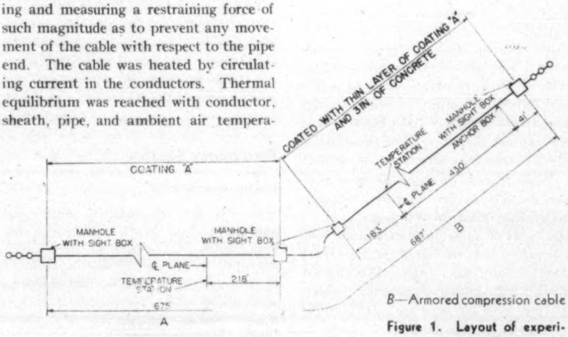
(*b*). That the volume ranges in the test cycles correspond to load conditions which are possible in service but are not normal for most installations.

In both of these respects, the test conditions are the more severe. The stresses required to produce the same amount of deformation in the sheath are very much less for slowly applied deformation. For a given deformation where the lead is not exposed to oxidation as under service conditions, a greater number of cycles is required to produce failure when the period is long than when it is short. In view of this and the fact that the number of full volume daily cycles per year will generally be less than 300, the approximate 50 years represented by 15,000 cycles is a conservative estimate for the life of the lead sheath.

LONGITUDINAL MOVEMENT

The usual practice for armored compression cable is to anchor the joints; however, it was felt that if the armor were eliminated, and the three conductors cabled with a comparatively short lay, the longitudinal movement would be restrained without undue stress on the cable or joints. The following is a brief account of the tests designed to measure the magnitude of such restraining forces.

The three conductors of a 50-foot section of 600,000-circular-mil unarmored compression cable were cabled to a six-foot lay, but not bound together, and then pulled into a six-inch inside diameter iron pipe of the same length. At one end the cable was anchored to the pipe, at the other end means were provided for applying and measuring a restraining force of such magnitude as to prevent any movement of the cable with respect to the pipe end. The cable was heated by circulating current in the conductors. Thermal equilibrium was reached with conductor, sheath, pipe, and ambient air temperatures of 83, 67, 43 and 23 degrees centigrade, respectively. The maximum restraining force required to prevent longitudinal movement during the thermal transition period was 475 pounds, which dropped to 350 pounds when the steady-state temperatures were reached.



A—Unarmored compression cable

B—Armored compression cable

Figure 1. Layout of experimental installation

In view of the very small forces developed by the cabled conductors, a similar test was made on the same type of cable, in which the conductors were not cabled or bound together in any manner. The final restraining force was 3,000 pounds, with a maximum of 3,500 pounds reached during the transition period, for a final conductor temperature rise of 66 degrees centigrade.

The cable sample above described was then bound by hand with heavy duck tape and tested. The maximum force reached was 4,500 pounds for a final conductor temperature rise of 59 degrees centigrade.

These test results show that when the three conductors of a compression cable are cabled together in a lay of about six feet, but not bound, the longitudinal forces due to restrained thermal expansion are so low that no expansion bends are necessary. This was further verified by the field tests. On the other hand, if the conductors are bound together, either straight or twisted, or kept straight without binding, the forces developed are large enough to endanger joints of usual design.

OUTER COVERINGS

A test was made to determine the ability of duck tape, applied over the outer lead-sheath reinforcing tapes of an unarmored compression cable, to withstand the friction incident to pulling this type of cable into steel pipe. It was observed from laboratory tests that very thin tape was damaged in moving through the pipe even in a short distance of 50 feet. One of the samples used for test was covered with a 20-mil duck tape with salvage. This sample was pulled in and out of the pipe until parts of the sample had been subjected to the same amount of abrasion as would have been received if the cable had been pulled through a 2,000-foot section of pipe. Examination of the sample showed that the heavy duck tape was uninjured.

Experimental Installation

GENERAL LAYOUT

The experimental circuit of the compression cable, shown in Figure 1, con-

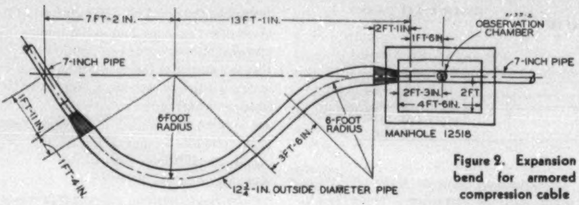


Figure 2. Expansion bend for armored compression cable

sisted of two cable sections each about 700 feet long, one armored and one unarmored, installed in a welded steel pipe. The two sections were connected by means of a joint, and their free ends were provided with terminals for parallel connection with an existing 120-kv overhead line. Suitable equipment was provided for artificially loading the cable by circulating current through the two cable sections with the overhead line providing the return path.

The Pipe Line

Electrically welded steel pipe having an outside diameter of seven inches and shipped in double random lengths was used for this installation. The ends of each length were expanded and beveled for use with chill rings. In the field gas welding was used, and the welds were tested at 500 pounds per square inch air pressure.

The terminal ends were housed in $3^5/_8$-inch outside diameter 0.120-inch wall copper pipes which were fitted and wiped into the trifurcating heads on the ends of the steel pipe line.

The pipe system was provided with nitrogen feeding equipment consisting of a standard cylinder of oil-pumped nitrogen and a gas-pressure regulator with pressure gauges. The line was equipped with a low gas-pressure alarm set for a pressure of 150 pounds per square inch.

The Expansion Bend

An expansion bend was installed near the end of the armored cable in order to protect the joint between the armored and unarmored compression cables from the longitudinal thrust exerted by the armored cable. Figure 2 shows the expansion bend used in this installation, which was made of 12-inch steel pipe with reducers on both ends. The usual practice abroad is to use an expansion bend on each side of the joint.

Corrosion Protection Coverings

The pipe on the unarmored compression cable was given a coating of a material used extensively for pipe covering. This coating, designated as coating *A*, consisted of: a hot application of wax, a spiral wrapping of reinforced asbestos, a hot application of a service coat incorporating natural asphalt, a spiral layer of a single membrane wrapper, and a spiral wrapping of heavy kraft paper. The wax was an adherent coating, chemically and mechanically fortified.

On the armored section concrete was applied around the pipe in the trench to a minimum thickness of three inches.

Cable Design

The armored cable was of conventional design as used abroad. The three 650,000-circular-mil Compack oval conductors were shielded and insulated with 500 mils of impregnated paper. The insulation on each conductor was shielded and covered with 85 mils of lead. There was applied over each sheath three paper tapes, two reinforcing bronze tapes, and two saturated cotton tapes. This assembly was cabled with saturated jute fillers and covered with a heavy saturated duck tape, jute bedding and galvanized steel-wire armor.

The individual legs of the unarmored cable were of the same construction as those in the armored cable, with the addition of two 30-mil presaturated canvas tapes. These legs were cabled without fillers or further finish. Figure 3 shows a sectional view of the unarmored compression cable installed in a steel pipe having an outer protective covering.

Anchoring of the Cables

The armored compression cable was anchored at two points:

1. At the joint with the unarmored cable, to prevent the joint from being moved by the expansion of the cable. Due to proximity of the expansion bend no large forces were involved.

2. At a point approximately 40 feet from the terminal end the armor wires were so anchored as to withstand large longitudinal forces in either direction. The expansion occurring in the 40-foot section was calculated to be negligible and would be taken up by the bend of the cables in the riser pipes. Therefore, at the joint between the terminal ends and the armored cable the armor wires were simply bound in place.

With the above arrangement of anchorage, a longitudinal movement roughly equal to that obtained in a 1,400-foot-long armored compression cable will take place in the direction of the expansion bend and joint between the armored and unarmored compression cables. To observe this movement a removable sight glass with reference mark was installed in the pipe near the end of the expansion bend as well as at other points in the line where such observation would be of use. The cable under each sight glass was equipped with a graduated scale which was fitted in proper position after the cable was placed in its final location.

Normal Joints

Figure 4 shows the normal joint used between the armored and unarmored sections of cable. Figure 5 shows the detailed design for each phase.

The design of the normal joint is of particular interest, because it achieves the connecting of two sections of cable without any appreciable structural change at the point of joining. The design results in a joint that is simple, reliable, and of very small diameter, yet similar in construction to those used on high-voltage single-conductor solid-type cables. Very little schooling is therefore necessary for splicers well-versed in solid-type cable jointing to enable them to construct this joint. Diaphragm action takes place along the joint in a manner similar to that of the cable.

Soldered-type countersunk copper connectors were used with an outside dimension the same as that of the cable conductor. Semiconducting tapes were used to shield the connectors and applied so as to make good contact with the conductor shielding tapes. Long pencils

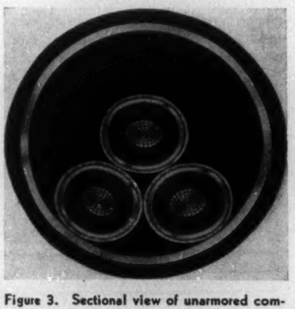


Figure 3. Sectional view of unarmored compression cable installed in steel pipe having an outer protective covering

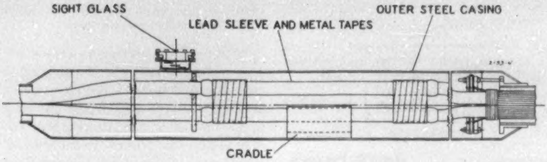


Figure 4. Normal joint between armored and unarmored sections of compression cable

were made on the factory insulation by tearing each tape individually at points one-eighth inch apart. Impregnated-paper tapes were tightly applied, butt-lapped, and wrapped in the same direction, until the thickness of the hand-applied insulation exceeded that of the factory insulation by 20 per cent. Shielding braid was applied over the insulation. The diameter was so made that it required considerable force to draw the oval sleeve over the joint core. The joint core was coated with cable oil before forcing the sleeve in place. This made the sleeve over the joint insulation fit as snugly as the regular sheath over the factory-applied insulation. The sleeve was then beaten down at the ends and wiped to the cable sheath. It was then reinforced with brass tapes similar to the reinforcing on the cable proper. After the individual joints were completed, they were tied together, the steel joint casing pulled over and welded in place.

Laboratory tests had been made previously on two joints of this design. The man making these joints had had no previous experience in making paper-taped joints other than a single similar practice joint. The voltage program as specified in the Association of Edison Illuminating Companies Specification for Oil-Filled Cable was used with the time reduced for the first step to $1^1/_2$ hours, and the 20 per cent increase in voltage every three hours obtained in small steps every ten minutes instead of one large step at three-hour intervals. Breakdown voltages, expressed in volts per mil of cable insulation, of 436 and 445 were obtained. In both joints the breakdown was radial and over the connector; there were no other signs of stressing. These results compared favorably with values obtained on standard normal oil-filled cable joints of this voltage class. These laboratory values are probably very conservative as compared with what might be expected from the field joints where a more nearly circular connector was used, and where the tapes were applied with greater tightness by experienced splicers.

Terminals

Figure 6 shows the design of the terminals used on the experimental line. These terminals have a brass-fitted bakelite stop-tube assembly of sufficient strength to withstand the high internal operating pressure of the cable oil. This stop-tube assembly ends in a stem at the upper end and a flange at the lower end. The porcelain insulator, with a barrier assembly and stress cone, metal rings at top and bottom, and an insulator cap, is placed over the stop tube and bolted to the flange. The space between the stop tube and the porcelain is filled with oil. The terminal is essentially the same design as that used for oil-filled cable with the addition of the stop tube.

Compensators

Compensators were used in order to allow for the expansion and contraction of the impregnating oil contained in the rigid stop tubes of the terminals, and thereby prevent overworking the sheath near the ends of the compression cable. The compensator may be described as a flexible membrane in a housing which is capable of withstanding 200 pounds per square inch pressure. The membrane separates the housing into two chambers; one containing oil is in direct communication with the inside of the stop tube, and the other containing nitrogen is connected to the pipe line. Expansion of the oil in the stop tube and cable due to increasing temperature is accommodated by the movement of the membrane which is spring-loaded to provide a characteristic similar to that of the cable sheath. During cooling the process is reversed, and thus complete impregnation of the terminal at all times without appreciable interchange of oil between the terminal and cable is insured. One compensator is connected to each terminal.

Installation

Methods usually employed for pulling cables into ducts were readily adapted to the installation of the compression cable in the steel pipe. At the start of the installation of the armored compression cable, high pulling stresses (16,000 pounds) were experienced on the pulling line when it passed around the expansion bend located at the pulling end of the section. As soon as the armored cable started to pass through the expansion bend, the pulling stresses on the line became normal.

A maximum pull of 7,800 pounds was experienced during the installation of the unarmored cable. This corresponds to a coefficient of friction of about 0.54. For future installation of unarmored compression cable where an even lower pulling stress is desired, a half-round copper wire wound over the fabric tapes of the individual cables can be employed.

In order to determine the effect on a

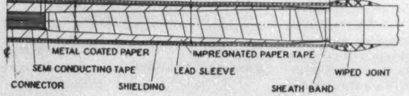


Figure 5. Detail for one conductor of the normal joint

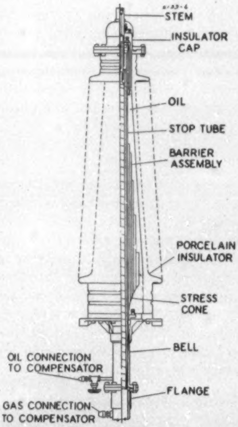


Figure 6. Terminal for 120-kv compression cable

compression cable of nitrogen coming in contact with the insulation, the lead sheath of one phase of the unarmored cable was perforated with holes approximately one-eighth inch in diameter at 25-foot intervals throughout its length.

Before the cables were pulled into their respective pipe sections, the piping was dried with hot air. After the cable was installed the pipe was evacuated and then filled with nitrogen at a low positive pressure. With the help of temporary seals, this nitrogen was kept in the pipes until the cable-splicing operations were completed, at which time the pipe was filled with nitrogen gas at a pressure of approximately 200 pounds per square inch. The nitrogen used was oil-pumped, guaranteed to contain less than 0.3 per cent oxygen and 0.03 per cent water.

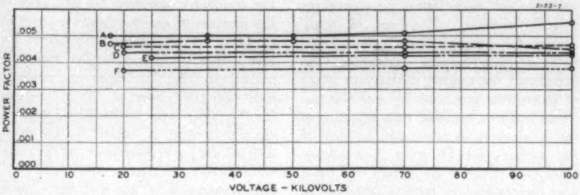


Figure 7. Curves of power factor versus voltage

Curve	Date (1941)	Cable Temperature (Degrees Centigrade)
A	March 29	2.6
B	May 7	14.0
C	May 17	14.8
D	May 27	19.2
E	July 2	20.9
F	August 6	23.5

TESTS ON THE CABLE BEFORE INSTALLATION

As the compression cable before installation is essentially a standard solid-type cable, routine factory acceptance tests, usually performed on solid-type cable of similar insulation thickness, were considered adequate for detection of nonuniformities. The impulse strength of compression cables had been determined by previous tests,[2] and calculations showed that this cable would fail at a voltage in the neighborhood of 1,100 kv. A sample of compression cable used in this installation withstood without failure impulse voltages up to 900 kv (the capacity of the generator). This is well above the 138-kv insulation level.

Field Tests

After the installation had been completed the cable was subjected to repeated load cycles, and periodic measurements were made of the power factor and movement of the cable within the pipe.

During the time that the cyclic loading tests were in progress (a little over five months), the cable line was subjected to 54 load cycles with load on for $2^1/_2$ hours and load off for $2^1/_2$ hours, and 249 load cycles with load on for 4 hours and load off for 4 hours. In addition, a number of cycles was used in which the load was applied for durations ranging from approximately 13 hours to 190 hours. During the majority of the load cycles, the current ranged from 500 to 550 amperes (or 10 to 20 per cent above the nominal 95,000-kva rating of the line); during others, particularly those of the longer duration, the currents ranged from 460 to 510 amperes.

The first few heating cycles produced a movement of seven-eighths inch in the armored cable at the entrance to the expansion bend over 613 feet of cable and a slight initial adjustment of position of the unarmored cable. Subsequent heating and cooling cycles produced a cumulative $3^3/_4$-inch movement of the armored cable and no appreciable movement (one-eighth inch) of the unarmored section. This indicated that without armoring, the cables would absorb the longitudinal movement, thus making expansion bends unnecessary. During the recent cold season and without load, the armored cable has returned to its original position.

Power-factor measurements were made on each conductor of the two connected sections at voltages from 20 kv to 100 kv to ground. An initial set of measurements was made before starting the load cycles, and subsequent measurements made at intervals during the period that the cable was under test. It was not practical to obtain the power factor of the test cable at elevated temperatures due to the relatively long time necessary to switch the test cable out of the circuit and connect up the power-factor measuring equipment. The measurements were made after the cable had been de-energized for a period of 15 hours or more.

These power-factor measurements as obtained on one of the conductors are shown in Figure 7. There was no significant difference in the measured values for the different conductors. It will be seen that there is a slight decrease in power factor in the later readings which were at slightly higher temperatures than the initial set.

No noticeable difference in power factor was apparent in the phase with holes in the sheath as compared to the other two phases. However, this cable has not been operating long enough to prove this point definitely.

Summary

1. Methods normally used for pulling cables into ducts can readily be adapted to the installation of compression cable in steel pipe.
2. Conditions in this country make it possible to eliminate the armor which is used on compression cable abroad.
3. By cabling the individual lead-covered and reinforced conductors with a suitable lay, longitudinal expansion becomes of no practical importance, and expansion bends as used abroad can be eliminated.
4. Diaphragm action of the lead sheath when in nitrogen shows indefinite life under simulated operating conditions.
5. Compression cable is well suited for handling loads in the range of 100,000 kva.
6. The experimental installation which has been installed for approximately one year shows no deterioration of the insulation even on the phase with the lead sheath perforated every 25 feet.

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120-Kv High-Pressure Gas-Filled Cable

I. T. FAUCETT FELLOW AIEE
L. I. KOMIVES ASSOCIATE AIEE
H. W. COLLINS ASSOCIAEE AIEE
R. W. ATKINSON FELLOW AIEE

THIS paper deals with the theory, manufacture, and testing of high-pressure gas-filled cable and describes an experimental installation by the Detroit Edison Company in co-operation with the General Cable Corporation and a subsequent commercial installation on the Detroit Edison system.

While high-pressure gas-filled cable has been in successful operation in England, these are the first experimental and commercial installations in the world of this type cable in a steel pipe. The installation of such cable in a steel pipe involves the removal of the lead sheath in the field, thus exposing the insulation to the atmosphere and to the possibility of mechanical injury during installation. Since the effect of such conditions is difficult to determine in the laboratory, the experimental installation was made.

The commercial line was subsequently installed on the Detroit Edison System and put in operation on December 31, 1941. This line is seven miles long and designed to transmit 95,000 kva continuously at 120 kv.

Experience Abroad

High-pressure gas-filled cable has been in successful operation in England since 1937 at which time installations of cable of this type were made for operation at 33 kv and 132 kv. Today there are approximately 25 conductor miles in successful operation at voltages ranging from 33 kv to 132 kv and at pressures from 50 to 225 pounds per square inch. These cables[1] are equipped with lead sheaths reinforced so as to withstand the operating gas pressure.

The high-pressure gas-filled cable makes effective use of the well-known fact that the dielectric strength of gas increases very greatly with pressure. The great gain possible by the use of high gas pressure in cable insulation has long been recognized. However, it was not until a more complete knowledge of other facts became available that effective use could be made of this principle.

The low permittivity and dielectric loss of unsaturated paper made the use of this dielectric seem most promising. Many of the expected results were realized in cables made in this way; dielectric losses were very low, and there was no ionization at voltages well above operating voltage. However, the short-time and impulse breakdown voltages were very low. Since there was no measurable ionization at voltages just below failure it seems that complete breakdown in such a cable follows promptly after the smallest amount of ionization.

An entirely satisfactory solution of the problem has been found by combining several features. The use of high-density low-porosity paper presaturated with a high-viscosity compound has eliminated the quickly destructive effect of ionization produced by overvoltage. These features, together with shielding of the conductors as advocated by two of the authors for all very high-voltage cable,[2] and with the use of very thin tapes near the conductors, have resulted in obtaining an insulation entirely free from ionization up to about twice operating voltage and a dielectric strength (short-time or long-time) in the same range as for oil-filled insulation. Perfection of methods of drying and saturation, the use of a saturant of very low loss, and the prevention of moisture absorption during taping have also resulted in extremely low dielectric loss and a high degree of uniformity throughout the thickness of the insulation.

Two different types of saturant have been used abroad. In the earlier ones, the saturant contained a very large percentage of rosin and had a very high viscosity. However, it also had a high power factor and for this reason was not considered entirely satisfactory. In later cables the saturant has been petrolatum. This has a low loss and does not migrate at a temperature below its melting point of 50 or 55 degrees centigrade, but does become very fluid at temperatures at which cables may operate in this country. Thus neither of these saturants was entirely satisfactory particularly for a pipe cable installation. The saturant used in the Detroit cables has a very high viscosity, possesses extraordinarily good stability, and gives a cable of as low dielectric loss as for the best of modern power cables. The good results obtained on these cables are attributable in no small part to the development and use of this saturant.

The value of the very thin tapes and of the conductor shielding lies primarily in the increase of specific dielectric strength of gas in very thin films. Because of the lower dielectric stress farther from the conductor, the thin tapes are required only near the conductor. Thus by grading the paper-tape thickness, the same dielectric strength is secured for the cable as if all the paper were thin; at the same time there is secured greater freedom in the penetration of gas and more rapid equalization of the pressure through the insulation wall. Moreover, the resulting cable withstands without damage the normal bending in manufacture and installation, as it could not do so fully if made entirely of the thinnest paper.

Nitrogen has been selected as the pressure medium, because of its good dielectric properties, its complete inertness with respect to the materials used in the cable, and its commercial availability with low moisture and oxygen content.

The SMD Type Cable

The previous paragraphs dealt with the basic principles behind the high-pressure gas-filled cable and apply to this type cable as manufactured in England and to the pipe type of cable manufactured in this country and hereafter designated as *SMD* type. The difference in construction is in the outer enclosure. Abroad a reinforced lead sheath is used over the insulated conductors to provide this gas-tight enclosure. An additional lead sheath is usually employed to protect the reinforcements. The *SMD* type of high-pressure gas-filled cable retains the essential insulation of the reinforced lead-sheath type but does away with the permanent lead sheaths as used abroad. All three phases of even the largest-size cables are installed in one common pressure chamber. This pressure chamber is a steel pipe which is in itself gas-tight, and which is designed to withstand the required pressure. A lead sheath which

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I. T. Faucett is chief engineer, General Cable Corporation, New York, N. Y.; L. I. Komives is engineer, underground lines department, and H. W. Collins is assistant to the superintendent of the electrical system, both with Detroit Edison Company, Detroit, Mich.; and R. W. Atkinson is director of the high-voltage research department, General Cable Corporation, Bayonne, N. J.

The authors wish to acknowledge the contributions of S. M. Dean, Detroit Edison Company, who suggested the use of a steel pipe as the pressure container in this installation, instead of the customary reinforced lead sheath. They are indebted for the valuable assistance given by their other associates in the Detroit Edison Company and the General Cable Corporation, and wish especially to thank W. S. Brown, F. G. Cox, R. C. Fellows, H. G. Hall, E. Johansson, L. Meyerhoff, J. D. Piper, E. Richman, W. D. Sanderson, G. J. Shurts, and J. Sticher.

serves as a protective covering during shipment and storage is removed as the cables are installed.

While the insulation of the *SMD* type cable is the same as for the reinforced sheath type, the removal of the lead sheath requires that a covering be placed over the insulation for protection during installation. In the cable used in the experimental line this was accomplished by the application of a strong canvas tape over the insulation shielding tapes. The cable for the commercial line was manufactured with a half-round copper wire over the canvas tape to increase the mechanical protection and reduce the coefficient of friction between the cable and pipe.

Since the installation of this type of cable requires the removal of the lead sheath in the field, the exposure of the insulation to the atmosphere, and the possibility of mechanical injury during installation, it was felt that an experimental installation was essential to demonstrate the practicability of installing this type of cable in a commercial line.

While the superiority of insulation made with graded tapes of five mils and less was recognized, the question of whether or not an insulation built from papers of standard thickness, five mils or over, would be satisfactory was raised. To obtain an answer to this question, it was decided to employ all five-mil tapes in the *SMD* type cable used in the experimental line. The five-mil thickness was used to facilitate taping and to depart as little as possible from the standard practice used in the manufacture of power cables. In the cables built subsequently for the commercial *SMD* line, grading of paper thickness was employed, thus conforming to the practice abroad.

Manufacture

The cable in the commercial installation was of the following construction: single conductor 600,000 circular mils concentric round strand, 600 mils preimpregnated wood-pulp paper of graded thickness from $2^1/_2$ to 5 mils, two metal-faced paper tapes applied next to the conductor, and two metal-faced tapes over the insulation (all metal-faced tapes included in the 600-mil wall thickness), bronze shielding tape intercalated with a saturated muslin tape, one 20-mil paraffin saturated tape, one half-round 200- by 100-mil copper wire applied in open spiral, and a 94-mil temporary lead sheath. Figure 1 shows a sectional view of this cable installed in the steel pipe having an outer protective covering.

Before being applied to the cable, the paper tapes were dried and saturated with an oil having a viscosity of 3,000 seconds Saybolt Universal (50 centipoises) at 100 degrees centigrade. The preimpregnated-paper tapes were wrapped on the conductor in a standard taping machine and in the usual manner, except that the entire machine was enclosed in a room where in the relative humidity was maintained below 20 per cent regardless of outside atmospheric conditions. This humidity control, together with precautions taken during impregnating, handling, and storing, greatly reduced the moisture content in the finished cable.

The cable for the experimental installation differed from the above only in the omission of the half-round copper wire, the use of five-mil paper tapes throughout, and the application of paper tapes without humidity control.

Specification

SMD type cable may be tested in accordance with the Association of Edison Illuminating Companies Specification for Oil-Filled Cable, provided that during the electrical tests an internal nitrogen pressure of 30 pounds per square inch is maintained on commercial lengths and 225 pounds per square inch on samples. The temporary lead sheath on the commercial lengths should, of course, be of sufficient strength to withstand this pressure.

Experimental Installation

The experimental circuit of *SMD* type cable consisted of one section of cable of approximately 700 feet in length with normal joints at each end connecting to the terminal end cables. Terminals were provided at each end for connection with an existing 120-kv overhead line. The cable was installed in a seven-inch-outside-diameter welded steel pipe having a wall of 0.231 inch and filled with nitrogen gas at a pressure of approximately 200 pounds per square inch. The pipe section housing the cable was provided with manholes at each end at which points sight boxes were provided for observing cable movement.

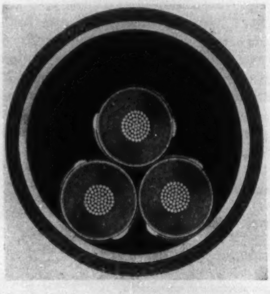


Figure 1. Sectional view of *SMD* type cable installed in a protective-coated steel pipe

Two different types of protective coverings were applied to the pipe. About half of this 700-foot section was covered with a coating of plasticized coal-tar enamel. Joints in the pipe were hand-coated with the same material. The balance of this section of pipe was covered with Somastic. This material consists of a primer coat over which was extruded, hot, a half inch of asphalt mastic material.

The joints and terminals were of the same general design as used on the commercial line and are described hereafter.

Since it was desirable not to expose the insulation to the atmosphere any longer than necessary, a mechanical stripping device was designed which folded the lead sheath back as the cable was pulled into the pipe. V-shaped grooves were made in the lead sheath during the lead covering process in order to facilitate stripping. It was found, however, that scoring of the lead sheath by hand in the field was still necessary, and that mechanical stripping increased the pulling stresses considerably. It was thus unnecessary to score the lead at the factory, as scoring in the field was all that was required to facilitate this operation.

Field Tests

Measurements were made after the installation had been completed in order to determine what effect cyclic loading of the cable would have on the power factor of the insulation and what movement of the conductors would take place.

The cable which was connected to the 120-kv overhead line was subjected to load cycles by the circulation of current through the conductor with the overhead line acting as the return path. During the first 54 cycles the load was on for $2^1/_2$ hours and off for $2^1/_2$ hours. This was followed by 249 cycles of longer duration with the load on for 4 hours and off for 4 hours. In addition there were a number of cycles in which the load was applied for durations ranging from 13 to 190 hours. For the majority of the load cycles the current ranged from 500 to 550 amperes (or 10 to 20 per cent above the nominal 95,000-kva rating of the line). The maximum change in copper temperature

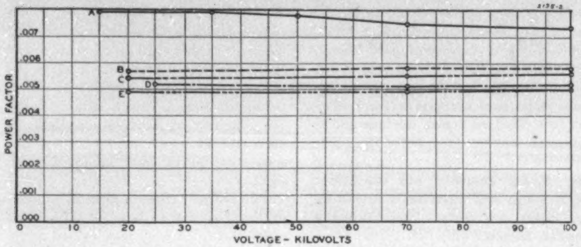


Figure 2. Curves of power factor versus voltage for experimental installation

Curve	Date (1941)	Cable Temperature (Degrees Centigrade)
A	March 29	2.6
B	May 17	19.6
C	May 27	22.3
D	July 2	25.8
E	August 6	27.7

that occurred during this period of loading was 58 degrees centigrade.

The above load cycles produced only a slight initial adjustment in the position of the cable, and no further movement of the cable occurred thereafter. This indicates that there is sufficient space in the pipe to absorb the longitudinal thermal expansion.

Power-factor measurements were made on each of the three conductors at voltages from 20 kv to 100 kv to ground. An initial set of measurements was made before loading and repeated at intervals during the period that the cable was being subjected to the loading cycles. The measurements were made after the load had been removed from the cable for a period of 15 hours or more. It was not feasible to obtain power-factor measurements at elevated temperatures, since it required a relatively long time to switch the experimental cable out of the circuit and connect up the power-factor measuring equipment.

The power-factor data as obtained on one of the conductors are shown in Figure 2. There was no significant difference in the measured values for the different conductors. It will be noted that the later power-factor values are noticeably lower than the earlier ones. The first tests were made at 2.6 degrees centigrade, and the last one at 27.7 degrees centigrade. This is entirely consistent with laboratory data, which show that in the lower range of temperatures the power factor of the insulation decreases as the temperature increases. These measurements show no evidence of deterioration of the insulation.

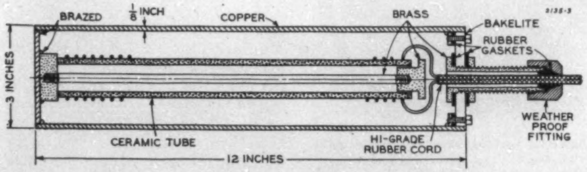
Soil-Resistivity Measurements

During the period that tests were being conducted on the experimental installation, soil-resistivity measurements were made for use in designing the commercial line. Measurements were made to determine the thermal resistivity of the earth under varying seasonal conditions and for different types and conditions of soil. A cell was used consisting of a copper cylinder twelve inches long and three inches in outside diameter, with a one-eighth-inch wall, and equipped with an internal electric-heating element. This heater was capable of dissipating 200 watts at 120 volts, uniformly over the walls. A copper-constantan thermocouple was embedded in the mid-point of the cylinder wall. The general construction of this cell is shown in Figure 3.

This cell was buried vertically in the ground; the heater supply and thermocouple leads were brought out to a convenient point. Means were also provided for measuring the ambient earth temperature nearby. Sufficient energy was supplied to the heater element to cause a 40- to 50-degree-centigrade rise between cylinder wall and ambient earth. For the soils so far tested an input of 50 watts has been necessary.

Figure 3. Cell for measuring thermal resistivity of soil

Ceramic tube wound with Chromel wire to dissipate 200 watts at 120 volts



The earth thermal resistivity (in degrees centigrade per watt per centimeter cube) may be computed from the formula $g=(128/W)(T_1-T_2)$: when W = watts input, T_1=temperature of cylinder wall, and T_2=temperature of ambient earth. This formula is empirical and was derived by comparison with the results obtained from such a cylinder with those obtained with a sphere.[3]

Figure 4 shows values of resistivity for three types of soil encountered along the route of the commercial line, as well as the variations in resistivity that are caused by the changes in moisture content during the different seasons of the year.

Tests with the thermal resistivity cell at what will probably be the locality where the soil has the highest thermal resistivity along the commercial line (in fine sand) show maximum average values of 120 during the dry season. In other areas where loam prevails, maximum average values of 95 and 75 were measured during the dry and wet seasons, respectively. Corresponding measurements where heavy clay prevails show resistivities of 105 and 95, respectively.

Commercial Line

General Layout

The commercial line consists of a seven-mile circuit installed under paved city streets. The cable used has previously been described under "Manufacture" and was shipped and installed in lengths of approximately 1,500 feet. The pipe line consisted of seven-inch steel pipe protected from corrosion by means of Somastic pipe covering. The line is sectionalized by stop joints, one near each termination and four at intermediate points, thus dividing the line into five approximately equal sections. These stop

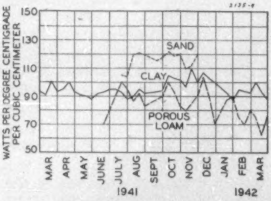


Figure 4. Seasonal soil-resistivity curves

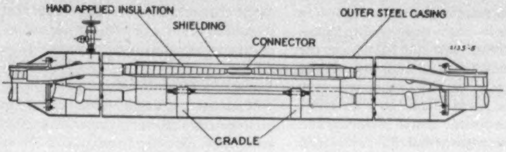


Figure 5. Normal joint for *SMD* type cable

joints are provided so that if repairs to the steel pipe are required, because of corrosion or for other reasons, it will be necessary only to discharge the nitrogen from the section involved. Sectionalizing the line also facilitates the location of a gas leak in the steel pipe if a leak should occur. In normal operation the stop joints are by-passed by a pipe containing a valve which may be closed for sectionalization. Nitrogen tanks were connected through pressure regulators to the ends of the pipe to take care of any leakage. A low pressure alarm was installed to operate when the gas pressure drops to 150 pounds per square inch.

INSTALLATION

The temporary lead sheath was scored and stripped by hand, as the three cables were pulled as a unit into the steel pipe. No difficulty was encountered in pulling these 1,500-foot lengths. The maximum pulling stress that occurred on any of the sections was 7,200 pounds which corresponds to a maximum coefficient of friction of 0.37.

For a distance of approximately 15 feet on each end of the cable sections, the lead sheaths were allowed to remain to facilitate making the joints, and to permit sealing the cables in the steel pipe during installation. The cables were sealed in the pipe by means of trifurcating heads, which consisted of a flange and gasket bolted to the pipe ends and three lead nipples wiped to the cable sheaths.

Previous to pulling in the cable, each section of pipe line between manholes was tested for leaks with 500 pounds per square inch air pressure for 24 hours. The pressure was then released, and the pipe filled with dry nitrogen gas at a pressure of 5 to 10 pounds per square inch. This pressure was maintained until the cable was pulled. After the cable was pulled, the ends of each section were sealed by means of the trifurcating heads, and dry nitrogen gas admitted at a pressure of 5 to 10 pounds per square inch. After each section between stop joints was completed, it was evacuated. When all sections of the line were completed, the line as a unit was filled with dry nitrogen gas at 200 pounds per square inch.

NORMAL JOINTS

The normal joints, shown in Figure 5, are essentially the same in the experimental and commercial lines and were hand-taped with presaturated paper. Soldered-type countersunk copper connectors having a diameter the same as that of the cable conductor were used. These avoid the regions of high stress that occur near the ends of standard connectors. Stress control was further improved by the use of semiconducting tapes for shielding the connectors. These tapes were applied so as to make good contact with the conductor shielding tapes. Long pencils were made on the factory insulation by tearing each tape individually at points approximately one-eighth inch apart. The paper tapes were applied butt lap to a thickness 20 per cent greater than that of the factory-applied insulation.

The hand-applied insulation was tapered at the ends of the joint by tearing the successive tapes at points $^{9}/_{16}$ inch apart. The joint was completely shielded with tinsel copper braid. Special rubber sleeves were temporarily pulled over the completely insulated and shielded joints to protect them against dirt and moisture and to permit maintaining low gas pressure on the insulation. The ends of these rubber sleeves were sealed with rubber tape to the short sections of lead sheath left on the cable ends. After all three joints were completed, the rubber sleeves were removed, the wipes at the trifurcating heads melted off, the trifurcating heads moved two inches from the pipe ends and the wipes remade. This procedure permitted entrance of gas into the joint casing and anchored the cables at the joint. The enlarged section of pipe forming the joint casing was then pulled over the joint and welded in place.

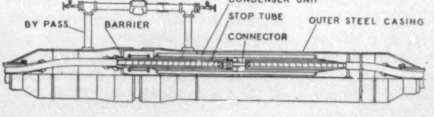


Figure 6. Stop joint for *SMD* type cable

STOP JOINTS

The stop joint, shown in Figure 6, is essentially the same as those used for oil-filled cable. The stop tubes have somewhat greater wall thickness to give strength to withstand safely a differential pressure of 225 pounds per square inch. At one end the three stop tubes are sealed to a diaphragm through which the cables pass. This diaphragm is sealed to the outer joint casing. The condenser unit for each of the three individual joints is similar to the condenser for oil-filled cable except that it is wound with preimpregnated paper, and no individual oil-tight outer sleeves are used. This allows the gas to come into direct contact with the insulation of the condenser, thus improving its dielectric strength in the same manner as for the cable proper.

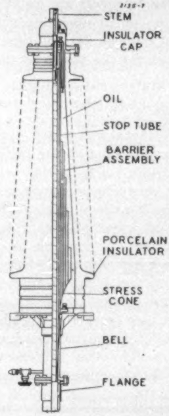


Figure 7. Terminal for *SMD* type cable

TERMINALS

The terminals used on both the experimental and commercial lines have a brass-fitted bakelite stop-tube assembly similar to those used in the stop joints but of greater length. This stop tube assembly ends in a stem at the upper end and a flange at the lower end. The porcelain insulator, with a barrier assembly and stress cone, metal rings at top and bottom, and an insulator cap, is placed over the stop tube and bolted to the flange. The space between the stop tube and the

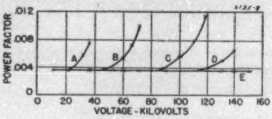


Figure 8. Ionization tests at different gas pressures on 600,000-circular-mil cable with 600-mil insulation

Curve	Pounds Per Square Inch
A	0
B	50
C	100
D	150
E	200 and 225

porcelain is filled with oil. Thus the terminal is essentially that used for oil-filled cable with the addition of the stop tube. The terminals are installed in a manner similar to single-conductor terminals on oil-filled or solid-type cables. Figure 7 shows the design of these terminals.

MANHOLES

The 24 normal and 6 stop joints were housed in manholes having brick walls, concrete floors, rail-supported brick ceilings, two chimneys, and built-in pulling eyes. The normal joint manholes were 12 feet long, 4 feet 9 inches wide, 5 feet high, and those housing the stop joints were 17 feet 10 inches long, 5 feet 6 inches wide and 5 feet high. Corrugated galvanized-iron culverts, concentric with the pipe, were used for storing the steel-joint casings out of the way of the splicers during construction. One such culvert was provided in the normal-joint manholes and two in the stop-joint manholes.

Laboratory Tests

POWER FACTOR VERSUS PRESSURE AND VOLTAGE

The effect of different gas pressures on the power factor at different voltages is shown in Figure 8. This cable operates at approximately 70,000 volts to ground and was designed to show no measurable ionization at double working voltage, or 140,000 volts, when under a nitrogen pressure of 225 pounds per square inch. In general, there was no appreciable ionization at 140,000 volts and 200 pounds per square inch. It is apparent also that at approximately 80,000 volts and 100 pounds per square inch, no appreciable ionization occurs. Thus the minimum safe operating gas pressure for this cable is approximately 100 pounds per square inch. Ionization data on this lot of cable may be summarized as indicated in Table A.

Table A

Gas Pressure (Lb. Per Sq. In.)	Voltage at Which Ionization Begins
Atmospheric	Approximately 23 kv
50	Approximately 50 kv
100	75–90 kv
150	100–120 kv
200	120–140 kv
225	140 kv or above

COMPOUND MIGRATION

The fact that this cable was to be installed in a steel pipe without a lead sheath made it necessary to determine whether at high temperatures the impregnant would flow out of the paper into the pipe, or when installed vertically, the compound would flow longitudinally of the cable. If such migration would be harmful, the effect would become apparent from ionization tests. In Figure 9 are curves showing ionization tests on a sample of cable after heating for 65 days at 100 degrees centigrade in an inverted U position. The sample in the bent position had an over-all height of 15 feet of which only a very small part was consumed by the storage of the small amount of drained compound at the lower ends in the space between the lead sheath and insulation. This offered as complete an opportunity for drainage as for a cable of any height. The cable was allowed to cool for eight hours each day for 38 of the 65 days. Initially and also after 43 days' heating there was no ionization at 140 kv and 225 pounds per square inch, but after 65 days' heating ionization started at 130 kv. Thus it is safe to assume, since there was no change after 43 days' heating at 100 degrees centigrade, and only a slight change after 65 days' heating, that migration is not a problem at normal operating temperatures even in vertical runs.

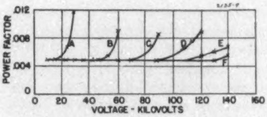


Figure 9. Ionization tests at different gas pressures on 600,000-circular-mil cable with 600-mil insulation, after being heated for 65 days at 100 degrees centigrade in an inverted-U position

Curve	Pounds Per Square Inch
A	0
B	50
C	100
D	150
E	200
F	225

RADIAL POWER FACTOR

It has been customary in the industry to measure radial power factor at 60 degrees centigrade. In order to exaggerate effects on power factor of small quantities of contaminants, notably moisture, radial power factor tests on *SMD* type cable were made at 80 degrees centigrade. The greater sensitivity of measurements at 80 degrees centigrade, as compared with 60 degrees centigrade, is markedly shown in Figure 10 where curves *C* and *D* show radial power factor at these respective temperatures. The sample of cable used for these measurements was an experimental section made before the procedure used in the construction of this type of cable had been perfected.

Figure 11 shows the very high degree of correlation that exists between the power factor and moisture content. The radial moisture-content curve has been superimposed on the radial power-factor curve, and the variations in the moisture content are closely followed by variations in the power factor. These measurements were made on samples taken from the section of experimental cable mentioned as having been made before the manufacturing technique was perfected. To obtain an accurate measure of moisture content, groups of tapes were used instead of single tapes. Tests made on a number

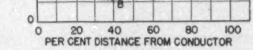


Figure 10. Radial power factor on experimental and commercial cables

A—New cable, "commercial," measured at 80 degrees centigrade

B—Same cable as *A* after 113 days at 100 degrees centigrade

C—New cable, "experimental," measured at 80 degrees centigrade

D—Same cable as *C* measured at 60 degrees centigrade

E—Same cable as *C* after 99 days at 100 degrees centigrade, measured at 80 degrees centigrade

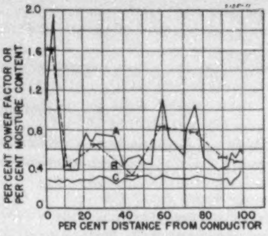


Figure 11. Radial power factor and moisture content of experimental cable

A—Radial power factor on "experimental" cable, measured at 80 degrees centigrade

B—Radial moisture content on "experimental" cable

C—Radial power factor on "commercial" cable, measured at 80 degrees centigrade

of samples show that when the moisture content is below one half of one per cent, it has no significant effect on power factor.

A very noticeable improvement in the 80-degree-centigrade radial power factor took place after the room humidity was maintained below 20 per cent during the taping operations. This improvement is demonstrated by the comparison of the radial power factor curves *A* and *C* shown in Figure 11 for cable made before and after the adoption of humidity control.

POWER FACTOR—TEMPERATURE CHARACTERISTIC

Figure 12 shows a typical power factor versus temperature curve for *SMD* type cable made by the latest practice. This compares very favorably with the best of other types of cable insulation, including that used on oil-filled cable.

TEMPERATURE STABILITY

The temperature stability of the insulation is as important for *SMD* type cable as for other types. The excellent temperature stability of the cable made for commercial use is shown by curves *A* and *B* of Figure 10. Curve *A* shows the radial power factor at 80 degrees centigrade for the new cable, and curve *B* shows the same characteristic on another sample of the same cable after 113 days at 100 degrees centigrade. In both cases the power-factor curve is essentially flat for the full section of the insulation. The aged sample shows no indication of an increase in power factor; in fact, this sample shows a somewhat lower average power factor than the unaged sample. Other samples show no appreciable change in over-all power factor after aging for 162 days at 100 degrees centigrade. Curves *C* and *E* show a similar radial power-factor comparison of "experimental cable," which was manufactured before moisture control was perfected.

DIELECTRIC STRENGTH

The results of dielectric-strength tests on thirteen 30-foot samples taken from the cable made for installation in the commercial line are shown in Figure 13. These samples were tested at a gas pressure of 225 pounds per square inch. There are shown, also, two tests on cable tested at 30 pounds per square inch pressure, one test on cable drained before voltage application, and one test on cable at high temperature. It will be observed that, for tests of a duration between about 1 hour and 100 hours, the approximate life to failure varies inversely as 7.5 power of the voltage. There is little or no decrease of strength for periods above 100 hours. For periods less than one hour the strength increases much less rapidly with decreasing time than in the range between 1 and 100 hours. The fact that the dielectric strength for a period of a few hours is reduced no more than about 10 to 20 per cent when tested at 30 pounds per square inch instead of 225 pounds per square inch is rather remarkable. Drainage under very severe conditions, that is, with the cable supported in an inverted U position and maintained at a temperature of 100 degrees centigrade throughout the cross section for a period of 44 days, reduced the strength 10 per cent when tested at a voltage which produced failure in 125 hours. The sample tested hot was held at 75 degrees centigrade by circulating current in the sheath of the cable for about 1,250 hours. Voltage was maintained on it, while hot, for about 530 hours at values increasing from 120 kv to 165 kv. The result of this test was plotted as the equivalent of 108 hours at 160 kv. Even on this basis the test was rather better than the average of the cables tested without heating. It is also to be noted that the power factor at 75 degrees centigrade remained at about 0.3 per cent throughout the test with no tendency to increase.

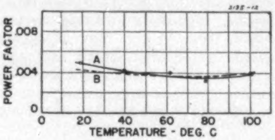


Figure 12. Curves of power factor versus temperature

A—*SMD* type cable "commercial"

B—Typical for oil-filled cable

Figure 8, which shows the power factor versus voltage characteristic, gives a basis for understanding the high long-time strength of this insulation. For a given over-all average stress in the insulation, the actual stresses in the paper and in the oil are lower than in other types of insulation, because of low specific inductive capacity caused by the gas spaces. Thus, as long as there is no breakdown in the gas spaces, it is clear that long-time dielectric strength should be at least as great as for the best of other types of insulation—namely oil-filled insulation. The "ionization" test is one of the best criteria of the beginning of ionization in the gas spaces. It is recognized from extensive tests and engineering experience that the effect of electrical stress in a gas changes sharply and completely at the beginning of ionization (ionization by collision). Above that stress the gas becomes a partial conductor. Even more serious than this is the effect of the bombardment of the solid and liquid insulation at the boundaries of the gas spaces in the gas. This bombardment produces "wax formation," charring of the paper, "coring," and ultimate breakdown. Below the ionization voltage, however, the stress has substantially no effect on the insulation, and, so far as the stress is concerned, the cable should last indefinitely.

A great deal of information has been obtained by dissecting completely (unwinding) the entire length of these many test specimens. In the tests in which complete failure took place after more

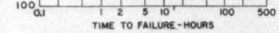


Figure 13. Voltage–time curve

x—Test made at room temperature and 225 pounds per square inch pressure

o—Test made at room temperature and 225 pounds per square inch pressure after the cable had been heated to 100 degrees centigrade in an inverted-U position for 44 days

●—Test made at 75 degrees centigrade and 225 pounds per square inch pressure after 1,250 hours at 75 degrees centigrade

Δ—Tests made at room temperature and 30 pounds per square inch pressure than 24 hours, there were frequently found one or more partial failures. Because of the small extent of the burning, it was possible to obtain from these partial failures very complete information as to their course. The different partial failures were evidently of similar character but in different stages of development. They originated in the butt space of the tape next to the conductor, usually so close to an edge of the tape as to burn it slightly. When several of these partial failures in different stages were examined, some were found with only a few layers of paper punctured, the size of the hole increasing outwardly from the conductor. In the more advanced stages, the outermost tapes were discolored (brown), usually to a diameter of $^{1}/_{32}$ inch to $^{1}/_{16}$ inch, but sometimes to a diameter up to $^{1}/_{8}$ inch. In still later stages, "tree design" or "dandrites" appeared at the tip of the advancing puncture. In later stages, these spread radially and outwardly toward final failure. The brown spots were evidently an immediate forerunner of the "tree design," since the first appearance of both occurred when the penetration had reached about five per cent or ten per cent of the total insulation thickness.

Elsewhere than at the complete and partial failures, the insulation seemed only slightly affected by the test. The odor ("mouse-nest odor") which characteristically accompanies insulation tested at high stress was prominent; but no other effects were observed except that on rare occasions traces of wax were found. Several samples were tested after repeated bending produced by rereeling 10 to 15 times around a drum having a diameter of about 16 times the cable diameter. These broke down at 140 kv after durations of 31 to 70 hours. Examination showed that the insulation had been displaced by the handling. This displacement had resulted in regions of wide interturn gaps or valleys between tapes (up to about 0.2 inch as compared with about 0.06 inch before bending) alternating with cross sections where these valleys were practically closed. This characteristic was substantially uniform throughout any cross section. In most of these samples there were several partial failures. All failures and partial failures were in the regions of wide valleys. Most of the cable made for the commercial line was made with slightly smaller valleys and was more resistent to the effect of repeated bending.

Cable handled as severely as required in installation was not affected by the handling. A sample from a length (the leading end of a 1,500-foot length) which had been subjected to excessive pulling strain during installation and had been pulled through about 3,000 feet of pipe, was tested and broke down at about 93 per cent of the voltage of the average new cable.

Impulse Tests

One of the main questions in connection with the *SMD* type cable was whether or not this cable would have a sufficiently high impulse strength if connected to overhead lines or outdoor substations; therefore, impulse tests were performed on samples of this cable. In this paper samples of cable from the experimental line shall be designated as *A*, and samples of cable subsequently produced, each of different construction, as *B*, *C*, and *D*. The *D* construction was used for the commercial installation.

The results of impulse tests on two *A* samples indicated impulse strengths of 680 and 580 kv. These values were not adequate, because a minimum impulse strength of 750 kv was considered necessary to co-ordinate with the 138-kv-class insulation used for the terminals and other apparatus on the system.

Samples designated as *B* and *C* represent two short experimental lengths on which a limited number of 60-cycle and impulse tests were made. On the first of these, satisfactory values of impulse strength were obtained, but the 60-cycle values were too low, whereas on the second type of construction the 60-cycle values were satisfactory, but the impulse strength was too low. Owing to the limited number of tests, the causes of this could not be determined. This became unimportant, however, as tests on cable with the *D* construction indicated satisfactory values both for 60-cycle and impulse tests. Table I shows the results of these impulse tests.

Table I. Impulse Strength of *SMD* Type Cable for Various Constructions

						Construction			
						Paper Tapes			
Sample No.**	Insulation Thickness (Mils)	No. of Surges	Breakdown Voltage (Kv)	Average Gradient (Volts Per Mil)	Maximum Gradient (Volts Per Mil)	No.	Thickness (Mils)	Density*	Saturant Viscosity† or Type
A-1	600	1	680	1,130	1,800	120	5	0.85	3,000
A-2**	600	1	580	965	1,535				
B-1	600	3	900+	1,500+	2,380+	40 24 Balance	3 5 5	1.20 0.85 0.85	1,000 1,000 3,000
B-2	600	3	905+	1,510+	2,390+				
C-1	600	1	610	1,030	1,615	30 16 Balance	2 3½ 5	1.00 1.00 0.85	Petrolatum Petrolatum 3,000
C-2	600	1	715	1,190	1,880				
C-3‡	600	1	520	865	1,375				
D-1	600	5	900+	1,500+	2,355+	19 24 Balance	2½ 3 5	1.20 1.20 0.85	3,000 3,000 3,000
D-2	600	6	940+	1,565+	2,460+				
D-9	600	3	980+	1,630+	2,560+				
D-11	600	4	925+	1,540+	2,420+				
D-12	600	3	920+	1,530+	2,400+				
D-13	600	3	900+	1,500+	2,355+				
D-14⊗	450	1	820	1,820	2,620				
D-15⊗	450	1	900+	2,000+	2,870+				
D-16	450	3	925+	2,055+	2,950+				
D-17⊖	385	1	580	1,505	2,085				
D-18	385	1	720	1,870	2,590				
D-19	385	1	775	2,010	2,790				

Plus values after breakdown voltage indicate that no failure occurred on these samples; on all other samples breakdown occurred in the cable.

Terminal failures occurred in seven additional *D* samples.

200 pounds per square inch nitrogen pressure was applied to each sample for one hour except samples *A*-2 and *C*-3.

** Pressure was applied for 60 hours.

‡ Tested at atmospheric pressure.

⊗ These samples were subjected to abnormal pulling stresses in field before test.

⊖ This sample was subjected to abnormal pulling stresses in field before test and visibly damaged.

* Density of paper tapes is the apparent specific gravity—the volume in cubic centimeters of a tightly wound roll of tape divided by weight in grams.

† Viscosity of saturant is given in Saybolt seconds at 100 degrees centigrade.

As the maximum potentials produced by the impulse generator did not cause failure in the cable with the improved construction (*D* samples), the actual impulse strength of such cables could not be determined. Breakdowns were obtained, however, on samples on which insulation thickness was reduced by removing a number of tapes from the outside of the insulation. The results of these tests are shown in Table I, samples *D*-14 to 19 inclusive. Two reduced insulation thicknesses were selected, so that breakdown voltage values could be conveniently compared with those obtained on other types of cables previously tested.

Figures 14 and 15 graphically illustrate the breakdown voltage values and maximum voltage gradients, respectively, of the samples tabulated in Table I, and compared with the results of previous tests by one of the authors[4] and others,[5,6] on the basis of equal insulation thicknesses. The slanting line on Figure 14 connects minimum breakdown values obtained by the authors and others on oil-filled, compression, Oilostatic, solid, and *SMD* cables, having insulation thicknesses of 385, 450, and 500 mils, respectively. These lines were extended to the full insulation thickness range for the *D* samples, in order to obtain an indication of the impulse strength which otherwise was not obtainable, because of the 1,000-kv limitation of the impulse generator.

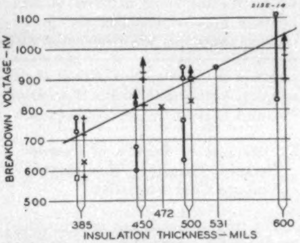


Figure 14. Impulse strength of *SMD* type cable

o—Oil-filled
□—Compression and Oilostatic
X—Solid
+—*SMD*

From Figures 14 and 15 it is evident that on the basis of *average stress* the *SMD* type cable in the commercial line has an impulse strength as high as has been obtained by various observers for well-made oil-filled cable of standard construction and equal insulation thickness. On the basis of *maximum stress* as customarily calculated, without taking into account the effect of stranding, the impulse strength of the conductor-shielded *SMD* type cable is higher than that of unshielded oil-filled cable. It is thought that the conductor shielding of the *SMD* type cable is responsible for this difference, although the extent of the benefit from this shielding has not been directly determined.

With 600 mils of insulation the impulse strength of the *SMD* cable is in excess of 1,000 kv. These tests indicate that 400 to 425 mils of insulation should be adequate for the desired impulse strength of 750 kv.

Summary

1. Nonleaded high-pressure gas-filled cable can be installed in a steel pipe without the insulation being adversely affected due to atmospheric exposure or mechanical handling.

2. No difficulty was encountered in the installation of 1,500-foot section lengths of cable on the commercial line. Based on the relatively low pulling stresses (7,200 pounds maximum), it appears perfectly feasible to install this type cable in lengths up to 2,000 feet.

3. The use of paper, preimpregnated with viscous compound, prevents migration at normal operating temperatures and allows installation on steep grades or in vertical runs without stop joints or other devices.

4. The satisfactory results on this type of cable are due in part to the development and use of a saturant of high viscosity and of excellent electrical properties.

5. The 60-cycle dielectric strength, both short- and long-time, is equivalent to that of oil-filled cable.

6. The impulse strength is at least the equal of oil-filled cable.

7. Power-factor values are approximately constant over a temperature range of 20 to 100 degrees centigrade.

8. The power factor was not affected even after six months at a temperature of 100 degrees centigrade.

9. The radial power-factor curve is flat even at 80 degrees centigrade, where impurities often cause erratic results.

10. Great uniformity of the insulation is indicated by the fact that on long-time voltage tests there are normally numerous partial failures, in addition to the completed failure.

11. The same insulation thicknesses as used on oil-filled cable can be used on high-pressure gas-filled cable, and all oil-fill cable tests met with a gas pressure of 30 pounds per square inch on reel lengths and 225 pounds per square inch on samples.

12. High-pressure gas-filled cable is practical for handling loads of the order of 100,000 kva and above.

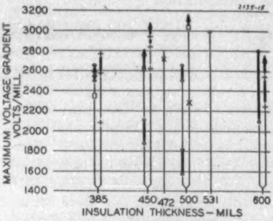


Figure 15. Maximum stress values for the impulse measurements shown on Figure 14

No correction made for the effect of stranding on maximum stress

o—Oil-filled
□—Compression and Oilostatic
X—Solid
+—*SMD*

13. Measured values of thermal resistivity are given for several kinds of soil.

14. High-pressure gas-filled cable with preimpregnated paper can be manufactured on standard equipment, with only minor changes, provided humidity is maintained below 20 per cent.

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Transient Recovery-Voltage Characteristics of Electric-Power Systems

H. P. ST. CLAIR
MEMBER AIEE

J. A. ADAMS
ASSOCIATE AIEE

1. Introduction

THE existence of transient recovery-voltage phenomena in connection with the interruption of short circuits by circuit breakers has been recognized for a number of years, and from time to time measurements and calculations have been made on actual field circuits.[2,5] However, while these more or less isolated and scattered instances have provided some valuable information, they have not afforded a comprehensive picture of transient-recovery voltage conditions existing in the field.

In order to provide the electric-power industry with a better understanding of and more complete data on transient recovery-voltage conditions, and, perhaps more particularly, to provide such data as a guide to the circuit-breaker designers, a field survey of several representative electric-power systems was recently carried out under the sponsorship of the committee on electric switching and switchgear of the Association of Edison Illuminating Companies. By permission of that organization and the participating electric power companies, some of the outstanding results and conclusions from this survey were made available to the AIEE committee on protective devices for presentation in this paper.

2. Transient Recovery Voltage—Theory and Definitions

While a number of excellent papers covering this phenomenon have been published in the literature, [1-6] a brief review is believed to be in order here.

The term recovery voltage applied to a circuit breaker refers to the voltage established across the open contacts after the arc is extinguished. The initial or transient recovery voltage is produced by a sudden change in circuit conditions, and its characteristics are determined by circuit constants. The normal-frequency recovery voltage is the generated system voltage finally maintained across the open contacts after the transient voltages have disappeared.

Since short-circuit currents are generally limited principally by reactance, it follows that the current is nearly 90 degrees out of phase with the voltage. Consequently, at the point of arc interruption, normally on or near the zero point of the current wave, the generated voltage is close to its maximum peak value. The voltage at the breaker, of course, prior to interruption, is held down to zero or nearly zero by the short circuit, but upon arc interruption attempts to recover immediately to the peak value of the wave. It is prevented from doing so instantaneously, however, by the effective capacitance across the breaker contacts, and there results instead a transient recovery-voltage oscillation. The frequency of this oscillation is determined by the inductance and capacitance of the circuit; the amplitude may reach double the normal-frequency crest voltage. In general, the combination of lumped reactance in the circuit between breaker and power source, with low capacitance-to-ground in the circuit between breaker and reactance, gives rise to the highest oscillating frequencies.

The severity of a given circuit is commonly characterized by the *rate of rise of transient recovery voltage*, which is determined by drawing a line from the zero point of the oscillating voltage wave, either to the peak of the first loop of the transient voltage oscillation, or as a tangent to the first voltage loop. This simple procedure, however, is not always adequate. The circuits involved are generally somewhat complex, so that the *transient recovery-voltage characteristics* are usually compounded of two or more superimposed oscillating frequencies. If, as occasionally happens, the first high-frequency loop has a smaller amplitude than a succeeding lower-frequency crest, the significance of rate of rise based upon the higher frequency but lower amplitude oscillation, may be decidedly questionable and subject to arbitrary judgment.

To avoid this difficulty, the complete transient recovery-voltage characteristic of a circuit should be determined. This characteristic, modified by circuit-breaker and test factors as described below, may be obtained directly from oscillograms taken in short-circuit tests, or it may be calculated from the constants of the circuit. In the AEIC survey of electric-power systems mentioned above, circuit characteristics were calculated in this manner, and the results were obtained in terms of an envelope of the transient recovery-voltage characteristics, composed of two and sometimes three major frequency components. In order to bring the task of calculating such characteristics within feasible limits in terms of man-hours of work required, certain approximations and simplifying assumptions were necessarily employed. A complete description of this simplified method of calculation, including the limits of accuracy obtained, is given in a companion paper.[7]

3. Results of Survey of Power-System Transient Recovery-Voltage Characteristics

The survey, which was made entirely by calculations except for direct measurements of a few circuit capacitances, was carried out on the systems of six power companies, selected as representing both concentrated urban types and distributed types of systems. Practically all of the circuit-breaker locations on these six systems were covered either by actual calculations or by a comparative estimating procedure. Where the system connections varied somewhat with different operating setups, and also where the assumed location of the fault affected the severity of the recovery-voltage characteristics, the calculations were made on the basis of the conditions which would give the most severe characteristics. With these results on the six systems covered by the survey, it was felt that a sufficient cross section had been obtained to draw general conclusions as to the transient recovery-voltage conditions existing throughout the industry.

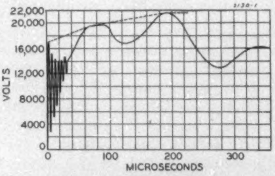

Figure 1. Typical recovery-voltage envelope and oscillating characteristic

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H. P. ST. CLAIR is system planning engineer, American Gas and Electric Service Corporation, New York, N. Y., and J. A. ADAMS is senior engineer, connections and control group, engineering department, Philadelphia Electric Company, Philadelphia, Pa.

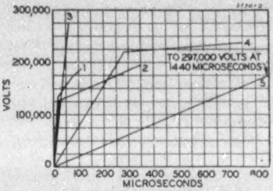


Figure 2. Representative envelopes of transient recovery voltage for 120,000- to 132,000-volt systems

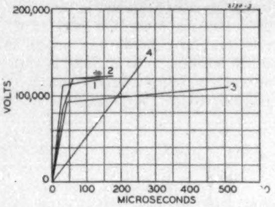


Figure 3. Representative envelopes of transient recovery voltage for 69,000-volt systems

As stated above, the survey was carried out on the basis of determining the envelope of the recovery-voltage characteristic, rather than on the basis of a simple rate of rise of transient recovery voltage. A typical recovery-voltage envelope, together with the complete oscillating characteristic from which it was drawn, is shown in Figure 1. Also groups of typical recovery-voltage envelopes, shown in Figures 2 to 6 inclusive, were chosen as illustrations from each of the following voltage classifications:

120,000- to 132,000-volt systems
69,000-volt systems
22,000- to 34,500-volt systems
11,000- to 13,800-volt systems
4,000- to 4,800-volt systems

For practical purposes, the envelope is sufficient to define the transient recovery-voltage characteristic, since it gives the magnitude of the first peak and the maximum peak, as well as any intermediate peaks found to be significant. Only the first peak and the maximum peak were recorded when the intermediate peaks were found to be less in amplitude than a corresponding point in time on the envelope connecting the first peak and the maximum peak. In other words, an intermediate peak was included if it raised the envelope, but not if it caused the envelope to drop below a straight line drawn between the first and maximum peaks. Also the envelopes were not extended beyond the point of maximum peak.

An analysis of the recovery-voltage curves shown in Figure 2 indicates a wide range of rates of rise for the various curves. Based on the first peak, curve 1 shows a rate of 8,300 volts per microsecond, while curve 5 shows only 210 volts per microsecond. The higher rates shown in curves 1 to 3 were obtained in connection with breaker locations where the fault was limited by a transformer fairly near the breaker, whereas the lower rates of curves 4 and 5 resulted from locations where there was no large lumped reactance in the circuit close to the breaker. Further examination of curve 1 shows that the first peak, which is 71 per cent of the final peak, occurs in 16 microseconds, the second peak in 80 microseconds, while the final and maximum peak is reached at 110 microseconds. Curves 2 and 4 show only two peaks, while curves 3 and 5 reach maximum values without any significant intermediate peaks. Figures 3, 4, and 6 in like manner show various typical characteristics for their respective voltage classes.

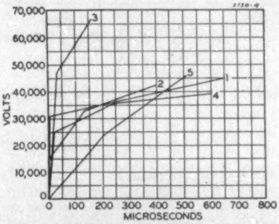


Figure 4. Representative envelopes of transient recovery voltage for 22,000- to 34,500-volt systems

The envelopes shown in Figure 5 have been drawn in two ways:

1. With a scale going to 1,000 microseconds in order to include the complete characteristic.
2. With a scale of only ten microseconds in order to show the relatively high-speed transients in greater detail.

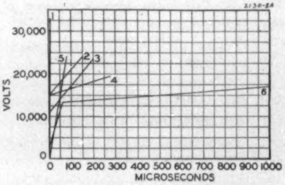
Complete numerical results of the survey were tabulated in terms of the information given on the envelope, that is, in terms of two or three significant peaks, and the time in microseconds required to reach each of these peaks. While these tabulations are not reproduced in this paper, the net results of the survey, in terms of rate of rise of transient recovery voltage, are given graphically in the form of cumulative percentage curves, shown in Figures 7 to 11 inclusive. The rate of rise used in these curves is based upon the first peak of the recovery characteristic, except in cases where this peak was found to be of abnormally low magnitude compared with later peaks.

The abscissae of these curves show the percentage of breaker locations where the rate of rise exceeds the value given by the ordinate. For example, referring to Figure 7, only 15 per cent of the breaker locations may be subjected to rates of rise of 5,000 volts per microsecond or higher. Also, it will be noted that 53 per cent of the breakers are located where the rate of rise does not exceed 1,000 volts per microsecond; the remainder of the breaker locations, or 32 per cent being subjected to rates of rise between 1,000 and 5,000 volts per microsecond.

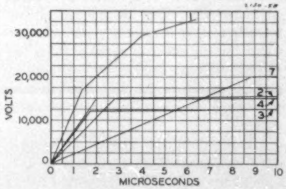
All of these results, of course, are given in terms of the characteristic of the circuits themselves and do not take into account the various factors which tend to modify the severity of the recovery-voltage characteristics actually obtained on a breaker. These factors include:

(*a*). Asymmetry of the current wave.

(*b*). Decay of flux in generators during short circuit.



5A—Envelopes to maximum peak



5B—Envelopes for first ten microseconds only

Figure 5. Representative envelopes of transient recovery voltage for 11,000- to 13,800-volt systems

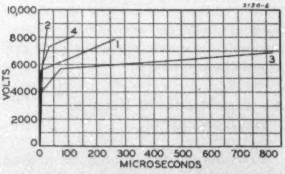


Figure 6. Representative envelopes of transient recovery voltage for 4,000- to 4,800-volt systems

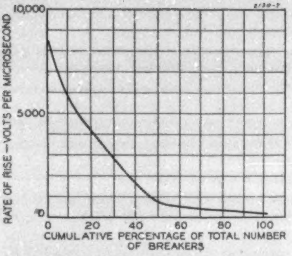


Figure 7. Cumulative percentage curves of rates of rise of recovery voltage for 120,000- to 132,000-volt systems

(Based on 345 breakers)

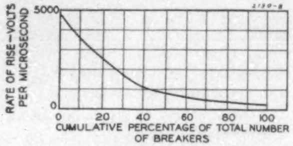


Figure 8. Cumulative percentage curves of rates of rise of recovery voltage for 69,000-volt systems

(Based on 342 breakers)

(*c*). Arc-voltage drop in the fault.

(*d*). Arc-voltage drop in the breaker.

(*e*). Conduction of current in the breaker after current zero.

In general, the above factors tend to reduce the severity of the recovery-voltage characteristic. On an actual circuit-breaker test, it is usually possible to account for the effect of these various factors by analyzing oscillograms of recovery voltage taken on the test and thereby to obtain a check on the calculated values.

The results of the survey, as summarized in Figures 7 to 11 inclusive, have shown that some of the calculated circuit recovery-voltage rates are substantially higher than the maximum rates disclosed by previous scattered information on this subject. Also the results show that the occurrence of high values of circuit recovery-voltage rates (on the order of 5,000 to 8,000 volts per microsecond) is considerably more widespread than had previously been suspected.

4. Significance of Results and Conclusions

In appraising the significance of these results, it is necessary first of all to take into account the modification of the maximum rate of rise of recovery voltage which is brought about in actual circuit-breaker operation. The data presented in a companion paper indicate the possibility that, for certain types of breakers at least, the effect of the breaker in reducing the severity of these transients increases as the calculated circuit values increase. Therefore, this may limit the actual rate of rise which a circuit can impress across the contacts of a breaker, regardless of the constants of the circuit. If this effect is rather general for breakers now in service, it may constitute one of the reasons why few outstanding instances of extreme rates of rise causing difficulty in the operation of circuit breakers have been recorded.

As to the significance of these results to circuit-breaker users, particularly in the operation of the great number of breakers already in service, and in view of the greater prevalence of fairly high rates of rise of recovery voltage than previously suspected, it may be said that there have been some but only a few instances recorded where circuit breakers were subjected to greater distress while interrupting short circuits under high rates of rise than under low rate conditions. In the group of systems covered by this survey, a study of operating records failed to disclose a systematic correlation between recovery rates and breaker distress or failures. Such effects as undoubtedly do exist would be evidenced in increased arc lengths and some additional maintenance due to increased burning and other phenomena. Experience seems to show in general, however, that circuit breakers have been designed with sufficient stroke to take care of even the highest rates of rise of recovery voltage, so that cases of breaker trouble directly attributable to recovery rates have been very rare.

On the other hand, it is believed that the results of the survey will be of major significance and importance in connection with the design and manufacture of circuit breakers. Important elements in the design, such as length of stroke and similar considerations, are determined to a large extent by rate of rise of recovery voltage. Therefore, while it may fortunately be true that liberal design allowances, combined with circuit-breaker modifying effects, have in the past produced breakers capable of withstanding the severe transient recovery-voltage conditions that are now known to exist, it may be more important in the future, particularly in connection with the development of radically new designs of circuit breakers, to have available a true picture of recovery-voltage requirements. It is believed that the results of the survey

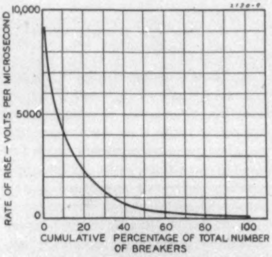


Figure 9. Cumulative percentage curves of rates of rise of recovery voltage for 22,000- to 34,500-volt systems

(Based on 1,975 breakers)

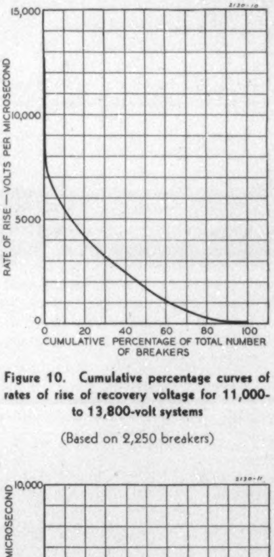


Figure 10. Cumulative percentage curves of rates of rise of recovery voltage for 11,000- to 13,800-volt systems

(Based on 2,250 breakers)

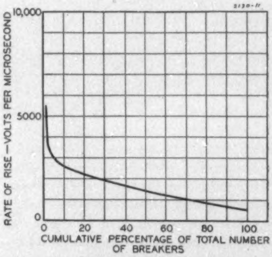


Figure 11. Cumulative percentage curves of rates of rise of recovery voltage for 4,000- to 4,800-volt systems

(Based on 3,975 breakers)

Emergency Overloads for Oil-Insulated Transformers

F. J. VOGEL
MEMBER AIEE

T. K. SLOAT
NONMEMBER AIEE

IN periods of high industrial activity transformer loads are generally high, and it is essential that operators know what overloads they can carry safely in emergencies. In order to do this effectively, it is necessary to know what temperatures will be reached after various overloads for different durations of time and the effects of temperature and time on the dielectric and mechanical strength of the insulation. If limitations between these things and the degree of insulation deterioration can be established, it is possible to make recommendations for safe overload times and temperatures.

In Part I of this paper some new data are presented on the effect of temperatures and oil acidity on the insulation, and some recommendations for maximum temperatures and their duration are made. This paper differs from previous studies on this subject in that definite evaluation of the effect of acidity on the insulation strength is given. Acids are formed in service by contact between oxygen and transformer oil. Excluding this contact by blanketing the transformer with inert gas results in a high mechanical strength of the insulation at a given temperature and time, or, conversely, permits a high operating temperature for a given loss in life. In considering this paper, it should be borne in mind that the data refer to the mechanical strength of the insulation only. Neither the 60-cycle nor the impulse dielectric strength are affected to any extent by the conditions of test. The true criterion of insulation deterioration therefore is not mechanical strength alone, but the dielectric strength must be given due consideration. Experience has shown that satisfactory operation can be obtained with much reduced mechanical strength.

Even after the recommendations for temperatures and time duration for insulation are made, it is necessary to know the overloads and times which will give corresponding temperatures. Various sizes and types of transformers vary widely in this respect. Some transformers which are well ventilated may have low gradients and correspondingly high overload capacities; most of the larger power transformers come within this class. Smaller transformers are wound with small wire with many turns in a single coil. They will normally have much higher gradients between copper and oil and correspondingly lower overload capacities. In Part II the characteristics of these various types of transformers are given along with recommendations for safe emergency overloads for given time and temperature limits.

Part I. Effect of Temperature on Cellulose Insulation

The life of cellulose insulation is decidedly influenced by three factors, namely, temperature, contact with oxygen, and exposure to acidity. Doctor C. F. Hill[1] has shown that cellulose insulation is stable for long periods of time below 90 to 95 degrees centigrade, when protected by an inert atmosphere. Doctor F. M. Clark[2] has classified the mechanical deterioration of cellulose as being caused by oxidation, pyrochemical changes, or both.

The object of this paper is to give additional evidence in regard to the rate of deterioration of cellulose insulation and to show that transformers can be exposed to short-time overloads even at high temperatures without damaging the mechanical strength of the insulation to a great extent.

Figure 1 shows the decrease in tensile strength of Manila paper when sealed at atmospheric pressure in an oxygen-free atmosphere. It is interesting to note the rapid decrease in tensile strength which takes place at 135 degrees centigrade. This occurs when temperatures are maintained at which thermal decomposition takes place. At 120 degrees centigrade, the tensile strength drops to approximately 50 per cent and levels off. It would appear that this 50 per cent strength would be maintained for long periods of time. Figure 2 shows the change in acidity of oil with Manila paper in oxygen-free oil protected by a nitrogen atmosphere. One of the main products of thermal decomposition is the formation of acid which forms a catalyst for further decomposition. The inert atmosphere prevents the formation of acid product at 120 degrees centigrade and below, showing the decided advantage of protecting the oil of a transformer against exposure to air.

The same data for Figure 1 are used to produce Figure 3. The remaining tensile strength of Manila paper after exposure to different temperatures in oxygen-free oil for different periods of time is plotted in this figure with temperature and time as ordinate + abscissae. These curves are of special interest to both the designer and the operator of transformers, because they can be used to predict the amount of mechanical damage done to transformer insulation after it has been operated at given temperatures for specified lengths of time. For example, if not less than 50 per cent mechanical strength is required at all times, it is interesting to see that the total period of operation at 140 degrees centigrade should be not

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F. J. VOGEL is consulting engineer of the Westinghouse Electric and Manufacturing Company, Sharon, Pa.; T. K. SLOAT is with the same company.

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will have their greatest value in providing this information.

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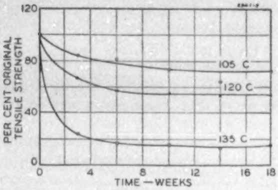

Figure 1. Decrease in tensile strength of Manila paper in oxygen-free oil protected by a nitrogen atmosphere

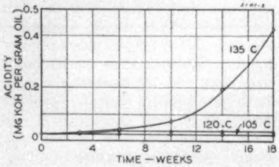

Figure 2. Change in acidity of oil with Manila paper in oxygen-free oil protected by a nitrogen atmosphere

more than two days, because in this time the *cellulose* has decreased in tensile strength to 50 per cent. However, with overloads at which the temperature reached only 135 degrees centigrade, a total of five days would be possible. If 40 per cent of the original mechanical strength is the lowest desired, a total of nine days at a temperature of 135 degrees centigrade may be allowed.

Figure 4 is a plot of the lowest tensile strength for paper insulation after 18 weeks under oxygen-free oil at various temperatures. From this figure one can select the temperature for transformer operation when the ultimate tensile strength has been selected. For example, if not less than 60 per cent of the original strength is required, the operating temperature should never be more than 115 degrees centigrade.

Based on the above data, the following practical conclusion might be drawn. The load which may be carried in the operation of a transformer is governed by the desired life of the transformer. From the experimental data presented here it is felt that an oil-insulated transformer may be operated at temperatures up to 120 degrees centigrade continuously, and even above, during short-time overloads, without seriously damaging the insulating materials, provided an inert (oxygen-free) atmosphere is maintained in contact with the oil at all times, and provided the acidity of the oil in contact with the insulating materials is maintained at a low value. Below 120 degrees centigrade the main deterioration in mechanical strength is caused by oxidation, while above this temperature the thermal decomposition of the cellulose is very detrimental. The thermal change results in the formation of gases, water, and other decomposition products. Oxidation, which takes place below 120 degrees centigrade, is eliminated to a very large extent by the use of an inert atmosphere.

Figure 5 shows the decrease of tensile strength of Fuller board when included with a varnished cotton tape in oxygen-free oil protected by a nitrogen atmosphere and at atmospheric pressure. It is quite interesting to compare this curve with Figure 1. At 135 degrees centigrade, the rate of decrease in tensile strength is quite similar to that of paper with no acids present other than those from decomposition. The decided effect of the acidity can be seen at 120 and 105 degrees centigrade. At 120 degrees centigrade, the final tensile strength is less than twenty per cent as compared with 54 per cent with no acids present; and for 105 degrees centigrade, 44 per cent as compared with 72 per cent. It would appear from these comparisons that it is of utmost importance to maintain low acidity and to avoid acid-forming materials in the construction.

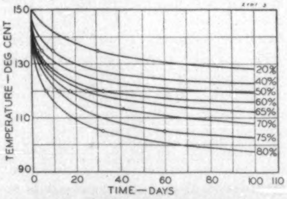

Figure 3. Percentage of initial tensile strength reached by Manila-paper insulation in oxygen-free oil protected by a nitrogen atmosphere

The acidity in contact with the Fuller board samples from the varnished cloth is shown in Figure 6. While these acidities are higher than those of usable transformer oil, it simulates the effect of lower acidities over longer periods of time.

The data for Figure 3 are used again to produce Figure 7. The curves show the part of the initial tensile strength remaining at the end of various periods versus temperature. If we choose 50 per cent as the minimum mechanical strength required, this would be reached in 1.5 days compared with two days from Figure 3. Likewise, at 135 degrees centigrade, the insulation would have reached 50 per cent strength in three days. For 40 per cent strength the total time of overloads could be seven days, compared with nine days with low acidity. A significant difference between this figure and Figure 3 is that the tensile strength continues to decrease even after 100 days, while in the low-acidity oil the decrease in tensile strength shows a greater tendency to reach a constant value for a given temperature.

Figure 8 shows the lowest tensile strength for Fuller board insulation after 18 weeks of exposure to high-acidity oil. If we choose 50 per cent as the minimum required tensile strength, the operating temperature should not be more than 100 degrees centigrade. However, this is for a period of only 18 weeks and since the tensile strength continues to decrease, due to the presence of acids, it would indicate a still lower temperature.

The curves of Figures 5 and 8 show that the presence of acids in contact with cellulose insulation seriously catalyzes not only oxidation changes but also thermal decomposition. Care should be taken to maintain low-acidity oil in order to obtain the longest life from the cellulose insulation. Varnishes and varnished tapes have been a source of acids in transformers. The use of synthetic materials in place of natural varnishes in modern transformer construction has practically eliminated this source of acids.

Figure 9 shows the similarity in behavior between Fuller board and paper insulation. The Fuller board was in contact with oxygen-free oil under the same conditions as for Figure 1. While the Fuller board is some better at 105 and 120 degrees centigrade, it is interesting to note that at 135 degrees, thermal decomposition destroys most of the tensile strength.

Figure 10 shows the rate of increase in acidity of oil samples with the same materials as for Figure 6, except that the oil has free access to the oxygen of the air. Here the acidity shows a decided increase in 18 weeks, much greater than in Figure 2. Comparison of these figures shows that to obtain full advantage of preserva-

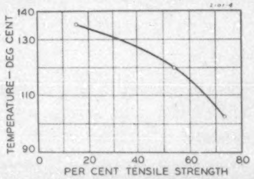

Figure 4. Lowest tensile strength for paper insulation after 18 weeks under oxygen-free oil protected by a nitrogen atmosphere

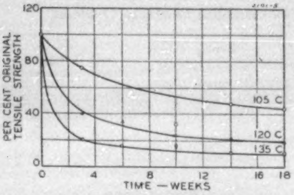

Figure 5. Decrease in tensile strength of Fuller board, together with varnished cloth, in oxygen-free oil protected by a nitrogen atmosphere

tive measures in transformers, no acid-forming varnishes or treatments should be used.

Figure 11 shows test data taken on paper in transformer oil at various temperatures with the samples open to the air. Comparison of these curves with Figure 1 shows the great benefit obtained by protecting the samples from oxygen, particularly at temperatures of 120 degrees centigrade and less.

It is possible from the data given above to make recommendations for short-time emergency overload temperatures of transformers based on given amounts of decrease in mechanical strength of the insulation. One set of values that has been proposed is given in Table A.

Inspection of these values will show that if a given temperature from the table were reached 20 times for the time specified, the insulation would have a tensile strength of approximately 80 per cent of its original value. If emergency overloads of the same character as described in the table were to be applied once a year for 20 years, we would, therefore, not expect that the insulation would be appreciably lower than 80 per cent of its original value. Figure 3 is used in reaching this conclusion. This assumes that there was no deterioration due to steady operation. It would be more practical, however, to consider that the transformer might reach 105 degrees hot-spot temperature for considerable durations of time, and, if this were true, its mechanical strength would be somewhere between 80 and 75 per cent of its original value. If the emergency operations were to take place after this initial deterioration, a method of estimating its ultimate deterioration is as follows:

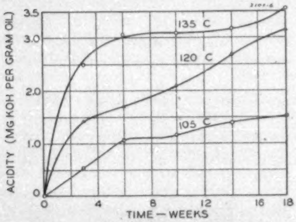

Figure 6. Change in acidity with Fuller board and varnished cloth under oxygen-free oil protected by a nitrogen atmosphere

Suppose we were to consider that there would be 20 overloads for 24 hours which would reach 115 degrees; then the deterioration due to the steady load would be equivalent to approximately 24 days at 115 degrees without the deterioration due to the overload. The additional 20 days would then be equivalent to 44 total days at 115 degrees, which would result in approximately 65 per cent of the original mechanical strength of the insulation. Similar methods of calculation could be used for any of the other emergency overload temperatures. If an operator desired to determine the amount of deterioration under other conditions of loading and time, the curve data provide an easy graphical method for making such an estimate.

It is believed that the decrease in mechanical strength resulting from Table A is reasonable and safe for general use. The ultimate strength requirements depend upon many factors determined by service conditions. The severity and frequency of short circuits is an important factor, as well as whether or not the apparatus remains fixed or is to be transported. Since the dielectric strength remains practically unchanged, a reduction of 40 to 50 per cent in the mechanical strength of the insulation over a 20-year period would not be considered excessive for normal operating requirements.

Summary

New data on the rate of deterioration of cellulose insulation at different temperatures have been obtained. From them a table of time periods and temperatures has been made at which oil-insulated transformers may be operated in order that specific cellulose insulation strengths of approximately 65 per cent of the original value may be maintained. It is expected that this value will be maintained even after operation of a considerable length of time at 105 degrees centigrade.

Table A. Duration of Overload Ultimate Temperatures

Hours	Degrees
1/4	145
1/2	140
1	135
2	130
4	125
8	120
24	115

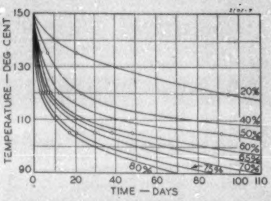

Figure 7. Percentage of initial tensile strength reached by Fuller board, together with varnished cloth, in oxygen-free oil protected by a nitrogen atmosphere

Part II. Recommended Emergency Overloads for Transformers

After Table A in Part I has been established, it is still necessary to correlate the data with overloads which will result in these temperatures after given lengths of time. In order to do this, it is necessary to know what the characteristics of average transformers are, both in respect to the gradient between the insulation and the oil, and the length of time which it takes for the oil to reach given temperatures under given load conditions. Data may be found in several places for estimating the gradients between oil and insulation and also the rate of oil rise during transformer overloads. One such reference is a recent paper on "Hot-Spot Temperatures in Transformers."[3] Before these data can be used, however, it is desirable to establish available characteristics for several sizes of transformers.

It is suggested that seven classes of

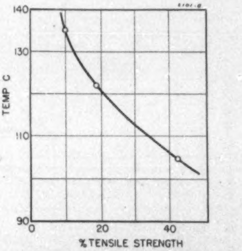

Figure 8. Lowest tensile strength of Fuller board, together with varnished cloth, after 18 weeks in oxygen-free oil protected by a nitrogen atmosphere

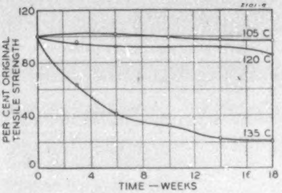

Figure 9. Decrease in tensile strength of Fuller board in oxygen-free oil, protected by a nitrogen atmosphere

transformers be considered. These classes may be described as follows:

Class 1. Many transformers use round wire, and there is a considerable gradient between the center of such coils and the oil. There are also some ribbon-wound coils, which have several layers and several turns per layer. We suggest that this class include transformers up to 150 kva, 25 kv, and transformers with windings of all voltage classes rated at 15 amperes or less. This class is to be oil-insulated, self-cooled.

Class 2. The next larger transformers than those described in class 1 generally have strap windings with at least one side or edge of each conductor exposed to oil. Surely, up to 69 kv such transformers would not have heavy conductor insulation; therefore, this class is limited to power and distribution transformers up to 69 kv, oil-insulated, self-cooled.

Class 3. This class is similar to class 2 except that it is for forced air-cooled transformers.

Class 4. Some transformers include regulating equipment and are used as unit substations. Such transformers are very similar to the ordinary self-cooled transformer with the exception that the tank is larger, and there is much more oil used than in the ordinary self-cooled transformer. The result is that the transformer has a longer time constant than the ordinary self-cooled transformer. It is suggested, therefore, that class 4 include transformers such as unit substation transformers and be similar to class 2 except for longer time constants.

Class 5. It is suggested that this class include oil-insulated unit substation transformers, which are cooled with forced air. This class is then the same as class 3, except that the transformers have longer time constants.

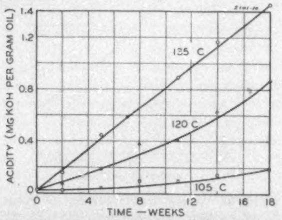

Figure 10. Change in acidity with Fuller board under oil exposed to air

Table I. Suggested Classes for Transformers for Calculating Emergency Overloads

	Class						
	1	2	3	4	5	6	7
Hot-spot rise	60°	60°	63°	60°	63°	62°	65°
Top-oil rise	40°	50°	47°	50°	47°	50°	46°
Loss ratio	2.5:1	2.5:1	4.5:1	2.5:1	4.5:1	2.5:1	4.5:1
Time constant	3	4	2.5	6	4	4	2.5

Class 6. Large high-voltage power transformers may require heavier insulation on the conductors than smaller transformers. This, in turn, will result in higher gradients between the copper and the oil. Therefore, slightly different characteristics should be used for these transformers than for smaller units exemplified in class 2; class 6 should be approximately the same as class 2 except for higher gradients.

Class 7. It is suggested that class 7 correspond to class 6 except that the transformers have forced air-cooled ratings.

A tabulation of the proposed characteristics of these various classes is given in Table I, and it might be well to describe how the various values were obtained. For example, in class 1 the designer can calculate the watts per square inch of losses transmitted from the coil surface to the oil. From this, he can make an approximate estimate of the gradient between the coil and the oil, based on designs in common use. For class 1 this gradient should be in the order of and not exceeding 20 degrees centigrade at normal load. Likewise, the top oil rise is in the order of 40 degrees for such transformers. The difference between average oil and top oil is in the order of five degrees. The loss ratio refers to the ratio of the copper loss to the iron loss at rated load, and this loss ratio approaches limits now commonly used. Time constants are given in hours. These values are average values for transformers of the ratings referred to above.

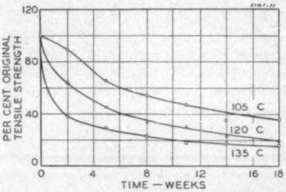

Figure 11. Decrease in tensile strength of Manila paper under oil exposed to air

In a previous paper[3] a method was indicated for calculating transformer hot-spot temperatures. In this paper it was shown that changes in viscosity tend to decrease gradients at high oil temperatures. These decreases further tend to counteract increases in gradients due to losses. Therefore, no great error should result if the change in copper resistance were ignored. This is not exactly true but probably is within reasonable error, considering that average characteristics of transformers are being considered. On this basis, it would be reasonable to assume that the gradient between the copper and oil varies approximately as the 0.8 power of the copper losses, which, in turn, are assumed to vary as the square of the load.

In the paper[3] previously mentioned, a method was shown for calculating oil temperatures after short-time loads. Cal-

Table II. Emergency Short-Time Overloads (Per Cent of Rated Load)

Time (Hr.)	Temperature	Class						
		1	2	3	4	5	6	7
		Following No Load						
1/4	145	253	360	291	372	288	333	252
1/2	140	230	320	248	334	259	289	229
1	135	205	265	210	295	230	249	196
2	130	180	215	172	239	188	202	166
4	125	155	170	145	193	157	167	143
8	120	138	145	128	158	135	141	127
24	115	130	130	123	132	124	128	121
		Following Full Load						
1/4	145	215	280	225	296	230	271	206
1/2	140	197	255	205	265	208	238	187
1	135	180	218	183	234	185	205	166
2	130	163	185	155	198	160	174	149
4	125	148	155	140	169	140	151	135
8	120	135	138	128	147	131	138	126
24	115	130	130	123	132	124	128	121

culations were made on the basis of average oil temperatures, and an approximate constant value added to them. The method in common use before was to calculate the top oil temperatures directly. Comparative calculations show that there is no great difference between the two methods of calculation. For the purpose of simplicity and also for greater conservatism, it is intended to use the older and simpler method in this paper.

For example, suppose we take class 1 and calculate the overload, which can be carried for eight hours to reach a temperature of 120 degrees centigrade, at an ambient temperature of 30, following full load. As shown in the preceding paper, it is easiest to use "cut and try" methods, and a value of 135 per cent load can be assumed as a basis. At 135 per cent load the copper losses will be 1.82 times as great as at full load. If the total losses at full load in the first case were 3.5 times the iron loss, in the second case it would be 5.55 times the iron loss. If the top oil rise were 40 degrees in the first case, it would be ultimately, $\frac{(5.55)^{0.8}}{(3.5)} \times 40$ degrees or approximately 58 degrees centigrade. The change in load from full load to 135 per cent results in an ultimate change in oil temperature of 18 degrees. This change will not be entirely completed in eight hours, and the rise which will result in that time will be:

T_8 hr $= 18(1-e^{-8/3})$ = approximately 17 degrees

This means that the oil rise after eight hours will be 57 degrees.

The gradient at full load is given as 20 degrees. At 135 per cent load the copper loss is 1.82 times the value at full load, and the gradient should be $1.82^{0.8}$ or 1.6 times as great as the gradient at full load. 1.6×20 degrees is 32 degrees. The total temperature would be the sum of the ambient temperature, 30 degrees, plus the gradient between air and top oil, 57 degrees, plus the gradient between oil and copper, 32 degrees, or a total of 119 degrees centigrade. Since this temperature is slightly below the 120 degrees allowed, this percentage was considered sufficiently accurate for use in Table II.

It will be seen upon inspection of Table II that the proposed overloads are much higher than those previously recommended for use in the American Standards Association Guides for Operation for Transformers. This occurs, partly because the original guides did not differentiate between different types of transformers, and partly because temperature rises which were permitted were lower than those proposed in Part I of this paper. Still another reason is that the number of emergencies was not specifically limited, and in the present paper 20 are assumed, on the basis of 20-year life and one emergency per year on the average. It is felt that sufficient information is given in this paper to permit the operator to use judgment and discretion in specifying safe overloads for his application.

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A New Jewel for Indicating Instruments

F. K. McCUNE
ASSOCIATE AIEE

J. H. GOSS
ASSOCIATE AIEE

IT has been almost universal practice to use highly polished sapphire jewel bearings, cut approximately in the form shown by Figure 1a, in electric indicating instruments. Such a jewel is commonly known as a "Vee." The other member of the bearing is a cone-shaped piece of hardened steel or other hard metal also accurately cut and highly polished. Figures 2 and 3 show both the common proportions of these parts and the clearances involved. Figure 2 shows an instrument which has the moving shaft vertical, and Figure 3 an instrument with the moving shaft horizontal. Figure 4a shows typical moving systems and jewel settings.

The making of sapphire Vees is a very highly specialized art, and the great majority of machine equipment and trained personnel has been concentrated in a small area in Europe, the principal production being in Switzerland.

The present conflict has cut off the source of supply of instrument bearings and has left this country with inadequate facilities for their production. Accordingly, it has been necessary to find substitutes. Furthermore, because of the vital part that instruments play in devices for the armed forces and industry, it has been necessary to find a substitute which would not in any way impair the usefulness of the instruments even under abnormally severe conditions of use.

Fortunately, instrument manufacturers have a good deal of experience to fall back on. About 30 years ago, the present so-called "miniature instruments" (Figure 4) were introduced, and the demand for them has steadily increased, particularly in the communication industry. Because of the light weights of the moving systems and proportionately lower torques available, the radius of the pivot used was considerably smaller than that used in larger instruments. This created a demand for a sapphire jewel with a correspondingly smaller radius of the spherical surface in the bottom of the Vee.

In general, the problem of producing a sapphire jewel of the required shape resolves itself into that of cutting very accurately to shape a piece of sapphire, which is in a group of materials next in hardness to diamonds. The reason that the shape is particularly difficult is that the apex of the Vee must be accurately spherical, with a radius of from 0.002 to 0.004 inch, and the side tangent to the spherical surface must be straight. At the center of the spherical surface if, for example, the cut in the sapphire is made in a lathe, the linear cutting speed is zero and even at a speed of, say, 30,000 rpm at a distance of 0.001 inch from the center, the cutting speed is only 0.3 foot per sec-

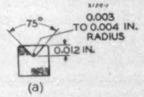

Figure 1a. Cross section of a sapphire jewel used for miniature instruments

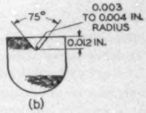

Figure 1b. Cross section of a hot-formed jewel as described in this paper

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F. K. McCune is assistant to the executive engineer, and J. H. Goss is engineer in the works laboratory, both with General Electric Company, West Lynn, Mass.

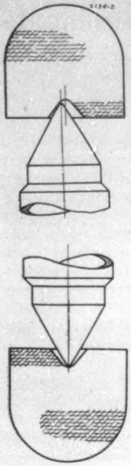

Figure 2. Scale drawing showing proportions and clearances of vertical shaft miniature instrument bearing

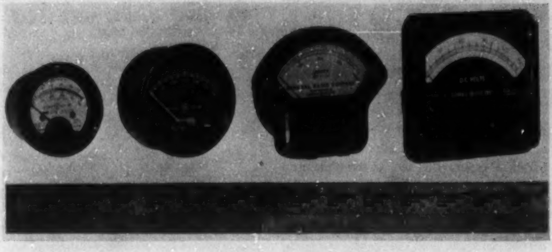

Figure 4 (above). A group of modern miniature instruments

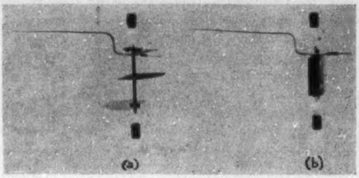

Figure 4a (right). Miniature instrument moving systems and jewel settings

(*a*). For a-c instruments
(*b*). For d-c instruments

ond. Also, as can be seen from the dimensions (Figure 1a), the cutting tool must be small in size. Therefore, the tendency in cutting, smoothing, or polishing is to produce a surface which is not truly spherical, but has either a lump in the center because of the low cutting speed, or a hollow which may be produced by crushing the center instead of cutting it.

As mentioned before, the small sapphire jewel for miniature instruments was necessary to limit the amount of side play in the jewel. As can be seen from Figures 2 and 3, the side play may amount to more than the end play. It is of importance both from the standpoint of its effect on mechanical clearances, and because excessive side play introduces uncertainty of pointer position. The latter is of most concern when the instrument may be used sometimes with shaft horizontal and at other times with shaft vertical. Referring to Figure 3, it is obvious that the only way to get a small degree of side play in a jewel of inherently large radius is to reduce the clearance between the pivots and the bottom of the jewels to a small fraction of a thousandth of an inch. This is an impractical solution, because of the hazard of very slight dimensional changes causing stickiness in the instrument, and because of the wedging action caused by the very small angle of slope of the jewel near the center.

The generally unsatisfactory contour in jewels available at the time led manufacturers to look for substitutes which could be produced by other methods. As a result of a large amount of development work done in the General Electric Company laboratories about 25 years ago, a method of producing a jewel by forming a drop of fused "hard glass" was developed. These jewels had very accurately controlled contours, since the glass could be made to almost any desired shape. The general shape is shown in Figure 1b. They also had very smooth highly polished surfaces. In respect to shape they were far superior to the sapphire then readily available. Such jewels had, however, two defects:

1. The manufacture had to be very carefully controlled and was not quite as simple as the description above may sound.
2. The jewel would show a microscopic indentation under an impact such as that produced by dropping the instrument, or by a heavy blow on the mounting panel.

This microscopic blemish in the surface would, however, in general not be noticeable in the performance of the instrument unless the pivot happened to run on the exact spot where the blemish had been made. Even in the latter case, the performance of the instrument would not be materially affected, unless a large number of pointer oscillations or the presence of vibration caused excessive wear at the roughened spot.

As has been mentioned before, a large number of these jewels were used, and their use probably served as an incentive to better the quality of sapphire jewels. In any event, after the hot-formed jewels had been used for several years, sapphire jewels became available with much improved contour, smaller radius of the spherical surface in the bottom of the Vee,

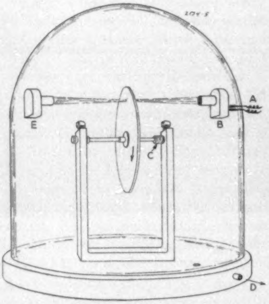

Figure 5. Apparatus for determining coefficient of friction

A—To amplifier and chronograph
B—Phototube
C—Vee jewel bearings or ring jewel bearings under test
D—To vacuum pump
E—Light source and lens

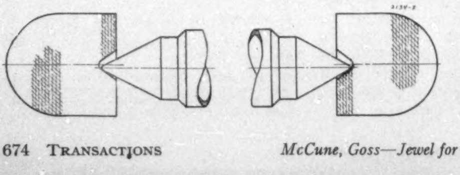

Figure 3. Scale drawing showing proportions and clearances of horizontal shaft miniature instrument bearing

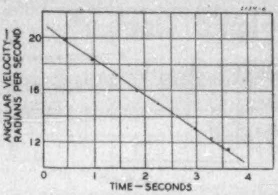

Figure 6. Velocity versus time curve

Deceleration 2.66 radians per second per second

and in every way suitable for instrument use. As a result, the use of the hot-formed jewel was practically abandoned although they have been used in certain instruments from time to time with satisfactory field performance.

The present emergency made necessary a review of the whole situation and the following steps were taken:

1. The performance of the hot-formed jewels and sapphire jewels was carefully re-evaluated.
2. A search for the optimum material for a hot-formed jewel was started, since it was recognized that new materials might be available which would be distinctly superior to that chosen as the best 10 or 15 years ago.

The evaluation of relative performance of the hot-formed jewel and the sapphire jewel was carried out in the following manner with results as shown in the corresponding tables and figures:

(*a*). *Friction.* One criterion of instrument performance is that portion of the error in an instrument with a horizontal shaft caused solely by the friction between the pivot and jewel. This, when measured by noting the difference between upscale and downscale readings untapped in an instrument free from magnetic hysteresis, is known as jewel roll. Jewel roll, expressed in radians, is a function of moving system weight, restoring torque, radius of the pivot, and coefficient of friction between pivot and jewel. The formula has been given as follows: for the usual case where the end of the pivot is spherical and the surface of the jewel conical:[1]

$$\text{Jewel roll} \propto \frac{WCr}{K}$$

W—weight of the moving system in grams

C—coefficient of friction between the pivot and jewel

r—radius of the spherical end of the pivot in millimeters

K—restoring force in gram-millimeters per radian

In order to determine the friction between an instrument pivot and the corresponding jewel surface, two jewels of the material to be tested and a rotating member made up to approximately duplicate an instrument moving system were mounted in a vacuum (Figure 5). The moving system was made to rotate, the force causing this rotation was removed, and the deceleration measured. Figure 6 is a typical deceleration curve. From this deceleration, the moment of inertia, and dimensions of the parts involved, the coefficient of friction was calculated. While not having direct bearing on this investigation, certain data taken on ringstone jewels are included.

Table I. Measured Coefficient of Friction When Run Against a High-Carbon Steel Pivot

Material	Form	Coefficient of Friction
Sapphire	Ring	0.140
Leaded Brass	Ring	0.188
Plastic *A*	Ring	0.160
Plastic *B*	Ring	0.190
Graphite	Ring	0.160
Sapphire	Vee	Range 0.15 to 0.19
Glass	Vee	0.18
Material *H* (Figure 11)	Vee	0.18
Ceramic polished	Vee	0.28
Ceramic unpolished	Vee	0.33

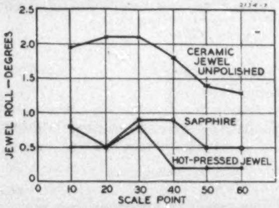

Figure 7. A comparison of jewel roll in a single instrument taken from three different sets of Vee jewels

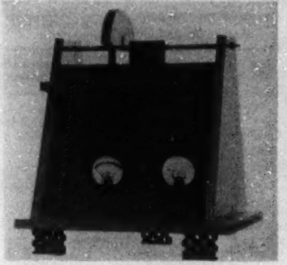

Figure 8a. Impact testing device—front view

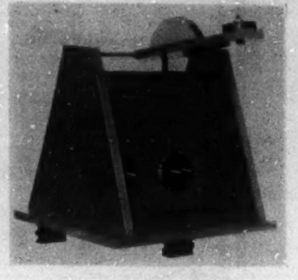

Figure 8b. Impact testing device—back view

(*b*). Actual measurements of jewel roll with three materials involved were made using a very low torque instrument, with the results shown in Figure 7. These measurements were, of course, not nearly as accurate as the measurements under (*a*), but were simply confirmation that the measurements as taken in (*a*) are directly applicable to the problem.

(*c*). An impact device was prepared, two views of which are shown in Figure 8. After some experimentation, a test schedule was worked out as follows: The pendulum was raised ten degrees from the vertical, allowed to fall and strike the panel, and caught on the rebound. The instrument jewel roll was measured and the process repeated, the angle of drop being increased each time by ten degrees until 180 degrees was reached. Figure 9 shows the performance of two instruments mounted as shown on the same panel, one with sapphire jewels and one with hot-formed jewels from material *G* (Figure 11), which was the material used for jewels in the past.

(*d*). After impact a group of instruments, half with sapphire and half with hot-formed jewels of material *G*, were run from zero to full scale and back approximately 900,000 times, and the jewel roll again noted with results as shown in Table II.

All of the above were done with several different instrument designs and torques, and representative values are given in the tables. Conclusions from the above were that it was highly desirable to increase the impact strength of the hot-formed jewel.

(*e*). An arrangement as shown in Figure 10 was made by which a jewel could be accurately located below the opening of a glass tube. A hardened steel instrument pivot was placed in the Vee and struck by dropping a pellet weighing 0.1 gram upon it. As shown in the figure, the pellet was raised by a magnet to an exact height and released by removing the magnet. After each drop, the jewel was carefully examined under the microscope for any traces of fracture of the surface and another trial made if no fracture occurred. The method finally worked out as giving the most consistent results was to

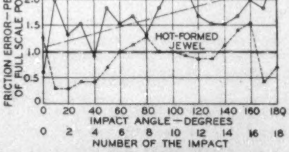

Figure 9. Increase of instrument friction as a result of successive impacts in ten-degree steps on panel impact device

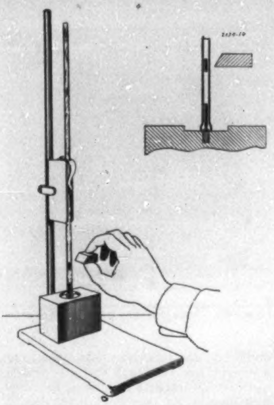

Figure 10. Jewel impacting device

drop the weight five times from a distance of 2 inches, five times from a distance of 6 inches, and five times from a distance of 12 inches. A jewel would be given a score of one for each time it successfully withstood a 2-inch drop, a score of three for each time it successfully withstood a 6-inch drop, and a score of six for each time it withstood a 12-inch drop. The total score of a group of five jewels was used in making the evaluation in each case. It should be noted that the pivots were initially selected by 100-diameter contour measurement and a 40-diameter visual inspection to be sure that they did not introduce another variable.

Figure 11 shows the comparative values of a number of different materials including material *G* which had been in use for many years.

It should be noted that the impact strength is not a function of material only, since jewels were actually produced from material *H* which varied in impact strength from practically zero when overstrained to a value of 30 when completely annealed as shown at *I*. The value plotted at *H* is the average which can be held for the optimum manufacturing process worked out.

The materials investigated covered ordinary soft glass *A*; a considerable range of so-called "hard glasses," *B*, *C*, *D*, *E*, *F*, *G*, the material used some years ago, and *H*. Material *H* has a very high softening point and is unique in having a large percentage of alumina.

Table II. Jewel Roll of Instruments After Approximately 900,000 Pointer Movements Up and Down Scale

	Instrument No.	Angle* (in Degrees)
Hot-formed jewels	1	3.2
	2	3.2
	3	0.3
	4	1.5
	5	0.6
Sapphire jewels	1	1.0
	2	0.9
	3	1.0
	4	1.0
	5	1.2

Initial jewel roll was negligible.

* Instrument scale angle 90 degrees.

Since vibration is present in many instrument applications, *H* jewels were compared to sapphire in respect to bearing life under vibration. A table which gave each point in the instrument a circular motion in a plane 45 degrees from the horizontal with a diameter of 0.020 inch and a frequency of 1600 rpm was used. Results showed the two bearings to be comparable.

From an analysis of the above data and examination of the pivots and jewels it was obvious that material *H* represents very distinct progress over the previously used glass jewel. Since sapphire is harder than the steel pivot, the effect of heavy impact is to cause a deformation of the pivot. When this deformation occurs, the precision of the instrument is impaired, because the moving system is no longer supported by a smooth bearing, but is rolling on an irregularly shaped mushroomed pivot. This effect will always be present and will be noticeable at any part of the scale, since the whole end of the pivot is deformed. In the case of material *H*, the point at which the jewel material deforms is approximately the same as that at which the pivot deforms, so that up to this critical point, impact has no effect. Beyond this point, there is very slight deformation of the pivots, and a minute area of the jewel surface is crushed. Because of the fact that the pivot flies up from its regular seat under impact, this deformed part of the jewel is very likely not to be at the point where the pivot usually travels and, therefore, is unlikely to affect the performance of the instrument in any way.

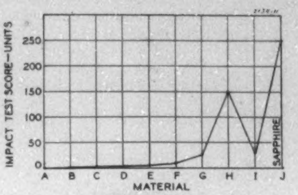

Figure 11. Impact tests on hot-formed jewels and on sapphire jewels

The technique of manufacturing instrument jewels with carefully controlled dimensions to give the impact strength of *H* has been worked out, and these can now be produced in quantities sufficient to assure that the jewel bearings will not be a limiting feature in the quantity of instruments which may be produced.

In conclusion, the original result desired was a jewel which could be produced in quantity, and which would involve the least possible sacrifice over sapphire for miniature instruments. The data indicate that there is little to choose between the new jewel and sapphire for actual miniature instrument application.

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A New Single-Phase-to-Ground Fault-Detecting Relay

W. K. SONNEMANN
ASSOCIATE AIEE

Synopsis: In the application of differential relays the need for a supervising relay which will detect the existence of a single-phase-to-ground fault condition to the exclusion of all others has frequently arisen. Heretofore, the only scheme available has been to utilize a relay energized by zero-sequence quantities. Generally, the relay has been energized by a current transformer connected in the station ground. Such a relay, however, will also detect the existence of a two-phase-to-ground fault and only partially solves the problem. The new relay described in this paper derives its operating force from the zero-sequence voltage at the bus and is restrained by the negative-sequence voltage at the bus. The addition of properly proportioned negative-sequence restraint provides the relay with a means of recognizing a single-phase-to-ground fault only. It is applicable on those systems where the zero-sequence impedance of the system exceeds the negative-sequence impedance by a reasonable margin.

Application

IN the application of differential relays to the protection of generating station busses, it is frequently the case that single-phase-to-ground fault protection only is desired. In such cases, the differential relay is connected in the residual circuit, or zero-sequence network, from the current transformers. Again, it is sometimes desirable to supplement three-phase differential relays with a fourth differential relay connected as above in order to obtain increased sensitivity for phase-to-ground faults. This latter condition is frequently encountered when systems are grounded through an impedance, so that the maximum value of phase-to-ground fault current is distinctly limited. In such cases the setting of the fourth or ground relay is considerably more sensitive than that of the phase relays, and the problem then becomes one of keeping the residual differential relay from tripping erroneously for heavy external phase-to-phase short circuits. Where conventional relays are used with current transformers, the major difficulty arises when a false differential current appears in the ground relay caused by unequal saturation of the various current transformers in the faulted phases, this condition being particularly emphasized by the presence of d-c transient. When the linear coupler relay scheme[1] is used, the fourth or ground relay set sensitive may also operate falsely for heavy external phase faults, not because of saturation, but depending upon the relative values of the maximum fault currents, the sensitivity of the relay, and the accuracy of the linear coupler transformers.

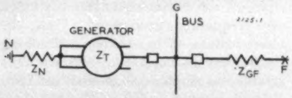

Figure 1. Schematic diagram of generator bus with fault on feeder circuit

Z_N—Grounding impedance
Z_T—Impedance of generator
Z_{GF}—Impedance from bus, G, to fault F
1, 2, 0—Where necessary, these subscripts are used in addition to those shown above to indicate the sequence network involved

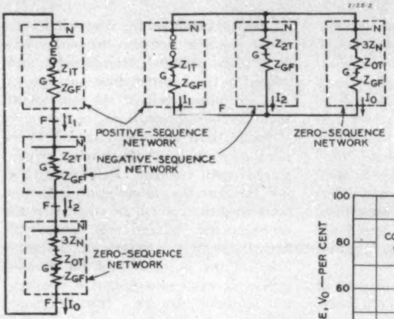

Figure 2. Sequence network connections for

(*a*). Single-phase-to-ground fault
(*b*). Two-phase-to-ground fault

A solution to this problem is to block out the ground relay for every type of fault except single-phase-to-ground. This applies whether conventional relays and current transformers, or the new linear-coupler scheme is used. The method is to connect the contacts of the supervising, or single-phase-to-ground fault-detecting relay, in series with the contacts of the main differential relay. If the latter relay then closes its contacts in error upon the occurrence of a heavy external two-phase-to-ground fault, for example, but the supervising relay properly maintains its contacts in the open position, no harm results. Up to now, no such supervising relay has been available which would recognize the existence of a single-phase-to-ground fault to the exclusion of all other types of faults. The new relay described in this paper is such a relay.

Operating Principle

The new relay operates in accordance with the magnitude of the negative-sequence voltage and the zero-sequence voltage at the bus. The contact-closing force in the relay is developed in proportion to the zero-sequence voltage, while the restraining force opposing contact closing is proportional to the negative-sequence voltage.

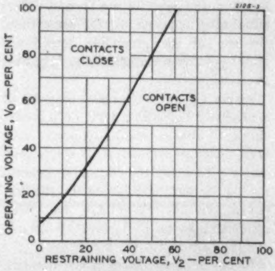

Figure 3. Typical operating characteristics of relay

The voltages shown are measured on the input side of the sequence filters
V_0—Zero-sequence voltage
V_2—Negative-sequence voltage

The proper application of the relay requires that the zero-sequence impedance of the system must exceed the negative-sequence impedance of the system by a reasonable margin. This requirement goes hand in hand with the need for the

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W. K. SONNEMANN is design engineer with Westinghouse Electric and Manufacturing Company, Newark, N. J.

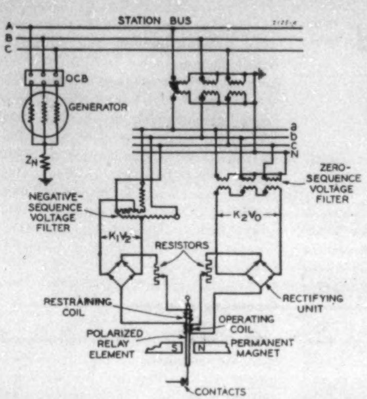


Figure 4 (left). Schematic diagram of connections of relay elements to station bus

K_1 and K_2 are design constants of the filter units

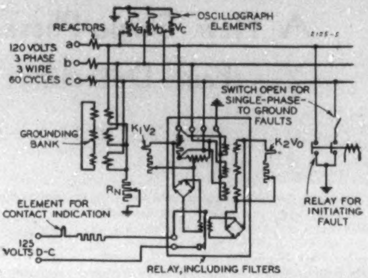

OSCILLOGRAPH ELEMENTS
REACTORS
V_a
V_b
V_c
120 VOLTS 3 PHASE 3 WIRE 60 CYCLES
a
b
c
SWITCH OPEN FOR SINGLE-PHASE-TO GROUND FAULTS
GROUNDING BANK
K_1V_2
K_2V_0
R_N
RELAY FOR INITIATING FAULT
ELEMENT FOR CONTACT INDICATION
125 VOLTS D-C
RELAY, INCLUDING FILTERS

Figure 5 (right). Diagram of test connections

relay. On solidly grounded systems, where the ratio of zero-sequence to negative-sequence impedance at the generating station bus is low, ground-fault protection will generally be obtained by means of the phase differential relays, thus eliminating the need for the supervision of a nonexistent sensitive ground relay. When a grounding impedance is used, however, the need for the ground differential relay appears. At the same time, the use of the grounding impedance makes possible the application of the new supervising relay.

Figure 1 shows a schematic diagram of a generator bus with a fault on a feeder circuit. The generating capacity has been shown as an equivalent single generator. To make the case more general for the detailed derivations in the appendix, an impedance, Z_{GF}, has been assumed between the generator bus, G, and the fault at F.

Figure 2 shows the sequence network connections for single-phase-to-ground and two-phase-to-ground faults.[3] Phase-to-phase and three-phase faults need not be considered, inasmuch as no zero-sequence voltage is involved for these faults, and hence the relay would not experience any operating force. For convenience in simplifying the explanation of the relay operation, consider for the moment that the impedance, Z_{GF}, between the generator bus and the fault, F, is zero. This is equivalent to assuming that the fault occurs directly on the bus. Referring to Figure 2b for the two-phase-to-ground fault condition, it will be seen that the negative-sequence voltage to neutral at the bus is of necessity equal to the zero-sequence voltage to neutral, since the two networks are connected in parallel. Under this condition the relay will receive equal negative- and zero-sequences voltages from potential transformers connected to the bus and must not operate. The restraining coil of the relay must, therefore, be so proportioned as to be more powerful than the operating coil of the relay when both of these relay circuits receive equal voltages. Referring to Figure 2a for the single-phase-to-ground fault condition, it will be noted that the zero-sequence voltage will exceed the negative-sequence voltage in the same ratio as the impedance of the zero-sequence network exceeds that of the negative-sequence network. It will be noted that the value of the grounding impedance, Z_N, has been inserted in the zero-sequence network at three times its actual value in accordance with the conventional theorem. Since the same numerical value of current flows through the negative-sequence and zero-sequence network of Figure 2a, it follows that it is only necessary for the impedance of the zero-sequence network to be sufficiently high with respect to the impedance of the negative-sequence network to cause relay operation. The ohmic value of the grounding impedance, Z_N, will generally run quite high compared to the negative-sequence impedance of the generator, so that this condition is not difficult to meet.

Assume that the negative- and zero-sequence networks of Figure 2 are equal in ohmic value. It then follows that the negative- and zero-sequence voltages would be equal for each of these two cases. This is the limiting value in that if the relay-restraining and operating coils were so proportioned that the relay would be on the verge of closing, its contacts for a two-phase-to-ground fault, Figure 2b, then it would also be on the verge of closing its contacts for a single-phase-to-ground fault, Figure 2a. It follows more or less obviously that if Z_0 is greater than Z_2, the relay can be properly proportioned to discriminate between single-phase-to-ground and two-phase-to-ground short circuits. (Discrimination is automatic for phase-to-phase and three-phase short circuits, since no zero-sequence voltage is involved in these faults.)

It is desirable to establish a criterion for limits of application, and in this case it is felt that if the ratio Z_0/Z_2 is equal to or greater than 2/1, then a relay having characteristics similar to those illustrated in Figure 3, will meet the requirements satisfactorily.

Typical Application

Assume that Figure 1 represents a 13.2-kv installation and that the equivalent single generator has a rating of 20,000 kva. This is a relatively small capacity but is so chosen as to be on the pessimistic side in that the negative-sequence impedance will be relatively high. Typical values for the subtransient and negative-sequence reactance have been taken at 12 per cent each, and a zero-sequence reactance of four per cent has been assumed for the machine. The grounding impedance, Z_N, is assumed to be a two-ohm resistor, inasmuch as this is a value very frequently encountered. Using the above values of impedance and taking 7,620 volts to neutral as 100 per cent voltage, single-phase-to-ground and two-phase-to-ground faults on the bus were calculated. For the single-phase-to-ground fault, the negative-sequence voltage is 16.2 per cent and the zero-sequence voltage is 93.2 per cent. From the curve, Figure 3, when $V_2 = 16.2$ per cent, the required value of zero-sequence voltage, V_0, to operate the relay is 26 per cent. It is thus seen that the margin of the zero-sequence voltage available over and above that required to barely operate the relay is ample. For the two-phase-to-ground fault, values of $V_2=49.5$ per cent and $V_0=49.5$ per cent were calculated for the assumed system. From the curve, it is found that at 49.5 per cent restraint voltage, the operating voltage, V_0, would have to be 79.5 per cent. Thus, the relay will not operate for the two-phase-to-ground fault. The calculations have been based on using the subtransient reactance of the machine in the positive-sequence network, since the relay is a high-speed relay.

While the relay has actually been made in the high-speed form, operating in one cycle or less, the principles involved are easily applied to other types of design so that the relay could be made "slow speed" if desired.

Figure 4 shows a schematic diagram of connections illustrating how the relay is energized from potential transformers connected to the station bus.

Tests

In order to check the actual operation of the relay against the theory, a test setup was made as shown in Figure 5. A system was represented in miniature. A variable resistor, R_N, was utilized as a grounding impedance in order that a minimum value for proper relay operation might be obtained and this checked against the calculations.

The location of oscillograph elements is shown in Figure 5. Elements were used to obtain traces of the phase-to-neutral voltages, V_a, V_b, and V_c, and the negative- and zero-sequence voltages, K_1V_2 and K_2V_0. The sixth element was used to obtain an indication of the operation of the relay contact. It should be noted that the output of the sequence filters is not numerically equal to the input quantities; hence the constants of proportionality, K_1 and K_2, have been used as indicated above.

Figure 6a shows the results of a test where phase *a* was faulted to ground. The ratio Z_0/Z_2 was 14.8 for this test. It will be observed that the relay properly closed its contact in 0.83 cycle. It will also be noted that there is a slight 60-cycle component and an appreciable harmonic component in the output of the negative-sequence filter prior to the fault. The 60-cycle component is explained by the fact that the filter was not perfectly balanced in its adjustment, and neither were the three-phase-to-neutral voltages perfectly balanced in the miniature setup. The existence of the harmonic component is occasioned by the fact that an iron core reactor is utilized in the filter design. In practice, the fact that the negative-sequence filter will have a combined output of fundamental and harmonic frequencies on the order of two volts or less under normal balanced conditions does not do any harm.

Figure 6b shows the results of a two-

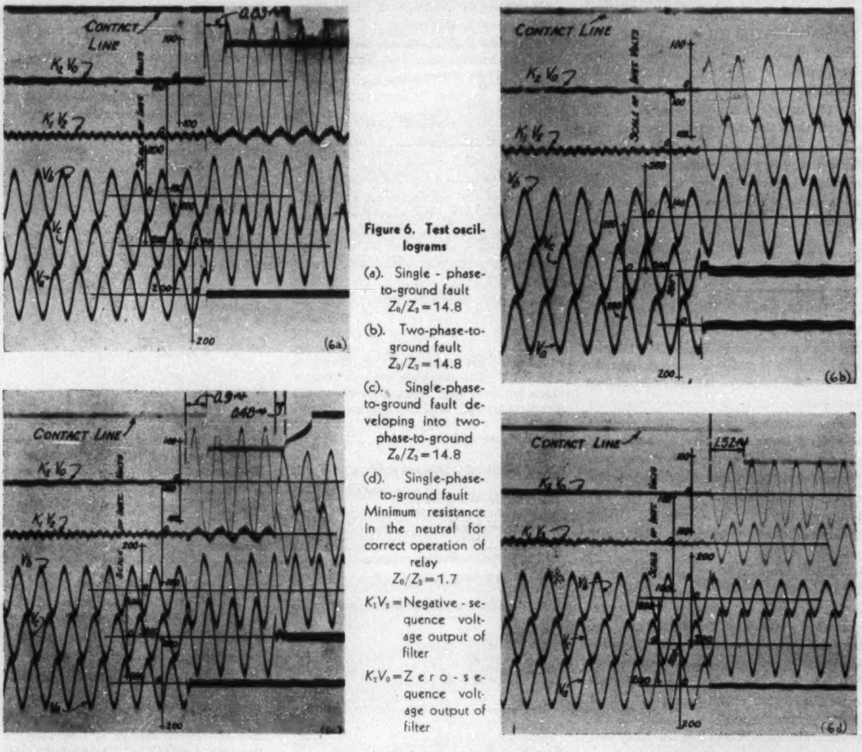


Figure 6. Test oscillograms

(a). Single-phase-to-ground fault $Z_0/Z_2=14.8$

(b). Two-phase-to-ground fault $Z_0/Z_2=14.8$

(c). Single-phase-to-ground fault developing into two-phase-to-ground $Z_0/Z_2=14.8$

(d). Single-phase-to-ground fault Minimum resistance in the neutral for correct operation of relay $Z_0/Z_2=1.7$

K_1V_2 = Negative-sequence voltage output of filter

K_2V_0 = Zero-sequence voltage output of filter phase-to-ground fault on phases *a* and *c*. The ratio $Z_0/Z_2=14.8$ was kept the same as before. The calibration of the oscillograph elements remained the same, as in all cases to follow. The contacts of the relay did not operate. The relative magnitude of the deflections of the elements measuring K_1V_2 and K_2V_0 should be compared for Figures 6a and 6b.

In Figure 6c, the results are shown for a fault which started as one phase-to-ground and developed into a two-phase-to-ground fault. As before, the ratio of Z_0/Z_2 was kept at 14.8. The contacts first closed, as they should, in 0.9 cycle. When the second phase faulted to ground, the contacts opened in 0.48 cycle. The film indicates that the contacts were slow in opening the contact circuit. This is for the reason that the d-c current used in this element was kept high—just about at the limit that the relay contacts would successfully break. In practice, these contacts would not be expected to open heavy currents.

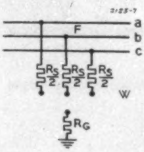


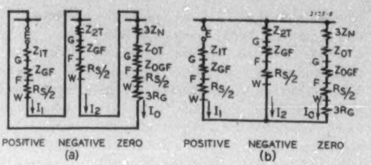
Figure 7. Diagram showing arc resistance at the point of fault

The resistance, R_N, was next reduced to such a value that the relay would just discriminate satisfactorily between one-phase- and two-phase-to-ground faults and the film, Figure 6d, was taken. For this case, the ratio Z_0/Z_2 was 1.7. Using the impedance values for the miniature system, a single-phase-to-ground fault condition was calculated. The calculated values were: $V_0=58.4$ per cent and $V_2=34.2$ per cent. At a negative-sequence restraining voltage of 34.2 per cent, the curve, Figure 3, indicates that the zero-sequence operating voltage required to just overcome this restraint should be not more than 54 per cent. An apparent error of 8.2 per cent is indicated in the calculations. This is explained on the basis that there were errors in the miniature setup which could be eliminated if it were found necessary to run the test with precision laboratory accuracy. The errors include the slight unbalance in the negative-sequence filter which was previously mentioned, a slight mutual coupling which existed between the reactors used in the supply circuit, and the fact that the burden of the sequence filters was appreciable with respect to other constants of the miniature system. None of these factors was taken into consideration in the calculations.

Figure 8. Connections of sequence networks for

(*a*). Single-phase-to-ground fault

(*b*). Two-phase-to-ground fault



In Figure 6d, the contact closing time 1.52 cycles under minimum operating conditions is still reasonably fast.

Conclusion

A relay design is now available which will properly discriminate between single-phase-to-ground faults and all other types of faults on grounded systems when the ratio of the zero-sequence impedance of the system to the negative-sequence impedance is greater than unity.

Appendix

Let it be assumed that the fault occurs at the point, *F*, of Figure 1, and that the arc resistances are represented as shown in Figure 7. The sequence network connections are shown by Figure 8 for the single-phase-to-ground and two-phase-to-ground faults. The points *G*, *F*, and *W* are shown in these diagrams.

Case I—Single-Phase-to-Ground Fault

Referring to Figure 8a, let

$$\mathbf{Z}_1=\mathbf{Z}_{1T}+\mathbf{Z}_{GF}+\mathbf{R}_{s/2} \qquad (1)$$

$$\mathbf{Z}_2=\mathbf{Z}_{2T}+\mathbf{Z}_{GF}+\mathbf{R}_{s/2} \qquad (2)$$

and $$\mathbf{Z}_0=3\mathbf{Z}_N+\mathbf{Z}_{0T}+\mathbf{Z}_{0GF}+\mathbf{R}_{s/2}+3\mathbf{R}_G \qquad (3)$$

In the series connections of networks

$$\mathbf{I}_1=\mathbf{I}_2=\mathbf{I}_0 \qquad (4)$$

and $$\mathbf{I}_1=\frac{\mathbf{E}}{\mathbf{Z}_1+\mathbf{Z}_2+\mathbf{Z}_0} \qquad (5)$$

The negative- and zero-sequence voltage drops at the bus, *G*, are

$$\mathbf{E}_{2G}=0-\mathbf{I}_2\mathbf{Z}_{2T} \qquad (6)$$

and $$\mathbf{E}_{0G}=0-\mathbf{I}_0(3\mathbf{Z}_N+\mathbf{Z}_{0T}) \qquad (7)$$

Substituting $\mathbf{I}_2$ for $\mathbf{I}_0$ (since these are equal, equation 4) in equation 7, and dividing equation 6 by equation 7, a ratio, R_1, is obtained

$$R_1=\frac{\mathbf{E}_{2G}}{\mathbf{E}_{0G}}=\frac{\mathbf{Z}_{2T}}{(3\mathbf{Z}_N+\mathbf{Z}_{0T})} \qquad (8)$$

Equation 8 gives the relationship existing between the negative-sequence restraining voltage and the zero-sequence operating voltage when the relay must operate.

Case II—Two-Phase-to-Ground Fault

Referring to Figure 8b, let equations 1, 2, and 3 stand as before. In this case, however

$$\mathbf{I}_1=\frac{\mathbf{E}(\mathbf{Z}_0+\mathbf{Z}_2)}{\mathbf{Z}_1\mathbf{Z}_0+\mathbf{Z}_1\mathbf{Z}_2+\mathbf{Z}_2\mathbf{Z}_0} \qquad (9)$$

$$\mathbf{I}_2=-\mathbf{I}_1\frac{\mathbf{Z}_0}{\mathbf{Z}_2+\mathbf{Z}_0} \qquad (10)$$

$$\mathbf{I}_0=-\mathbf{I}_1\frac{\mathbf{Z}_2}{\mathbf{Z}_2+\mathbf{Z}_0} \qquad (11)$$

As in Case I,

$$\mathbf{E}_{2G}=0-\mathbf{I}_2\mathbf{Z}_{2T} \qquad (6)$$

and $$\mathbf{E}_{0G}=0-\mathbf{I}_0(3\mathbf{Z}_N+\mathbf{Z}_{0T}) \qquad (7)$$

Substituting for $\mathbf{I}_2$ and $\mathbf{I}_0$ in terms of $\mathbf{I}_1$, equations 6 and 7 become

$$\mathbf{E}_{2G}=0+\mathbf{I}_1\frac{\mathbf{Z}_0}{\mathbf{Z}_2+\mathbf{Z}_0}\mathbf{Z}_{2T} \qquad (12)$$

$$\mathbf{E}_{0G}=0+\mathbf{I}_1\frac{\mathbf{Z}_2}{\mathbf{Z}_2+\mathbf{Z}_0}(3\mathbf{Z}_N+\mathbf{Z}_{0T}) \qquad (13)$$

A new ratio, R_2, between the negative-sequence restraining voltage and zero-sequence operating voltage, is obtained by dividing equation 12 by equation 13

$$R_2=\frac{\mathbf{Z}_0\mathbf{Z}_{2T}}{\mathbf{Z}_2(3\mathbf{Z}_N+\mathbf{Z}_{0T})} \qquad (14)$$

Summary

If the relay is to discriminate properly between the conditions of case I and case II, then the ratio, R_2, must be greater than the ratio, R_1. Dividing equation 14 by equation 8, there results

$$R_2/R_1=\frac{Z_0}{Z_2} \qquad (15)$$

Equation 15 indicates that if $Z_0/Z_2>1$, then $R_2/R_1>1$, and the relay will receive more restraining voltage in proportion to operating voltage for the two-phase-to-ground fault condition than it will for the single-phase-to-ground fault.

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On Eddy Currents in a Rotating Disk

W. R. SMYTHE
NONMEMBER AIEE

A DEVICE which often occurs in electric machines and instruments consists of a relatively thin conducting disk rotating between the pole pieces of a permanent magnet or electromagnet. The author has received inquiries as to the method of calculating the paths of the eddy currents and the torque in such cases. The following rather simple method, which is quite accurate for a permanent magnet, seems not to be described in the literature. It assumes that the disk is so thin that the skin effect can be neglected. This is true for all frequencies that can be produced mechanically. To facilitate calculation in the special case of circular poles it is also assumed that $2\pi\omega ab\gamma = \epsilon a$ is much less than one where ω is the angular frequency of rotation in radians per second, a the pole-piece radius, b the disk thickness, and γ the electric conductivity, all in centimeter-gram-second electromagnetic units. This produces a fractional error of less than ϵa in the eddy current densities and of less than $(\epsilon a)^2$ in the torque. In the case of the electromagnet the situation is complicated by the presence of the permeable pole pieces in the magnetic field of the eddy currents. This may send a large demagnetizing flux through the electromagnet. An approximate solution for this case will be considered.

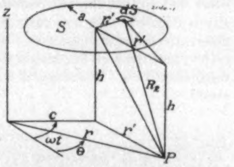

Figure 1. Geometrical relations for derivation of formulas for stream function

Maxwell's Formula

This calculation starts from a formula given by Maxwell in 1873,[1] but apparently little known to engineers. To apply it one should know its derivation which, as given by Maxwell, is difficult for modern students to follow. A simplified proof which brings out the points essential for our problem is given below.

The object is to calculate the magnetic induction **B** produced by the eddy currents of density *i* induced in a thin plane sheet of thickness b, unit permeability and conductivity γ lying in the xy plane by a fluctuating magnetic field of induction **B'** Evidently the only components of *i* effective in producing magnetic effects parallel its surface. Let the eddy currents be confined to a finite region of the sheet which may or may not extend to infinity, and let us define the stream function $U(x, y)$ at any point in the sheet to be the current flowing through any cross section of the sheet extending from P to its edge. The line integral of **B** or **H** over the closed path that bounds this section equals $4\pi U$. From symmetry the contribution from the upper and lower halves of the path is the same so we may write

$$4\pi U = \oint \boldsymbol{B}\cdot d\boldsymbol{s} = \pm 2\int_x^\infty B_x dx = \pm 2\int_y^\infty B_y dy \qquad (1)$$

where the choice of sign depends on the side of the sheet chosen for the integration. Differentiating this equation gives

$$bi_x = \frac{\partial U}{\partial y} = \pm\frac{B_y}{2\pi} \qquad bi_y = -\frac{\partial U}{\partial x} = \mp\frac{B_x}{2\pi} \qquad (2)$$

These equations connect the eddy current density with the tangential components of the magnetic induction **B** produced by *i* at the surface of the sheet.

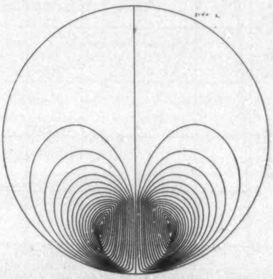

Figure 2. Lines of flow of eddy currents induced in rotating disk by single circular magnet pole

The eddy currents are generated not only by the changes in the magnetic induction **B'** of the external field, but also by the changes of the magnetic induction **B** of eddy currents elsewhere in the sheet. One of Maxwell's equations combined with Ohm's law gives the induced current to be

$$\nabla\times\boldsymbol{E} = \nabla\times\frac{\boldsymbol{i}}{\gamma} = -\frac{\partial}{\partial t}(\boldsymbol{B}'+\boldsymbol{B}) \qquad (3)$$

Writing out the z component of this equation and using equation 2 give

$$\frac{1}{\gamma}\left(\frac{\partial i_y}{\partial x}-\frac{\partial i_x}{\partial y}\right) = \mp\frac{1}{2\pi b\gamma}\left(\frac{\partial B_x}{\partial x}+\frac{\partial B_y}{\partial y}\right) = -\frac{\partial}{\partial t}(B_z'+B_z) \qquad (4)$$

Another of Maxwell's equations states that

$$\nabla\cdot\boldsymbol{B} = \frac{\partial B_x}{\partial x}+\frac{\partial B_y}{\partial y}+\frac{\partial B_z}{\partial z} = 0 \qquad (5)$$

Combining equations 4 and 5 gives

$$\pm\frac{\partial(B_z'+B_z)}{\partial t} = \frac{1}{2\pi b\gamma}\,\frac{\partial B_z}{\partial z} \qquad (6)$$

When $\partial\boldsymbol{B}_z'/\partial t$ is known, this equation gives the boundary condition on $\boldsymbol{B}_z$ in the plane of the sheet. This, combined with the equations $\nabla\times\boldsymbol{B}=0$ and $\nabla\cdot\boldsymbol{B}=0$ which hold outside the sheet, and the fact that **B** vanishes at infinity serves to determine **B** everywhere. By equations 1 and 2 the current density and stream function anywhere in the sheet can be found.

The explicit expression for **B** in terms

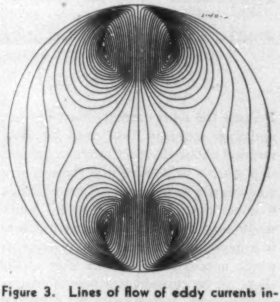

Figure 3. Lines of flow of eddy currents induced in rotating disk by two circular magnet poles

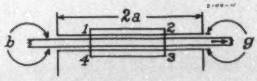

Figure 4. Geometrical relations for calculation of demagnetizing flux

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W. R. Smythe is professor in the department of physics, California Institute of Technology, Pasadena, Calif.

of $\boldsymbol{B}'$ which was given by Maxwell[1] can be obtained as follows. The right side of equation 6 is finite at all times which means that if $\Delta t \rightarrow 0$ then $\Delta(B_z'+B_z)\rightarrow 0$. Thus an abrupt change in $\boldsymbol{B}'$ instantaneously induces eddy currents such as will maintain $\boldsymbol{B}'+\boldsymbol{B}$ unchanged in the sheet. Therefore, for a specified change in $\boldsymbol{B}'$ the initial value of $\boldsymbol{B}$ is known, and, if no further changes in $\boldsymbol{B}'$ occur its subsequent values as the eddy currents decay are found by putting $\partial\boldsymbol{B}'/\partial t=0$ in equation 4 and solving. A second abrupt change in $\boldsymbol{B}'$ produces a second set of eddy currents, and so forth. At any instant the actual field of the eddy currents is a superposition of these. As the magnitudes of the discontinuous changes in the external field become smaller, and the intervals between them shorter, we approach as a limit a continuously changing magnetic field.

Suppose that the sources of the inducing field lie above the xy plane where $z>0$. At $t=0$ the source changes abruptly the induction being $\boldsymbol{B}_1'=\boldsymbol{F}_1(x,\ y,\ z)$ when $-\infty<t<0$ and $\boldsymbol{B}_2'=\boldsymbol{F}_2(x,\ y,\ z)$ when $0<t<\infty$. As just shown the eddy currents generated at $t=0$ initially keep the field on the negative side of an infinite sheet unchanged. When $z<0$ we have therefore

$$\boldsymbol{B}_{t=0}=\boldsymbol{B}_1'-\boldsymbol{B}_2'=\boldsymbol{F}_1(x,\ y,\ z)-\boldsymbol{F}_2(x,\ y,\ z) \quad (7)$$

Since $\boldsymbol{B}_2'$ is not a function of t, equation 6 reduces to

$$\pm\frac{\partial B_z}{\partial t}=\frac{1}{2\pi b\gamma}\,\frac{\partial B_z}{\partial z} \quad (8)$$

These equations, 7 and 8, are satisfied by

$$\boldsymbol{B}=\boldsymbol{F}_1\left(x,\ y,\ z\pm\frac{t}{2\pi b\gamma}\right)-\boldsymbol{F}_2\left(x,\ y,\ z\pm\frac{t}{2\pi b\gamma}\right) \quad (9)$$

Because the eddy currents must die out, and their magnetic field must be symmetrical about the sheet, we take the plus sign when z is positive and the negative sign when z is negative. Thus equation 9 shows that, in addition to $\boldsymbol{B}_2'$ which would exist if no sheet were present, there is a decaying field due to eddy currents which appears, from either side of the sheet, to be caused by a pair of images receding with uniform velocity $1/(2\pi b\gamma)$. Suppose our inducing field has the form

$$\boldsymbol{B}'=\boldsymbol{F}(t,\ x,\ y,\ z) \quad (10)$$

The change in this field in an infinitesimal time interval $d\tau$ is given by

$$\frac{\partial\boldsymbol{B}'}{\partial t}\,d\tau=\frac{\partial}{\partial t}\,\boldsymbol{F}(t,\ x,\ y,\ z)d\tau \quad (11)$$

The initial field of the eddy currents formed in that interval must be equal and opposite to this and must die out as if their source moved away with a uniform speed $1/(2\pi b\gamma)$. Thus the eddy currents at a time t due to a change in the interval $d\tau$ at a time τ before t is given by

$$d\boldsymbol{B}=-\frac{\partial}{\partial t}\,\boldsymbol{F}\left(t-\tau,\ x,\ y,\ z\pm\frac{\tau}{2\pi b\gamma}\right)d\tau \quad (12)$$

This is Maxwell's formula. It has many applications.[2] When the field is produced by moving permanent magnets, it is convenient to express U in terms of the scalar magnetic potential Ω. Since we have unit permeability we may write

$$U=\frac{1}{2\pi}\int_r^\infty B_r dr=\frac{-1}{2\pi}\int_r^\infty\frac{\partial\Omega}{\partial r}\,dr=\frac{\Omega}{2\pi} \quad (13)$$

Application to Magnet Moving in a Circle

We now take the case of a magnetic field produced by a long right circular cylinder of radius a, uniformly and permanently magnetized parallel to its axis, so as to give a total flux Φ. The magnetic pole density in the face is therefore $\Phi/(2\pi a)^2$. This magnet moves in a circle with a uniform angular velocity ω its axis being c centimeters from the z axis, and its lower end h centimeters above the $z=0$ plane in which lies an infinite plane sheet of thickness b and conductivity γ. Its upper end is too remote for consideration. Polar co-ordinates will be written unprimed or primed according as they refer to the axis of rotation or to the pole-piece axis. The scalar magnetic potential Ω' of its lower face, of area S, at the point P is seen from Figure 1 to be

$$\Omega'=\frac{\Phi}{(2\pi a)^2}\int_S\frac{dS}{R_0}=\frac{\Phi}{(2\pi a)^2}\times\int_0^a\int_0^{2\pi}\frac{r_1'dr_1'd\theta'}{\sqrt{h^2+r_1'^2+r'^2-2r_1'r'\cos\theta'}}$$

where $r'^2=r^2+c^2-2rc\cos(\omega t+\theta)$. This combined with equations 12 and 13 give the stream function to be

$$U=\frac{\Phi}{(2\pi)^2a^2}\int^a\int_0^{2\pi}\int_0^\infty\frac{\partial}{\partial t}\times\left(\frac{r_1'dr_1'd\theta'd\tau}{\sqrt{h_\tau^2+r_1'^2+R_\tau'^2-2r_1'R_\tau'\cos\theta'}}\right) \quad (14)$$

where $R_\tau'^2=r^2+c^2-2rc\cos(\omega(t-\tau)+\theta)$ and $h_\tau=h+\tau/(2\pi b\gamma)=h+u$. Let us now bring the pole piece down close to the plate so that $h_\tau=\tau/(2\pi b\gamma)$, and bring up a similar pole of opposite sign from the other side, so that the eddy current density is doubled. We now carry out the differentiation with respect to t and set $t=0$ so that the $\theta=0$ line bisects the pole piece when $t=0$. The integral then becomes

$$U=\frac{rc\Phi\epsilon}{\pi(2\pi a)^2}\int_0^a\int_0^{2\pi}\int_0^\infty\frac{\sin(\theta+\epsilon u)(R_u'-r_1'\cos\theta')r_1'dr_1'd\theta'du}{R_u'(u^2+r_1'^2+R_u'^2-2R_u'r_1'\cos\theta')^{3/2}} \quad (15)$$

where $\epsilon=2\pi\omega b\gamma$ and $R_u'^2=r^2+c^2-2rc\times\cos(\theta+\epsilon u)$. For 3,000 rpm with a copper sheet 0.25 millimeter thick $\epsilon\approx 0.01$ so that u^2 in the denominator has reached the value 100 when ϵu reaches 0.1. In calculating such a quantity as the torque where the current density is integrated over the pole piece, the neglect of ϵ produces a fractional error less than $(\epsilon/a)^2$, so that the result should be good to one per cent for a sheet one millimeter thick. We may therefore drop the ϵ terms so that R_u' becomes the r' in Figure 1 and integrate with respect to u giving

$$U=\frac{K\sin\theta}{r'}\times\int_0^a\int_0^{2\pi}\frac{(r'-r_1'\cos\theta')r_1'dr_1'd\theta'}{r'^2+r_1'^2-2r'r_1'\cos\theta'} \quad (16)$$

where we have written K for the coefficient of the integral in equation 15. The integral with respect to θ', from Dwight's table of integrals 860.2, is zero when $r'<r_1'$ and π/r' when $r'>r_1'$. Thus the upper limit for the r_1' integration is a when $r'>a$ and r' when $r'<a$, which gives

$$r'>a \qquad U=\frac{\omega rcb\gamma\Phi\sin\theta}{2\pi r'^2} \quad (17)$$

$$r'<a \qquad U=\frac{\omega rcb\gamma\Phi\sin\theta}{2\pi a^2} \quad (18)$$

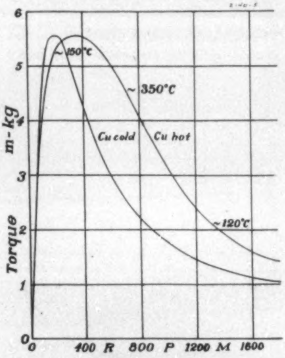

Figure 5. Curves showing torque versus speed for large disk rotating between the four rectangular pole pairs of an electromagnet, measured by Lentz

The next question is how to restrict the eddy currents to the interior of the disk bounded by the circle $r=A$. We observe that if we use equation 15 to calculate U for a second magnet also carrying a flux Φ but with circular pole pieces of radius $a''=Aa/c$ centered at $c''=A^2/c$, so that $R_u''^2=r^2+(A^2/c)^2-2r(A^2/c)\cos(\theta+\epsilon u)$, and change the variables of integration from r_1' to $r_1'A/c$, and from u to Au/c, then the resultant expression is identical with equation 15, except that we have cR_u''/A instead of R_u' and $A\epsilon/c$ instead of ϵ. But when $r=A$ we see that $cR_u''/A=R_u'$, so that both magnets, one outside and one inside the circle $r=A$, give the same U on this circle. Furthermore by taking the air gap in each magnet small, the fluxes are confined to the areas under the pole pieces, so that neither induces directly eddy currents on the other side of the circle $r=A$. It is evident that if the fluxes from the two magnets cut the sheet in opposite directions, then $U=0$ when $r=A$ and the currents induced by the inner pole are kept inside the circle. This is exactly the boundary condition for a disk of radius A, except that the calculated system includes the currents induced in the region $r<A$ by the magnetic field of the eddy currents in the region $r>A$, which does not exist in the case of the disk. This field is proportional to $\Phi\epsilon$ which is, by hypothesis, small compared with Φ, and in addition the source is further away, so that the fractional error in U will be less than ϵ. We should note also from the symmetry that the radial component of these secondary currents is opposite in sign on the two sides of the $\theta=0$ line, so that their effect cancels out completely in calculating the torque which therefore should be accurate to terms in ϵ^2. The contribution to U from the outer magnet is found by putting $c^2R''^2/A^2$ for R^2 in equation 17. Adding this to equations 17 and 18, we obtain for the stream function of the eddy currents in the disk

$$R>a \quad U=\frac{\omega rcb\gamma\Phi\sin\theta}{2\pi}\left(\frac{1}{r^2+c^2-2rc\cos\theta}-\frac{A^2}{c^2r^2+A^4-2rcA^2\cos\theta}\right) \tag{19}$$

$$R<a \quad U=\frac{\omega rcb\gamma\Phi\sin\theta}{2\pi a^2}\left(1-\frac{A^2a^2}{c^2r^2+A^4-2rcA^2\cos\theta}\right) \tag{20}$$

The torque may be calculated by integrating the product of the radial component of the current by the magnetic induction and by the lever arm and integrating over the area S of the pole piece. Thus, using equation 2, we have

$$T=\int_S\frac{rbi_r\Phi}{\pi a^2}dS=\frac{\Phi}{\pi a^2}\int_{c-a}^{c+a}\int_{-\theta_1}^{+\theta_1}r\frac{\partial U}{\partial r}rdrd\theta$$

where θ_1 and r are connected by the relation $r^2+c^2-2rc\cos\theta_1=a^2$. Substituting for U from equation 20 and integrating with respect to θ give

$$T=\frac{\omega cb\gamma\Phi^2}{\pi^2a^4}\times\int_{c-a}^{c+a}\left(r^2\sin\theta_1-\frac{a^2A^2r^2\sin\theta_1}{c^2r^2+A^4-2A^2rc\cos\theta_1}\right)dr \tag{21}$$

The integration is simplified by taking a new variable u so that $4acu^2=r^2-(c-a)^2$ which gives the limits 0 and 1. Thus we obtain, writing out ϵ,

$$T=\frac{\omega b\gamma\Phi^2c^2}{2\pi a^2}\left(1-\frac{A^2a^2}{(A^2-c^2)^2}\right)=\omega\gamma\Phi^2D_1 \tag{22}$$

This formula gives the torque in dyne centimeters when ω is in radians per second, Φ in maxwells, a, b, c, and A in centimeters and γ in electromagnetic units. If we are given the volume resistivity ρ of the disk in ohm-centimeters $\gamma=10^{-9}/\rho$.

If the magnet is fixed, and the disk rotates, the arrangement described exerts an undesired force on the disk axis which may be avoided by using two identical magnets on opposite sides of the axis and equidistant from it. This approximately doubles the torque given by equation 22. The additional torque from the eddy currents of one magnet flowing under the poles of the other may be found by an integral similar to equation 21 which is

$$T'=\frac{\omega b\gamma\Phi^2c}{\pi^2a^2}\int_{c-a}^{c+a}\left(\frac{r^2\sin\theta_1}{r^2+c^2+2rc\cos\theta_1}-\frac{r^2A^2\sin\theta_1}{r^2c^2+A^4+A^2rc\cos\theta_1}\right)dr \tag{23}$$

Integrating by the same substitutions as equation 21, adding to equation 22 and multiplying by two give

$$T=\frac{\omega b\gamma\Phi^2c^2}{\pi a^2}\left(\frac{4c^2+a^2}{4c^2}-\frac{2a^2A^2(A^4+c^4)}{(A^4-c^4)^2}\right)=\omega\gamma\Phi^2D_2' \tag{24}$$

This holds when the two magnet fields are antiparallel. If we subtract the integral of equation 23 from equation 22 and multiply by two we get

$$T=\frac{\omega b\gamma\Phi^2c^2}{\pi a^2}\left(\frac{4c^2-a^2}{4c^2}-\frac{4a^2c^2A^4}{(A^4-c^2)^2}\right)=\omega\gamma\Phi^2D_2'' \tag{25}$$

This holds when the two magnetic fields are parallel. The arrangement of equation 24 gives more torque than that of equation 25. The eddy-current flow lines corresponding to constant values of U as calculated from equations 19 and 20 applied to the cases of equations 22 and 24 are shown in Figures 2 and 3 where $a=\sqrt{7}$ cm, $c=7$ cm, $A=10$ cm and $\omega b\gamma\Phi/(2\pi)=3.5$. The value U on the outer boundary is zero and changes by steps of one in Figure 2 and steps of two in Figure 3.

Demagnetizing Effects

So far the magnet pole pieces have been assumed to be so hard that they do not short-circuit the flux of the eddy currents. This is not true for the permeable pole pieces of an electromagnet, whose effect may be calculated approximately by observing that the current $2U$ is enclosed by the rectangular path 1-2-3-4-1 in Figure 4, which lies in the upper and lower pole pieces except where it cuts across the disk and gap normally at $r=r_1$ and $\theta=\pm\theta_1$. If the reluctance of this circuit lies entirely in the air gaps, each of length g, then the magnetic flux density B_e due to the eddy currents alone at r_1, $\pm\theta_1$ is $4\pi U/g$. Substituting for U from equation 20 and writing as before $\epsilon=2\pi\omega b$ gives

$$B_e=\frac{c\epsilon r_1\Phi\sin\theta_1}{\pi a^2g}\left(1-\frac{A^2a^2}{c^2r_1^2+A^4-2r_1cA^2\cos\theta_1}\right) \tag{26}$$

This shows that when b and g are comparable in size B_e cannot be neglected compared with the original flux density $\Phi/(\pi a^2)$. The $\sin\theta_1$ term shows that the radial component of the eddy currents induced by B_e have opposite signs under the two halves of the pole piece, so that they contribute nothing directly to the torque, but on the other hand they form closed circuits about the central portion and so produce a demagnetizing magnetomotive force in the electromagnet. The

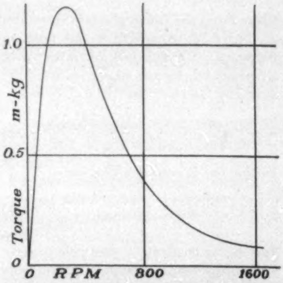

Figure 6. Curves showing torque versus speed for a large disk rotating between the single pair of circular pole pieces of an electromagnet as given by equations 22, 27, and 28

stream function U_e of these eddy currents is calculated as U is, but to simplify matters we carry out the operations from equations 14 to 16 for a single element of the pole face, along with its image element outside the circle $r=A$. We then give each element the strength indicated in equation 26 and set up a definite integral for U over the pole-piece area. This method is less exact than setting up equation 14 for the whole face, because it ignores that part of the flux threading dS from the current induced by B_e outside this area, which is of the order $\epsilon B_e dS$. The eddy currents U_e are evidently equivalent to a magnetic shell of variable strength U_e in the sheet and to get $\mathfrak{F}_e$ the demagnetizing magnetomotive force we must find the equivalent uniform shell. Thus we have

$$\mathfrak{F}_e=\frac{1}{\pi a^2}\int_S U_e dS$$

where S is the area of the pole face. We now have a complicated quadruple integral involving the variables r', R', θ' and θ_1' whose evaluation can be simplified somewhat by integrating in the proper order. The result is

$$\mathfrak{F}_e=\frac{\omega^2b^2\gamma^2c^2}{4g}\Phi\left(1-\frac{2a^2A^2(A^2+c^2)}{c^2(A^2-c^2)^2}+\frac{2A^4}{c^4}\log_e\frac{(A^2-c^2)^2}{(A^2-c^2)^2-c^2a^2}\right)=\beta^2\gamma^2\omega^2\Phi \quad (27)$$

If the flux penetrating the sheet at rest is Φ_0, then when in motion we have, if $\mathcal{R}$ is the reluctance of the electromagnet, $\Phi=\Phi_0-\beta^2\gamma^2\omega^2\Phi/\mathcal{R}$, so that

$$\Phi=\frac{\mathcal{R}\Phi_0}{\mathcal{R}+\beta^2\gamma^2\omega^2} \quad (28)$$

The expressions for the torque now become

$$T=\frac{\omega\gamma\mathcal{R}^2\Phi_0^2D}{(\mathcal{R}+\beta^2\gamma^2\omega^2)^2} \quad (29)$$

where D has the values given in equations 22, 24, or 25, according to the pole arrangement. There is now a definite speed for maximum torque which is found by setting $\partial T/\partial\omega=0$ to be

$$\omega_m=\frac{1}{\beta\gamma}\sqrt{\frac{\mathcal{R}}{3}} \quad (30)$$

Putting this in equation 29 gives

$$T_m=\frac{3\sqrt{3\mathcal{R}}\Phi_0^2D}{16\beta} \quad (31)$$

This is independent of the conductivity which is surprising, although there is some evidence for it in Lentz's experimental curves shown in Figure 5 which give hot and cold copper disks the same T_m for different ω_m.

These calculations of demagnetizing effects have been worked out for a single pole. For an even number of poles with alternating signs, we have seen that the torque per pole is increased, but the demagnetizing forces are also increased so that the torque obtained by multiplying equation 29 by the number of poles will probably not be far wrong. The speed for maximum torque given by equation 30 will certainly be decreased, perhaps considerably, because of the increase in β.

The only formula we can find for this torque is one derived by Rüdenberg.[3] This formula is written as a double infinite series and is derived by considering a thin conducting strip bounded by straight lines which moves lengthwise in the narrow gap between magnetic poles with rectangular faces. The fields of adjacent poles are antiparallel, so that the inducing fields can be expanded in a double series of odd harmonics. This formula was checked qualitatively by Zimmermann,[4] but could not be verified quantitatively, as the theoretical and experimental boundary conditions did not agree. Lentz found only those terms involving the lengthwise harmonics were of importance and dropped the rest. His experimental brake had the center of the disk removed to simulate a ring whose width roughly equaled that of the postulated strip. His four poles were so far apart that their action was nearly independent. We have redrawn in Figure 5, his experimental curves giving the torque in meter kilograms against angular velocity in revolutions per second. The ring had inner and outer radii of 5 centimeters and 25 centimeters and was 0.4 centimeter thick. The air space was 1.2 centimeters, and the centers of the rectangular pole pieces were 20.75 centimeters from the rotation axis and were 6 centimeters (radial) by 8 centimeters (tangential). The inducing field was 2,150 gauss at rest. The figures on the hot copper curve show the estimated stable mean temperature for that speed.

A direct quantitative comparison of our formula with Lentz's data is difficult, because he used rectangular poles, his air gap was so large as to spread the inducing field over an unknown area, the center of his disk was cut away, and we do not know where his flux density was measured. Although our formulas are inaccurate for such large dimensions at the high speeds, it is interesting to see what results they give for a comparable case. Let us take $b=0.4$ cm, $A=25$ cm, $a=4$ cm, $c=21$ cm, $g=0.6$ cm, $B=2{,}000$ gauss and assume the reluctance entirely in the air gap. In equation 22, $D_1=1.23$, in equation 27, $\beta=3.85$ and in equation 28 $\mathcal{R}=0.012$. The angular velocity for maximum torque for copper ($\gamma=1/1{,}700$) is given by equation 30 to be 27.9 radians per second or 267 rpm. T_m is 1.15×10^8 dyne cm or 1.17 kilogram-meters for this single pole and roughly four times this for four poles. Expressing T' in kilogram-meters and ω' in rpm, equation 29 becomes

$$T'=\frac{0.00785\omega'}{(1+0.0000047\omega'^2)^2}\text{kg-m}$$

This formula is plotted in Figure 6. A comparison of Figure 5 with Figure 6 indicates that our formula gives too rapid a falling off in torque at high speeds. It should be pointed out that other conditions, such as the degree of saturation of the iron in the magnet will upset the assumed relation between magnetomotive force and Φ and may modify equations 28, 29, 30, and 31 considerably.

The methods given in this paper may be extended to any number of poles by the method used for two and to other than circular faces. Several such calculations have been carried out, but it is doubtful if the additional theoretical accuracy justifies publishing them. The difference between the ideal boundary conditions used here and those found in apparatus is such that we recommend that the torque for one pole be calculated by equation 22 for permanent magnets or by equation 29 for electromagnets, and the result multiplied by the number of poles to give the total torque. In power apparatus the heating of the disk will change its resistivity and may cause it to expand and buckle and otherwise upset the calculations.

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Poles and Pole Treatment

REGINALD H. COLLEY
NONMEMBER AIEE

TO write about poles and the preservative treatment of poles today is like writing the review of a very successful play that has been running a long time. The players are not men, but inanimate poles. The natural backdrop is the forest. The stage is as broad as the United States and the setting is a criss-cross of thousands of miles of utility and communication lines. The engineers of these lines are the producers and directors of the play; and it is they who assign the parts to the various actors and write a changing script as the play progresses. It is the purpose of this paper to present certain highlights of the more important scenes, such as pole use and the drain on the forest, pole manufacture and treatment under standard specifications, and pole strength, and to discuss some possible rearrangements in the setting that may become necessary.

An Outline of the Pole Problem

In the forest are standing trees of various diameters and heights, some of which become poles. There are broad-leaved trees called hardwoods, and needle-leaved trees or conifers, called softwoods. In any tree there is a central core of more or less durable, nonliving wood known as the heartwood, which is surrounded by a layer of living wood known as the sapwood. There are durable woods in both the hardwood and softwood groups. The durable woods have relatively large heartwood cores and relatively thin sapwood; and the nondurable woods, on the other hand, are likely to have relatively thick sapwood.

The history of change in practice that has taken place since the first chestnut lines were built is a story of the shift from naturally durable woods to nondurable woods, and this change has involved the introduction and use of preservative materials to give the nondurable woods an economic service life.

One of the most important reasons for the shift was a national disaster in the form of the chestnut blight disease which has wiped out completely the commercial chestnut forests of the United States.

At the same time the supply of northern cedar became inadequate, at least as far as the sizes required for joint use were concerned. The question of selecting proper pole size became more and more important. The growth of the electrical industry had not followed a simple pattern, and the need for correlation of ideas and a rational standardization of pole practice led to the writing and acceptance of the American Standard Specifications for Wood Poles.[1] These specifications have had a most important influence on modern pole production. They were written around the natural poles of the forest; and they were designed to make suitable species interchangeable and to encourage the use of the greatest possible number of pole-size trees.

Other specifications had to be written or rewritten to cover the preservative treatment. The writers of these specifications focussed their attention on the finished pole, leaving only so much of process control in the specifications as was necessary to protect the timber from injury. This focussing generally took the form of defining acceptable limits for penetration. Practically speaking, the only universally accepted preservative was coal tar creosote. One major objective only was held in view in the preservation processes, namely, to fill the nondurable sapwood, whether it was very thin as in chestnut, or whether it made up 95 per cent of the pole as in many southern pines, with enough toxic creosote to prevent the growth of wood-destroying fungi.

Pole Use

Figure 1 is a copy of an old map made about the turn of the century to show the economic limits for the distribution within the Bell system of certain pole timbers. For the greater part of the country chestnut and northern cedar were favorites. In the western states western cedar was a natural selection. Creosoted southern pine had just begun to spread up from the southern pine forests, reaching as far north as St. Louis. The contrast between Figure 1 and Figure 2, which represents conditions in 1941, is striking. Chestnut is gone. The eastern scene is almost completely dominated by southern pine. Western cedar is distributed from the northwest and from the concentration yards at Minneapolis. Northern cedar is confined to parts of the New England states and the Lake states. Lodge-pole pine serves the Mountain states area. Douglas fir is being shipped from the fog-belt forests of Oregon and Washington, either directly or through Minneapolis to the eastern and southern states. The lines of distribution cross and recross in a perplexing pattern woven from preference, policy, price, and freight rates.

Many of the changes indicated in Figure 2 have been inevitable. Expansion of joint use, which has been easier since the standard pole specifications have become more widely accepted, has had a tendency to increase the length of the poles required for certain services. This has inevitably thrown the demand back on southern pine and western red cedar; and within the last two years the practical difficulties in securing large quantities of long southern-pine and western-cedar poles have practically forced the production and use of Douglas fir.

Figure 3 is a composite diagram, worked out by Doctor J. G. Segelken of the Bell Telephone Laboratories, presenting in a new fashion a picture of the relative use by the Bell system of some sizes of the pole species shown in Figure 2. Certain facts stand out plainly. One is the relative use of northern cedar and western cedar in the 30-, 35-, and 40-foot groups. Another is the major position of creosoted southern pine. A few years ago the Bell system bought between 30 and 50 per cent of all creosoted-pine poles produced in any one year. Now only about 15 per cent of the production goes into Bell system lines; but the predominance of creosoted southern pine in the Bell system use picture is a factor of major importance.

The Drain on the Forest

The degree of this importance is illustrated by Figure 4, derived from the annual reports prepared by R. K. Helphenstine, Jr.,[2] of the United States Forest Service. Piles and poles are much alike, and they come from the same forests, so that one cannot speak of a natural supply of trees for poles without taking the demand for piles into consideration.

The number of standing trees per acre in the pine forest that will make either piles or poles 35 feet or more in length is relatively small. Therefore, any increase in the demand for long poles inevitably means the working over of a larger num-

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REGINALD H. COLLEY is timber products engineer with Bell Telephone Laboratories, Inc., New York, N. Y.

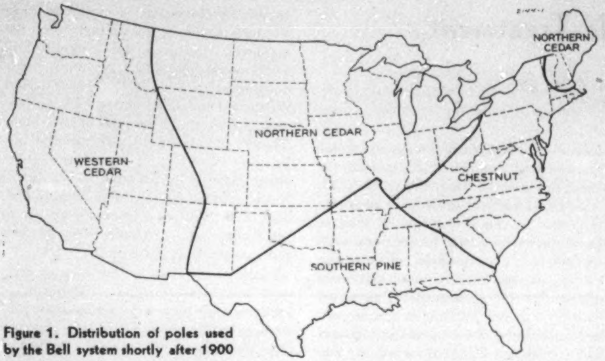

Figure 1. Distribution of poles used by the Bell system shortly after 1900

ber of acres with consequent increased labor and longer hauls. The Southern Forest Experiment Station at New Orleans conducted a unique survey in 1934–1936 which revealed that there was a more than adequate supply of pole trees of small diameter under 25 feet in length, but that the number of suitable trees in the larger sizes was relatively small. Figure 5 shows the relative use within the Bell system and outside of the Bell system for 35-, 40-, and 45-foot southern-pine poles, classes 1 to 7, American Standard. The 35-foot class 5 pole, which is a favorite for joint use makes up roughly 10 per cent of the total requirements. The relative natural occurrence in the forest of trees to make these poles is only one per cent of the total forest stand. The survey figures indicated that the number of trees suitable for 35-foot class 5 poles and for piles of about the same size taken together was approximately 3,500,000. This looks like an adequate supply. But approximately 23 per cent of the poles required for use outside of the Bell system are for 35-foot poles of smaller diameter, and the heavy demand for 30-foot class 6 and class 7 poles for cross-country lines and rural extensions during the last few years has meant cutting trees which, had they been left standing, might have furnished an entirely satisfactory supply of 35-foot poles at some later date.

The current requirements for 35- and 40-foot southern-pine poles are relatively much in excess of the occurrence of standing trees. The rapid expansion of the pulp and paper industry in the southern states and the heavy emergency demand for southern-pine lumber and piles have probably changed the relative proportions of the various sizes of standing trees since the survey. If the demand for creosoted-southern-pine poles 35 feet and longer were to continue at the present rate, a situation would soon develop in which these poles would be at a premium, and rigid straightness requirements would have to be relaxed in order to facilitate production. It would seem to be the better part of wisdom to use as many sizes as possible and to broaden the use of species to include western-cedar and Douglas-fir poles whenever it is practicable to do so.

Pole Manufacture and Standard Framing

Since the establishment of standard dimensions for the different species, there has been a definite trend toward greater mechanization in pole production. The most evident result of this trend is the pole-shaving machine for removing inner bark and smoothing the pole surface. Under Bell system specifications southern-pine poles are now trimmed by approved machine, and the trimming is limited so that the average size of the poles shall not fall below certain minimum circumferences at six feet from the butt, at mid-point, and at the top. The shaving machines are finding their way into the cedar industry, and western cedar is shaved to facilitate preservative treatment or to reduce the sapwood thickness so that it will not hold enough moisture to support decay. Machine trimming has improved the appearance of the southern-pine pole by removing the natural bumps at the knots, and by reducing the exudation of creosote, or "bleeding," from the pole surface. It also accelerates drying and facilitates production-line operation.

Generally speaking, the majority of the southern-pine poles manufactured for Bell system use are manufactured and framed to a standard pattern. At present the roof is cut square with the pole axis, and instead of the individual mortises for the crossarms, a slab gain is provided by machining a flat surface on the appropriate side of the upper part of the pole. The slab-gained pole is an economical all-purpose pole that takes the place of the mortise-gained pole, cable pole, guy stub, and push brace. There are, of course, cases in which special framing may be necessary or desirable; but special framing is a kind of custom job, and unless production orders are placed well in advance, it generally happens that specially framed poles must be produced on order.

The acceptance of standard framing has met with less resistance in southern pine than in western cedar. It is only within the last few years that the concept of a standard finished western-cedar pole has begun to take form. In the case of southern pine the principle of complete manufacture before treatment was established as a practically universal procedure before

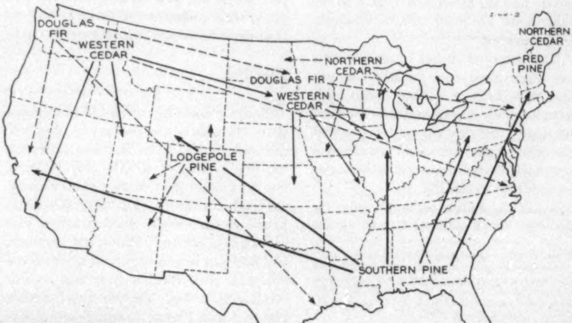

Figure 2. Current distribution of pole timbers

the creosoted-pine pole began to be accepted as a regular piece of line equipment, and the development of standard framing followed more or less naturally. In all cases gaining and boring the bolt holes is permitted after treatment in southern pine if the sapwood is 100 per cent penetrated.

Pole Preservation

The main purpose of pole preservation is to protect the nondurable sapwood from decay. The principles are relatively simple. A toxic substance is put into the wood in sufficient quantity to make the wood unpalatable to the wood-destroying fungi that cause decay. The addition of the toxic substance must be accomplished without material damage to the wood itself.

The preservative most in favor is coal tar creosote. The wood is impregnated by soaking the pole butts in open tanks or by applying pressure to the whole pole in closed cylinders. The butt-treating method has been applied with greatest success to the naturally durable thin sapwood species like chestnut and cedar, the aim being to put a protective layer of sapwood saturated with creosote around the naturally durable heart. In the case of cedar it is necessary to puncture the sapwood to secure satisfactory penetration. The open-tank method has also been used for the treatment of the thicker sapwood of lodge-pole pine, and it is now being employed as a supplementary process to the butts of poles that have been given a full-length treatment with a preservative salt.

Current treating specifications for full-length pressure treatment place the emphasis upon the product. Penetration is relatively more important than quantity of preservative retained. The Bell system, after some years of observation and experimentation, now specifies that the treatment of southern pine shall be by an empty-cell process, that the penetration shall be at least $2^{1}/_{2}$ inches unless 85 per cent of the sapwood is penetrated, and that the net retention shall average eight pounds of creosote per cubic foot of wood. Detailed discussion of the development of these requirements may be found in the *Proceedings* of the American Wood-Preservers' Association. The double requirement, based on both depth and per cent of sapwood penetrated, is necessary, because the sapwood thickness varies in poles of different lengths and circumferences.

A relatively low-residue creosote has been specified, defined by limiting the residue that remains after distillation has been carried to 355 degrees centigrade. Much of this low-residue creosote has been imported. At present such importation has practically ceased, and some adjustments are in order. The purpose of the low-residue creosote requirement was to make the product as acceptable as pos-

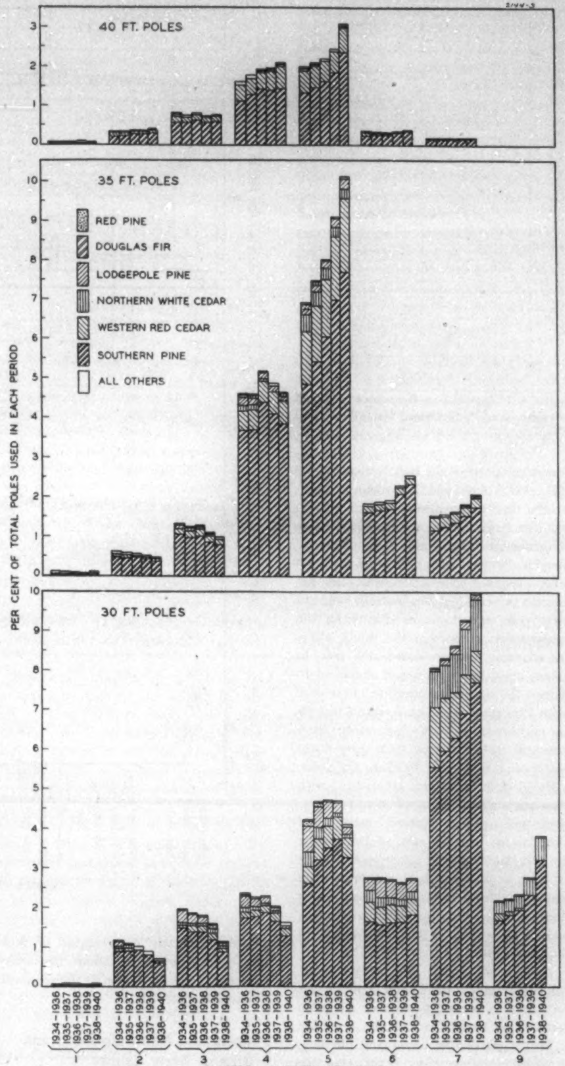

Figure 3. Relative use by the Bell system of 30-, 35-, and 40-foot poles, three-year moving averages

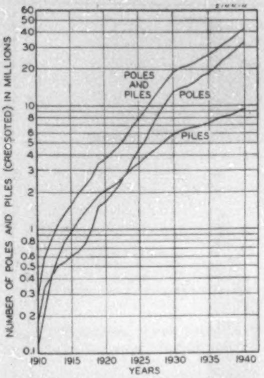

Figure 4. Creosoted-southern-pine poles and piles produced in the United States since 1910

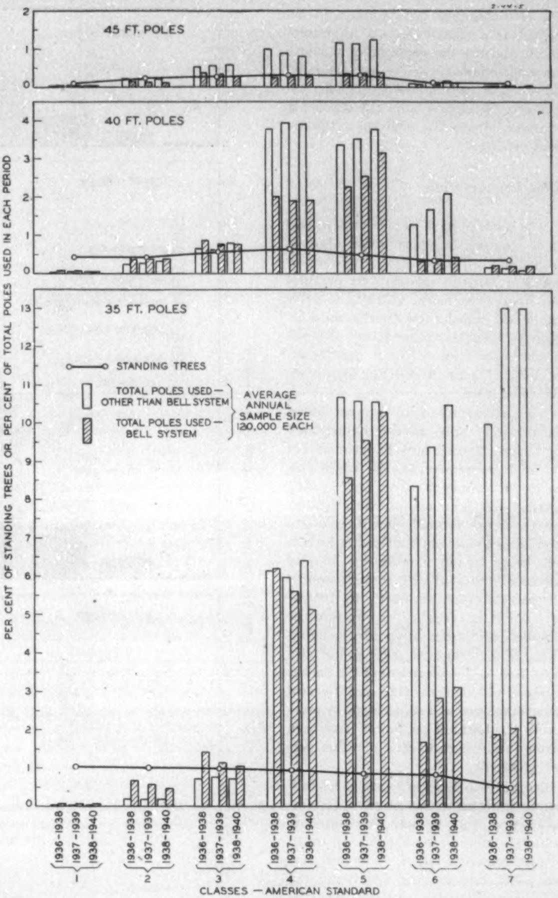

Figure 5. Forest supply and use in and outside the Bell system of 35-, 40-, and 45-foot southern-pine poles

sible to all parts of the Bell system and for all types of lines, whether urban or rural. At the time the requirement was written into the specification, the heavier residue creosotes available had a tendency to bleed badly, that is, to ooze from the poles after the poles were set in line, often for months or years. A black sticky pole is a dirty pole, and it makes trouble for the construction crews and the public alike. In adapting the creosoted-pine pole to urban use it has been found expedient to reduce the standard retention of creosote from 12 pounds per cubic foot to 8 pounds per cubic foot, to specify low-residue creosote and, in some cases, to require steam baths after treatment to clean the poles.

Full-length pressure treatment with creosote is also being applied to red pine, lodge-pole pine, western cedar and Douglas fir. In the case of Douglas fir the treatment usually takes several times as long as treatment of southern pine; and although the sapwood of Douglas fir is thinner, it is sometimes hard to treat as high a proportion of the sapwood as in southern pine.

In expanding the use of red pine, lodge-pole pine, and Douglas fir, it was necessary to focus attention on adequate penetration and to reduce the retention as far as practicable to prevent bleeding. In making compromises of this type one is forced to sidestep the main argument that has been so often advanced, that the reduction of retention from 12 pounds to 8 pounds in southern pine, for example, might mean inadequate service in line. The compromises appear to be working out fairly well, and, as a matter of fact, the actual concentration in the thinner sapwoods of Douglas fir and lodge-pole pine are at about the same level as the concentration in the sapwood of southern pine.

Penetration, Nonconformity, and Risk of Early Failure

Only in the case of southern pine has the evidence on the practicability of the penetration and retention requirements been fairly well established. The latest available information on southern-pine treatment by an eight-pound empty cell process is illustrated by Figure 6, which shows the smooth distribution of 17,556 poles, nonconforming for penetration, representing approximately 3.5 per cent out of a total sample of 525,000 produced in 1941. The distribution has been divided into nonconformity zones, numbered 1 to 5. The interpretation of the distribution is illustrated in Table I. From studies of the behavior of creosoted-southern-pine poles in line[3,4], certain failure factors have been determined which

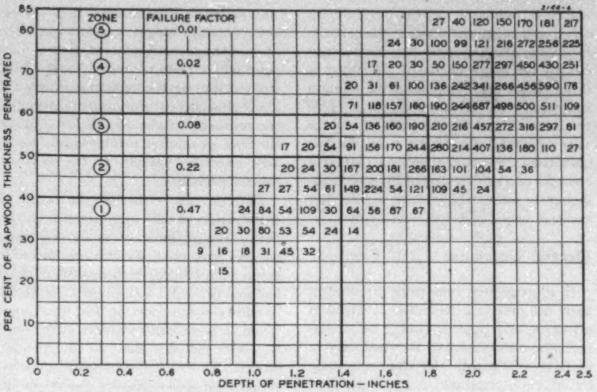

Figure 6. Distribution of southern-pine poles nonconforming for creosote penetration, 1941 production, arranged in nonconformity zones according to risk of early failure in line

are applicable to the percentage of poles in the respective nonconformity zones. These failure factors may be defined as the percentage of the poles within these zones which may be expected to fail within 15 years of installation. Recent experience indicates that these failure factors are probably high. It appears likely that the total risk of early failure in a group of poles like those under consideration is less than the estimated 0.128 per cent. The method of interpretation illustrated was first advanced several years ago, and up to the present no reason has been brought forward for changing it. Broadly, it means that poles which meet the penetration requirements of current standard specifications should have an entirely adequate service life.

Green Salt Treatment

A new preservative for this country, called green salt, a mixture of five parts potassium dichromate, three parts copper sulphate, and one part arsenic acid, in a cold water solution, has been used recently for the commercial treatment of seasoned southern-pine poles. The treating solution contains about seven per cent of the salts. The penetration has averaged about 98.5 per cent of the depth of the sapwood, which is better than the average generally obtained in creosote treatments; and the number of nonconforming poles was approximately three per cent. The distribution of nonconformity appears to be of the same order as that shown in Figure 6, with fewer poles in zones 1 and 2. The finished product is cleaner, lighter in weight, and stiffer than the creosoted pole, and the outer fibers are somewhat harder, making the green salt poles somewhat more difficult to climb than the creosoted pole. Generally speaking, the reaction of the field forces to the new poles has been excellent.

Immersion Treatments—Creosote, Petroleum, and Pentachlorphenol

The virtual shutting off of creosote importation by the war has stimulated the use of other types of preservatives such as mixtures of creosote and petroleum, with or without the addition of some other material such as pentachlorphenol; or the use of a five per cent solution of pentachlorphenol in petroleum alone.[5] The latter solution is being used for the full-length treatment of western cedar by an open-tank immersion process as well as for pressure treatment of southern pine.

Machine-shaved western cedar poles are being creosoted by immersion in hot creosote, followed by the usual butt treatment. The end in view in these western cedar treatments is to produce poles in which the hazard to linemen from sapwood decay will be eliminated.

Salt-Creosote Poles

During the last 15 years approximately 800,000 southern-pine poles treated with zinc meta arsenite have been placed in line. The majority of them have been purchased and set by power and light utilities. The deterioration that has occurred in these poles has happened at or below the ground line and, in most cases, the reduction in circumference at that point has taken place slowly. Above ground line the condition of the poles is excellent. To prevent the deterioration at the ground line, poles that are treated full length with zinc meta arsenite are now being given a butt treatment with creosote. Specifications for this double treatment call for a penetration in the above ground part of the pole of $2^1/_2$ inches unless 85 per cent of the sapwood is penetrated, as in the case of creosoted southern pine. Below ground a full sapwood penetration of creosote is not required. The aim of the specification is to provide for adequate protection of the butt without the use of too much creosote. This salt-creosote pole is being distributed in the northeastern states on the bases of strength and life expectancy equivalent to creosoted southern pine. Other salts than zinc meta arsenite, such as those covered by the current Federal specifications for use in treatment of timber not in contact with the ground, are under consideration. It is interesting to note in this connection that double treatment using zinc chloride followed by a creosote butt treatment was in use in Texas prior to 1910. Some of the poles of this vintage have given at least 34 years' service in line and are still good.

Conditioning Poles for Treatment

This brief discussion of preservation cannot be dropped without some mention of the problem of preparing poles for treatment. In theory at least, it would be desirable to dry poles of all species before treating them. Practically speaking, it is almost impossible, except at certain seasons of the year, to air-season southern-pine poles for either creosote or salt treatments without great risk of the poles becoming infected with molds and strength-reducing fungi during the process. The larger the pole, the harder it is to dry it. Charges of either green or dry poles may be treated satisfactorily with

Table I. Southern-Pine Poles—1941 Production—Estimate of Risk of Early Failure in Line

Non-conformity zone	Number of Poles	Per Cent of Total Poles	Failure Factor	Risk of Early Failure Per Cent of Total Poles
1	132	0.025	0.47	0.012
2	839	0.160	0.22	0.035
3	2,862	0.545	0.08	0.044
4	5,432	1.035	0.02	0.021
5	8,291	1.579	0.01	0.016
	17,556	3.344		0.128

due care in the control of the treating process. Partially seasoned poles, either alone or in mixture with seasoned or green poles, may make the treating results very variable. The Bell system specification requires steam conditioning for at least six hours at 259 degrees Fahrenheit before impregnation with creosote, in order to smooth out some of the variables and to sterilize the wood.

To prevent deterioration during seasoning, some producers follow the practice of pretreating all pole stocks as soon as they have dried down to the point where such treatment can be given economically with approximately four pounds of creosote per cubic foot. The poles are then stacked in the yard and held for orders. The additional poundage required to meet the minimum retention specified is added by a second treatment. In other cases the poles are treated under pressure with weak solutions of preservative salts.

The kiln-drying of southern-pine poles[6] has been carried far enough experimentally during the last few years to show that it is a distinctly promising method of preparing the poles for treatment. Ironically enough, it begins to look as if the creosoted kiln-dried poles would have to be steamed for a short period at the end of the creosoting process in order to clean them and prevent subsequent bleeding of the creosote from the pole surface.

In the case of certain of the salt preservatives organic matter in the green or partially seasoned pole causes a very rapid chemical reduction of the preservative solution if either the wood or the solution is heated; and this reduction does not occur to the same degree if the wood is dried before treatment, and the solution is applied cold. In any event a dry pole treats quickly, and the plant is able to save cylinder hours, and a pole that has once been dried and then given either a creosote or a salt treatment dries out subsequently more rapidly than a pole that was not seasoned before treatment.

Electric Resistance

It is extremely important to bear in mind the practical difficulties involved in seasoning and treating timber before entering on a discussion of the conductivity of the treated wood. Measurements made by improved methods[7] confirm the generally accepted conclusion that the electric resistance varies inversely as the moisture content. There is a temperature factor, but it can be more or less ignored if the measurements are made within certain temperature limits. It is not so easy to control moisture content. The moisture may come from two sources, that is, it may be the moisture that is left in the sap that was in the tree when it was felled, and it may be moisture absorbed from rain or melting snow as the pole is lying in a concentration yard or standing in line.

The sapwood of a green pole may contain more water than its own dry weight. At a moisture content equal to 27–30 per cent of its dry weight, wood is said to be at the fiber saturation point, that is, at a point where there is no free water in the wood cells. If the moisture content is above this fiber saturation point, the electric resistance of the wood is low. Below the fiber saturation point electric resistance increases sharply as the moisture content decreases.

Suppose that, either as the result of well-considered seasoning practice on the part of the producer, or on the insistence on the part of a possible customer, a group of relatively high-resistance poles has been produced and shipped. It would be quite wrong to assume that these poles would all have high resistance at the time they were placed in line and equipped for service. During shipment moisture may get into the open seasoning checks and be absorbed by the inner sapwood and untreated heartwood of the pole. Or the poles may be exposed to rain and snow for months before they are erected. In fact, any method of open flat storage previous to line use is almost certain to bring about a great variation in the resistance of poles that may have started out on a relatively high resistance level. Even the best of storage conditions, under cover or in vertical piles, is no guarantee of high resistance of the pole in line. Driving rains, sleet, or snow storms may wet the windward side of the pole and bring it to practical saturation during the course of a storm. The character of the preservative itself simply adds another variable to these general conditions over which very little control can be exercised.

Aside from drying a pole properly before treatment so that it will dry out more quickly after treatment, there is little that can be done in writing specifications, or in actually producing poles, that will make it possible to turn out high resistance units; and after the poles have once been produced, each stick becomes an independent variable in its resistance characteristics. In practical operation, therefore, no pole can have a high resistance all the time.

Pole Strength

In writing the American Standard Specifications for Wood Poles,[1] all of the species were dimensioned so that they would have approximately equal resisting moments at the ground-line for any given class and length. These strength requirements have been translated into what may be called standard breaking loads starting at 1,200 pounds for class 7 and running to 4,500 pounds

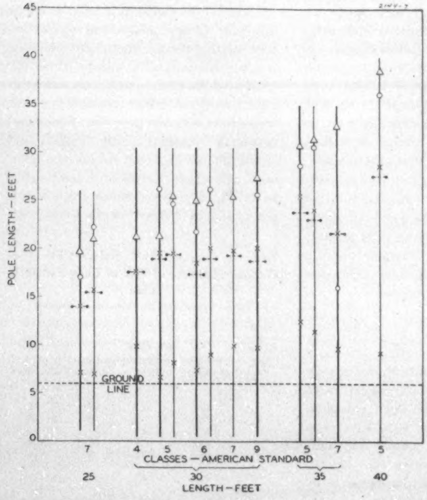

Figure 7. Independence of maximum knots and points of break in southern-pine poles and pole-top sections

Δ Maximum group of knots in any 12-inch section

o Maximum single knot

•--• Ground line for test of top section

x Point of break

—Creosoted poles

—Green salt poles

When only Δ is shown, the maximum single knot is in the maximum group

The upper x shows location of point of break in top section

Table II. Southern-Pine Poles—Breaking Test Data

	American Standard		Average Diameters in Inches		Whole Pole Relative Average		Top Section Relative Average	
No.	Class	Length	Maximum Single Knots	Maximum Knot Groups	Breaking Load*	Modulus of Rupture**	Breaking Load*	Modulus of Rupture**
Creosoted Poles								
1	4	30	1.2	1.8	122	119	178	117
4	5	30	1.7	3.8	118	103	156	101
3	5	35	1.1	3.9	150	130	192	118
5	6	30	1.6	5.0	112	99	127	80
5	7	25	1.5	4.9	116	104	140	96
1	7	35	1.9	5.2	127	124	175	133
3	9	30	1.6	4.8	†	120	†	140
22			1.5	4.5	122	110	152	105
Green Salt Poles								
3	5	30	2.2	4.1	150	129	205	107
8	5	35	1.6	3.5	146	127	182	104
2	5	40	1.4	2.7	138	130	223	119
38	6	30	1.7	4.5	121	104	151	99
11	7	25	1.2	3.7	149	135	189	119
8	7	30	1.5	3.9	148	129	170	94
70			1.6	4.1	133	116	167	103
92			1.6	4.2	100§	100§	125	90

* The standard breaking loads were taken as 100 in each case. These standard loads are: class 4—2,400 pounds; class 5—1,900 pounds; class 6—1,500 pounds, and class 7—1,200 pounds.

** The standard ultimate fiber stress for creosoted-southern-pine poles—7,400 pounds per square inch—was taken as 100 in each case.

† No standard breaking load for class 9 has been established. For the three poles listed the average breaking loads for the poles and for the pole-top sections were 1,317 pounds and 1,840 pounds, respectively.

§ The averages for the whole poles were taken as 100 for comparison with the top sections.

for class 1, approximately on a 25 per cent geometric progression.

Three years ago R. C. Eggleston of the Bell Telephone Laboratories began a series of breaking tests on machine-shaved southern pine treated with creosote, and with green salt. A novel scheme was introduced. The top sections of the poles as well as the poles as a whole, have been tested whenever possible. The results of part of these tests, on 92 poles and pole-top sections, are summarized in Table II. The poles averaged well above their rated class breaking loads, above the American Standard fiber stress—7,400 pounds per square inch—and within the expected variation for modulus of rupture. The modulus of rupture of the pole-top sections averaged 90 per cent of the modulus of rupture of the poles as a whole. The top sections of the poles were broken with short lever arms and, in spite of lower modulus of rupture, were on the average still sufficiently strong to meet their specified pole-class breaking load.

The position of the maximum knot and the maximum-knot group in relation to the point of break for the poles as a whole and for the pole-top sections are shown in Figure 7. Each line in the figure represents one of the groups defined by columns 1, 2, and 3 in Table II, and within given class length groups the creosoted-pole and the green-salt-pole diagrams are placed side by side to facilitate comparison. The independence of maximum knot position and break position is obvious. Only the pole-top sections, which had to be clamped in the holding device to represent the point of guying, broke at conspicuous knot groups. In some instances the very first point of break seemed to be at small knots an inch or less in diameter or even in hidden knots which did not appear on the surface of the poles at all. Occasionally poles will break during unloading. Apparently, such breakage is the result of impact shock far in excess of what the poles could stand. Generally, this impact shock comes from dropping the pole or from catching it between others as it is rolled off the car or trailer. The breaking in such cases at knot groups is coincidental with the shock and point of application and does not indicate dangerous weakness in the pole itself.

Ground-Line Treatment

One other subject remains for brief mention, namely the prolongation of the life of the existing pole plant. The evidence from test plot experiments and observation of poles in service indicates that some sort of ground-line treatment should be applied to untreated poles that are worth saving at the time of inspection, as a part of the regular inspection procedure. When one looks back over the years, it appears that the mistake, if any, that was made in earlier ground-line treatments was not in the treatment itself, but rather in the assumption that a single application would be effective for the life of the pole. The methods now recommended are the Osmose process or the simplified procedure that consists of the regular inspection and excavation, and treatment with a water-soluble salt and creosote. Originally a pound of sodium fluoride was dumped into the excavation around the pole, and the hole lightly backfilled. Then, after making a narrow V-shape trench against the pole, about one gallon of cold creosote was poured into the hole by letting it run against the pole, at a height of about 12 inches above the ground line, from the flattened spout of a watering can. The backfill was then finished, leaving the earth around the ground line fairly saturated with creosote and leaving a water-soluble salt to be taken up by the moisture in the wood. Sodium fluoride has been replaced in part by borax in the general plan as a result of the shortage of the former salt. The protection afforded by such a treatment seems to be good for about five years.

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Interim Report on Guides for Overloading Transformers and Voltage Regulators

AIEE COMMITTEE ON ELECTRICAL MACHINERY
Transformer Subcommittee*

THE present war emergency has crystallized the need for additional information concerning the maximum load capability of modern-design transformers and voltage regulators in order that these apparatus may be utilized to a greater extent for meeting present and new load requirements with a minimum amount of new equipment, thus conserving critical war materials. It will be noted that the papers[1–3] presented at the AIEE convention June 24, 1942 give somewhat divergent views on permissible overloads and on the effect of temperature on transformer insulation.

The relation between the life expectancy of insulation as indicated by laboratory tests and the actual life of a transformer is largely theoretical, so that the use of such information must be tempered by sound judgment based on operating experience. This report represents a compromise of views that is considered conservative and satisfactory for immediate general use, subject to the limitations and cautions given herein.

Consideration is given to operation with normal life expectancy and also with moderate sacrifice in life expectancy, thus taking into account the effect of the following factors:

(*a*). Characteristics and limitations of the apparatus involved.

(*b*). Ambient temperature.

(*c*). Load factor.

(*d*). Supplemental cooling.

(*e*). Sacrifice of life expectancy.

Normal Life Expectancy

In this report normal life expectancy is based on continuous operation at rated load with a daily average ambient temperature of 30 degrees centigrade for self-cooled or forced-air-cooled transformers, 25 degrees centigrade for water-cooled transformers, and other usual service conditions as given in American Standard *C*-57, Section 2.000. The "Guides for Operation of Transformers" included in American Standard *C*-57 give general recommendations for the loading of oil-immersed transformers and voltage regulators for recurrent and emergency overloads with small effect on normal life expectancy, while this report will supply, in addition to other information, data for heavier overloads with moderate sacrifice of life expectancy.

Loading for Normal Life Expectancy

The maximum load capability is determined to a considerable extent by the type and the electrical characteristics of the specific piece of apparatus. However, some general guides may be set up, based upon the more usual comparatively modern design characteristics, and with provision for taking into account the factors of temperature of the cooling medium, load factor, thermal ability of bushings and accessories, expansion of oil due to temperature increase, possible pressure in sealed transformers, method of cooling, and so forth.

DESIGN CHARACTERISTICS

The general information on transformer loading in this report applies to the comparatively modern-design transformers built since about 1928. The information will not apply in the usual case to the older-design apparatus without consideration of the design characteristics of each piece of apparatus.

AMBIENT TEMPERATURE

For low ambient temperatures the continuous kilovolt-ampere loading may be increased one per cent for each degree that the daily average temperature is below 30 degrees centigrade for self-cooled transformers, three fourths of one per cent for each degree below 30 degrees centigrade for forced-air-cooled transformers, and one per cent for each degree below 25 degrees centigrade for water-cooled transformers.[4] Similarly, the loading should be decreased two per cent for each degree that the daily average ambient is above these temperatures. For continuous loading these rules result in normal life expectancy, the same as for operation at rated load with ambient temperatures of 30 degrees centigrade and 25 degrees centigrade respectively. Detailed applications of these rules are given in the American Standard *C*-57, "Guides for Operation of Transformers."

Under some conditions greater permissible overload capability can be obtained by using "equivalent annual ambient" instead of the daily average ambient when applying the rule for overloads due to change in ambient. The equivalent annual ambient is the temperature which, if maintained constantly, would result in the same aging as that occurring under the actual ambient temperature throughout the year. This matter is being given further consideration.

LOAD FACTOR

For daily load factors below 100 per cent the loading may be increased 0.3 per cent for each per cent that the daily load factor is below 100 per cent,[5] with normal life expectancy. More nearly accurate corrections based on an actual load curve are not usually justified for normal loading. In no case should the overload permitted by this factor exceed 15 per cent, corresponding to 50 per cent daily load factor.

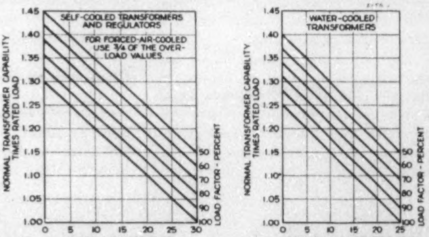

Figure 1. Transformer and regulator capabilities for normal life expectancy

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Paper 42-156, recommended by the AIEE committee on electrical machinery for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942 and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942. Manuscript submitted June 29, 1942; made available for printing July 9, 1942.

* Personnel of AIEE transformer subcommittee: M. S. Oldacre, chairman; F. S. Brown, B. S. Bundy, J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

Acknowledgment is made of the assistance given by R. T. Henry, H. W. Hartzell, J. S. Lennox, and D. L. Levine.

Temperature Rise of Windings

The information in this report is based on apparatus designed for the standard 55 degrees centigrade rise. If a transformer has been designed for other than 55 degrees centigrade rise, a rating to give 55 degrees centigrade rise can be calculated, and then the correction factors stated herein can be applied.

Supplemental Cooling—Existing Transformers

The load that can be carried on existing transformers can be increased by adding auxiliary cooling equipment such as radiator fans, external forced oil coolers, or water spray equipment. The amount of additional loading that such devices permit varies widely depending on

(*a*). Design characteristics of the transformer.

(*b*). Type of cooling equipment.

(*c*). Permissible increase in voltage regulation.

(*d*). Limitations of associated equipment.

Specific data should be obtained for each individual transformer.

When applying supplemental cooling to existing transformers, the permissible overloads should be based on the hottest-spot temperature rise instead of on average winding temperature rise. Usually the hottest-spot temperature rise over top oil temperature increases as the 1.6 power of the load.

If it is found that the bushings, leads, or other accessories are not a limiting feature, oil-insulated self-cooled transformers by the addition of fans may have the output increased in many cases 25 per cent and in some cases up to 33 per cent. Similarly, the addition of external forced oil cooling to oil-insulated self-cooled or oil-insulated water-cooled transformers may increase the output in many cases 25 per cent and in some cases up to possibly $66^{2}/_{3}$ per cent.

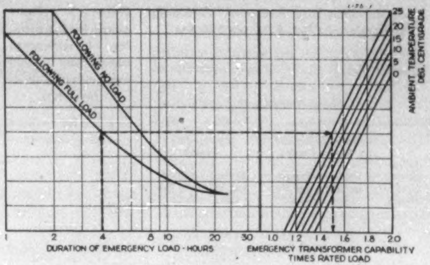

Figure 3. Emergency load capability of water-cooled transformers larger than 500 kva

See text for cautions and limitations

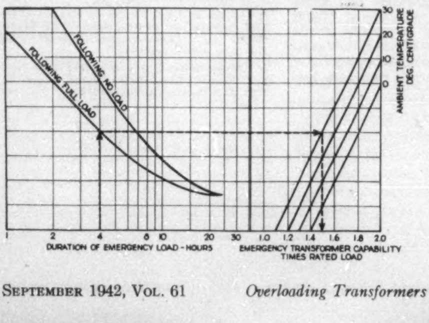

Figure 2. Emergency load capability of self-cooled transformers larger than 500 kva

See text for cautions and limitations

Water-spray equipment may be used in some cases as an emergency method of additional cooling. It is not generally considered satisfactory as a means of obtaining continuous additional capacity or for year-round operation. The added capacity obtainable by the use of water spray on oil-insulated self-cooled transformers is dependent on the air and water temperature, humidity, design of bushings, leads, and other accessories of the transformer. The water spray equipment may increase the capacity in many cases 25 per cent and in some cases up to possibly 60 per cent.

Emergency Loading With Moderate Sacrifice of Life Expectancy

The foregoing discussions have dealt with loading of transformers on the basis of normal life expectancies. In this section loading with moderate sacrifice of life expectancy during infrequent emergencies to values above those given in the American Standard C-57 "Guides for Operations of Transformers" is discussed.

On the basis of available data it is reasonable to consider that hottest-spot temperatures for durations shown in Table I represent an average sacrifice of life with each such emergency operation of not more than one per cent of the normal life expectancy as determined by the tensile strength of the winding insulation.

Table I. Temperatures for Short-Time Emergency Loads With Moderate Loss of Life of Insulation

Duration of Load—Hours	Hottest-Spot Temperature—Degrees Centigrade
1	137
2	130
4	125
8	120
24	110

With the above temperature limits and where specific data on the individual transformer are not available, the following loads are considered satisfactory for modern transformers and regulators with average ambient temperature during the overload period of 30 degrees centigrade for self-cooled and forced-air-cooled transformers and a water temperature of 25 degrees centigrade for water-cooled transformers. These overload values apply to the normal continuous name-plate kilovolt-ampere rating for output voltages equal to or above name-plate rating, and

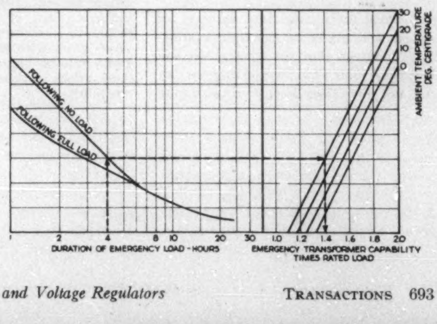

Figure 4. Emergency load capability of forced air-cooled transformers larger than 500 kva

See text for cautions and limitations

Table II. Short-Time Emergency Overloads for Transformers Rated Above 500 Kva*

(See text for cautions and limitations regarding ambients, rating, number of overloads, and so forth)
(Times Rated Load)

Duration of Load—Hours	Self-Cooled and Water-Cooled		Forced-Air-Cooled	
	Following Full Load	Following No Load**	Following Full Load	Following No Load**
1	1.9	2.0†	1.6	1.8
2	1.7	2.0	1.45	1.6
4	1.5	1.7	1.35	1.4
8	1.35	1.45	1.25	1.25
24	1.25	1.25	1.15	1.15

* Special consideration should be given to installations involving exceptional heavy-current designs.

** Values for operation "Following No Load" are on the basis that the transformer has been excited for at least several hours' time prior to application of the load. The loads that can be carried following partial load can be determined with sufficient accuracy by direct interpolation.

† It is considered that two times rated load is the maximum that should be carried, regardless of time, without special consideration.

Table III. Short-Time Emergency Overloads for Transformers Rated 500 Kva and Below,* and for Regulators

(See text for cautions and limitations regarding ambients, rating, number of overloads, and so forth)
(Times Rated Load)

Duration of Load—Hours	Transformers 500 Kva and Below		Regulators	
	Following Full Load	Following No Load**	Following Full Load	Following No Load**
1	1.7	1.9	1.5‡	1.5‡
2	1.55	1.7	1.5	1.5‡
4	1.4	1.5	1.4	1.5
8	1.3	1.35	1.3	1.35
24	1.2	1.2	1.2	1.2

* Special consideration should be given to installations involving exceptional heavy-current designs.

** Values for operation "Following No Load" are on the basis that the transformer has been excited for at least several hours' time prior to application of the load. The loads that can be carried following partial load can be determined with sufficient accuracy by direct interpolation.

‡ It is considered that 1.5 times rated load is the maximum that should be carried, regardless of time, without special consideration.

to normal continuous rated amperes for output voltages below name-plate rating. It is assumed that the emergencies will occur not more than once on any one day and not more than 25 or 30 times during the normal life of the transformer, and that the insulation, windings and oil are reasonably clean and free from excessive amounts of moisture and sludge.

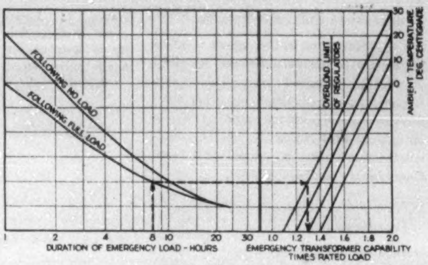

Figure 5. Emergency load capability of self-cooled transformers (500 kva or less) and of regulators

See text for cautions and limitations

Table II shows permissible loads for infrequent emergencies for modern sealed-type transformers rated above 500 kva which have the oil effectively protected from exposure to air and moisture. Table III shows permissible loads for infrequent emergencies for modern transformers rated 500 kva and below and for modern regulators.

Conclusions

Information has been presented herein concerning the effects of the characteristics of specific apparatus, ambient temperature, load factor, supplemental cooling, and so forth, in determining the load capability of transformers and voltage regulators for both normal and emergency conditions.

The effects of these factors are cumulative, as follows:

For *normal operation* with normal life expectancy the effects of load factor, ambient temperature, and supplemental cooling may all be added in determining the load capability. Figure 1 illustrates the application of factors for ambient temperature and low load factor in determining the normal capability for transformers and regulators.

For *short-time emergency operation* with moderate sacrifice of life the effect of the actual ambient temperature during the emergency and of supplemental cooling should be considered in addition to the values given in Tables II and III. Figures 2 to 5 inclusive illustrate the application of these values to determine emergency load capabilities.

The principles and facilities described in this report may be applied to existing or proposed new equipment and should be effective in conserving critical war materials.

Caution: It must be recognized that overloads should not be applied to transformers or regulators without a thorough study of the various limitations involved. Among these limitations are oil expansion, pressure in sealed-type units, bushings, leads, soldered connections, tap-changers, and so forth, and the thermal capability of associated equipment, such as cables, reactors, circuit breakers, disconnecting switches, current transformers, and so forth. These may constitute the practical limit in load-carrying ability.

Before overloading transformers or regulators to the full extent covered in this report, it is recommended that the overload capabilities of the equipment be checked with the manufacturer.

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A Compressed-Air Operating Mechanism for Oil Circuit Breakers

R. C. CUNNINGHAM
ASSOCIATE AIEE

A. W. HILL
MEMBER AIEE

General Field of Application

THE recent trend toward better use of existing lines and power sources has brought a demand for faster operation of oil circuit breakers and further simplification of control. In the past it has been common practice to use a solenoid or motor to provide closing energy for these breakers, either of which requires a substantial battery, means for keeping it up to full charge at all times, large conductors between the battery and the breaker to handle the heavy currents which exist for approximately one second while the breaker is closing, and generously proportioned control equipment. Consequently, the cost of apparatus associated with the breaker, but actually installed apart from the breaker, becomes a significant item.

Compressed air can be used as a source of energy to close these breakers, and with several obvious advantages. In the first place, the electrical drain on a battery at the instant of closing is only the control relay current, not the full solenoid closing coil current. This may be a reduction of the order of 1 to 100 which is reflected in smaller batteries and chargers, lighter control cables, and contactors. Another advantage is that full air pressure is instantly available to start closing the breaker, whereas a solenoid builds up in power at a slower rate, depending on the inductance of the coil. Thus it is possible to secure faster breaker operations.

In a fundamental way the apparatus required for closing a breaker with compressed air consists of:

(*a*). A source of air comprising a compressor and reservoir.

(*b*). A cylinder and piston within which the air can be released to force the breaker to the closed position.

(*c*). Suitable means for controlling the flow of air by means of magnetically operated valves operated from a distant point.

Obviously, it is not practical to build the commercial apparatus in quite such simple form, and various requirements of operation have made it desirable to add refinements as the following will disclose.

The Air Supply

It is desirable that an air reservoir be located close to each operating cylinder, as air flowing a long distance through small pipes will lose pressure to such a degree that the delivered air may not be able to accomplish the work of closing the the breaker in the required time. Hence, a 20- or 30-gallon reservoir is installed in the same housing with the operating mechanism and control and mounted on the end of the breaker.

For reliable service it has been recommended that a motor-driven compressor be mounted in each housing, thus making each breaker entirely independent of any other. It could be argued that this is an unnecessary expense where several breakers are installed in close proximity to each other, and where one compressor could perhaps handle the several reservoirs. However, it appears that most operating men look at the possible dollar saving as overbalanced by the hazard of shutting down the entire group of breakers, in case of the failure of the one compressor, and the possible difficulty of maintaining interconnecting air piping clear and tight.

If it is preferred to use a single compressor unit for an entire station, it is recommended that a double compressor be used so as to provide increased reliability. Also, proper consideration must be given to protection against mechanical damage to piping and to the drainage of any water which might condense in the air line. If freezing temperatures will be encountered in the locality of installation, adequate sizes of pipe and sumps must be provided to prevent blocking with ice.

The assembly of compressor and reservoir is built to exceed the standards required for industrial systems and railway brake installations, in which continuity of performance has been an important factor worked out years ago. It is designed to meet all usual safety code inspection requirements, and the reservoir carries the stamp of the boiler code inspector of The American Society of Mechanical Engineers. An automatic governor maintains a maximum pressure of 200 pounds per square inch, starting the compressor whenever the pressure drops to 15 pounds below the preset maximum. A safety valve limits the reservoir pressure to 225 pounds. Ordinarily only one-half or three-fourth horsepower is required in the compressor motor so that little current is drawn from the a-c supply line. In ordinary service the motor will run only a few minutes each day, as required for the occasional breaker operation and in order to overcome slight leakage loss. Starting with an empty reservoir, a full charge can be pumped in one-half hour. The motor need not run each time the breaker operates, as the amount of air used is frequently so small as not to drop the reservoir pressure to the point where the automatic governor picks up. In case of the failure of the supply voltage to the compressor motor, a full reservoir contains enough air to operate the breaker at least five times. Further, if there should be no immediate demand for an operation, it would require more than 12 hours for a normally tight air system to leak down to a dangerously low pressure, giving a wide margin of time to re-establish compressor power or service. A low-pressure switch indicates through an alarm circuit when the drop in reservoir pressure is becoming acute, and another switch locks out the closing relay circuit so as to prevent the attempt to close, when the air pressure is too low to insure completion of the operation.

The Lever System

Figure 1 shows a typical mechanism assembly mounted on the tank of a 138-kv outdoor oil circuit breaker. The compressor and reservoir occupy the space at the right of the assembly. At the left and center is the operating mechanism itself, with a magnetically operated inlet valve conveniently located directly in front of the main cylinder. The current demand of this magnet valve is well under two amperes.

One requirement for a mechanism of this type is that it shall be capable of reclosing the breaker promptly, under some conditions in not over 20 cycles (one-third second). This mechanism meets the demand by retaining a positive mechanical connection between the breaker and the operating piston at all times, so that at any time it may be desired air admitted to the cylinder will immediately act to close the breaker, regardless of

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R. C. CUNNINGHAM is with Westinghouse Electric and Manufacturing Company, and A. W. HILL is with the switchgear engineering department of Westinghouse Electric and Manufacturing Company, both in East Pittsburgh, Pa.

whether it may have reached the full open position or not. A single lever, hinged at one end, and carrying a roller at the other end to be held under the hook shaped latch when the breaker is closed, is attached to both the piston and the breaker pull rod at the center.

The breaker is closed under the effect of the compressed air and latched in the closed position, so that the air can be cut off at the end of this stroke. When the breaker is called upon to open, the latch is released by a trip magnet, and the usual accelerating spring on the breaker furnishes the impetus needed to secure the proper contact opening speed. The opening is therefore independent of the condition of the air supply, and required interrupting time of the breaker is controlled entirely by the mechanical time of unlatching the mechanism and accelerating the contacts by springs which were preloaded during the closing stroke of the breaker.

It is evident that when a direct mechanical connection exists between the breaker and the piston, closing and reclosing operations are easily obtained. Experience with solenoids has shown that it is necessary to provide them with mechanically trip-free linkages so as to permit full opening speed without the contacts being delayed by the magnetic drag of the closing solenoid. The mass of the solenoid core also acts as a drag to prevent full opening speed. These difficulties are not present in the air mechanism when a light piston is used, and when some means is provided for dumping from the operating cylinder, the air still under pressure, so that it will not restrict the opening speed of the contacts.[1] The latter demand is met by a series of exhaust ports—on the side wall of the main cylinder, near the fully closed position—which are opened simultaneously with the operation of the breaker trip coil. The total area of these ports is approximately four square inches, and oscillographic pressure records show that the collapse of pressure is so fast that less than 25 pounds remains in the cylinder at the time the contacts part. The extreme speed of this dump valve is secured by utilizing the high-pressure air on the end of the cylindrical valve, after it has once been cracked by a pilot piston or small magnet.

While the single-lever design adequately meets the requirements of quick reclosing, some modification is necessary where manual operation of the smaller breakers is concerned. In this class of apparatus, one man can complete the closing operation with a single quick stroke of the removable handle, but must be protected against holding the breaker in on a fault, as would be the case with the non-trip free mechanism. A solution to this problem is shown in Figure 2, where the single-lever system for power operation is modified when it is to be manually operated, by unlocking the fixed hinge point and temporarily restraining the automatic latch over its roller, so that, if necessary, the breaker can be tripped in the manner of the conventional double-lever system. Thus, the benefits of trip-free action can be secured, while in the same parts the inherently fast reclosing possibilities of the non-trip free action are retained.

Controlling the Air Flow

There is a characteristic mechanical load curve for all breakers having the general shape of that marked "Breaker Load" in Figure 3. It is made up of the dead load, balance and accelerating springs, and the extra load of contact pressure springs at the closed position. The breaker toggles may alter the magnitude of these combined loads at different parts of the stroke, but the fundamental characteristic remains. The simple solenoid pull curve is well adapted to this, since it develops a greater pull as the cores come together (see curve marked "Solenoid Mechanism" also in Figure 3). The surplus of power shown for the solenoid

Figure 1. Type CAS-8 mechanism for outdoor service

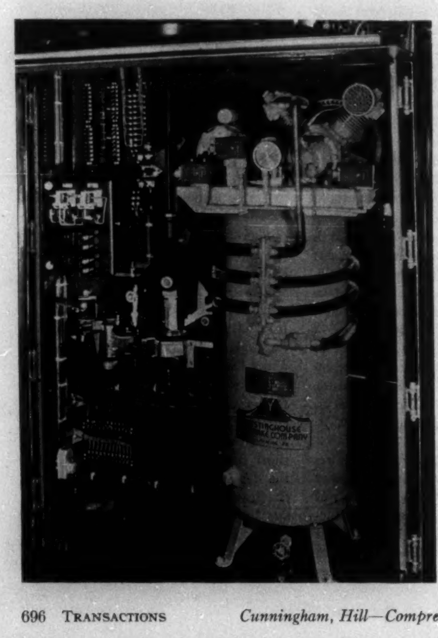
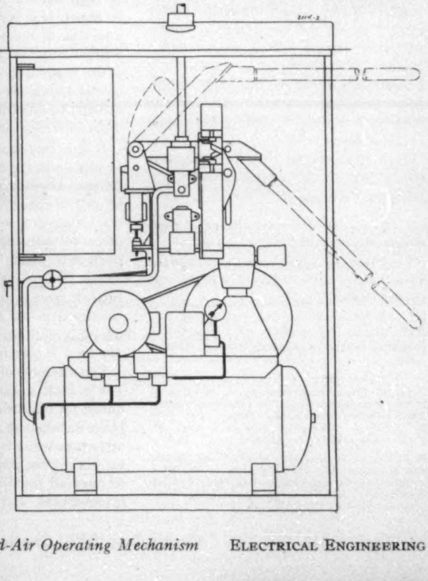


Figure 2. An outdoor-type compressed-air mechanism with trip-free hand-closing lever



goes into speed and, of course, is eventually lost, as breaker strikes its stops in the closed position, but the general shape of the pull curve of the solenoid usually falls just above the load curve of the breaker, so that there are no large instantaneous surpluses or deficiencies. With the same breaker and a simple air-operated mechanism, "Elementary," the pull characteristic would be largely flat, drooping slightly toward the closed position, as the air was expanded into the operating cylinder or dropped in pressure, because of resistance to flow in the connecting passages between the reservoir and the cylinder. Rather than change the breaker levers to co-ordinate the simple air-mechanism pull and the conventional breaker load, a scheme has been worked out to throttle or control the flow of air, and the air-mechanism pull curve takes the general shape of that shown in Figure 3, "Air Mechanism—Controlled."

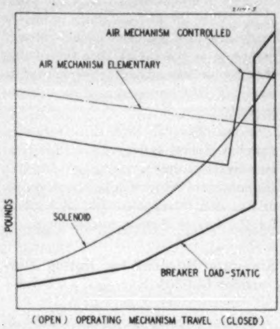
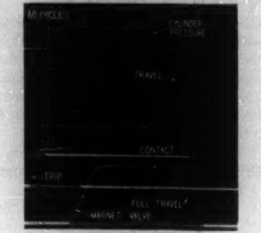


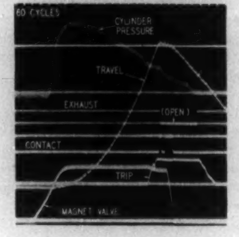
Figure 3. Typical curve of a breaker load and mechanism pull

The principle of the throttle is that when the load is light the air passage is kept small. When the load is about to rise, the throttle is opened by a mechanical linkage, and with the augmented air supply, the stroke is completed with just the right amount of pull to insure positive latching and yet not with excessive slam. Since the throttle is adjustable both as to amount of opening and to position at which it becomes effective, it makes possible the use of one size of mechanism on several types and ratings of breaker.

The throttle is operated through a slotted link, since it is found that full air pressure is required over a greater part of the stroke when fast reclosure is attempted, as compared to that required to complete a simple closing and latching. Under this condition of fast reclosure greater power must be available, first to arrest the opening motion, and then to bring the contacts back to the closed position. The use of a throttled air system of course means the choice of an air pressure and piston area such that the required maximum pull will be secured, but this does not mean that it takes more air than an unthrottled system—rather, that through control, the power available is released as demanded by the load, with proper provision made for those operations particularly requiring high speed.



(a). Reclosing in less than 20 cycles



(b). A close-open operation

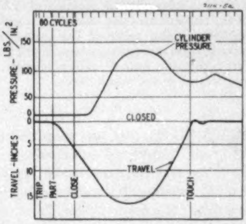
Figure 4. Oscillograms of breaker timing

Result of Tests

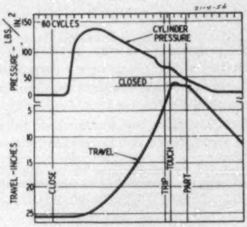
A timing test setup was made on a typical 138-kv oil circuit breaker equipped with an eight-inch compressed-air mechanism of the type described above. Oscillographic records, shown in Figures 4a and 4b show the variation in cylinder pressure, the motion, or travel, of the movable contacts, the motion of the exhaust valve, a battery indication of contact engagement, and the trip-coil and magnet-valve-coil currents. For the sake of clarity the essential data have been traced from these oscillograms and are shown in Figures 5a and 5b.

Figure 5a shows an *OC* operation for which the time from energizing the trip coil to retouching the contact was 19.3 cycles. It will be noted that the magnet valve was energized $4^1/_2$ cycles after the trip coil, and that the contacts had traveled approximately 36 per cent of the entire stroke at the instant the air valve was opened. The contacts continue opening with reducing speed until there is a cushioned reversal of motion at 57 per cent of the full stroke. The cylinder pressure falls as the contacts travel toward the closed position, due to expansion and throttling. The result is a quick reclosure without excessive strain on the mechanism or pole unit parts, and without excessive slam as the breaker reaches the closed position.

Figure 5b illustrates a *CO* operation using the same breaker and mechanism. This test was set up for pretripping, as soon as the auxiliary switch contacts in the trip circuit were made up. Even under this condition the "air-trip-free" action of the mechanism is demonstrated by the fact that there is no back pressure on the operating piston after the first few cycles of travel. A comparison of the opening portion of Figures 5a and 5b shows that at the end of eight cycles after energizing the trip coil, the contact separations exceed that required to interrupt normal short-circuit currents.



(a). Reclosing in less than 20 cycles



(b). A close-open operation

Figure 5. Clarified speed and pressure records from Figure 4

Field service throughout a full range of seasonal changes in weather verifies the high degree of reliability obtained in laboratory testing. The flexibility in design and the compactness of the unit obtained by the use of compressed air, and the encouragement gotten from commercial contacts, indicate an increasing demand for this type of mechanism.

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Current- and Potential-Transformer Standardization

AIEE COMMITTEE ON PROTECTIVE DEVICES
Current Transformer Subcommittee*

Synopsis: This report has been written to summarize the work of the AIEE current transformer subcommittee and to discuss the considerations which led to the adoption of the material in the revision of section 4, "Instrument Transformers," of the Proposed American Standard for Transformers, *C*-57. Special attention is given to the principles underlying the establishment of the overcurrent requirements of current transformers for relaying service and the revision of the method of specifying the accuracy of instrument transformers for metering service. Mention is also made of the reasons for making other changes, such as in the preferred primary current ratings, potential-transformer ratios, and so forth.

DURING the past two years the current transformer subcommittee of the relay subcommittee revised the section on instrument transformers of the American Standard, *C*-57. When this group was appointed, its assignment was to formulate specifications for current-transformer performance in the overcurrent range for relay application. During the progress of the work, however, the activities of the group were enlarged until they covered the complete revision of section 4 of the Proposed Standard for Transformers. This was the result of a desire to correlate the new material with the old material and to save time and duplication of effort by having the work done by one group. Contacts however were made with the other interested groups so that the work proceeded smoothly with the support and co-operation of these groups. This paper has been written to present the reasons why the material in section 4 is there in its present form.*

Primary Current Rating of Current Transformers (4.021)

The original proposal in the printed draft of *C*-57 contains 29 values of primary current ratings divided into four groups. It was generally felt that this represented an excessive number of ratings and that the number of primary current ratings could be drastically reduced and still adequately cover the field.

The first step was to set up a list of ratings based on one of the sequences in the system of preferred numbers. This gave a list of approximately 16 ratings which covered the range adequately. However, no agreement could be reached upon the values obtained by this simplified method of approach, because certain active ratings were not included and, accordingly, a different method of approach had to be used.

Primary current ratings of 10, 25, 50, 100, 200, 400, and 800 had been established by the joint AEIC-EEI metering group of the association of Edison Illuminating Companies and the Edison Electric Institute, and these ratings were used as a nucleus around which the list now appearing in the standards was established. It was necessary to effect a compromise among the requirements of the groups interested in applying relays, meters, and instruments. Because of the whole-hearted co-operation among the interested groups in reaching a solution which would afford a saving to the industry, the number of preferred primary ratings has been substantially reduced, the reduction being from 29 to 21 ratings.

The preferred primary current ratings are now in one table in paragraph 4.021. It is not the intent that all the values be made available for each circuit voltage; rather, it is the intent that the ratings used for any circuit voltage be limited to as few of values appearing in the table as possible.

It was also felt that there were too many preferred ratings for double-ratio current transformers, and the reduction in the number of values required was a simple matter after the problem had been solved for single-ratio current transformers.

Standard Burdens for Rating Purposes (4.030)

Section 4.030 of the revised standard concerns standard burdens for current transformers, combining and taking the place of the former two sections 4.030 and 4.031 dealing respectively with "Burdens for Current Transformers for Metering Service" and "Burdens for Current Transformers for Relay Service."

Formerly, burdens designated as X, Y, and Z, or 2.5 and 15 volt-amperes at 0.9 power factor, and 50 volt-amperes at 0.5 power factor, respectively, at five amperes and 60 cycles were set as standard metering burdens; then a separate series was set up for relay service as 12.5, 25, 50, 100, and 200 volt-amperes at 0.5 power factor at five amperes and 60 cycles, these burdens to be used for rating purposes.

These two groups have now been combined into one list of standard burdens designated as *B*-0.1, *B*-0.2, *B*-0.5, *B*-1, *B*-2, *B*-4, and *B*-8 corresponding to the ohmic values at 60 cycles of the burden of 0.1 ohm, 0.5 ohm, and so forth. The first three have a burden power factor of 0.9, and the last four have a power factor of 0.5 at 60 cycles. The old meter burden Y of 0.6 ohm at 0.9 power factor and the relay burden 0.5 at 0.5 power factor have been replaced by a single burden *B* 0.5, corresponding to 0.5 ohm at 0.9 power factor or 12.5 volt-amperes at 0.9 power factor. There is no essential difference in these three values as far as performance is concerned, but the new value fits into the list of burdens in more orderly fashion.

It was also found necessary to add an additional value of burden; namely, that

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* Report prepared by J. E. Clem, central station engineering department, General Electric Company, Schenectady, N. Y.; J. H. Neher, senior engineer in charge of relay design, engineering department, Philadelphia Electric Company, Philadelphia, Pa., and E. L. Harder, central station engineer, Middle Atlantic district, industry engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

Personnel of AIEE current transformer subcommittee: J. E. Clem, *chairman;* G. Camilli, F. E. Davis, Jr., G. B. Dodds, T. R. Halman, D. R. Laib, P. O. Langguth, C. V. Morey, J. H. Neher, A. T. Sinks, D. W. Taylor, E. C. Wentz, C. O. Werres.

* Numbers following subheads refer to paragraphs of the currently proposed revision of section 4 of the Proposed American Standard for Transformers *C*-57 which are given in appendix 3 of this paper.

designated as B-0.2, or five volt-amperes at 0.9 power factor for metering service. This burden is now required for certain current transformers used on low-voltage circuits.

In the revised table then, burdens B-0.1, B-0.2, B-0.5, and B-2 are standard burdens for metering service for rating purposes, and burdens B-0.5, B-1, B-2, B-4, and B-8 are standard burdens for relay service for rating purposes. It should be noted that these burdens also may be designated by the volt-amperes and power factor at five amperes and 60 cycles of each, if the longer designation is preferred. The new designations are different from the old ones in order to avoid confusion with former designations. The proposed values are as shown in paragraph 4.030.

Burdens for Potential Transformers (4.036)

Former section 4.032 concerned standard burdens for potential transformers for rating purposes in all classes of service. This is now included in the revised standard in 4.036. Only one change was made; namely, the volt-amperes of burden W was changed from 13 to 12.5 merely so that it would be one half of the value of the X burden, which is 25.

Classification of Current Transformers for Accuracy Metering Service (4.031)

Former section 4.033 concerned standard accuracies of current transformers and section 4.035 concerned standard accuracies of potential transformers for metering service. In the revised standard these are included as sections 4.031 for current transformers and 4.037 for potential transformers. Accuracy classes have been specified on a different basis from that previously used. Formerly, fixed limits were assigned separately to ratio error and phase angle for each accuracy class, but on this basis the class designation did not in itself indicate the probable effect of the transformer on the accuracy of measurement of watts or watt-hours.

Since the primary concern with respect to ratio and phase angle of an instrument transformer is the effect on the accuracy of measurement of watts or watt-hours, it is more consistent to use this effect as a criterion of performance, rather than merely the arbitrary ratio error and phase-angle limits which might be chosen. A measure of this effect is the ratio of what a wattmeter would read in a circuit with an instrument transformer having no ratio error or phase angle to what it would read in the same circuit with an instrument transformer having the usual errors. This ratio has been called "transformer correction factor" or TCF, and the standard defines it as "the factor by which the watt-hour meter registration or the wattmeter indication must be multiplied to correct for the error introduced by the transformer through the combined effect of the ratio correction factor (RCF), phase angle (β),* and power factor angle (θ) of the metered load.

Thus for a current transformer (see appendix 1):

$$\mathrm{TCF} = \mathrm{RCF}\frac{\cos\theta}{\cos(\theta-\beta)}$$

At the same time it was also decided to use RCF instead of per cent ratio error.

$$\mathrm{RCF} = \frac{\text{true ratio}}{\text{marked ratio}}$$

The values of TCF for a transformer are dependent upon the power factor of the circuit being measured and the RCF and phase angle of the transformer. Where the transformer angle is small, the relation between these various factors can be expressed in a rather simple manner within accuracy limits sufficiently good for the purpose. By choosing limits for θ and TCF, the limiting values of phase angle and RCF are set. The range of power-factor angle chosen is from 0 to 53 degrees 08 minutes, corresponding to circuit power factor of 1.0 and 0.6 lagging respectively, which covers the usual range of power factors encountered in practice. For these conditions the relation between the limiting values becomes:

For CTs, $\beta = 2{,}600\,(\mathrm{RCF}-\mathrm{TCF})$, in minutes

For PTs, $\gamma = 2{,}600\,(\mathrm{TCF}-\mathrm{RCF})$, in minutes

If e represents the permissible per-unit error in the measurement, it may be positive or negative, and the corresponding limiting values of TCF will be $(1-e)$ and $(1+e)$. See Figure 1.

The limiting values of TCF as set up in the standard have been chosen entirely from the practical viewpoint of transformer accuracies which are required and those attainable with transformers either of modern or earlier design. The new accuracy classes and, consequently, the limiting values of TCF correspond very closely to the former accuracy classes at circuit power factors at or near 1.0. A new class of wider limits than before has been added to permit an accuracy rating for current transformers with ratio errors in the order of two per cent at 100 per cent rated current such as for certain bushing and similar types.

It will be noted on investigation, for example, that the former 1/4 class results in the limiting TCFs for current transformers at 100 per cent rated current of 0.99365 and 1.00635 in the range of line power factors from 0.6 to 1.0, which produces maximum meter errors of about ±0.635 per cent. This is more than double the 0.25 per cent error sometimes assumed to be indicated by the former 1/4 class designation, which corresponded to the maximum ratio error only without consideration of phase angle and over-all metering error.

For the new 3/10 class at 100 per cent rated current β varies from $+15.6$ to -15.6 minutes (see Figure 1), the limits of which are 50 per cent greater than formerly for the 1/4 class but which result in only half the maximum possible meter error for that class. It should be noted that this method is the equivalent of that used for the former class M only which has been continued as the class 5/10.

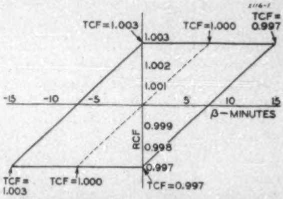

Figure 1. Parallelogram showing limits of ratio correction factor, RCF, and phase angle, β, for a current transformer having a 3/10 accuracy class rating

There accordingly results a means of accuracy rating of instrument transformers for metering service that is of more significance than formerly to the average meterman, since the accuracy class rating is associated directly with the effect of transformer characteristics on the accuracy of measurement. The limits set appear to be practical and adequate for all ordinary purposes, and the method utilized is not involved but provides the necessary rigidity in a simple manner for specification purposes.

Standard Accuracy Classes for Current Transformers for Relaying Service (4.032)

This section, not previously included in the proposed American Standard for

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* β is defined as the angle by which the secondary current leads the primary current.

Transformers is the result of an intensive effort to establish standard classes of overcurrent performance. It provides a much needed benchmark for comparison of the performance of various current transformers and for specifying their accuracy at the high currents involved in protective-relay applications. It also establishes in a general way the limits within which a given transformer does not exceed specified errors under overcurrent conditions.

It is important to note that the terms "accuracy" and "performance" as covered by this standard refer to steady-state conditions only. The performance of current transformers under transient conditions is far too complicated to permit of standardization at the present time. While the generalization may be made that the better the steady-state performance, the better the performance will be under transients, nevertheless, it must be realized that adequate performance within the limits of these standards is not necessarily indicative of adequate performance under transient conditions. Reference should be made to the various items of the bibliography.

Current transformers are classified primarily on the basis of the standard burden which each can carry with a specified secondary current without exceeding a specified per cent error. To standardize ratings five amperes has been selected as the standard rated secondary current, and for overcurrent performance the transformers are classified primarily on the basis of their ability at secondary currents of the same value, namely 20 times rated.

Since all the transformers are standardized on the secondary current, the current need not be part of the necessary designation. The designation proposed to identify or express the accuracy classification is, for example, 2.5*H*400 or 10*L*-200. The first number identifies the accuracy class and the limiting per cent error. The letter identifies the type of accuracy classification. The second number identifies the terminal voltage which can be developed on one of the standard burdens at 20 times rated secondary current without exceeding the specified per cent error. This scheme of designation was adopted, because it was expected to give the most useful information.

In the application of relays it is essential that the operating engineer have a knowledge of the performance of the current transformer under overcurrent conditions. While considerable error can frequently be tolerated, it is essential to know the amount of the error or a limiting value which will not be exceeded. This point of view led to the establishment of two accuracy classes for current transformers for relaying service.

In general, the change in the phase angle need not be considered in relaying applications because often it is of no consequence and is relatively small as long as the ratio error is not excessive. Therefore, most attention is focused upon the ratio error. The standard limiting ratio errors are 10 per cent and $2^1/_2$ per cent, the particular one applicable being indicated by the first figure in the accuracy designation. The figure of 10 per cent has been chosen, both because errors not in excess of this figure can be disregarded in most relay applications, and because for errors in excess of this figure the accuracy of most current transformers falls off very rapidly. The second accuracy class with $2^1/_2$ per cent as the limit in ratio error has been set up to cover those cases where 10 per cent cannot be tolerated. The error is expressed in per cent of the secondary current.

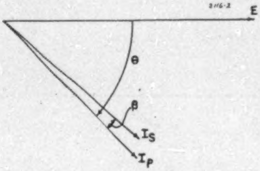

Figure 2. Actual and indicated currents

The overcurrent requirements for current transformers are based upon an overcurrent of 20 times rated in the secondary winding. With a five-ampere rated secondary current, the choice of 20 times rated secondary current is preferable to 20 times rated primary current, because the former results in a secondary current of 100 amperes, which is a convenient number for such purposes.

While the current transformers are classified primarily on the basis of the burden which can be carried at 20 times rated with the rated accuracy, it is desirable to specify, if possible, the burden which can be carried at lesser overcurrents than equal accuracy. An analysis showed that this could be done very simply for transformers of relatively high internal impedance,* such as wound types. Transformers having relatively low internal impedance can be standardized at only one point. Therefore, any method conceived of standardizing the performance at reduced overcurrents for the transformers of relatively low internal impedance, such as bushing type, had serious disadvantages. It was either too complicated or, if simple, necessitated in many cases transformers much larger than would be required to meet only the accuracy requirement at the basic standardizing point of 20 times rated.

In order to capitalize on the ability to standardize the accuracy of the high-internal-impedance types over the range of 5 to 20 times rated secondary current, without penalizing the relatively low-internal-impedance types, two separate classifications have been set up, designated as *H* and *L*. The letter *H* or *L* indicates whether the transformer is classified as a "high-internal-impedance" or a "low-internal-impedance" type. The reason for this distinction is as follows:

The excitation voltage upon which exciting current and consequent ratio error depend is the sum of the secondary terminal voltage plus the internal-impedance drop caused by the secondary current. If the secondary current is reduced from 20 times rated to 5 times rated, but the burden increased inversely, so as to keep the secondary terminal voltage constant, the excitation voltage will decrease due to the reduced internal-impedance drop. The exciting current decreases quite rapidly with a decrease in excitation voltage and for the high-internal-impedance-drop types this effect is great enough that the per cent error tends to remain constant. Thus, for the designs of high-internal-impedance drop, the requirement can be imposed that rated accuracy must be maintained for currents from 5 to 20 times rated with such burdens as to give a terminal voltage no greater than that obtained at 20 times rated current with rated burden. In only relatively few cases will this further requirement increase the size.

Two examples will clarify the meaning of the accuracy designations.

1. A 10*L*400 current transformer is of low internal impedance (as a bushing type). It can carry a *B*-4 burden from one to 20 times rated secondary current, or 5 to 100 amperes, with not over 10 per cent error. It can induce 400 volts with not over ten amperes exciting current.

2. A 2.5*H*200 current transformer is of high internal impedance (as a wound type) capable of supplying a *B*-2 burden at from one to 20 times rated secondary current with not over 2.5 per cent error. It can also supply any standard burden which will not require over 200 volts with secondary currents from 5 to 20 times rated, without exceeding 2.5 per cent error. At 2.5 am-

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* The term "internal impedance" is used to identify that elusive characteristic of current transformers through which the exciting current required for a specified secondary induced voltage becomes different for different values of secondary current.

peres on the open-circuit saturation curve the transformer will induce over 200 volts.

Application Data for Current Transformers (4.033)

There is a growing appreciation of the fact that where applicable the saturation-curve method of calculating the performance of current transformers presents a distinct advantage over other methods involving the use of ratio and phase-angle curves. Transformers of the low-internal-secondary-impedance type are susceptible to such treatment. Accordingly, it was thought desirable to establish in the standard application data which shall be available from the manufacturers for use in this connection.

These data should include curves showing the excitation current as a function of the excitation voltage, or volts per turn, together with sufficient data to determine the inphase (watt) component and out-of-phase (magnetizing) component, or the phase angle. The data should also include the resistance of the secondary winding and such information in reference to the internal impedance as may be usable.

Calculations for transformers having low internal impedance may be rather simply made. The initial step is to determine required excitation voltage as based upon the secondary current and the sum of the burden ohms and the secondary resistance.

The exciting current is the maximum error current and may be read directly from the curve, and usually this is all that is required. If the phase angle or a more precise ratio error is required, it may be determined by one of the methods discussed in some of the references in the bibliography. All of these methods use the exciting current in magnitude and position with respect to the excitation voltage, although the mechanics may be different, and they are all essentially a solution of the vector diagram, Figure 3.

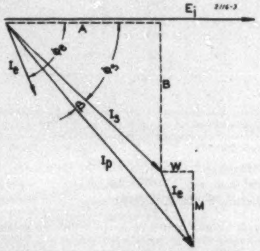

Figure 3. Vector diagram of transformer—low internal secondary impedance type

A few of the methods are given in appendix 2, and it is hoped that experience will indicate a preference for one or the other of these methods.

It is very unfortunate that transformers of the high-internal-impedance type are not directly susceptible to the simple method of calculation, although a somewhat similar method has been proposed. The Standards, therefore, continue the use of the familiar ratio and phase-angle curves for expressing the performance of this type of transformer.

Mechanical Limit for Current Transformers (4.046)

Because the mechanical strength specifications (paragraph 3.050, American Standard *C*-57) of power and distribution transformers is stated in a manner which involves a time element ranging from two to five seconds, it was suggested that the mechanical limit for current transformers should also involve a time element, and one second was suggested. However, the situation is quite different for the two types of transformers; the power or distribution transformer limits the short-circuit current to a definite value, while a current transformer has no appreciable effect in limiting the fault current. Actually, the time element for power and distribution transformers has more concern with the thermal requirement than with the mechanical. If the transformer does not fail within the first few cycles of a fault it will not fail at all mechanically.

There is some opinion that a current transformer may fail mechanically after the crest of the first maximum half cycle has passed, and this has apparently been confirmed to some extent by tests. Also it was quite strongly felt that one second was entirely too long a time in view of the fairly rapid decrement in the fault current. To facilitate testing the current is specified on a symmetrical basis, and it is permitted to cover the time range in successive applications. The proposed requirements are shown in paragraph 4.046.

This paragraph as written includes material which properly belongs in the test code. Due to the lack of a test code at the present time, this material is placed in the standard, because it involves the demonstration of the specifications of mechanical limit. The rated mechanical limit is specified in rms symmetrical amperes, coupled with the stipulation that the transformer must withstand the forces produced by this value of current when the current wave is completely offset. In testing this may be demonstrated by a symmetrical wave of twice the value of the rated mechanical limit maintained for the specified time of six cycles; or it may be demonstrated by successive applications of current with no intentional delay between applications, in which case the crest current may be measured for the final significant cycle of each application and must not be less than twice the crest corresponding to the crest of the rated mechanical limit.

Open Secondary Circuit of Current Transformers (4.047)

The original *C*-57 simply required that current transformers operate with the secondary circuit open "without damage to the transformer." In many cases the strict interpretation of this rule resulted in an abnormal design without any compensating features. Accordingly it was thought better to correlate the requirement for this condition with the test on the secondary windings. The proposed requirements are shown in paragraph 4.047.

Preferred Ratios for Potential Transformers (4.020) and Standard Insulation Classes and Tests for Potential Transformers (4.050)—Table IX

The question of what insulation test was standard for a potential transformer of a given ratio for circuits below 15 kv was the cause of many misunderstandings. The solution of this problem consisted of combining the tabulation of preferred ratios with the tabulation of insulation classes and tests.

When the preparation of *C*-57 was first begun, it was recognized that certain instrument transformers had impulse levels one step lower than those being considered for the circuit voltages with which the transformers were being used, although they could withstand the proposed low-frequency test required for these same circuit voltages. The circuit voltages in question were in the 5.0-, 8.7-, and 15-kv classes, and to take care of this situation note *c*, reading as follows, was proposed:

> "(*c*) Indoor instrument transformers are available in the 2.5-, 5-, 8.66-, and 15-kv classes insulated one level lower than the values of this tabulation."

This note was the cause of great confusion, because its wording permitted misinterpretation, and in the printed draft of *C*-57 it was interpreted as applying to the lower-frequency test also. It resulted in a line of potential transformers having ratios 20:1, 40:1, and 60:1, which had impulse insulation levels corresponding to the present insulation classes 2.5, 5.0, and 8.66, respectively. These transformers, according to the present standard, cannot however be connected in wye for use between line and neutral at their rated excitation voltage, because they are not insulated for this service. Because of this, there has been considerable effort to eliminate them entirely from the standard. However, these transformers represent existing practice and are used extensively, particularly for metering applications. Placing them in the table and associating their ratios with their proper insulation class permits their continued use and eliminates the possibility of confusing them with the trans-

Preferred Ratios Standard Insulation Classes and Dielectric Tests for Potential Transformers

Table IX of Proposed Standard C-57

		Rated Voltage		Normal Circuit Voltage on Which Transformers May Be Used		Dielectric Tests			
						Low Frequency	Impulse		
							Chopped Wave		Full Wave
Insulation Class (Name-Plate Rating) Col. 1[a]	Preferred Ratio Col. 2[b]	Secondary Volts Col. 3[b]	Primary Winding (Name-Plate Rating) Col. 4	When Δ Connected Volts Col. 5[a,c]	When Y-Connected Volts Col. 6[a,d,e]	Kv RMS Col. 7[f]	Kv Crest Col. 8[f]	Minimum Time to Flashover µsec. Col. 9[f]	Kv Crest Col. 10
1.2	1-1	120	120	120	208	10	36	1.0	30
	2-1	120	240	240	416	10	36	1.0	30
	4-1	120	480	480	832	10	36	1.0	30
	5-1	120	600	600	1,040	10	36	1.0	30
2.5	20-1	120	2,400	2,400	2,400	15	54	1.25	45
5.0	20-1	120	2,400/4,160 *Y*	2,400	4,160	19	69	1.5	60
	40-1	120	4,800	4,800	4,800	19	69	1.5	60
8.66	35-1	120	4,200/7,280 *Y*	4,200	7,200	26	88	1.6	75
	40-1	120	4,800/8,320 *Y*	4,800	8,320	26	88	1.6	75
	60-1	120	7,200	7,200	7,200	26	88	1.6	75
15*L*	60-1	120	7,200/12,480 *Y*	7,200	12,000	34	110	1.8	95
	70-1	120	8,400/14,560 *Y*	8,400	14,400	34	110	1.8	95
	100-1	120	12,000	12,000	12,000	34	110	1.8	95
	120-1	120	14,400	14,400	14,400	34	110	1.8	95
15 *H*	60-1	120	7,200/12,480 *Y*	7,200	12,000	34	130	2.0	110
	70-1	120	8,400/14,560 *Y*	8,400	14,400	34	130	2.0	110
	100-1	120	12,000	12,000	12,000	34	130	2.0	110
	120-1	120	14,400	14,400	14,400	34	130	2.0	110
25	200/115-1	69.3/120	13,800/24,000 *Y*	—	24,000	51	175	3.0	150
	200-1	120	24,000	24,000	24,000	51	175	3.0	150
34.5	300/173-1	66.4/115	19,900/34,500 *Y*	—	34,500	70	230	3.0	200
	300-1	115	34,500	34,500	34,500	70	230	3.0	200
46	400/231-1	66.4/115	26,600/46,000 *Y*	—	46,000	93	290	3.0	250
	400-1	115	46,000	46,000	46,000	93	290	3.0	250
69	600/346-1	66.4/115	39,800/69,000 *Y*	—	69,000	140	400	3.0	350
	600-1	115	69,000	69,000	69,000	140	400	3.0	350
92	800/462-1	66.4/115	53,100/92,000 *Y*	—	92,000	185	520	3.0	450
	800-1	115	92,000	92,000	92,000	185	520	3.0	450
115	1,000/577-1	66.4/115	66,400/115,000 *Y*	—	115,000	230	630	3.0	550
	1,000-1	115	115,000	115,000	115,000	230	630	3.0	550
138	1,200/693-1	66.4/115	79,700/138,000 *Y*	—	138,000	275	750	3.0	650
	1,200-1	115	138,000	138,000	138,000	275	750	3.0	650
161	1,400/809-1	66.4/115	93,000/161,000 *Y*	—	161,000	325	865	3.0	750
	1,400-1	115	161,000	161,000	161,000	325	865	3.0	750
196	1,700/982-1	66.4/115	113,100/196,000 *Y*	—	196,000	395	1,035	3.0	900
	1,700-1	115	196,000	196,000	196,000	395	1,035	3.0	900
230	2,000/1,155-1	66.4/115	132,800/230,000 *Y*	—	230,000	460	1,210	3.0	1,050
	2,000-1	115	230,000	230,000	230,000	460	1,210	3.0	1,050
287.5	2,500/1,444-1	66.4/115	166,000/257,500 *Y*	—	287,500	575	1,500	3.0	1,300
	2,500-1	115	287,500	287,500	287,500	575	1,500	3.0	1,300
345	3,000/1,733-1	66.4/115	199,200/345,000 *Y*	—	345,000	690	1,785	3.0	1,550
	3,000-1	115	345,000	345,000	335,000	690	1,785	3.0	1,550

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[a] Intermediate voltage ratings are placed in the next higher insulation class unless otherwise specified.

[b] Two types of potential transformers are available in insulation classes 25 kv and above. One type is for primary line-to-neutral connection and is provided with a tapped secondary winding, or with two secondary windings, to make available nominal 115 or 120 secondary volts when connected Y or Δ. These transformers are designated in Col. 2 by the double ratio, for example 200/115:1. The other type is for primary line-to-line connection to make available 115 or 120 secondary volts from each transformer. These transformers are designated in Col. 2 by the single ratio, for example 200:1.

[c] When transformer apparatus is used on a Δ-connected system which operates with one phase grounded, special consideration should be given to the selection of the insulation class.

[d] Y-connected transformers for operation with neutral solidly grounded, or grounded through an impedance, may have reduced insulation at the neutral as specified in 2.031.

[e] All potential transformers for Y connection on a three-phase three-wire ungrounded or impedance-grounded system (or on a four-wire system where the ground may be disconnected during a line-to-ground fault) should be designed to have magnetic induction low enough to enable them to operate continuously at line-to-line voltage.

The secondary windings of instrument transformers shall withstand a low-frequency test of 2,500 volts to ground and normally grounded core and other metal parts but no impulse tests are required. See notes (*g*) and (*i*) of Table II.

formers of the same ratio having the higher test which may be connected in wye at their rated excitation voltage.

In the proposed initial draft of American Standard *C*-57, the ratios of potential transformers intended for line-to-neutral connection did not give 115 volts line-to-neutral in all cases when connected wye at the rated voltages indicated. This was felt to be an undesirable situation, and these ratios were adjusted slightly to correct this condition. A double-ratio rating was also given to provide for 115-volt line-to-line as well as line-to-neutral connection.

The new proposal in Table IX is given here under the same table number. The third column, "Secondary Volts," has been added to make clear which transformers have two secondary voltages available. In the fourth column the item, "Name-Plate Rating," has been added to the heading, together with the proper entries in the column, to eliminate the previous confusion which existed as to which transformers were suitable only for line-to-neutral connection. The fifth and sixth columns were added to indicate the circuit voltage for which the transformers were intended. The seventh column was added to point out the limit voltages between line terminal and ground for steady-state operation.

Standard Insulation Classes and Tests for Current Transformers (4.051)

In the initial proposed draft of *C*-57, the insulation tests for current transformers were included in the same table as tests for potential transformers. This obviously led to much confusion and misunderstanding which has been overcome by the use of a separate table for the insulation tests for current transformers. The proposal is given as Table X.

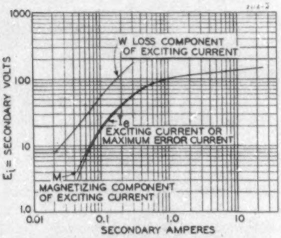


Figure 4. Typical saturation curve and loss and magnetizing component curves of a bushing-type current transformer

60 cycles
Nominal ratio 500/1
Secondary winding resistance 0.19 ohm

Bushing-Type Current Transformers

The printed draft of *C*-57 did not include any material on bushing-type current transformers. It was felt that this should be done, and wherever practicable, bushing-type current transformers have been covered, but not as completely as could be desired.

Name Plates (4.075)

A new item, "Data Sheet Number," has been added to the required information on name plates. This was done, because in many cases the name plate will not be large enough to include all the information which might be thought desirable. It is the intent that the manufacturer shall have such material available under this number and will be able to furnish it upon request by reference to the proper design number.

It is expected that the "Data Sheet Number" will serve the same general purpose as the instruction book or sheet referred to in 3.090 (16). The "Data Sheet" will be the means whereby the user may obtain such information as thermal and mechanical limits, accuracy classifications, application data, or any other information of general use in the application of instrument transformers.

Standard Insulation Classes and Dielectric Tests for Current Transformers

Table X of Proposed Standard C-57

		Dielectric Tests			
		Low Frequency	Impulse		
			Chopped Wave		Full Wave
Insulation Class (Name-Plate Rating) Col. 1[a]	Maximum Line-to-Line Voltage (Kv) Col. 2	Kv RMS Col. 3[b]	Kv Crest Col. 4[b]	Minimum Time to Flashover μsec. Col. 5[b]	Kv Crest Col. 6[b]
1.2	1.2	10	36	1.0	30
2.5	2.5	15	54	1.25	45
5.0	5.0	19	69	1.5	60
8.66	8.66	26	88	1.6	75
15*L*	15.0	34	110	1.8	95
15*H*	15	34	130	2.0	110
25	25	51	175	3.0	150
34.5	34.5	70	230	3.0	200
46	46	93	290	3.0	250
69	69	140	400	3.0	350
92	92	185	520	3.0	450
115	115	230	630	3.0	550
138	138	275	750	3.0	650
161	161	325	865	3.0	750
196	196	395	1,035	3.0	900
230	230	460	1,210	3.0	1,050
287	287	575	1,500	3.0	1,300
345	345	690	1,785	3.0	1,550

[a] Intermediate voltage ratings are to be placed in the next higher insulation class unless specified otherwise.

The secondary windings of instrument transformer shall withstand a test of 2,500 volts to ground and normally grounded core and other metal parts. No standard impulse tests have been established for secondary windings of instrument transformer.

Appendix I. Development of Expression of Limiting Phase Angle β

In Figure 2 are shown the actual and indicated currents in a current transformer on a 1–1 turn basis. The relationship among the transformer correction factor, TCF, the ratio correction factor, RCF, and the phase angle, β, is developed as follows on the assumption that there is no error in the potential circuit:

$$\text{Actual power} = EI\cos\theta$$

$$\text{Indicated power} = EI_s\cos(\theta-\beta)$$

$$\frac{\text{Actual power}}{\text{Indicated power}} = \frac{I_p}{I_s}\,\frac{\cos\theta}{\cos(\theta-\beta)}$$

But

$$\frac{\text{Actual power}}{\text{Indicated power}} = \text{TCF by definition}$$

$$\frac{I_p}{I_s} = \text{RCF by definition}$$

so that

$$\text{TCF} = \text{RCF}\,\frac{\cos\theta}{\cos(\theta-\beta)}$$

$$= \text{RCF}\,\frac{\cos\theta}{\cos\theta\cos\beta+\sin\theta\sin\beta}$$

A positive angle β indicates in this development that the secondary current is

Table A

Primary Current Rating	Preferred Ratio
(a) Single-ratio current transformers	
10	2–1
15	3–1
20	4–1
25	5–1
30	6–1
40	8–1
50	10–1
75	15–1
100	20–1
150	30–1
200	40–1
300	60–1
400	80–1
600	120–1
800	160–1
1,200	240–1
1,500	300–1
2,000	400–1
3,000	600–1
4,000	800–1
5,000	1,000–1
(b) Double-ratio current transformers	
10/20	2/4–1
25/50	5/10–1
50/100	10/20–1
75/150	15/30–1
100/200	20/40–1
150/300	30/60–1
200/400	40/80–1
300/600	60/120–1
400/800	80/160–1
600/1,200	120/240–1
(c) Multiratio transformers (bushing type)	
600*	120/100/90/80/60–1 50/40/30/20/10–1
1,200*	240/200/180/160/120–1 100/80/60/40/20–1
2,000*	400/360/320/280–1 240/160–1
3,000*	600/400/320–1
4,000*	800/600/400–1
5,000*	1,000/800/600–1

* Maximum.

ahead of the primary current, from which the following close approximation is obtained:

$$\mathrm{RCF}=\mathrm{TCF}(1+\beta\tan\theta)\quad(\beta\text{ in radians})$$

and then

$$\beta=(\mathrm{RCF}-\mathrm{TCF})\cot\theta\quad(\beta\text{ in radians})$$

The value of the phase angle β in minutes is given by

$$\beta=3{,}438\cot\theta\,(\mathrm{RCF}-\mathrm{TCF})$$

For a given power-factor angle θ, phase angle β and ratio correction factor RCF, the value of the transformer correction factor TCF at this condition will not be exceeded for angles less than this value of θ.

Since the maximum angle θ in the transformer accuracy limits is taken as $\cos^{-1}0.6$, then these TCF limits will not be exceeded for any value of θ from $\cos^{-1}0.6$ to $\cos^{-1}1.0$.

With $\cos^{-1}0.6$

$$\beta=2{,}600\,(\mathrm{RCF}-\mathrm{TCF})\text{ very nearly}$$

in which both the maximum and minimum values of β, RCF, and TCF are taken for the purpose of specifying their limits.

Table B

Designation of Burden*	Burden Characteristics		Secondary Burden at 60 Cycles and Five Amperes Secondary Current		
	Resistance (Ohms)	Inductance (Millihenrys)	Impedance (Ohms)	Volt-Amperes*	Power Factor
B-0.1 (*X*)	0.09	0.116	0.1	2.5	0.9
B-0.2	0.18	0.232	0.2	5.0	0.9
B-0.5 (*Y*)	0.45	0.580	0.5	12.5	0.9
B-1	0.5	2.3	1.0	25	0.5
B-2 (*Z*)	1.0	4.6	2.0	50	0.5
B-4	2.0	9.2	4.0	100	0.5
B-8	4.0	18.4	8.0	200	0.5

* In accordance with 1.130 the burden may also be designated by means of the volt-ampere characteristic: that is, va 2.5, or va 50.

Table C

Accuracy Class	Limits of Transformer Correction Factor				Limits of Power Factor (Lagging) of Metered Power Load
	100 Per Cent Rated Current		10 Per Cent Rated Current		
	Minimum	Maximum	Minimum	Maximum	
2.4	0.976	1.024	0.952	1.048	0.6–1.0
1.2	0.988	1.012	0.976	1.024	0.6–1.0
6/10	0.994	1.006	0.988	1.012	0.6–1.0
3/10	0.997	1.003	0.994	1.006	0.6–1.0
5/10	0.995**	1.005**	0.995	1.005	0.6–1.0

** These values also apply to 150 per cent of rated current.

Appendix 2. Calculation of Current-Transformer Ratio-Correction Factor and Phase Angle

Method 1

Starting with a given value of secondary current I_s, determine the internal voltage E_t necessary to force the secondary current I_s through the secondary resistance R_s in series with the external burden Z_b, and the angle ϕ_s by which I_s lags E_t. Express the secondary current vectorially with respect to E_t as a reference.

$$I_s=A-jB$$

From the curve such as Figure 4, determine the exciting current as a vector with respect to same reference voltage E_t.

$$I_e=W-jM$$

Determine the primary current, on the same turn base, by adding vectorially the secondary and exciting currents.

$$I_p=\sqrt{(A+W)^2+(B+M)^2}$$

$$\mathrm{RCF}=I_p/I_s$$

Phase angle β is the angle by which I^s leads I_p

$$\beta=\left(\tan^{-1}\frac{B+M}{A+W}\right)-\phi_s,\text{ in degrees}$$

Table D

Accuracy Class	Limits of Transformer Correction Factor	Limits of Power Factor (Lagging) of Metered Power Load
1.2	1.012–0.988	0.6–1.0
6/10	1.006–0.994	0.6–1.0
3/10	1.003–0.997	0.6–1.0

Table E

Maximum Per Cent Ratio Error	Accuracy Class	Maximum Per Cent Ratio Error	Accuracy Class	Maximum Secondary Terminal Volts*	Maximum Secondary Burden Ohms**
10	10 *H*50	2.5	2.5 *H*50	50	2
10	10 *H*100	2.5	2.5 *H*100	100	4
10	10 *H*200	2.5	2.5 *H*200	200	8
10	10 *H*400	2.5	2.5 *H*400	400	16
10	10 *H*800	2.5	2.5 *H*800	800	32

* Secondary voltage and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary terminal voltage at current values from 5 to 20 times rated secondary current.

** Secondary burden and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary burden ohms at current values from one to five times rated secondary current.

Method 2

Express the secondary current as

$$I_s=I_s\epsilon^{j\phi_s}$$

and the excitation current as

$$I_e=I_e\epsilon^{j\phi_e}$$

From these the RCF is

$$\mathrm{RCF}=\sqrt{1+2\frac{I_e}{I_s}\cos(\phi_e-\phi_s)+\left(\frac{I_e}{I_s}\right)^2}$$

And β, the angle by which I_s leads I_p is, providing ϕ_s and ϕ_e are looked upon as being positive angles

$$\tan\beta=\frac{I_e\sin(\phi_e-\phi_s)}{I_s+I_e\cos(\phi_e-\phi_s)}$$

RCF and β may be determined directly from the chart given in Figure 5.

Method 3

Let I_s, I_e, ϕ_s, and ϕ_e be the same as in method 2 and let

$$a=\frac{I_e}{I_s}\text{ and }p=a\cos(\phi_s-\phi_e)+\frac{a^2}{2}$$

Then the ratio correction factor

$$\mathrm{RCF}=1+p-\frac{p^2}{2}+\frac{p^3}{2}-+\text{ etc.}$$

And the phase angle β, by which I_s leads I_p is, providing ϕ_s and ϕ_e are looked upon as positive angles

$$\tan\beta=\frac{a\sin(\phi_e-\phi_s)}{1+a\cos(\phi_e-\phi_s)}$$

Appendix 3. Excerpts From Proposed American Standard for Transformers C-57 (Currently Proposed Revision of Section 4)

4.021 PREFERRED PRIMARY CURRENT RATINGS AND RATIOS OF CURRENT TRANSFORMERS

The preferred primary current ratings and ratios of current transformers shall be as shown in Table A.

Standard Burdens and Standard Accuracy Classes for Current Transformers

4.030 STANDARD BURDENS FOR RATING PURPOSES

(*a*). Standard burdens for rating purposes shall have constant-impedance ohms and linear-reactance characteristics over the entire current range on which they are used and shall have resistance and inductance values, together with impedance and volt-ampere values, at 60 cycles as shown in Table B.

(*b*). Burden *B*-0.1, *B*-0.5, and *B*-2 are standard burdens for current transformers used for metering service and correspond to the *X*, *Y*, and *Z* designation previously used. Burdens *B*-0.2 and *B*-0.5 are new standard burdens for metering service.

Burdens *B*-5, *B*-1, *B*-2, *B*-4, and *B*-8 are standard burdens for current transformer used for relaying service.

4.031 STANDARD ACCURACY CLASSES OF CURRENT TRANSFORMERS FOR METERING SERVICE

(*a*). The accuracy classification of current tranformers for metering service shall be based on the requirement that the transformer correction factor (TCF) shall be within specified limits over a specified range of power factor of the load being metered.

For the purpose of these standards the term "Transformer Correction Factor" (TCF) is used instead of the more complete term "Instrument Transformer Correction Factor" defined in 1.134 to designate the factor by which the watt-hour meter registration or the wattmeter indication must be multiplied to correct for the error introduced by the transformer through the combined effect of the ratio correction factor (RCF) and phase angle *B*.

(*b*). For purposes of standardization the limits of transformer correction factor shall be specified at 100 per cent of rated primary current and at 10 per cent of rated primary current.

(*c*). For the purposes of accuracy classification, the rated secondary current shall be five amperes.

(*d*). The standard accuracy classes and corresponding limits of transformer correction factor (TCF) shall be as shown in Table C.

(*e*). For any known ratio correction factor (RCF) of a given transformer, the positive and negative limiting values of the phase angle β in minutes may be expressed as follows:

$$\beta = 2{,}600(\text{RCF} - \text{TCF})$$

in which TCF is taken as the minimum and maximum transformer correction factor specified in (*d*) above.

Table F

Maximum Per Cent Ratio Error	Accuracy Class	Maximum Exciting Current	Maximum Per Cent Ratio Error	Accuracy Class	Maximum Exciting Current	Maximum Secondary Terminal Volts*	Maximum Secondary Burden**
10	10*L*50	10	2.5	2.5*L*50	2.5	50	*B*-0.5
10	10*L*100	10	2.5	2.5*L*100	2.5	100	*B*-1
10	10*L*200	10	2.5	2.5*L*200	2.5	200	*B*-2
10	10*L*400	10	2.5	2.5*L*400	2.5	400	*B*-4
10	10*L*800	10	2.5	2.5*L*800	2.5	800	*B*-8

* Secondary voltage and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary terminal voltage at 20 times rated secondary current.

** Secondary burden and current limitation—in any accuracy class the specified per cent ratio error shall not be exceeded for the specified secondary burden ohms at current values from 1 to 20 times rated secondary current.

The limiting values of the ratio correction factor are the same as the limits of transformer correction factor given in (*d*) above since the phase angle of the current transformer does not introduce a significant error when it is small and when the load power factor is 1.0. These limits of ratio correction factor, together with the corresponding limits of phase angle, keep the transformer correction factor within the specified limits for all values of power factor (lagging) of the measured power load between 0.60 and 1.00.

(*f*). A current transformer shall be given an accuracy rating in accordance with the accuracy class (or classes) in which it falls for the specified standard burden (or burdens). Double-ratio transformers shall be given accuracy ratings for each ratio.

(*g*). In practice, current transformers shall be designated in reference to accuracy rating by the accuracy class number followed by the burden number. For example 6/10 *B*-2 and/or 5/10 *B*-0.1 and so forth.

4.037 STANDARD ACCURACY CLASSES FOR POTENTIAL TRANSFORMERS FOR METERING SERVICE

(*a*). The accuracy classification of potential transformers for metering service shall be based on the requirement that the transformer correction factor (TCF) shall be within specified limits over a specified range of power factor of the load being metered [see paragraph 4.031 (*d*)].

(*b*). Standard output and accuracy ratings of potential transformers shall be on the basis of their standard rated secondary voltage.

(*c*). The standard accuracy classes and corresponding limits of transformer correction factor for potential transformers shall be as shown in Table D.

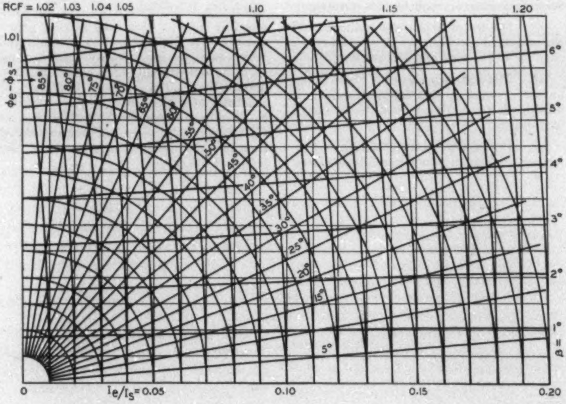
(*d*). For any known ratio correction factor (RCF) of a given transformer, the positive and negative limiting values of the phase angle "β" in minutes may be expressed as follows:

$$\gamma = 2{,}600(\text{TCF} - \text{RCF})$$

in which TCF is taken as the maximum and minimum transformer correction factor specified in (*c*).

The limiting values of the ratio correction factor are the same as the limits of the transformer correction factor, since the phase angle of the potential transformer does not introduce a significant error when it is small and when the load power factor is 1.0. These limits of ratio correction factor, to-

Figure 5. Chart for calculating RCF and β



gether with the corresponding limits of phase angle, keep the transformer correction factor within the specified limits for all values of power factor (lagging) of the measured power load between 0.6 and 1.0.

(*e*). These limits shall apply from 10 per cent above rated voltage to 10 per cent below rated voltage, at rated frequency, and from no burden on the potential transformer to the specified burden (see paragraph 4.002).

(*f*). In practice, a potential transformer shall be designated as 6/10 W if its ratio and phase-angle characteristics conform with the requirements of accuracy class 6/10 when used with any secondary burden not exceeding W with the power factor specified for W burden.

(*g*). In plotting curves of potential transformer accuracy, power factors shall be chosen to conform with the power factors of the standard burdens.

4.032 Standard Accuracy Classes for Current Transformers for Relaying Service

(*a*). 1. The overcurrent performance of current transformers for relaying service shall be specified on the basis of a standard per cent ratio error at 20 times rated secondary current, the standard relaying burdens, and the secondary terminal voltages established by the specified secondary current (20 times rated) operating into these burdens.

In these standards the term "per cent ratio error" is defined as 100(RCF−1), RCF = ratio correction factor.

2. A current transformer shall be given an accuracy rating in accordance with the maximum secondary terminal voltage at which the specified error will not be exceeded on the basis of 20 times normal secondary current operating into a standard relaying burden.

3. Accuracy classification for multiratio current transformers shall be given for the maximum secondary winding, and for double-ratio transformers shall be given for each ratio.

(*b*). Unless otherwise specified the rated secondary current shall be five amperes.

(*c*). For the purposes of standardization the standard specified per cent ratio errors shall be 10 per cent and $2^{1}/_{2}$ per cent.

(*d*). The accuracy classes of those transformers having a high internal secondary impedance (wound-type) shall be as shown in Table E.

(*e*). The accuracy classes of those transformers having a low internal secondary impedance (bushing-type) shall be as shown in Table F.

(*f*). The induced secondary voltage and corresponding value of exciting current form a good criterion of transformer performance and burden capacity. The induced secondary voltage is the vector sum of the secondary terminal voltage and the voltage drop due to the internal impedance of the secondary winding. The internal impedance may be negligible in certain designs, such as the bushing type, and quite large in others, such as the wound type.

Transformers having a relatively high internal impedance can maintain constant secondary terminal voltage under load for a range of 5 to 20 times rated secondary current, due to the fact that the internal voltage drop decreases more rapidly with reduction in secondary load current than the induced secondary voltage decreases with lower values of exciting current.

Transformers having relatively low internal impedance do not as a rule have this compensating internal voltage drop and can be standardized at one point only.

Recognition of this fact has resulted in the establishment of separate accuracy specifications for the two forms of current transformers.

(*g*). In practice, current transformers shall be designated by the accuracy class number.

The first number indicates the per cent ratio error. The letter H or L indicates which type of accuracy is specified. The second number gives the maximum secondary voltage at which the specified accuracy is obtained, with a secondary current of 20 times rated.

For example, a transformer of the $10H400$ accuracies class has high internal impedance and the ratio error is not more than ten per cent at a secondary terminal voltage not exceeding 400 volts for secondary current values from 25 amperes to 100 amperes, or from 5 times to 20 times rated current.

4.046 Mechanical Limit for Current Transformers

(*a*). The mechanical limit of a current transformer is defined by specifying the maximum rms symmetrical primary current which the transformer is capable of withstanding with its secondary winding short-circuited for six cycles (0.1 second) and which may be in successive applications with no intentional time delay between applications. Transformers shall not be required to withstand a test for the mechanical limit exceeding one-half the thermal limit at six cycles (0.1 second) as calculated from the thermal limit at one second [see section 4.045 (*d*)].

(*b*). The transformer shall be capable of withstanding the mechanical forces produced by this value of current when the current wave is initially completely offset.

4.047 Open Secondary Circuit of Current Transformers

Current transformers conforming to these standards shall be capable of carrying rated primary current continuously with the secondary circuit open, without damage to the transformers, except when the open-circuit voltage exceeds 3,500 volts crest, which usually will occur when the ratio exceeds 300–1. When the secondary peak voltage exceeds 3,500 volts, the secondary of such a transformer shall be insulated so that it can operate continuously with current in the primary to produce 3,500 volts peak across the secondary terminals. (However, the operation of current transformers under such conditions should be guarded against to prevent excessive voltage in the secondary winding.)

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Motor Insulation, Heat, and Moisture

P. H. McAULEY
ASSOCIATE AIEE

Insulation Tests

THE subject of nondestructive tests on insulation has received much attention. Since a function of insulation in a given application is to provide a certain minimum dielectric strength, tests at their greatest usefulness would give a measure of dielectric strength without requiring a dielectric breakdown. Additional indications like proneness to loss of dielectric strength if subjected to certain hazards of service are valuable, and information on the deterioration or aging of insulation with service may follow from periodic tests.

Current Practices

Although an ideal test has yet to be devised, the insulation resistance and power-factor methods are widely used for nondestructive tests on insulation. Insulation-resistance measurements are recognized in many test codes and standards.[1,2] A formula is given for the insulation resistance to be expected in a clean dry machine at 75 degrees centigrade. In some cases the same formula is given for a minimum safe value. In 1934 Wieseman[3] pointed out inadequacies of this formula and suggested improvements, and his work seems to deserve more recognition. Dielectric absorption has appreciable influence upon indicated values of insulation resistance but is not mentioned in recent standards like the Test Code for Single-Phase Motors, November 1941. This absorption effect is appreciable and may be taken into account by establishing a definite time of voltage application for making observations. Actually, many of the deductions made by an experienced Megger operator are hard to formulate or standardize.

Capacitance and power-factor measurements have proved valuable for field testing of high-voltage insulation as in bushings or transformers. These measurements are largely detectors of moisture in sealed or oil-insulated types of apparatus.[4,5] For rotating apparatus perfect sealing is difficult, and a varnish film protects the insulation. Appreciable moisture can be tolerated, at least to the extent that power-factor measurements seem less significant.

An extensive and prolonged study of nondestructive tests leading to many improvements in technique has been conducted on very large machines by an Edison Electric Institute committee.[6] Field tests provide convincing evidence of any conclusions or limits established. Laboratory investigations however permit accelerated tests with one variable under study at a time, and dielectric strength tests leading to correlation with the nondestructive indicators are possible. In the belief that a study of testing methods under extreme conditions might prove helpful in interpreting field data and permit suggesting limits based on dielectric strength, some motors of the five-horsepower 440-volt class or smaller have been subjected to severe temperature or moisture conditions.

Laboratory Tests

Two similar five-horsepower three-phase 220–440-volt induction motors have been operated more than two years on an overload cycle. Each working day one motor has been brought to a temperature of 125 degrees centigrade by winding resistance, the other to 100 degrees centigrade. Insulation resistances have been observed before starting and immediately after shutdown each day. The motors have been started across the 230-volt line with 15-kw d-c generators connected. Operation thus has been similar to actual service conditions with one unit delivering about 7.2 horsepower and the other 6.4 horsepower.

Several fractional-horsepower motors have been subjected to high humidity conditions. After several trials with various cooling cycles by refrigeration, the simple arrangement of exposing the samples at room temperature in a closed metal chamber with water in the bottom (appendix I) was adopted. Insulation resistances, dissipation factors[*] and capacitances on samples so conditioned have been recorded periodically. Dielectric breakdown tests have been made on a number of the samples.

Motors are not suitable samples on which to secure extensive data on dielectric breakdown voltages. Accordingly, small wax-dipped paper-insulated capacitors, which could be conditioned readily, have been used to observe the relation between dielectric strength, insulation resistance, and dissipation factor under exposure to high humidity.

Insulation-Resistance Measurements

Careful technique and analysis show promise of increasing the significance of insulation-resistance measurements. The following considerations should be given attention:

(*a*). The influence of external leads should be eliminated.

(*b*). The effect of time of voltage application should be taken into account by making observations at a definite time such as one minute after test voltage is applied, or by exploring the dielectric-absorption curve.

(*c*). A definite trend in insulation resistance for weeks or months may be of significance where individual values are not.

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P. H. McAuley is with the engineering laboratories and standards department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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[*] Dissipation factor is the cotangent of the angle of which power factor is the cosine. It is often multiplied by 100 and given in per cent.

(*d*). Measurements at room temperature are attractive for practical reasons.

In both the high-temperature and the moisture tests on motors, leads influenced resistance readings. About ten feet of rubber-insulated cable lying on a wooden floor was connected to each of the five-horsepower motors. The cables were isolated and checked for insulation resistance at the start of the test which happened to be in the winter time. Later it was found, under summer humidity conditions, that the cable resistances were less than those of the motors. Similarly, on the small motors subjected to high humidity, leakage between the leads and frame in the terminal boxes determined observed insulation-resistance values at times. Hence, an essential precaution to obtaining significant values on motor insulation is to make sure that leads[7] are not influencing the results.

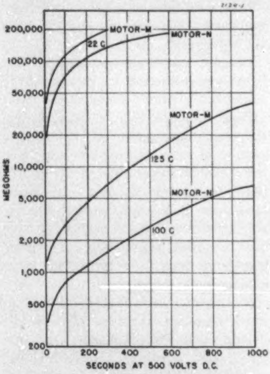

Figure 1. Insulation resistances hot and at room temperature on two five-horsepower motors on daily load cycle

The effect of time of voltage application is shown in Figure 1 which gives insulation resistance values before and after one of the daily runs for each of the five-horsepower motors. For the curves while hot, the machines were cooling rapidly from the high temperatures of operation. Thus, cooling as well as dielectric absorption contributed to the increase of apparent insulation resistance with time. The 125 degrees centigrade motor, for instance, in 15 minutes has reached a resistance higher than the five-second value at room temperature. Times longer than 15 minutes appear necessary to arrive at steady-state conditions. These are not practical for small apparatus, but the rapid changes with time indicate the desirability of selecting a definite time arbitrarily for comparative values. For data in this paper insulation resistance values are for one minute after application of 500 volts d-c. Bridges with electronic balance indicators were used.

An enclosed d-c motor operated daily at 165 degrees centigrade has shown a reversal of the trend of change in insulation resistance. With the test started in August, the hot insulation resistance of the armature increased gradually from 2.2 to 1,000 megohms with drying and curing of the insulation. However, after several months operation enclosed and overloaded, the effect of brush dust reversed this trend. It was even more apparent in room-temperature-resistance values. In one case, these dropped from 7,500 megohms to 50 megohms over a period of six months. Simply cleaning the armature with compressed air raised the resistance to 30,000 megohms.

Electrical standards usually suggest a temperature of 75 degrees centigrade for insulation-resistance measurements. This temperature tends to minimize the effects of moisture, but a machine may require many hours or days at 75 degrees centigrade to reach a steady state of insulation resistance. On large generators the pertinent question often is whether continued operation is safe. On small machines the information may be required to determine the advisability of applying voltage and starting operation without drying. Therefore, the usefulness of tests would be enhanced by making measurements and establishing limits for a temperature in the ambient range.

Aging of Insulation

The temperature aging of insulation is difficult to detect from insulation-resistance measurements. It does not seem possible to draw conclusions on the relative insulation condition of the motors operated at 125 degrees centigrade and 100 degrees centigrade, although further experience may disclose significant relations. A physical examination shows that the 125 degrees centigrade insulation is somewhat brittle. Probably, it would be much more vulnerable to long or severe exposure to vibration or moisture. But where the operating schedule is regular, standard temperature limits appear conservative for many conditions of operation. From these tests the following observations were made, applying of course to thoroughly baked insulation:

Table I. Insulation Resistances in Megohms on Two Five-Horsepower Motors Following Daily Load Runs

Motor	Mon	Tues	Wed	Thurs	Fri
125 C	1,100	750	1,300	2,000	1,500
100 C	700	650	700	800	750

(*a*). At room temperature the induction motors clean and dry had insulation resistances of from 100 to 100,000 megohms. In summer the values were below 1,000 megohms. In winter in a heated room 10,000 to over 100,000 megohms were obtained.

(*b*). A heated motor showed insulation resistance upon cooling of 10,000 to 1,000,000 megohms or an increase of 10 to 100 times.

(*c*). The 125 degrees centigrade motor had hot resistances of 1,200 to 2,500 megohms after 18 months operation, while the 100 degrees centigrade motor varied between 500 and 1,200 megohms. These values appear to have increased with aging of the insulation, but this point is submerged by the effects of the leads in the early part of the tests. The daily changes with temperature correspond fairly well with temperature correction curves[8] given by Rylander.

(*d*). One feature in comparing results on the two motors is the relative consistency of the resistance values. The day-to-day variations seem closely related.

(Table I shows values for one week. When one motor changed up or down, the other tended to do likewise. Many exceptions occurred, but there seemed to be a definite consistent relation, which suggests the influence of external conditions affecting both motors alike. Possibly it was simply temperature variations resulting from changes in line voltage or ambient temperature. The evidence is good that certain influences were functioning consistently and, that wide variations in insulation resistance may appear more logical with a better understanding of all the variables involved.)

(*e*). During weekends or occasional longer periods of shutdown, insulation resistance values tended to fall. This suggests that the room temperature reading at any given time is influenced by the integrated effect of moisture absorption since the previous heating period. It follows that a single humidity reading at the time of a test is not likely to be significant.

Moisture

Insulation is affected by exposure to moisture in spite of varnish treatments and usual methods of enclosure. These

Table A

Operating Voltage	Minimum Megohms 20 C–30 C	AIEE Formula Megohms 75 C
110	0.1	0.11
220	0.5	0.22
440	3	0.44
550	5	0.55

factors influence the rate of moisture absorption and drying. On motors standing idle, the absorption is cumulative and appears to be readily reversible.

Four $^1/_4$-horsepower three-phase 440-volt enclosed motors were used as test samples. Measurements were made under the following conditions:

(*a*). As received from the factory.

(*b*). After five hours at 48 degrees centigrade.

Table II. Insulation Resistance in Megohms for Four Small 440-Volt Motors at Controlled Temperatures

1 Motor	2 28 C	3 48 C 5 Hrs	4 75 C 20 Hrs	5 95 C 19 Hrs	6 22 C 23 Hrs	7 25 C (18 Days) 5 C (15 Hrs) 25 C (7 Hrs)
A	460	125	430	415	22,000	27
B	475	119	400	445	65,000	16
C	455	99	340	360	42,000	25
D	495	106	310	340	38,000	31

(*c*). After 25 hours at 74.5 degrees centigrade.

(*d*). After 44 hours at 95 degrees centigrade.

(*e*). During 18 days standing at room temperature.

(*f*). After 15 hours at five degrees centigrade and seven hours at room temperature.

These test conditions were consecutive and represent a drying cycle, an idle period, and a cooling cycle conducive to condensation. Figure 2 gives capacitances and dissipation factors observed with a 115-volt 60-cycle bridge for conditions (*a*) to (*e*) inclusive on one motor, and the others were quite consistent. The record of moisture absorption is definite. The 30 per cent dissipation factor at the start and the decrease in capacitance and dissipation factor with drying are characteristic of moisture. The later increase in dissipation factor and capacitance when exposed to prevailing summer humidities indicates a slow but definite reabsorption of moisture. The rate varies appreciably and the trend reverses at times. In 18 days the original condition was not yet attained again.

Table II gives insulation-resistance data on the four motors for conditions (*a*) to (*f*) except that only one set of values was recorded during the 18-day idle period. The consistency in performance of the four motors is noteworthy. In all cases values on the different motors are of the same order of magnitude under given conditions. Columns 2, 6, and 7, however, are all for room-temperature measurements at different stages of moisture absorption. Drying increased insulation resistances about 100 times. A cooling cycle conducive to condensation later caused a 1,000 to 1 decrease. In analyzing measurements made at room temperature these variations must be recognized.

Accelerated test data for motor *A* without rotor or end bells in the 100 per cent humidity test chamber are given in Table III. After a few days dissipation factor and capacitance increased to the point where readings could not be obtained. The insulation resistance decreased steadily with time of exposure to well below one megohm, and the process seems to be an extension of the rate and the degree observed at ambient humidity.

A number of used motors which had been in storage for years were tested. The dissipation factors were between 30 and 45 per cent. Motor insulation appears to absorb moisture to this extent under normal conditions of storage in summer, and this effect has been observed on idle generators.[6] New and used motors do not appear greatly different unless in the time element involved following a drying treatment.

Further moisture-exposure tests were made on the four motors after redrying. Figure 3 shows the insulation-resistance data.

Curve *A*—Stator only as in Table III.

Cuve *B*—Retreated stator with a different varnish.

Curve *C*—Same as *A*, but enclosed and with synthetic tubing leads to eliminate lead leakage.

Curve *D*—Retreated stator with a different varnish immediately after drying.

The motors were exposed at high humidity for 71 days, and insulation resistances decreased steadily and consistently. Dissipation factors also indicated similar absorption with time, but readings were less consistent in the later stages of the test.

Curve *A*, Figure 3, duplicates the similar test of Table III rather well. By comparison, *C* shows that enclosure retarded absorption materially. Better varnish treatment in cases *B* and *D* did likewise and this result agrees with statistical service data. Improved results have been obtained under some service conditions with additional and special varnishes. These protective measures influence the insulation state reached in a given time.

For part of the test, a voltage of 115 and later 230 volts, 60-cycle, was applied between the windings and iron of stator *A*, Figure 3. When this voltage was removed, the insulation resistance dropped from a 0.4-megohm level to 0.08 megohm. No improvement occurred on application of the voltage, but further depreciation was prevented. The evidence is meager, but further study of

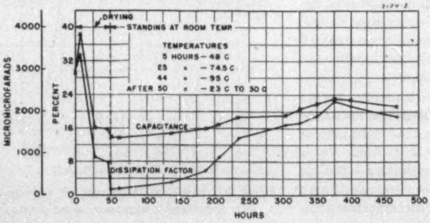

Figure 2. Effect of drying and exposure at summer humidity conditions on capacitance and dissipation factor of small motor insulation

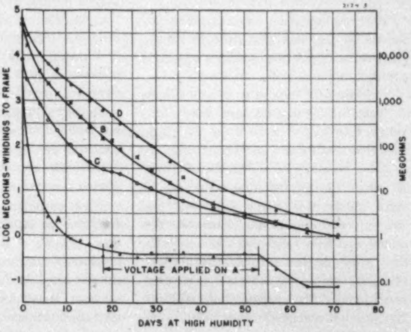

Figure 3 (right). Insulation resistances of small motors in humidity-conditioning chamber

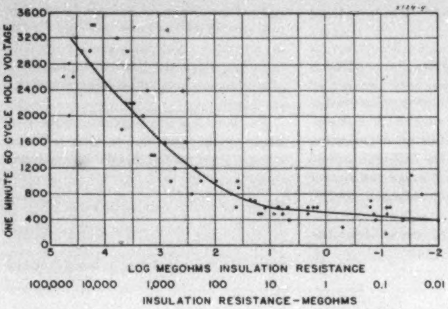

Figure 4. Relation between dielectric strength and insulation resistance on capacitors in moisture-exposure tests

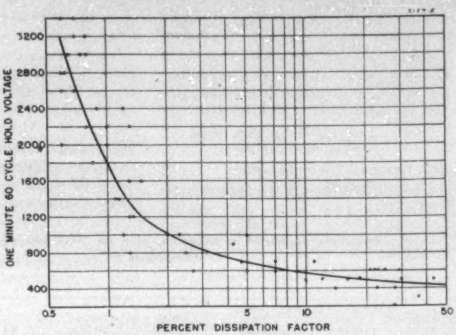

Figure 5. Relation between dielectric strength and dissipation factor on capacitors in moisture-exposure tests

the value of keeping some voltage on insulation to ground under adverse moisture conditions seems warranted. Unfortunately, this measure is not readily applicable to intercoil insulation.

The process of moisture absorption appears to be reversible. If the insulation is dried or even stored in a heated room, recovery is appreciable and rather rapid. The motors were dried between the tests of Table III and Figure 3 apparently without affecting later performance. Table IV gives readings on the stators three days after completion of the tests of Figure 3. Insulation resistances and dissipation factors, in spite of dielectric breakdown tests in the meantime, indicate appreciable drying has occurred at room temperature. The rate depends again upon the varnish treatment. The problem resolves into avoiding the application of voltage when the insulation is in a weakened condition.

Dielectric Strength

The real criterion of suitability for service is the dielectric strength of the insulation. Moisture reduces dielectric strength appreciably.

Following the exposure tests of Figure 3, dielectric breakdown tests were made on the four motors as in Table IV. The dielectric test for new motors of this class is twice normal plus 1,000 volts for one minute. Two of the stators, *A* and *C*, were below this value following the moisture exposure tests. However, the poorer one held 1,000 volts for one minute, which leaves a little margin over operating voltage in spite of severe test conditions. The motors operated at 100 degrees centigrade and 125 degrees centigrade for two years stood a 2,000-volt momentary test after each year of operation.

Maximum 60-cycle test voltages held for one minute between motor windings and frame for different values of insulation resistance are given in Figure 7. The points at or above 4,000 volts are for new stators, and other points are following different times of exposure to moisture. The curve is drawn near the lower limit of the points. Since the points are the highest voltages held for one minute, and failures occurred at the next highest 200-volt step, the curve is of the nature of a minimum breakdown voltage curve, although a larger number of tests probably would modify it some. There is a fairly definite relation between insulation resistance and dielectric strength for these fractional-horsepower motors.

The capacitors used for dielectric breakdown tests have three layers of about 0.0005-inch paper insulation and are rated at 220 volts alternating current. Operating volts per mil are much higher than in motors, and very little moisture can be tolerated. Samples were exposed at high humidity, and a few were removed periodically and tested for insulation resistance, dissipation factor, and dielectric breakdown strength, as shown in Figures 4 and 5. There seems to be reasonably good correlation between the electrical test values and dielectric strength. For insulation resistances below 100 megohms or dissipation factors over two per cent, the dielectric strength is reduced appreciably to rapidly approach a minimum value.

These capacitors also illustrate another phenomenon on the insulation-resistance test. Figure 6 shows the variation of insulation resistance with time for a sample after 24 days exposure. The insulation resistance first increased with time as in the usual dielectric-absorption curve. After about 20 seconds the trend reversed, and after 40 seconds insulation resistance decreased rapidly with time, possibly because of a partial breakdown at 500 volts direct current. Recovery was rapid and fairly complete, however, as the sample immediately afterwards held 600 volts 60-cycle for one minute and failed at 700 volts. The same action has been observed on motors after appreciable exposure. Davis and Leftwich[9] have reported the decreasing resistance section of the curve due to defective auxiliary apparatus on generators. These effects accent the desirability of observing insulation resistance at one minute or more after voltage is applied. A 10- or 15-second Megger reading would not have disclosed the situation. Also, in this

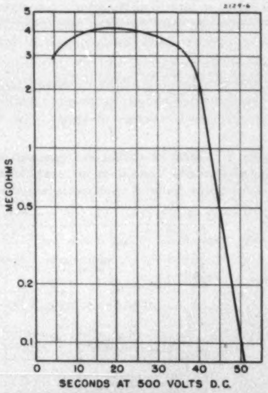

Figure 6. Insulation resistance on capacitor after considerable exposure to moisture

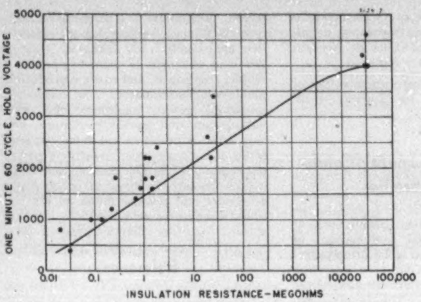

Figure 7. Relation between dielectric strength and insulation resistance for fractional-horsepower 110- to 440-volt motors

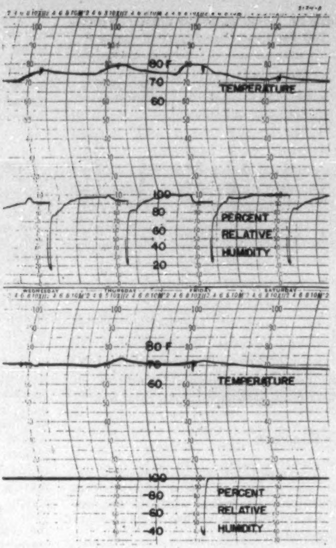

Figure 8 (right). Temperature and humidity chart from metal test chamber with water in the bottom

case at least, a weakened condition of the insulation was detected at a d-c voltage of one-half the 60-cycle crest breakdown value. These capacitor samples are small, however, and results may have been affected by heating or drying at test voltages.

Nondestructive Tests Versus Dielectric Strength

The purpose of these investigations is, of course, to determine if nondestructive insulation tests can be relied upon to indicate the suitability of apparatus for service.

1. There is some possibility of establishing for some devices minimum insulation-resistance values which will assure satisfactory operation in spite of appreciable moisture absorption.

2. The test methods studied do not appear dependable for revealing insulation defects other than moisture absorption, with the possible exception of dirt.

To establish satisfactory limits for motors considerable dielectric-strength data and field experience should be available. Table IV and Figure 7 indicate, however, that insulation strength decreases with insulation resistance. This is recognized in the standards formula for expected values at 75 degrees centigrade. However, the 75 degrees centigrade temperature implies some drying. Also, no time of voltage application is specified, which leaves to chance the inclusion of such effects as in Figure 6. Recognizing the greater usefulness of tests at room temperature, it may be reasonable to suggest from Figure 7 the following tentative limits above which small motors can be safely connected to the line for starting. The values in column 2 are for room temperature at one minute after the d-c test voltage is applied. A ratio of about 60 has been given[8] for values taken at 25 degrees centigrade and 75 degrees centigrade.

Table III. Measurements Between Windings and Frame on Stator *A* Exposed to 100 Per Cent Humidity

Days Exposed	Megohms	μμf	Per Cent Dissipation Factor
0	200,000	1,275	1.2
1	5,100	1,420	5.1
5		7,100	38
6	3.1	8,200*	> 50*
7	1.5	11,100	> 50
8	0.85	14,800	> 50
11	0.42	24,200	> 50
12	0.31	30,000	> 50
13	0.21	30,000	> 50

* These and later readings are very approximate as the bridge could not be balanced for dissipation factors greater than 50 per cent.

These limits allow some margin of safety at operating voltages, but wide variations in practice are to be expected. Appreciably lower values may not prove fatal in many cases. Also, where moisture is not involved, these limits may give no assurance of a satisfactory condition. In fact much higher values often may be associated with poor insulation. Additional experience on many sizes, types, and makes of motors under service conditions will be necessary for proper determination of the best values, but the possibility of establishing useful limits seems good.

The detection of defects under dry conditions is very uncertain for low-voltage apparatus. The roasted condition of the motor operated at 125 degrees centigrade was not apparent from the electrical tests. Columns 5 and 6 of Table IV also are significant. The motors had been tested to failure, but insulation resistances of 1,950, 2.1, and 3.2 megohms were later recorded for three of the windings. Motor *C* did show evidence of the previous breakdown by its zero insulation resistance. It is quite likely that higher insulation test voltages would reveal the condition in all cases. There is always danger, however, in testing at voltages much above operating voltage, or the nondestructive feature of the test may be sacrificed. The failure of insulation-resistance test voltages to disclose punctures or breaks in insulation is readily understandable. The smallest air gap will not sparkover much below 1,000 volts in short-time tests. The separation between conductor and core in small motors may be approximately 0.05 inch, and the dielectric breakdown of air for this distance usually exceeds 4,000 volts with creepage strengths somewhat lower. Consequently, if the puncture or fault is clean and reasonably dry, the insulation resistance may be affected to a small extent. The strength of the insulation between turns, coils, and phases in motors also is important. However, under

Table IV. Tests on Stators After 71 Days at 100 Per Cent Humidity as in Figure 4

1	2	3	4	5	6
	As Removed			After 3 Days in a Heated Room	
Motor	Megohms	Per Cent Dissipation Factor	Volts Held for 1 Minute	Megohms	Per Cent Dissipation Factor
A	0.083	> 50	1,000	1,950	17.5
B	0.88	40	1,600	2.1	31
C	1.08	> 50	2,200	0	15
D	1.95	37	2,400	3.2	34.5

service conditions, the testing of this insulation becomes rather elaborate. In a well-proportioned machine test results on the ground insulation should be indicative of general machine condition.

Conclusions

1. Test codes and standards should recognize the effect of dielectric absorption on insulation resistance and suggest a time such as one minute after voltage application for observing resistance.

2. For industrial apparatus rated below 600 volts room-temperature measurements of insulation resistance seem more practical than 75 degrees centigrade values.

3. Aging of insulation due to temperature is difficult to detect from electrical measurements. These indicators are sensitive to moisture absorption and possibly to dirt.

4. There appears to be a very good correlation between insulation resistance as affected by moisture and dielectric strength for fractional-horsepower motors.

5. Minimum room-temperature values of insulation resistance for starting small motors are suggested.

Appendix I. Humidity-Conditioning Chamber

In seeking a convenient testing cycle at high humidity, several combinations of cooling and exposure to moist atmospheres were tried. It was difficult to reproduce conditions, and considerable handling of the samples was required. The simple expedient of a closed metal can with water in the bottom finally was adopted. For most of these tests the chamber consisted of a can about 30 inches in diameter by 36 inches high with a deep lid about 34 inches in diameter. The assembly was turned upside down from the usual position, and the lid filled with water. Thus, the water provided both a seal and a source of moisture. A pinhole permitted entrapped air to escape. No temperature or humidity controls were provided other than the laboratory heating system which determined ambient conditions. Both temperature and humidity in the chamber were recorded with a Friez Hythergraph, Figure 8. These records indicate a saturated atmosphere most of the time, but this depended to a certain extent upon the condition of the samples and the frequency of opening the chamber. The time to return to maximum humidity after opening the chamber varied with ambient humidity and the duration of the open condition. Times from 0.5 to 20 hours were observed. This might be a handicap in short-time tests, but a fan should speed evaporation appreciably. For insulation tests of this kind, this type of chamber provides a simple, convenient, and easily reproducible conditioning atmosphere. The correlation to actual service conditions with wide temperature and humidity changes and moving air is problematical, but that, relatively, it is a very severe moisture-exposure test seems certain. One possible case which it does simulate closely occurs where water penetrates and remains inside an idle enclosed machine for some time.

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Ignitron Rectifiers in Industry

J. H. COX
ASSOCIATE AIEE

G. F. JONES
NONMEMBER AIEE

WHEN a new device is developed, it is usually adopted and proved by a particular industry. In the case of the ignitron rectifier, the first applications were in transportation service in mines and railways. The apparatus and its performance in these early installations were discussed by the present authors in an earlier Institute paper.[1]

Since that time, as superior operating results became known, the ignitron has been taken up by other industries, most notably the electrochemical. In excess of 2,000,000 kw of ignitron-rectifier units have been purchased by that industry alone.

This paper will discuss primarily installations for large power concentrations.

Rectifier Description

Continuously pumped ignitrons have been applied to date in four sizes which, in six tube assemblies, have continuous ratings at 600 volts of 1,250, 1,667, 2,500, and 4,000 amperes. The 2,500-ampere size has been applied in greatest numbers. However, larger-capacity sections are desired for the larger installations, and so the majority of applications have been made using 12-tube assemblies. One design will be described.

The ignitron consists of a drawn steel tank with a removable cover plate into which is mounted the main anode bushing and anode shield terminal. Graphite continues to be the best material for rectifier anodes. The deionizing shield surrounding the anode and the baffle between anode and cathode pool, are also of graphite. This latter is insulated from the cathode to avoid the transfer of the cathode spot from cathode to baffle when high overloads are applied. The tank itself constitutes the cathode connection, since the ignitron does not require an insulated cathode and is bolted to the cathode copper bars on the assembly frame. The ignitor is mounted on a rod which passes through the side of the tank, insulated with a glass-kovar seal, on a flexible steel diaphragm. The mounting is provided with adjusting screws to enable adjustment of the ignitor from outside the tank. Solder-to-porcelain vacuum-tight seals are used for the main anode bushing. A small solid radiator terminal holds this seal within safe temperatures. The low-current potential connection to the anode shield is made through an aviation spark-plug-type bushing with a soft copper gasket. The vacuum-sealing gaskets in the separable joints of the ignitron proper, are enameled aluminum wire rings. Such gaskets operate satisfactorily at 100 degrees centigrade, and the rectifier operating temperature is objectionably close to the maximum safe temperature of rubber. However, rubber constitutes a most convenient, and semiflexible vacuum-tight gasket which requires relatively low flange pressures. Therefore, the external connections to the vacuum pumping system, which operate at low temperatures, are gasketed with iron-band-protected rubber. The ignitron tanks are cooled by water passed through copper tubing, which is wrapped around the tank and coiled on the bottom, and attached to the steel with high-temperature solder. Additional cooling in high-current units is provided by an internal steel coil. This latter is copper-lined to keep all water-cooled surfaces nonferrous. Figure 2 shows a cross-section view and Figure 3 an external view of the ignitron described.

A rectifier unit consists of a transformer and the rectifier assembly or assemblies to which it is connected. In large-capacity units two or more rectifier assemblies may be connected to one transformer.

Rectifier circuits which efficiently utilize the circuit elements are arranged for three-phase operation and require multiples of three anodes. (Smaller installations are sometimes made with four anodes.) Where the power required can be supplied with six anodes, or where the small number of units suggests sectionalization to insure continuity of service, ignitrons are assembled in groups of six on one frame with one cooling system and one vacuum pumping and measuring system. However, in large stations where ample reserve is maintained with one unit out of service, simplicity dictates assembly in units of 12. Such assemblies have proved most convenient, and one vacuum system of any established type has proved adequate. The individual tanks are connected to the vacuum pumps through a vacuum manifold with water-cooled pumping connections which condense the mercury vapor and prevent transfer of mercury between tanks and pump. On these large-size tubes an individual vacuum valve on each facilitates both manufacture and repair, since vacuum tightness can be checked more easily by isolating smaller elements. Also, one ignitron tube can be treated as an entity and interchanged with a spare in less than one hour if necessary.

Figure 4 shows an assembly of 12 ignitrons rated at 5,000 amperes d-c in the 600-volt class.

All high-power rectifiers have been water-cooled. Since cooling water which is pure enough to pass through the rectifier jackets or tubes directly without trouble due to corrosion or scale is rarely available, practically all installations are provided with heat exchangers which permit the recirculation of pure or treated

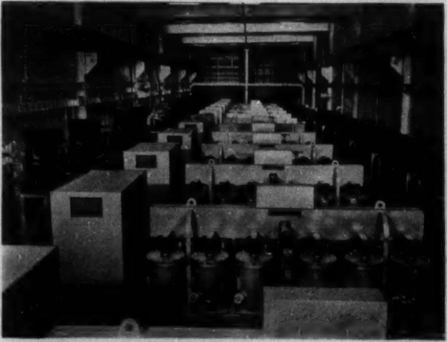

Figure 1. Typical ignitron installation in an electrochemical plant

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J. H. Cox is section engineer, mercury-arc-rectifier engineering department, Westinghouse Electric and Manufacturing Company, and G. F. Jones is also with Westinghouse Electric and Manufacturing Company, both in East Pittsburgh, Pa.

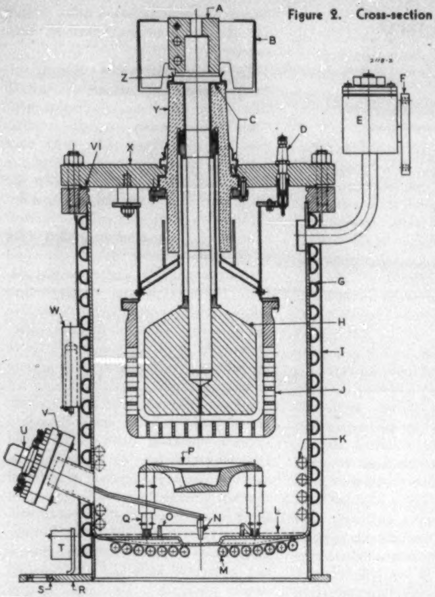


Figure 2. Cross-section view of an ignitron

A—Anode radiator and terminal
B—Radiator cover
C—Asbestos seal
D—Shield entrance bushings
E—Tank valve
F—Vacuum manifold connection
G—Tank-cooling coils
H—Anode head
I—Tank cover
J—Anode shield
K—Internal cooling coils
L—Mercury cathode
M—Cathode-cooling coils
N—Ignitor tip
O—Quartz ring
P—Baffle
Q—Quartz tube
R—Steel plate
S—Copper insert
T—Terminal block
U—Ignitor entrance assembly
V, V1—Aluminum seal rings
W—Thermal switch
X—Anode plate
Y—Anode porcelain
Z—Solder seal

water. Where ample raw water is available, water-to-water exchangers are used, otherwise water-to-air. Water-to-air exchangers are nearly always used in mining installations where cooling water is either not available or of very low quality.

Unit Excitation and Control

The earlier types of ignitron excitation which utilized thyratron tubes have mostly been superseded by circuits which generate impulse voltages by means of saturating reactors.[2] These circuits utilize static devices throughout: that is, simple transformers, reactors, capacitors, and Rectox rectifiers. The excitation-circuit devices, together with the vacuum- and cooling-system control, are mounted in a cabinet located on or near the rectifier. Two types of phase control of rectifier voltage have been used. One is a mechanical phase shifter, which consists of a wound-rotor induction machine. The excitation power is passed through this phase shifter, and the phase position of the excitation impulses is determined by the position of the phase-shifter rotor in its stator. This position is controlled by a fractional-horsepower motor through a gear box. The other type of phase control utilizes some form of phase-shifting network in which the phase position of the excitation impulses is determined by the value of direct current in a saturable reactor. This latter is a less simple circuit than the mechanical phase shifter, but is more flexible where automatic control is required, and speed of response is important. Particularly, the phase-shifting network is suitable for use with a simple type of voltage regulator such as the Silverstat. Also, such a network may be designed to include voltage-compensating characteristics which will maintain full energy excitation through supply-line voltage fluctuations as great as 50 per cent.

Station Layout

The simplest and most economical arrangement of apparatus in a rectifier substation is to effect a straight line run of power from the a-c supply source to the d-c bus. Figures 5 and 6 show a typical multiunit station layout having this arrangement. Incoming power is delivered to the a-c bus, from which it is distributed to the ignitron units through a-c switchgear, the transformers, ignitrons, and d-c switchgear to the d-c bus.

For the layout shown, the a-c switchgear is in a small masonry house separated from the main building. This can be modified to supply conventional outdoor switchgear, outdoor metal-enclosed gear, or the switchgear can be located in the substation building.

All transformer equipment is outdoor. It can be supplied with conventional bushings and an overhead bus structure or with potheads and an underground system as shown.

The substation building is designed with two levels, the ignitrons and all necessary operator control devices being located on the operating floor with some accessories and low-voltage switchgear in the basement. All of the equipment in the basement is operated electrically from the operating floor and is completely supervised by indicating devices. The operator need go to the basement only for routine inspection and maintenance. In many installations the low-voltage switchgear is also located on the operating floor.

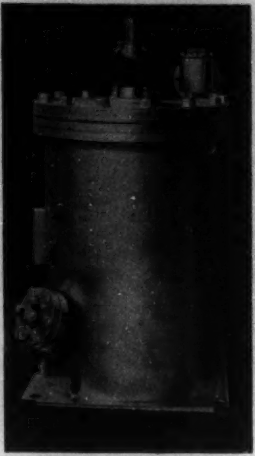
Some detail equipment which is required for station operation and which can be located as convenient is not shown. This includes the station battery with charging equipment, the degassing equipment, air-cleaning apparatus if required, storage space for spare parts, and equipment for supplying power to plant auxiliaries which are not associated with the substation equipment.

Building appurtenances such as stairways, lavatory facilities, and operators' office must be included. A repair bay should be provided and is usually at the end of the substation building. Crane facilities and sufficient head room for untanking the largest transformer will determine its construction. An enclosed clean room for working on interior parts of rectifiers is desirable.

Rectifier-Switching Arrangements

Figure 7 shows the several switching arrangements which are in use for ignitron units, rated at 8,000 to 10,000 amperes in the 600 d-c voltage class, in multiunit substations.

Circuit *A* is used when the concentration of power supply to the a-c bus is heavy, a condition which would require large rupturing capacity and costly oil circuit breakers in the rectifier-unit circuits. Circuit breakers are not used, the primary switching consisting of a disconnecting switch which is capable of opening the transformer magnetizing current. For this arrangement, primary faults must be opened by the switchgear in the supply circuit or circuits to the plant. This involves a plant shutdown until the faulty circuit is isolated by operation of the disconnecting switches.



• **Figure 3. A single ignitron**

The secondary switching consists of high-speed anode switchgear with one pole for each phase of the transformer secondary.

High-speed switching, which will limit the d-c rise in the average circuit in the order of one-third cycle, on a 60-cycle basis, is essential for large multiunit substations. At the time of arc back, all normal units will contribute direct current to the arc back in the form of reverse current. This current increases rapidly, depending upon the inductance of the circuit, and must be limited by the switchgear to a value which the switchgear can open successfully and without undue maintenance. It is also advantageous to limit the stresses on the other equipment involved in the circuit, particularly the transformer.

When an arc back occurs, the sound anodes of the ignitron assembly in which the arc back has occurred will supply current to the faulty anode. This feed to the arc back is in addition to the back feed from other ignitron units and is a short circuit, through the arcs involved, on the transformer secondary. This fault must be suppressed by opening the a-c supply to the transformer or by opening the faulty circuit between the transformer and the ignitron.

For arrangement *A*, the a-c supply to the transformer cannot be opened except by operation of the circuit breakers in the station supply. High-speed anode switching is therefore essential for this circuit.

The anode circuit breakers are supplemented by semihigh-speed cathode circuit breakers. These are used for normal switching operations. Disconnecting switches permit maintenance of the circuit breakers with a minimum of equipment out of service.

Circuit *B* is quite similar to circuit *A* except that an oil circuit breaker is used in the circuit to the rectifier transformer. Its use can be justified if the rupturing capacity required is 500,000 kva or less. The circuit breaker is then relatively inexpensive, and each ignitron circuit is completely independent of the others. Circuit faults are isolated without plant shutdown. Complete flexibility of control is afforded.

The use of high-speed anode switching makes it unnecessary to open the oil circuit breaker at the time of arc back. When two ignitron assemblies with independent low-voltage switching are connected to one transformer, the anode switching can be opened for one assembly without affecting operation of the other. This is of advantage in keeping maximum conversion equipment in service. The importance of this feature is in inverse ratio to the number of conversion units normally connected to the common d-c bus.

Circuit *C* is similar to circuit *B* except that the high-speed anode switches are omitted, and a high-speed cathode circuit breaker is substituted for the semihigh-speed switch. With this arrangement reverse current to the ignitron assembly which has arced back is opened with high-speed action by the cathode circuit breaker. Arc quenching may be used to suppress the alternating fault current by preventing the normal anodes of the assembly from firing to the faulty anode. This latter operation is effective in approximately one cycle.

Circuits *B* and *C* are therefore comparable except for two features. Arc quenching sometimes fails in its operation. It is about 90 per cent effective. When it fails in operation, the oil circuit breaker must be opened by overload relay action to remove the fault. Anode switching is fully operative, but there are six poles of switchgear to maintain, compared to a single larger pole of cathode switching. The choice between the two circuits is therefore dictated by consideration of maintenance, as compared with an occasional failure to segregate the fault in a half-rectifier unit.

If arc quenching is not used in connection with circuit *C*, the primary oil switch unit must be interlocked with the cathode switch so that the entire unit is removed from service in the event of arc back, and the a-c short circuit endures for the opening time of the oil switch, which is usually of the order of six cycles.

Circuit *C* makes use of anode disconnecting switches in the anode circuits to each ignitron section, one assembly making up one half of the unit. These are used so that each assembly can be isolated for maintenance and continue with normal operation of its associated assembly. They are also of use during the degassing process.

For those installations, where a complete unit outage is permitted because of spare capacity available, the anode disconnecting switches can be omitted. This applies equally to circuits *A*, *B*, or *C*. Circuit *D* shows a circuit similar to circuit *C* except with the anode disconnecting switches omitted.

For single rectifier units connected to an isolated d-c bus, the switching can be simplified. The unit may consist of one or more ignitron assemblies. For either condition, the amount of current in-

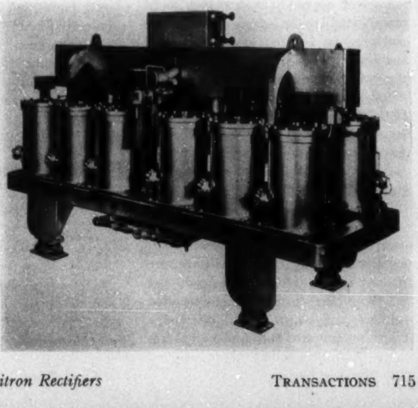
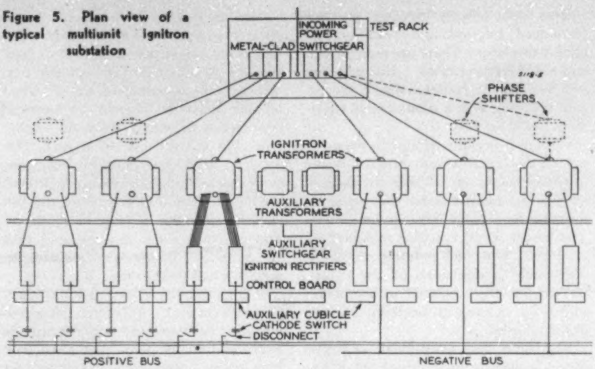


Figure 4. An assembly of 12 ignitrons rated at 5,000-amperes d-c in the 600-volt class

Figure 5. Plan view of a typical multiunit ignitron substation



volved in an arc back is relatively small and semihigh-speed switchgear, having a speed of $1^1/_2$ to 2 cycles to current limitation, is adequate for the service. This may be arranged, as in the case of high-speed switching, either with anode breakers with suitable cathode switching to provide the type of operation desired, or with a cathode breaker and an oil switch in the primary of the transformer. Reverse current for a single assembly is applied from the electrolytic cells. Where a cathode switch is used, an arc back is cleared by the opening of the cathode circuit breaker, followed by the opening of the oil circuit breaker.

For double assembly units, arc quenching can be used in combination with the semihigh-speed cathode switches to secure continuous half-unit operation. Anode disconnecting switches can be used for half-unit isolation. Circuit *E* illustrates the use of semihigh-speed cathode switching with a single rectifier unit supplying an isolated d-c bus.

A special use can be made of high-speed anode switching for a single rectifier unit supplying an isolated load where continuous service is an absolute essential. The switch can be arranged to trip individual poles and on reverse current only. At the time of arc back only the pole of the circuit involved in the fault will open, the other anodes continuing in service.

Station Voltage Control

The great majority of industrial loads requiring one- or two-unit stations, particularly including electric railways, are adapted to the normal five to eight per cent shunt characteristic of noncontrolled rectifiers, and phase control of voltage is not necessary. For applications requiring flatter voltage regulation one of the phase-control systems is used, and there is a wide choice of control method ranging from manual to fully automatic. Provision can be made for flat compounding and cross compounding where more than one unit on a bus is involved. Where small units are located at the end of a low power feeder, which frequently occurs in mines, and the a-c supply to the rectifier transformer varies widely, some form of compensator is used which by phase control reduces the voltage at the lighter loads and provides a nearly flat voltage regulation from light load to full load. Above full load the uncompensated voltage regulation prevails.

In large stations containing many units on one bus, phase control is always used, primarily to compensate for minor differences in impedances and equalize the currents in the several units, and to provide small variations in voltages as desired for the load. However, since large phase-control angles adversely affect power factor and increase harmonics, the minimum feasible transformer tap is used in order to keep the phase control angles at a minimum.

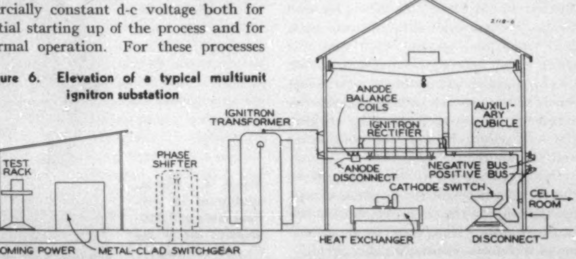
Some electrolytic processes require commercially constant d-c voltage both for initial starting up of the process and for normal operation. For these processes no-load voltage taps can be provided in the rectifier transformers to compensate for seasonal voltage changes and for electrolytic cell aging.

Ignitor or phase control of d-c voltage can be used for short times in the range required to pick up and drop load due to switching requirements. It can also be used for small voltage adjustments over long-time periods of operation.

Some processes require a wide voltage range for initial starting up in order to condition new cells in small blocks, but require essentially constant voltage for normal operation. For these processes an autotransformer of sufficient capacity to supply all rectifiers connected to one or several cell strings in parallel can be supplied. No-load taps are supplied in sufficient number and range to accomplish the desired results. Phase voltage control can be used for d-c voltage adjustment between taps. It is not however recommended for long-time wide-range voltage adjustment, because of its adverse effect on power factor and the increase in harmonics. The autotransformer can also be used for permanent adjustment of d-c voltage when required by a change in production schedule.

Some processes require that constant current be maintained through the electrolytic cells, even though the cell characteristics change frequently and rapidly. For these processes an autotransformer having a combination of no-load taps and tap changer under load can be supplied. The no-load taps cover the extreme voltage range required. The tap changer under load is arranged to boost or buck the d-c voltage from any no-load tap and in the range required. The tap points of the tap changer under load equipment can usually be made sufficiently fine so that phase control of voltage is not required between taps or, if used, will be limited to a few per cent in range. In many processes the fluctuations in voltage required to maintain constant current are so great and frequent as to require pro-

Figure 6. Elevation of a typical multiunit ignitron substation



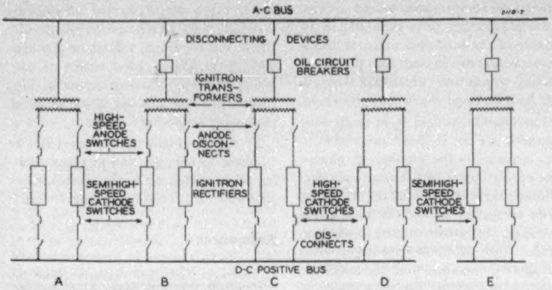

A-C BUS
2118-7
DISCONNECTING DEVICES
OIL CIRCUIT BREAKERS
IGNITRON TRANSFORMERS
HIGH-SPEED ANODE SWITCHES
ANODE DISCONNECTS
SEMIHIGH-SPEED CATHODE SWITCHES
IGNITRON RECTIFIERS
HIGH-SPEED CATHODE SWITCHES
SEMIHIGH-SPEED CATHODE SWITCHES
DISCONNECTS
D-C POSITIVE BUS
A
B
C
D
E

Figure 7. Schematic diagrams showing various arrangements of rectifier switching

hibitive frequency of tap changing or large phase-control angles. In these processes attempts to maintain constant current have been abandoned.

At some locations the demand clause in power contracts is such that high power rates must be paid over a year, because of a short-time swing over the demand ceiling. For processes where load conditions fluctuate rapidly and over considerable range, autotransformers with tap changer under load equipment can be used to secure constant kilowatt input to the cells. This will insure maximum production at the normal power rate, or at minimum power cost. Where more than one station is supplied from the same source, the averaging effect of the several stations proves most advantageous in reducing the number of tap changes required.

Regulation for either constant direct current or for constant kilowatt input can be made full automatic.

Figure 8 shows an autotransformer connected to the rectifier a-c bus, the voltage of which can be varied according to the electrolytic process requirements.

Since the loads on the large station busses constitute practically a short circuit for a single rectifier unit, it is not possible to place units on the bus individually at full voltage following a station outage. Where the power-supply system is large with respect to a single station, it has been general practice to connect all units to the power supply and the a-c bus without excitation, and then to close the excitation contactors to all units simultaneously with a master control switch. Where the supply system is not adequate for this procedure, station phase control of voltage is used. In this method of starting a station following an outage, all units are connected to the bus, either simultaneously as above or individually, but with phase-delay angles set uniformly to limit the station current to a value satisfactory to the supply system or to the rectifier unit, depending on the procedure. The phase-control angles are then advanced uniformly on all rectifier units to increase load to normal at a rate satisfactory to the supply system.

Telephone Interference

A rectifier of any type creates harmonics which are present in the a-c and d-c systems to which it is connected. Harmonics may be induced in telephone lines, which are adjacent to the power systems, under certain conditions of exposure. The telephone-interference problem becomes of increasing importance when the rectifier load is a large percentage of the system load in the exposure areas.

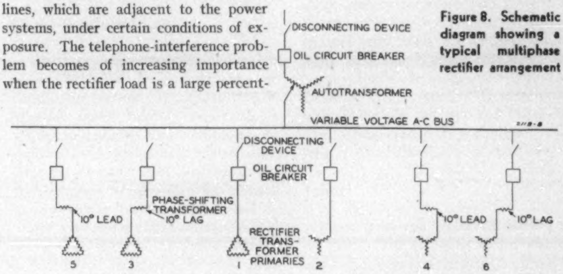
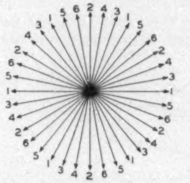

DISCONNECTING DEVICE
OIL CIRCUIT BREAKER
AUTOTRANSFORMER
VARIABLE VOLTAGE A-C BUS
2118-8
DISCONNECTING DEVICE
OIL CIRCUIT BREAKER
PHASE-SHIFTING TRANSFORMER
10° LEAD
10° LAG
RECTIFIER TRANS-FORMER PRIMARIES
5
3
1
2
4
6
10° LEAD
10° LAG

Figure 8. Schematic diagram showing a typical multiphase rectifier arrangement


3 1 5 6 2 4 3 1 5
6
2
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3
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3 1 5 6 2 4 3
5 1
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4

For electrolytic plants, the d-c load is concentrated, and exposures between the d-c circuits and telephone lines are easily controlled. When the a-c power is transmitted a considerable distance, or is supplied from a power network system, exposures are probable, and the telephone interference problem must be considered.

The number of phases at which any rectifier unit can operate cannot be greater than the number of anodes of the unit. Small units having only six anodes are therefore limited to six-phase operation, which operation is used as a base for rectifier harmonic consideration. Since the power involved in a six-anode unit is usually small, the influence on exposed telephone lines is usually too small to be important. As the number of phases in multiples of six are increased, some of the base harmonics of six-phase operation are cancelled, the cancelling increasing as the number of phases is increased. Operation of rectifier units in the higher multiphase relationships, therefore, decreases the telephone-interference problem.

A single rectifier unit of 12 anodes can be supplied from a transformer which is wound to give 12-phase operation. It is not economical to build a transformer for a higher number of phases in a single unit. For those installations which have a large number of rectifier units, multiphase operation can best be secured by operating the units out of phase with respect to each other, and in symmetrical relationship.

The combination of two six-phase rectifier units having a delta and a star-connected transformer primary respectively will result in over-all 12-phase operation, since the delta and star are inherently 30 degrees out of phase. The supply voltage to a transformer can be shifted any number of degrees desired by the use of a phase-shifting transformer.[4–6] A combination of delta and star-connected transformers together with phase-shifting transformers can be used to effect any degree of multiphase relationship, the only limit being the number of rectifier units available. For phase arrangements greater than 12, the usual inductances in the circuits are sufficient to provide interphase action.

Table I

Number of Units in Substation	Anodes Per Unit	Base Unit Phases	Maximum Over-All Phase Operation Per Station
1	6	6	6
1	12 or 24	12	12
1	18	6	6
2	6 or 12 or 24	6	12
2	18	6	12
2	12 or 24	12	24
3	6 or 12 or 24	6	18
3	12 or 24	12	36
4	12 or 24	6	24
4	12 or 24	12	48
5	12 or 24	6	30
5	12 or 24	12	60
6	12 or 24	6	36
7	12 or 24	6	42
8	12 or 24	6	48

Figure 8 shows the circuits to the primaries of six rectifier units to secure 36-phase over-all plant operation.

To secure maximum benefit from multiphase operation, the units should be of equal rating, and their loads should be balanced. The impedance of the phase-shifting transformer will result in some unbalance in load between duplicate phase-shifted and unshifted rectifier units, unless some compensation is used. Compensating reactors can be connected in the circuits of the unshifted units, or transformer action can be built into the phase-shifting transformer which will compensate for its voltage drop at any given load.

The benefits secured from multiphase operation are not in direct proportion to the increase in the number of phases. The proportionate benefit from each succeeding increase in number of phases drops off sharply beyond 30- to 36-phase operation. Experience to date has demonstrated that 36-phase operation gives satisfactory operation from the telephone interference standpoint in most cases.

Recently the rectifier load on some power systems has become quite large and concentrated. Spot loading of three or more 36-phase operating groups may be encountered. For these special cases the sheer magnitude of the higher harmonics may be sufficient to warrant an increase in multiphase operation. Also a resonant condition for one or more of the higher harmonics may exist. For these installations operation at 72 or 108 phases may be of advantage.

If one rectifier unit, in a multiphase set-up, is taken out of service, an unsymmetrical phase relationship will result. Under this condition, a 36-phase arrangement gives just as good results as any higher-numbered phase arrangement. Operation with one unit out of service gives reasonably satisfactory results.

By the use of base units connected 6 or 12 phase, as required, and by phase shifting of associated units, the combinations shown in Table I can be secured.

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Low-, Medium-, and High-Pressure Gas-Filled Cable

G. B. SHANKLIN
MEMBER AIEE

Synopsis: After preliminary development work systematic laboratory tests, closely simulating service conditions, were started in 1934 leading towards final development and improvement of gas-filled cable. The first full-size field installation of gas-filled cable was placed in service in 1938. The results of this work up to that time were summarized in an AIEE paper presented at the winter convention, January 1939. The present paper will deal with progress made since then, covering the whole range of voltage ratings from 10 kv to 138 kv, divided as follows: low-pressure gas-filled cable systems 10 kv to 40 kv, operating at gas pressures from 10 to 15 pounds per square inch; medium-pressure systems 40 kv to 69 kv, operating at gas pressures from 24 to 40 pounds per square inch; high-pressure systems 69 kv to 138 kv, operating at gas pressures from 150 to 200 pounds per square inch.

AFTER preliminary development work on gas-filled cable a systematic series of long-time load-cycle endurance tests closely simulating service conditions were started in our laboratories the early part of 1934, and this work is still being continued. These laboratory studies have proved a most useful guide in perfecting gas-filled cable design. They have allowed the best selection of materials and the most effective methods of construction and treatment in the factory.

The first full-sized field installation of gas-filled cable was placed in service during the summer of 1938, and since then quite a number of 15-kv and 27-kv installations have been added. Our first and only published report on gas-filled cable was in the form of an AIEE paper presented at the winter convention, January 1939.[1] It seems an appropriate time to summarize advancements made since then and co-ordinate with field experience.

Gas-filled cable has, we believe, technical merit and advantages that more than warrant its full development. It is simple, economical and of small size. Pressure control means uniform control of both insulation and sheath behavior with a lesser chance of trouble with either. The self-supervising feature is also important in view of the fact that most cable troubles come from sheath defects or damage. Finally, compound migration troubles are eliminated and there is no concern about the contour of the cable run within tensile strength limits of the cable itself. Three-conductor, 27-kv low-pressure cable has been in operation for several years past in vertical tunnel shafts from 200 to 250 feet in depth without trouble of any kind, nor has bleeding of compound from the insulation been any more than experienced in ordinary runs.

Three-Conductor Cable

The theory, characteristics, and design of gas-filled cable were dealt with in some detail in the previous paper and will not be repeated here. Briefly, this cable is similar in materials and construction to ordinary solid-type paper-insulated cable, with the exception that longitudinal gas feed channels are provided for uniform gas-pressure maintenance and control.

For low- and medium-pressure use at voltages up to 69 kv the three-conductor shielded type represents the most practical and economical form, since the gas feed channels can be conveniently located in the three triangular filler spaces that are otherwise merely waste space. Figure 1 gives a good idea of the cross-sectional construction. Two of these gas channels are of open steel spiral construction, giving free access to the insulated and shielded conductors. The third is a solid-wall metal tube filled with dry nitrogen gas and sealed off at each end before treatment of the cable length. The function of this tube after installation will be described later. Vacuum drying and impregnation treatment in a large tank before leading is exactly like that for solid-type cable. After impregnation and while still hot, the compound is drained from the tank, being displaced by dry nitrogen gas. The reel of cable is left in the heated tank until all surplus compound drains off, and only that compound held by capillary attraction in the dense cross section remains. The channels and outer surfaces present a clean appearance, but it is surprising how much of the heavy compound is held in the cable cross section by capillary balance, the finished cable being almost as well filled as a solid-type cable.

The treated reel length is then removed to a nitrogen-filled chamber directly behind the lead press, and the lead sheath is applied under a flow of nitrogen. After end sealing the gas pressure is raised to approximately ten pounds per square inch and the cable length is maintained at this pressure until ready for splicing in the field. Before the shipping reels leave the factory, the whole cable cross section is saturated with nitrogen to the same pressure as that in the gas feed channels. This assists greatly in maintaining uniform cross-sectional pressure in service.

Joints

Figure 2 shows a typical three-conductor joint. With exception of the tube insert connections and the fact that the lead casing is filled with nitrogen gas instead of compound, the joint is the same in construction and size as an ordinary solid-type cable joint, the same method of con-

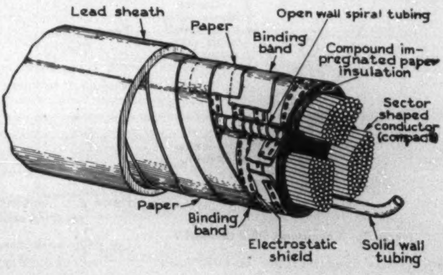

Figure 1. Three-conductor gas-filled cable

Cut-away view showing details of construction

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G. B. SHANKLIN is engineer of the cable division, central station department, General Electric Company, Schenectady, N. Y.

A number of engineers associated with the author have contributed to this work. The author wishes especially to acknowledge the help of J. A. Scott and J. B. Felter who carried out the laboratory tests, and of W. C. Hayman, V. A. Sheals, L. Wetherill, L. L. Phillips, L. Zickrick, F. H. Buller, and A. R. Lee.

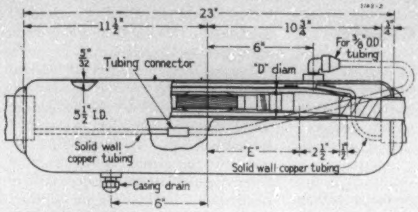

Figure 2. Normal joint

For 27-kv three-conductor gas-filled cable

necting, stepping, reinforcement taping, and shielding being followed. In preparation for splicing, the cable pressure is lowered to about two pounds, the ends are cut, conductors are spread, and temporary sponge rubber seals are applied in the crotch to prevent appreciable loss of gas during the remaining operations. The total time is the same as that required for constructing a corresponding solid-type joint, because of the fact that the time required for the special operations is compensated for by the absence of compound filling operations.

Pressure Control

The primary function of the solid-wall metal-tube insert in the cable is to furnish a by-pass path for gas flow at low points or dips in the cable run, where slugs or surplus compound might gradually form in the open channel spaces during initial periods of heavy load, or during hot summer weather. The method of interconnecting the tube ends and the joint casing, as shown in Figure 2, traps any compound that might enter the casing and assures a free gas path and uniform pressure control over the entire length of the cable line. The lead sleeve casing in Figure 2 is shown eccentric, the bottom part acting as a sump for collection of surplus compound. It has since been found that an ordinary concentric casing gives sufficient capacity for this purpose.

Field tests have shown that, with the help of this tube insert, pressure can be controlled at one terminal end only of lines up to about ten miles in length. A pressure relay at this one location will give an indication of gas leakage at any point in the line, and when loss of pressure occurs, additional gas can be injected from an emergency storage cylinder until the leak can be located and repaired. Where feasible, pressure control and relaying at both ends of long lines is desirable but not necessary.

The first installations of gas-filled cable, as described in the previous paper did not have the metal-tube insert. They are operating successfully, and there has not been any special trouble in maintaining and controlling pressure. It is necessary, however, or at least advisable, to "blow out" the cable line periodically with nitrogen gas and make sure the channels are sufficiently clear of compound to transmit gas pressure. The metal-tube by-pass insert has completely eliminated this need, and there is no question but that it simplifies and facilitates operation of three-conductor gas-filled cable.

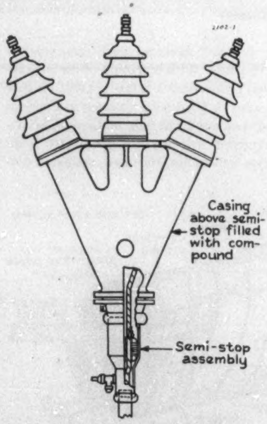

Figure 3. Compound-filled terminal

For 34.5-kv three-conductor gas-filled cable

Terminals

A typical soft compound-filled, three-conductor terminal for gas-filled cable is shown in Figure 3. The only departure from terminals used with solid-type cable is the semistop gasket assembly in the base to prevent the soft compound from draining into the cable channels. A simpler all-gas-filled terminal without the semistop has been developed for use at 15 kv and is also being made available for higher voltages. Both kinds of terminals are also designed as single-conductor and spreader three-conductor types. The only difference between terminals for low-pressure cable (10 to 15 pounds per square inch) and medium-pressure cable (25 to 40 pounds) is that heavier porcelains are required to withstand the higher gas pressure.

Single-Conductor Cable

The gas feed channel for single-conductor cable is in the form of an annular space directly under the lead sheath as shown in Figure 4. Copper tape with stamped button spacers centers the insulated shielded conductor and gives support to the lead sheath. In comparison, the single-conductor cable is less economical than the three-conductor type, because of the increased over-all diameter and cost this channel construction represents. In the three-conductor cable decreased insulation thickness and smaller diameter compensate for the extra gas operations in the factory and field, and the costs of low-pressure gas-filled cable and of solid types are practically the same. Single-conductor gas-filled cable, however, is of higher cost than equivalent solid type, because of the channel requirements mentioned.

This handicap is not very serious and in large sizes the low-pressure single-conductor type has found a useful place as primary station tie cable where numerous vertical runs are encountered. Absence of compound migration troubles and the self-supervision obtained from gas pressure control justify the extra cost. Another promising field for single-conductor gas-filled cable is for power transmission at 46 kv and above, when the load per circuit is too large or the ducts too small for three-conductor cable. In this voltage range medium-gas-pressure operation is more economical than low

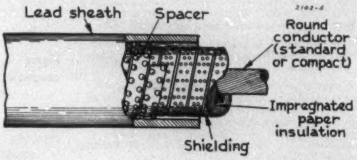
2102-4
Lead sheath
Spacer
Round conductor (standard or compact)
Impregnated paper insulation
Shielding

Figure 4. Single-conductor gas-filled cable

Cut-away view showing details of construction pressure, as will be discussed later, and the further reduction in insulation thickness gained thereby offsets the added cost of the gas channel construction.

The solid-wall tube insert for by-passing compound slugs, such as used in three-conductor cable, is not well adapted to single-conductor construction. This is largely overcome by paralleling the gas connections at each manhole of the three cables making up one circuit, assuring three separate paths for gas flow. The possibility of slug formation in all three cables at one time, in one location, and of sufficient number to materially interfere with pressure control, is remote.

Ionization as a Function of Gas Pressure

A fundamental and important characteristic of gas-filled cable is clearly brought out by a study of Figure 5, which shows ionization starting voltage as a function of gas pressure. A similar curve was given in the previous paper. At that time we had only limited test data above 15-pound pressure and little operating experience to confirm test results. Additional data obtained since allow a more accurate construction of this curve. It closely simulates operating conditions, in that it shows ionization voltage after capillary balance has been reached from load-cycle bleeding of compound, but before appreciable wax formation from test overvoltage has started.

The earlier paper described the self-extinguishing and self-healing properties of wax formation in voids of gas-filled cable. Further work has fully confirmed this important characteristic. It offers an additional factor of safety at those locations along a cable line where ionization might get started. Expressed in another way, gas pressure and ionization control automatically eliminate weak spots and result in more uniformity along the cable length. This in turn allows working voltage stress to more safely approach the critical voltage stress at which cumulative ionization deterioration occurs.

The whole question is whether the measured ionization starting voltage curve in Figure 5 represents this critical voltage stress or something less. From all of the evidence we have obtained it is the writer's opinion that the curve in Figure 5 is something less than critical stress and is actually a goal representing an upper limit of safe working stress in service to be approached as more experience is gained. There are sound reasons for believing this.

First, it has already been proved by numerous long-time load-cycle tests at pressures up to 30 pounds per square inch that gas-filled cable remains stable at stresses considerably higher than represented by the curve in Figure 5. Instability and ultimate breakdown on these tests occur at stresses from 40 to 70 per cent above the Figure 5 curve.

Admittedly, laboratory tests on relatively short lengths are not conclusive in establishing safe working stresses until confirmed by field experience. We have had confirming field experience of this kind with low-pressure gas-filled cable operating during recent years at an average gas pressure of 12 pounds per square inch and an average voltage stress of 65 volts per mil. Reference to Figure 5 will show that at this pressure ionization starts at 80 volts per mil. Accordingly, operating experience has been at a stress only 15 volts per mil less than this. No service failure has yet occurred in gas-filled cable systems and no signs of ionization have been detected. One length of 27-kv cable was removed for test after one year of service. It was found to be in perfect condition. This experience would indicate that there is a good margin of safety and some latitude for increasing present working voltage stress. It also indicates that uniformity in the field is under the same control as obtained in the laboratory on relatively short test lengths.

Additional proof that the initial ionization versus pressure curve in Figure 5 represents a stable and safe condition is found in the characteristic behavior of all representative lengths on long-time load-cycle tests. Typical test results of this kind are shown in Figures 6 and 7. Figure 6 is a chart of the whole test in terms of power-factor stability. Three load cycles per day from room temperature to 80 degrees centigrade were applied, and the gas pressure was maintained at ten pounds per square inch cold and not more than 14 pounds hot. It will be noted that the test was started at 85 volts per mil, only slightly above the ionization-pressure curve in Figure 5. After stabilization was assured, the voltage was increased, and these steps were repeated until final failure occurred after 16 days at 120 volts per mil.

Figure 7 gives power-factor voltage curves on this length as measured initially and at the end of each voltage step shown in Figure 6. Initial ionization at

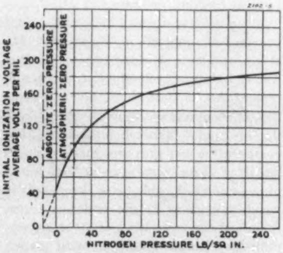

Figure 5. Ionization voltage versus gas pressure

Average voltage stress on solid insulation at which initial ionization starts in gas-filled cable after full drainage of compound before endurance-test voltage is applied

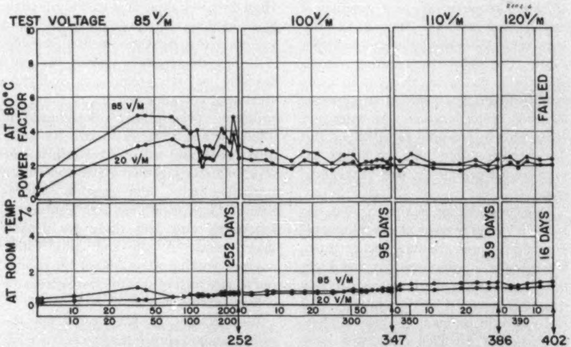

Figure 6. Load-cycle endurance at ten pounds pressure

Three-conductor gas-filled cable of standard construction, with exception that inner 50 per cent of insulation wall consists of three-mil paper tape instead of usual six-mil tape. Total insulation thickness 0.200 inch

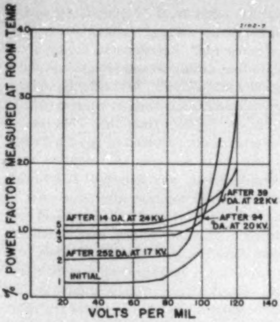

Figure 7. Ionization during load-cycle test

Showing increase in ionization starting voltage, due to wax formation, during different stages of the load-cycle endurance run charted in Figure 6

ten pounds pressure, and before wax formation started, was at 77 volts per mil (equivalent to 80 volts per mil at 12 pounds). Wax formation in the voids extinguished ionization, and the cable became stabilized in about 70 days, thereby automatically raising the ionization voltage to a value equivalent to endurance test voltage. This was repeated each voltage step until the last, 120 volts per mil. Here the stress was too high for the same kind of stabilization to take place and failure occurred in a few days.

If space would allow, additional evidence could be offered showing that the initial ionization-pressure curve in Figure 5 is a useful and practical guide in the design of gas-filled cable systems. Insulation thickness is an important economic factor in such design work. Figure 5 gives a direct indication of insulation thickness requirements as a function of working gas pressure. Increase in pressure means lesser insulation and a smaller cable. The higher the operating voltage, the greater the saving that can be made in this direction. There is an opposing economic factor however. An outer covering must be provided to withstand this gas pressure safely, and the higher the pressure the greater the cost of this covering.

Due consideration of these factors has led to a logical division of gas-filled cable design into three classifications or types, low-pressure, medium-pressure and high-pressure systems. Each covers a certain economic range of voltage ratings. All three have in common the general characteristics of gas-filled cable that have been described. Individual features, limitations, and advantages will be briefly outlined.

Low-Pressure Systems

The low-pressure system has already been described. Aside from comments on pressure control and leak location, only those features that distinguish it from medium- and high-pressure systems will be dealt with here.

Experience has shown that the normal gas capacity of the cable itself and the joint casings are sufficient to limit pressure fluctuations from daily load cycles to a range of only one to two pounds. It accordingly is not necessary to use automatic pressure-regulating equipment. Seasonal adjustment of pressure by hand to an average working value of 12 pounds per square inch is all that is necessary. An ordinary high-pressure storage cylinder of nitrogen is connected permanently to the cable line through a reducing valve. An exhaust valve is also provided. Normally these valves are closed. During routine inspection and for purposes of compensating for seasonal temperature changes, average working pressure is readjusted to 12 pounds by hand manipulation of these valves. If there is a loss of pressure in between these periods, a pressure relay gives notice when minimum pressure drops to ten pounds.

Leak location is not a difficult matter. It is first necessary to disconnect the external tube by-pass fittings on the joints (Figure 2) at intervals along the line and insert pressure drop reading devices. A graph plot of these readings indicates the point of leakage. The pressure-reading devices can be left permanently connected if desired, and one utility prefers this. Obviously, small slow leaks require a longer time and closer readings to locate than do fast leaks. It is entirely feasible to make these lines gas-tight initially and maintain this tightness without trouble.

It has already been shown that safe working voltage stress and insulation thickness are governed by working gas pressure. This, in turn, is determined by allowable hoop stresses in ordinary single lead sheath that will avoid sheath fatigue and hold creepage and expansion within negligible limits. The question of stress limits for lead sheath has been a subject of laboratory and field study for a good many years. A safe and conservative average working stress limit of 125 pounds per square inch of sheath cross section for copper bearing lead is well established.

Low-pressure gas-filled cable has the same standard sheath thicknesses as solid-type cable. On this basis and with a stress limit of 125 pounds an average working gas pressure of 12 pounds per square inch results. Present day insulation thicknesses for low-pressure gas-filled cable represent an average working voltage stress of from 65 to 68 volts per mil. Reference to Figure 5 will show this practice safe and conservative.

These design limitations of insulation and sheath properties and dimensions automatically set the economic voltage range. This range is found to be from 10 kv to 40 kv, and within these limits the cost of three-conductor low-pressure gas-filled systems is closely comparable to the cost of other available types of cable systems.

Above 40 kv, the proportionate saving in insulation thickness makes increased working gas pressure economical in spite of increased cost of the outer covering. This leads logically to medium-pressure systems for moderately high-voltage service.

Medium-Pressure Systems

In theory and principle there is no difference at all between low- and medium-pressure systems. Accessory design and methods of manufacture, installation and operation are the same for both types.

There are two available methods of sheath construction that allow increase of gas pressure:

(*a*). Reinforced sheath.

(*b*). Substitution of a special alloy sheath of greater creep strength than ordinary single sheath.

Both constructions are fully developed. The latter is economically the most promising, but both will be dealt with. It would be well, however, to first consider results of long-time load-cycle stability tests at medium pressure.

A typical test of this kind at 30 pounds pressure is charted in Figure 8. The three-conductor shielded-type cable used was of the same general construction and dimensions as that in Figure 6, having 350,000-centimeter compact sector conductors and 0.200-inch insulation. For comparative test purposes this size and a length of 75 feet were standardized for all of our gas-filled cable tests, the details of which are given in the previous paper. Standard compound was used for impregnation. Strand shielding with paper-backed aluminum tape was also used. Further tests with and without strand shielding will be discussed later, but it will be said here that, as far as load-cycle aging tests on gas-filled cable are concerned, strand shielding appears to im-

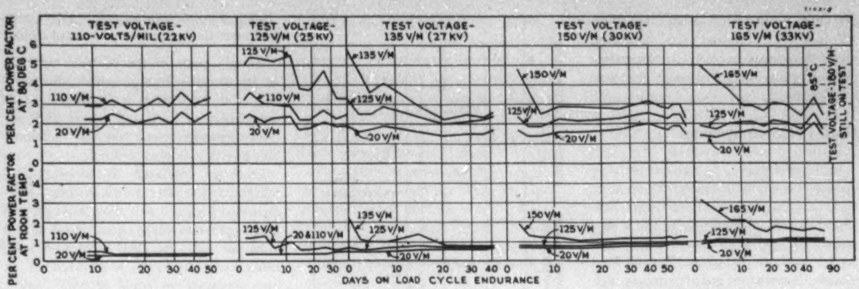

Figure 8. Load-cycle endurance at 30 pound pressure

Three-conductor gas-filled cable of standard construction, with exception of aluminum-paper strand shielding. Total insulation thickness 0.200-inch. No-load nitrogen gas pressure held at 30 pounds per square inch (medium pressure)

part no special benefit. This is due to the fact that voids, ionization, and wax formation are distributed uniformly in the butt spaces through the wall of insulation and do not concentrate at the conductor surface alone.

Referring to Figure 8, the 30-pound load-cycle test was started at 110 volts per mil. According to Figure 5, this is the initial ionization starting voltage at this pressure. There were practically no signs of ionization or instability at this stress, so after 50 days test voltage was increased to 125 volts per mil. Here, ionization was apparent but was extinguished after 15 days because of wax formation. As soon as there was assurance of stability (40 days) the test voltage was increased to 135 volts per mil. The same method of stepping voltage was followed to the end of the chart, and it will be noted that self-healing action and stability were retained at 165 volts per mil. This length is still on test at the present writing at 180 volts per mil without signs of failure.

It might be concluded from this test that an increase in gas pressure from 10 to 30 pounds imparts more benefit than would be indicated from the initial ionization curve in Figure 5. However, it appears sounder to use Figure 5 as a guide and not depend too greatly on self-healing action in service, even though it does appear more pronounced as gas pressure is increased. On this basis we believe that an operating stress in the order of 95 volts per mil is safe and conservative at 30 pounds pressure.

Double Reinforced Sheath

Double reinforced sheath was developed originally for oil-filled cable and has been used extensively for this purpose. There have been a good many years of experience at pressures from 30 to 40 pounds per square inch. Based on this, we believe 40 pounds a safe top limit and have designed medium-pressure gas-filled cable with this type of sheath for an average working pressure of 35 pounds and an operating voltage stress of 100 volts per mil. The economic voltage range is from 40 kv to 69 kv.

The double reinforced sheath consists of treated paper and copper tape reinforcement, sandwiched between inner and outer sheaths. The cost is higher than that of ordinary single sheath. Insulation reduction from increased gas pressure does not fully compensate for the increased cost of sheath. Accordingly, the cost of this type of three-conductor medium-pressure cable is higher than that of equivalent single-sheath oil-filled cable. Accessories however are simpler and less costly, as are also installation operations in the field. The result is that over-all costs of the two types of cable systems are not far apart.

High-Creep-Strength Alloy Sheath

From the beginning we have appreciated the economic possibilities of substituting an alloy sheath of higher creep strength than ordinary copper-bearing lead sheath. The requirements are exacting and proved difficult to attain. Such an alloy must retain all of the desirable and proved properties of copper-bearing lead, plus a sufficient increase in long-time creep strength to allow a real increase in working gas pressure. The requirements to be met are:

(*a*). At least twice the long-time creep strength of copper-bearing lead at operating temperatures.

(*b*). A reliable source of supply at reasonable cost.

(*c*). Readily adapted to sheath-extrusion methods in the factory.

(*d*). Equally resistant to corrosion, vibration, bending movement, and aging.

Out of a number of possible lead alloys investigated in recent years the calcium alloys of lead are the most promising, and one in particular, Asarco, will be dealt with. Figure 9 shows the long-time room-temperature creep strength in comparison with that of copper-bearing lead. Similar tests at 100 degrees centigrade plainly show that increasing the average working stress from 125 pounds per square inch for copper-bearing sheath to 250 pounds for Asarco sheath is conservative.

This means that with present standard thicknesses of single lead sheath we can, by using such an alloy, safely increase working gas pressure to 24 pounds per square inch, and corresponding voltage stress to 90 volts per mil, a gain of 38.5 per cent in voltage rating as compared with low-pressure systems operating at 12 pounds pressure. For the same voltage rating the corresponding reduction in insulation thickness is 27.5 per cent.

Regardless of whether the object is to increase voltage rating or to decrease insulation thickness for a fixed voltage rating, this theoretical gain is more or less offset by the increased costs of the special alloy sheath, and the lower the voltage rating the more pronounced this offset becomes. Because of these conflicting cost factors, this particular type of medium-pressure system, like the double reinforced sheath system previously described, has an economic voltage range from 40 kv to 69 kv. Below 40 kv the saving practically disappears, and much above 69 kv, such as 115 kv for instance, three-conductor cable becomes too large to be economical. There is some promise, however, in single-conductor cable above 69 kv. Here the extra cost of the gas-channel construction and the alloy sheath are offset by the other savings described. All told, we consider this simplified medium-pressure gas-filled cable system an important technical and economic advancement.

High-Pressure Systems

Full advantage of high-gas-pressure operation can only be realized by drawing the cable into a welded steel pipe line. There have been proposals to use a special double lead sheath construction, reinforced for both radial and longitudinal strains, to operate at gas pressures as high as 200 pounds per square inch. This type of cable could be installed in underground ducts in the usual manner which is an advantage, but there appear to be two serious obstacles.

1. The over-all cost is too high in comparison with other available cable systems, such as oil-filled, medium-pressure gas-filled, and, finally, high-pressure gas-filled drawn into steel pipe.
2. There is serious doubt of the feasibility of safely operating reinforced sheath at such high pressures, because of mechanical strength limitations.

There is no doubt but that the steel pipe construction is the most promising, and further comments will be confined to that type.

When properly designed and installed, the high-pressure gas-filled pipe cable system is technically sound and economically attractive. It has however certain limitations that should be fully understood.

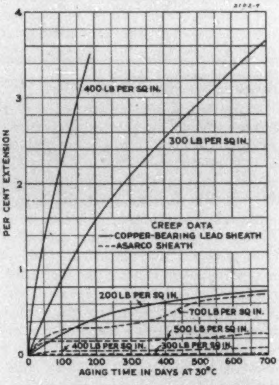

Figure 9. Creep properties of lead sheath

Long-time room-temperature creep properties of Asarco and copper-bearing lead sheaths. Tests started two weeks after extrusion

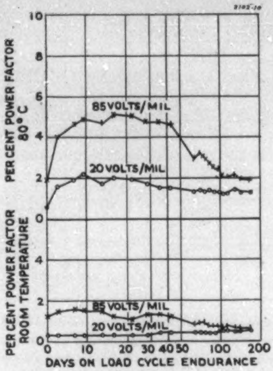

Figure 10. Load-cycle endurance at ten pounds pressure

Standard three-conductor gas-filled cable with aluminum-paper strand shielding similar to cable in Figure 8. No-load nitrogen gas pressure held at ten pounds per square inch (low pressure). Cable still on test at 100 volts per mil

1. It is best adapted to direct burial in open ground, and its use is likely to be limited in this country where most underground systems are in congested areas under paved city streets. The standard duct and manhole construction (and the flexibility it represents) has always proved superior to direct burial under these conditions.
2. Important high-voltage tie-line loads in this country are growing, and the steel pipe cable system is not well adapted to handle extra heavy loads. It is not possible to use one single-conductor cable per pipe, with three pipes spread out to better dissipate copper losses. Instead, it is necessary to draw all three cables into one congested pipe, and the losses to be dissipated are much higher. In this respect pipe cable is no different from other forms of three-conductor cable and has the same well-recognized limitations for economic loading.

These aspects of the problem are not so important in Europe where the practice of direct burial in open ground is commonly followed, and where load requirements per circuit average considerably less than in this country.

From an insulation standpoint gas-filled pipe cable offers no difficulties. Reference to Figure 5 will show that at 200 pounds pressure initial ionization is at 180 volts per mil. In view of greater self-healing properties from wax formation at high pressures, we could probably operate with safety at an average stress not less than 160 volts per mil. In this respect high-pressure gas-filled cable, like oil-filled cable, has greater 60-cycle strength than can be utilized. Insulation thicknesses for oil-filled cables are based on transient voltage requirements rather than normal 60-cycle rated voltage stress, and the same thing should hold for high-pressure cable. Accordingly, insulation thicknesses for high-pressure gas-filled cables are the same as for oil-filled cable, the rated voltage stress varying from 126.5 volts per mil at 69 kv to 142.5 volts per mil at 138 kv.

Figure 5 shows that this range of voltage stresses is conservative at 200 pounds pressure. In fact, the gas pressure could be reduced to 125 pounds without danger. There is no particular object, however, in not taking advantage of the higher factor of safety offered by the higher pressure, since standard steel pipe will withstand

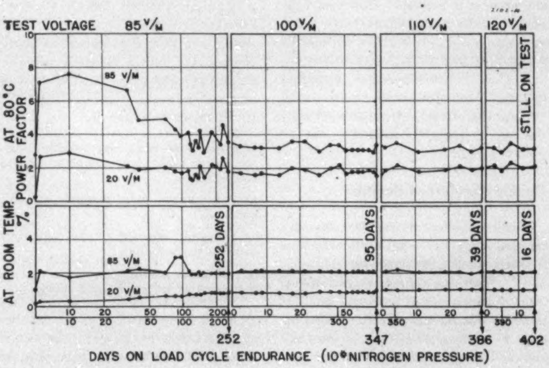

Figure 11. Load-cycle endurance at ten pounds pressure

Standard three-conductor gas-filled cable exposed to room air for eight hours before applying the lead sheath. Chart shows the effects of oxygen absorption during exposure

200 pounds pressure just about as well as 125 pounds.

Details of pipe construction, welding, covering for corrosion protection, joint sleeves, compressed air test for leakage, and so forth will not be dealt with here, since this practice is well-known and common to all of the various pipe cable systems, of which there is a number of installations in Europe and a few in this country.

Sheaths Intact

After the pipe is laid and ready to receive the gas-filled cable, the remaining operations vary, depending upon whether the sheaths on the three single-conductor cables are left intact or stripped off as the cable lengths are drawn into the pipe. Each of these two methods has advantages and disadvantages that need careful balancing. For loads involving conductor sizes above approximately 750,000 circular mils, short-circuited sheath losses become so great that there is not much choice, economy dictating removal of the sheaths. For lighter loads and smaller conductor sizes there is much to be said in favor of leaving the sheaths intact, but it is difficult to draw an exact dividing line between these two practices without studying each specific case.

With sheaths intact the methods of manufacture, shipment, installation, and splicing are exactly the same as already described for low-pressure systems in ducts, the only difference being that the gas channel between the insulated shielded conductor and the sheath is smaller and simpler than shown in Figure 4, an open spiral wrapping of fibrous tape being substituted for the metal button spacer tape. Each joint is enclosed in a wiped lead sleeve, and since splicing is done under a slow flow of nitrogen gas, the cable insulation is at no time exposed to air or moisture. It is not necessary to remove these impurities from the pipe until the whole line is completed. At that time the pipe can be dried out by short-circuit current in the conductors, or circulation of hot air, or both. Afterwards, the dry air in the pipe is washed out by nitrogen flow assisted, if desired, by drawing a rough vacuum first. These same operations can be done piecemeal on sections of the line as completed if more convenient, and the finished sections maintained at a few pounds nitrogen pressure.

After the whole line is ready, the outlet plugs in all lead joint sleeves are removed, the plugs in the outer steel sleeves replaced, and the gas pressure is brought up to 200 pounds. There is then an interconnection of gas pressure between cable and pipe at every joint sleeve. Equalized pressure throughout removes any question of stress burden on the lead sheaths.

If future repair work, involving replacement of cable lengths, is ever required, these operations are reversed. After lowering the gas pressure, outlet plugs in the lead joint sleeves are replaced, segregating the cable from the pipe. Replacement of cable can then proceed as with low-pressure cable, and at no time is the insulation exposed to moisture and air absorption.

Sheaths Stripped Off

In addition to elimination of sheath losses and reduction in conductor size obtained thereby, stripping of sheaths as the cables are drawn into the pipe has one other advantage. The removed lead can be salvaged. The disadvantages are:

(*a*). An outer tape wrapping and copper armor are necessary for protection during pulling.

(*b*). Extra cost and time for stripping the sheath and protecting cable ends from exposure.

(*c*). Pipe must be dried and filled with nitrogen gas piecemeal before the cable is pulled in, and pulling must be done under a slow flow of nitrogen.

(*d*). Absorption of air and moisture during unavoidable exposure will increase dielectric losses and require a heavier wall of insulation than when sheath is left intact.

(*e*). Future repair work without undue exposure of insulation is more difficult and complicated.

Comparative over-all installed costs and service records will be required before the relative merits of these two high-pressure gas-filled cable systems can be finally decided in those cases where sheath losses are not a dominating factor.

Summary of Recent Test Results

Space will not allow a detailed presentation of all the test data obtained on gas-filled cable since the last published report. We have carried out comparative test studies of the best available impregnating compounds, particularly as a function of viscosity, the effects of paper thickness, strand shielding, exposure to air, further tests on the effects of gas pressure, and some impulse tests. Conclusions drawn from these tests will be summarized, and pertinent results relating to these conclusions given.

Thickness of Paper Tape

The load-cycle test in Figure 6 represents a test length of cable with the inner half of the insulation composed of three-mil paper tape instead of the usual six-mil tape. In all other respects this length was of standard construction. Theoretically, thinner tape means smaller butt space voids and higher ionization voltage. Actually, Figure 7 shows that ionization voltage was no higher than usually obtained with standard six-mil tape. Standard cable retains stability up to 100 volts per mil on load-cycle test at ten pounds pressure and ultimately fails at 110 to 120 volts per mil. The thin paper length in Figure 6 lasted little if any longer, failing in 16 days at 120 volts per mil. In view of the fact that thin tape is more expensive and difficult to apply, tends to wrinkle more, and will not withstand cable bending as well, it is doubtful that there is any benefit that overbalances these disadvantages.

Strand Shielding

It has already been explained that the test length represented in Figure 8 had strand shielding, and the test results did

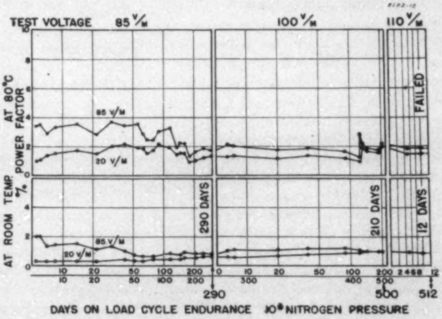

Figure 12. Load-cycle endurance at ten pounds pressure

Three-conductor gas-filled cable of standard construction, with exception that viscosity of impregnating compound was increased to 350 Saybolt at 100 degrees centigrade by addition of 1.5 per cent soluble synthetic compound not appear to show any particular improvement in ionization characteristics over equivalent cable without strand shielding. This is confirmed by load-cycle tests on an additional strand-shielded length as given in Figure 10. Ionization starting voltage is no different, and it required about the same length of time (75 days) to stabilize at 85 volts per mil as experienced with standard lengths without strand shielding. The length in Figure 10 is still on test at increased voltage stress. Possibly ultimate results will show some benefit from strand shielding, but we haven't found it yet. The explanation seems to be that ionization and wax formation occur more or less uniformly in butt spaces throughout the insulation thickness and do not concentrate at the conductor surface alone.

Exposure to Air

To study the effects of casual exposure to air, as may happen in jointing operations or through some accidental exposure, a standard test length was exposed to room air for eight hours after removal from the treating tank and before the lead sheath was applied. The results of load-cycle testing are given in Figure 11. It will be noticed that oxidation action from the absorbed air increased dielectric power factor at both room temperature and 80 degrees centigrade, the latter increasing to about double that met with in standard cable. It will also be noticed that in spite of this the cable had good endurance and was removed from test after 16 days at 120 volts per mil without failure. The conclusion can be drawn that, within accumulative heating limits, oxygen absorption does not affect voltage endurance but does appreciably increase dielectric loss and should be avoided for this reason. This is particularly true of medium- and high-pressure systems operating at the higher voltage ratings, where liberties with impurities and dielectric loss cannot be taken without serious risk.

Effects of Compound Viscosity

The series of tests we have carried on over a period of years on gas-filled cable indicate rather definitely that there is such a thing as optimum viscosity of impregnating compound and that this is in the order of 100 Saybolt at 100 degrees centigrade, representing the standard all-mineral compound used for some time past. We invariably obtain better results with this compound than with either thinner or heavier impregnants, apparently for reasons explained in the previous paper.

Check tests, that will not be given, again show that thin oil, such as used in oil-filled cable and having a viscosity of 37 Saybolt at 100 degrees centigrade, drains too freely from the insulation and results in poor ionization and endurance characteristics. This is not surprising and would be expected from theoretical considerations. The surprising thing is that various high-viscosity compounds give from fair to very poor results, but even the best shows no advantage over the standard compound.

We have tried about all of the recognized available types of high-viscosity compounds, including rosin mixtures, soluble synthetic mixtures, and hydrogenated oil, with viscosities from 200 to more than 1,000 Saybolt at 100 degrees centigrade. All of these, with one exception, gave poor and unstable results that are hardly worth showing. The best consisted of standard compound mixed with 1.5 per cent soluble synthetic to give a viscosity of 350 Saybolt at 100 degrees centigrade. An incomplete test on a length impregnated with this compound and designated as test length 8, was reported in the previous paper. The complete test is charted in Figure 12. The results obtained from both an ionization and voltage-endurance standpoint were about an average of those obtained with standard cable.

Since extra high-viscosity compound means longer periods of impregnation treatment and drainage, and more difficulty in clearing out gas feed channels, we have, as yet, found no advantage that would justify its use. A study is also being made of preimpregnated tape and, so far, no advantages have been found that would justify its complications and greater cost. Further and more complete tests may modify this belief.

Impulse Tests

After 21 days of vertical drainage with 75-degree-centigrade load-cycles five lengths of single-conductor, 1,000,000-circular-mils gas-filled cable, without strand shielding and having 0.203-inch insulation, were subjected to standard impulse breakdown at a gas pressure of ten pounds per square inch. An average breakdown of 367 kv was obtained with a deviation of 35 kv, or 9.6 per cent. This corresponds to an average stress of 1,800 volts per mil, compared with an average of 1,680 volts per mil obtained on 33 lengths of ordinary solid and oil-filled cable. On the basis of maximum stresses, which some prefer, the gas-filled cable gives 2,130 volts per mil, and the solid and oil-filled, 2,260 volts per mil. For all practical purposes it can be said that the results on all three types are closely the same. It is reasonable to assume that the same thing would hold for medium- and high-pressure gas-filled cable, since the insulation thickness is not under compression and does not vary with pressure.

Reference

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Improvements in Preventive-Coil Control for A-C Locomotives With Particular Reference to Resistor Transition

P. H. HATCH
MEMBER AIEE

H. S. OGDEN
MEMBER AIEE

SINCE the original development of the a-c locomotive there have been many attempts to improve the locomotive control over that provided on early designs.

Control problems have come under three general classifications, namely:

1. Voltage control for the acceleration of the locomotive.
2. Functional control for adjusting various fields of the series traction motor for best commutating characteristics.
3. Protective relays for protection of locomotive and traction motor circuits from grounds and faults of various kinds and for protection of the traction motor from overspeeding.[1,2]

This article will deal only with the first of these problems, namely, those incident to the voltage control as it relates to accelerating a locomotive, with particular reference to a new development in the art. The new development is the resistor transition scheme for switching preventive coils from one pair of transformer taps to the next. A résumé of service experience with a locomotive so equipped is also included.

While the search for a better method of acceleration has proceeded, there have been no revolutionary improvements made, and progress has been essentially a refinement in design.

Early locomotives made use of a switching reactor, or preventive coil, in tapping from one set of transformer taps to the next, and the scheme which developed the most favor soon grew to what was termed as the three-preventive-coil notching scheme. In this scheme two small preventive coils feed a large preventive coil which in turn feeds the traction motor circuits through its mid-tap. The two small preventive coils are connected to two sets of bus bars, which are connected by tap switches to the main transformer taps in the correct order. Each tap switch carries one quarter of the load current in the normal design where all preventive coils have their mid-tap equidistant from the two end terminals. The three-preventive-coil scheme of control has persisted and is now the scheme most generally used on a-c locomotives in the United States.

During recent years several voltage-control schemes of a somewhat different nature have been devised and in some instances have been tried out in service both in this country and abroad, but by and large the schemes have met with indifferent success. Schemes tried in this country include one that has used a relatively large buck-boost transformer, the primary of which is controlled by means of light-current relatively high-voltage switches. When the large buck-boost transformer has been notched up to its maximum voltage, balanced voltage conditions are obtained. This condition is retained through suitable transfer switches while the buck-boost transformer is reconnected to repeat its operation from higher main taps on the main transformer. Also there have been tried variations of European schemes, utilizing a motor-driven switching device which shifts taps under zero voltage conditions obtained by mechanical interlocking with arcing switches designed to handle the arcing duty.

Since the year 1937 improvements in the basic type of voltage control have been evolved which have consisted essentially of improvements in the contactors themselves, in the interlocks which are used to insure against faulty closure of switches, and further in an arrangement of auxiliary relays known as "sequence or interlocking" relays which, while having increased the number of operating coils in the system, have materially decreased the number of contacts in the control circuits and have given an overall improvement in reliability and a decrease in maintenance.[3]

One of the chief sources of trouble that arises with notching schemes using individual contactors is the failure of one of the individual contactors to open its circuit when it is called upon to do so. This may arise from purely mechanical reasons such as sticking of the operating rod or some of the mechanical portions of the contactor, or from electrical reasons, such as those which cause the tips of the contactor to weld together. Because of the relatively large number of tapping contactors used upon a modern locomotive (24 in the case of the latest New Haven and Pennsylvania Railroad locomotives), the number of interlocks required for complete protection against contactor closure when a given contactor has stuck closed for some reason was prohibitive. For this reason previous to 1937 it was considered sufficient to interlock positively against direct short circuits upon a given preventive-coil bus, and no attempt was made to prevent possibilities of overvoltage being applied to a preventive coil.

The development of the "sequence relay" made interlocking possible which would prevent the closure of any switch that might set up undesirable conditions after another had stuck closed.

To protect against an unbalanced preventive coil, caused by failure of a switch to close in sequence, differentially connected current transformers supplying a thermal-element preventive-coil relay are provided. The relay characteristics are so proportioned that on normal switching operations, in which one tap switch is opened before the next is closed, when the preventive coil is left connected with but one leg for a few cycles, the relay will not trip. Should a tap switch remain open, the heater element will cause the relay to trip after a definite time, opening the control circuits to all the tap switches. Such thermal relay connections also provide protection against internal failures of the small preventive coils, as such faults give rise to current in the same direction as exciting currents and thus are effective in tripping the thermal element.

Operating experience prior to 1937 developed the weakness of utilizing the three possible unbalanced preventive-coil combinations as the first three starting notches for a locomotive. Locomotives so connected gave rise to an abnormal number of preventive-coil failures directly traceable to operation on one of the unbalanced notches while starting heavy trains at difficult locations. Modern practice therefore prescribes the use of balanced connections for the coils for all notches—except possibly the first—

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P. H. HATCH is assistant mechanical engineer with the New York, New Haven, and Hartford Railroad Company, New Haven, Conn. H. S. OGDEN is in the transportation control engineering department of General Electric Company, Erie, Pa.

where currents are low, and there is no occasion for extended operation.

The aforementioned improvements, however, do not progress greatly toward the solution of one of the first criticisms of the a-c locomotive, namely, the sag in tractive effort that occurs between notches. During the transfer from one notch to the next it is necessary that one end of a preventive coil be open-circuited completely before closing it in on the next higher voltage. During this time the total load current for the particular preventive coil is carried through one half of its winding. This winding offers a considerable impedance to the load current because of the unbalanced ampere turns in the coil. The impedance drop under these conditions becomes the impedance that will be supported by the iron plus the series-air-core impedance of the turns in the coil itself. As used here, "series-air-core impedance" refers to the reactance of the coil, assuming no iron is present, as the ampere turns available are many times in excess of those required to saturate the iron.

The insertion of the high impedance of one half of a preventive coil in the circuit causes a very marked decrease in the voltage left to be applied to the traction motors, with the result that there is a considerable falling off of traction motor current, and thus a sag occurs in the motor tractive effort. Under severe conditions of starting and running when operating at tractive efforts very close to the slipping point of the wheels, such a reaction during the notching is very undesirable. Most of the control schemes tried out or proposed have had as one of their main objectives the smoothing out of the notching characteristics between notches. Some of the schemes have been more or less successful in this, but for the most part they have introduced other undesirable features.

Another characteristic inherent in the three-preventive-coil scheme is the presence of the extremely high voltages which occur in the saturated preventive coil undergoing tap change. Such voltages are of the "peaked-wave" variety, maintaining for only a very small portion of each cycle and occurring when the current wave passes through zero. At the higher transformer voltages and motor currents the "peaked-wave" voltages have been measured which reach values of approximately twice the transformer secondary voltage. This comes about from the fact that all of the available voltage of the transformer secondary may appear across one half of the small preventive coil for an instant during each cycle, and the turn ratio of the preventive coil will give twice this voltage between its two outside terminals. When it is considered that the nominal rated voltage of a small preventive coil is of the order of 128 volts, and the secondary voltage of the main transformer may be of the order of 1,400 volts, the amount of overvoltage that is applied to a preventive coil is extreme.

The presence of peak voltages during switching operation imposes a very severe switching duty upon the tap switches, as is evidenced by the loud noise and substantial arcing that usually occurs during operation of the tap switches under load conditions.

The saturation of the preventive coil during the transition period has another effect which at times in the past has been the most important phenomenon in connection with the switching. This is the occurrence of a displaced current wave upon the closure of the next higher tap switch with the result that very high values of transient currents are obtained. If the value of air-core reactance in the preventive coil undergoing switching is not of sufficient magnitude, currents high enough to cause welding of the tap switch may occur.

These extremely high currents come from:

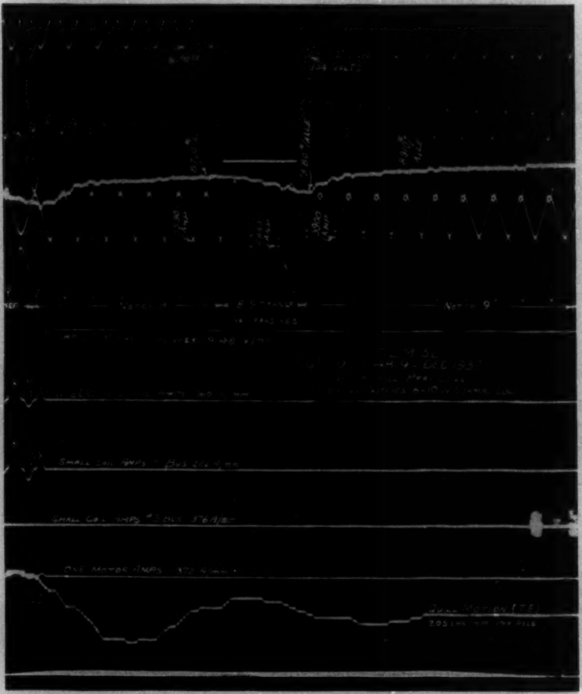
1. Trapped flux in the coil being switched due to the reversal of applied voltage. This phenomena is similar to that which sometimes occurs when a power transformer is energized and the switch closure comes when the instantaneous applied voltage does not correspond to the flux in the transformer iron, with the result that a distinct "bump" can be heard if one is in the immediate vicinity of the transformer.

2. A component from the trapped flux of the large preventive coil which may be saturated, and many times is, during the switching operation.

3. The proportionate share of the load current for the particular switch involved.

Such currents do not occur every time a transition is made, as their occurrence is dependent upon the instant during the

Figure 1. Film 52—transition between notches 8 and 9 without transition resistor



current wave that the tap switch is closed. For this reason tests designed to record the maximum values of current obtained upon contactor closure have to be made recording the results of very large numbers of closures.

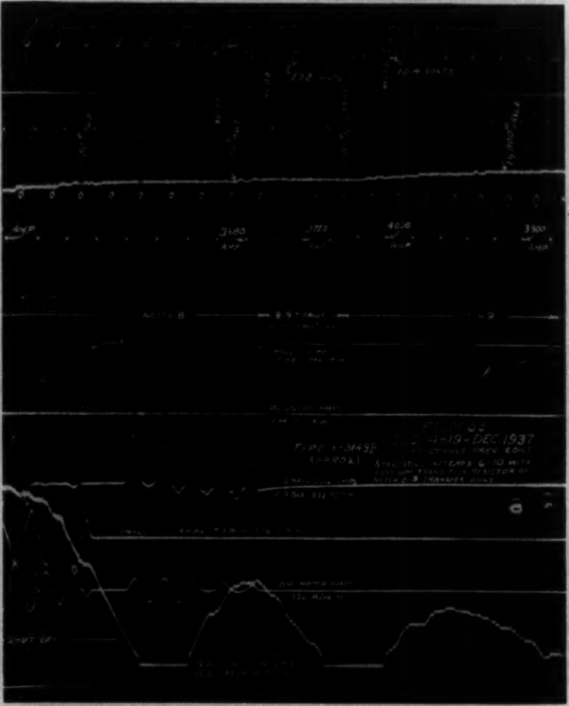
The surge current peaks have been measured as high as 23,000 amperes in a 1,250-ampere contactor, and possible values may be calculated with a reasonable degree of accuracy. Preventive-coil air-core reactances are usually figured to limit these currents to around 12,000 to 14,000 amperes, which seems to be a satisfactory value for the circuits and contactors involved.

Most of the early difficulties with the three-preventive-coil scheme of control centered around the welding of the tap-switch contacts, as then it was not known what magnitude in surge currents would be obtained, nor what steps would have to be taken to limit these currents to satisfactory values.

One of the suggestions made during the early days involved the connection of a resistor across the preventive coil to prevent the extreme amount of saturation which occurs in the coil. However, this suggestion was not followed up as it appeared simpler and more helpful to connect special air-core reactances in series with the preventive-coil bus circuit to limit the flow of exciting current through the small preventive coils. While the air-core reactance coils were so connected as to limit the flow of exciting current, the arrangement was such that the effect of load currents was to balance out the reactances of the coils. In later designs the reactance of the external coils was built into the preventive coils themselves so that the extra pieces of equipment are no longer required.

In 1937, during the search for a better scheme of control, it was suggested that a resistor of proper ohmic value be connected across the terminals of the preventive coil that was open-circuited during the transition. With this connection, while the preventive coil was operating "one-legged" the current would divide properly and flow through the resistor to the preventive coil and then to the motors. The value of resistance was determined by the maximum current to be encountered in the service of the locomotive and the designed voltage of the preventive coil. The voltage across the preventive coil at no time then would exceed its maximum designed value, and there would be no possibility of saturation of the coils.

Figure 2. Film 53—transition between notches 8 and 9 with transition resistor



Early Experiments With Transition Resistor

Such a scheme of connection was tried out upon the Pennsylvania Railroad in 1937, on locomotive 4819, at which time oscillograms were taken which showed very clearly the results to be expected from such an arrangement. In addition to the currents and voltages recorded on the films, it was also possible to measure directly the torque of the traction motors involved so that the results of the sag in tractive effort during ordinary notching and the lack of a sag in tractive effort in notching with the transition resistor were definitely recorded.

The drive from the traction motors to locomotive driving wheels is of the well-known spring cup type. In order to measure the motor torque, it was necessary only to provide an electric position-measuring device to indicate the deflection of the drive springs and record the indications on one of the oscillograph elements. When the spring constants are known, it is a simple matter thereafter to calibrate the indication of the element in pounds tractive effort.

The tests were confined to observing the effects on one transition only, namely from notch 8 to notch 9. The resistor used was of very small physical size, being capable of withstanding the currents involved for only a few cycles. The tests were all made at standstill, as it was impractical to measure the motor torque while the driving wheels of the locomotive were rotating.

The first of the oscillograph films (film 52) gives a record of the transition from notch 8 to notch 9 without the use of the resistor. Referring to film 52 (Figure 1) it will be observed that during the transition the tractive effort fell from 8,700 pounds per axle in notch 8 to 7,580 pounds per axle during the transition; the traction motor current fell from 3,530 peak amperes per motor to 2,880 peak amperes per motor and then rose again to 3,900 peak amperes per motor in notch 9 after

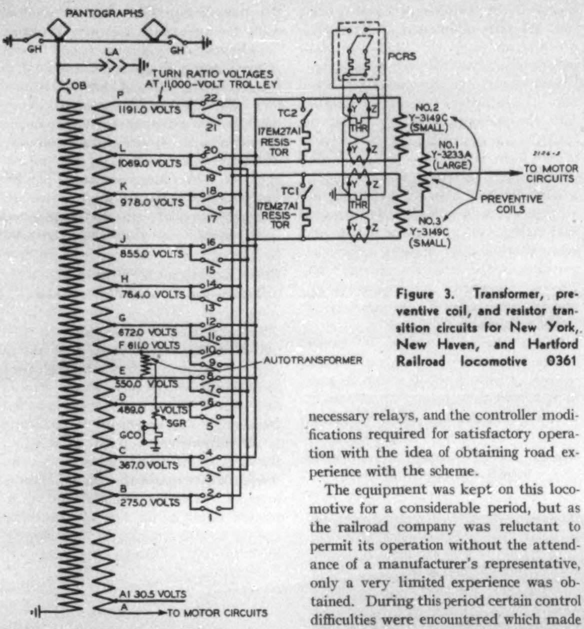


Figure 3. Transformer, preventive coil, and resistor transition circuits for New York, New Haven, and Hartford Railroad locomotive 0361

the transition was completed, this current corresponding to 8,910 pounds per axle as measured on the film. Also to be observed are the very high peaks in small preventive-coil voltage, which curve goes clear off the bottom edge of the film to a value impossible of determination. The rms value of the preventive-coil voltage for notch 8 is 43.5 volts, and for notch 9 is 49.8 volts.

Film 53 (Figure 2), taken immediately after film 52, with the transition resistor functioning, shows the very striking improvement obtained. In this case the tractive effort per axle moved gradually from 11,180 pounds per axle to 11,900 pounds per axle some time after the transition was complete. The increase in traction motor current was also gradual, raising from 3,680 peak amperes in notch 8 to 3,820 peak amperes during the transition, and then to 4,090 peak amperes after the transition was complete. The preventive-coil voltage is kept down to its normal value; the increase during the transition is only to 91 volts rms as compared with the nominal rating of the preventive coil of 128 volts.

Pennsylvania Railroad locomotive 4819 was subsequently equipped with a pair of resistors, a pair of resistor contactors, the necessary relays, and the controller modifications required for satisfactory operation with the idea of obtaining road experience with the scheme.

The equipment was kept on this locomotive for a considerable period, but as the railroad company was reluctant to permit its operation without the attendance of a manufacturer's representative, only a very limited experience was obtained. During this period certain control difficulties were encountered which made the scheme of getting the resistor switch closed before the tap switch opened, and then opening the resistor switch after the next higher tap switch had closed, somewhat unreliable. However, there was a noticeable improvement in locomotive acceleration and a very marked reduction in the amount of arcing on the tap switches, both in noise and the amount of visible arc, during the time the resistor transition equipment was working.

The equipment was subsequently removed from Pennsylvania Railroad locomotive 4819, and returned to the manufacturer's plant for reconditioning.

Service Experience on New York, New Haven, and Hartford Railroad

Having observed the performance of Pennsylvania Railroad locomotive 4819 with the resistor transition equipment functioning, New Haven representatives expressed interest and a desire to have the equipment turned over to them for use and service experience on one of their locomotives, in order to explore fully the possibilities of improved locomotive performance and reduced tap-switch maintenance.

New Haven electric locomotive 0361 (Figure 6) was selected for the installation of the resistor transition apparatus. This locomotive is one of six built by the General Electric Company in 1938 and used in main-line passenger service between New York and New Haven. A brief description of the locomotive follows:

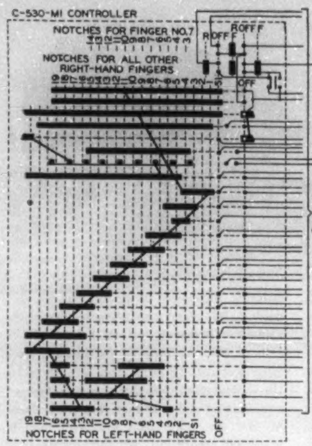
Class	0361–0366
Service	Passenger
Power supply	11,000 volts, 25 cycles, a-c, 660 volts, d-c
Wheel arrangement	$2C+C2$
Number and type of traction motor	$6GEA$-622
Total weight	432,000 pounds
Weight on drivers	273,000 pounds
Length between coupler pulling faces	77 feet 0 inches
Over-all width	10 feet 2 inches
Height over a-c pantographs down	14 feet 8 inches
Maximum tractive effort (25% adh.)	68,500 pounds
Continuous tractive effort	24,100 pounds
Speed at continuous tractive effort	56.0 miles per hour
Continuous rated horsepower	3,600
Photograph	Figure 6

This locomotive has 22 tapping contactors and 20 a-c controller notches without intermediate buck-boost notches. No sequence relay interlocking is provided.

The application was made while the locomotive was in the shop for its first truck overhaul and traction-motor commutator turning after making a total mileage of 263,000. It was released from the shop July 16, 1941, after making various yard tests to determine that the new apparatus was functioning properly. Before assignment to any revenue trains, however, it was given a series of light runs on the main line, still further to insure that operation was correct.

In order to find out if duty on the tapping contactors was actually lessened by reason of the elimination of severe current and voltage transients in the preventive coils, new tips and arc chutes were installed on all such contactors. On subsequent periodic inspections burning and wear of tips and arc-chute sides could be compared with conditions on similar locomotives without the resistor transition.

The transformer and preventive coil connections for this locomotive are as shown in Figure 3. The control circuits in Figure 4 show the master controller and the relay circuits associated with the operation of the resistor transition switches $TC1$ and $TC2$. Figure 5 is a sequence diagram for the operation of the relays, tap switches, and resistor transition contactors for certain representative steps The following is a brief description of the operation of the control circuits which have as their function the closure of the resistor transition switch before the opening tap switch breaks its


C-530-M1 CONTROLLER
NOTCHES FOR FINGER NO.7
NOTCHES FOR ALL OTHER RIGHT-HAND FINGERS
ROFF
ROFF
ROFF
OFF
NOTCHES FOR LEFT-HAND FINGERS
OFF

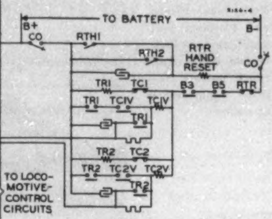


Figure 4. Control circuits for resistor contactors

*B*3, *B*5—Blower contactors
*RTH*1, *RTH*2—Thermostats
RTR—17*LV*40*H*11 relay
*TC*1, *TC*2—*ME*-432 contactors with interlocks and short travel
*TC*1*V*, *TC*2*V*—17*MVZ*12*A*1 valves with interlocks on *TC*1 and *TC*2
*TR*1, *TR*2—17*LV*28*K* relays with special slow drop-out coils

contacts, and thereafter the holding of the resistor transition switch closed until the next tap switch is closed.

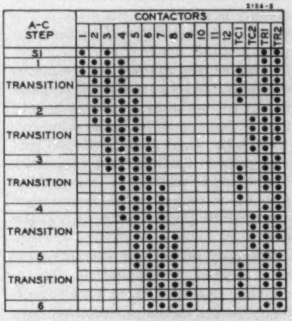
Upon the movement of the master controller from one notch to the next, a segment between notches gives an impulse of electric energy to an appropriate wire which is connected to the magnet valve of the resistor transition switch it is desired to close. The operating coil of the magnet valve is wound for approximately half the normal control voltage of the system so that its operation will be very fast. The coil is protected against the control voltage, should the controller be left midway between notches, by a resistor that is shorted out of the circuit by a relay (*TR*1 or *TR*2), whose circuits are made up before the transition is initiated, as described later. Should the controller remain between notches, the relay is de-energized by interlocks on the resistor switches, thereby inserting the protective resistance in series with the magnet valve coil. Attached to the armature of the magnet valve is an auxiliary normally open contact (*TC*1*V*, *TC*2*V*) which "makes" upon the initial travel of the armature before the latter starts to open the air valve. This contact forms a holding circuit from the battery to cause the magnet valve to continue to close, thus operating the air valve and supplying air to the cylinder of the contactor, regardless of the length of time the impulse remains on the wire from the controller. The transition switch closing its main contacts also opens the circuit to the transition relay by means of interlocks. Another set of transition relay contacts (*TR*1, *TR*2) are in series with the holding circuit to the magnet valve of the particular switch involved. The transition relay has a time delay built into it of sufficient duration to keep its contacts closed until enough time has elapsed for the transition of the main tap switches to have been completed. At the end of the prescribed time the transition relay operates, opening the holding circuit and causing the transition contactor to open. The interlocks on the transition contactor (*TC*1, *TC*2) again make up the circuit to the transition relay, which again "picks up" and is then ready for a repetition of the same cycle when it is initiated by an impulse from the master controller. There are two sets of relay equipments, one for each transition resistor.

The resistors are located over the exhaust air stream from the radiator of the main transformer, and the control is so interlocked with the blowers that it will not operate unless the blowers are running. As another safety factor a thermostat has been placed over each resistor so that, when they exceed a safe temperature, the thermostat will close, energizing the thermostat relay (*RTR*) which latches in its energized position and de-energizes the resistor transition circuits. The resistor transition equipment cannot then be operated until the resistor has cooled, and the thermostat relay has been reset by hand. The resistor thermostats are in the nature of "backup" protection, as the resistors themselves have been designed liberally enough so that, with the aid of the ventilation from the transformer radiator, it would be permissible to let them stay on the preventive-coil bus energized continuously without damage to themselves. They have a very high temperature coefficient of resistance so that, as they heat up, they inherently reduce the current flowing through them. However, in order to protect the adjacent equipment and cab structure from excessive heat, it was considered desirable to add the thermostat to each resistor.

The resistor transition switches are standard tap switches equipped with a reduced tip gap to obtain short travel and space filler to reduce the surplus volume in the cylinder. Minimizing the clearance volume of the cylinder reduces the volume of air required to start the switch moving, and reducing the switch travel cuts down the time required for closure. These refinements in the switch, combined with a magnet valve that is operated on approximately twice normal voltage, suffice to give a switch with the required operating speed.

This arrangement of the control circuits appears to have been eminently satisfactory, and where the segments in the master controller have been properly aligned so that the impulse to the magnet valve is started before its corresponding tap switch has been de-energized, no difficulty has arisen in insuring that the resistor switch is closed before the tap switch breaks its contacts. In normal notching the duration of the impulse from the master controller is of the order of $^1/_{100}$ of a second.

When the trial of resistor transition on a New Haven electric locomotive was first considered, two trains immediately came to mind as offering the best chance to observe the effects. These two trains were exceptionally heavy morning and night



A-C STEP	CONTACTORS															
	1	2	3	4	5	6	7	8	9	10	11	12	TC1	TC2	TR1	TR2
S1	●		●												●	●
1	●	●	●	●											●	●
TRANSITION	●	●	●	●									●		●	●
		●	●	●									●		●	●
		●	●	●	●								●		●	●
		●	●	●	●								●			●
2		●	●	●	●										●	●
TRANSITION		●	●	●	●									●	●	●
			●	●	●									●	●	●
			●	●	●	●								●	●	●
			●	●	●	●								●	●	
3			●	●	●	●									●	●
TRANSITION			●	●	●	●							●		●	●
				●	●	●							●		●	●
				●	●	●	●						●		●	●
				●	●	●	●						●			●
4				●	●	●	●								●	●
TRANSITION				●	●	●	●							●	●	●
					●	●	●							●	●	●
					●	●	●	●						●	●	●
					●	●	●	●						●	●	
5					●	●	●	●							●	●
TRANSITION					●	●	●	●					●		●	●
						●	●	●					●		●	●
						●	●	●	●				●		●	●
						●	●	●	●				●			●
6						●	●	●	●						●	●

Figure 5. Sequence diagram for representative notch transitions with transition resistors

commuter trains operating as local expresses between New York and New Haven; each required higher locomotive output than any other passenger train handled electrically. The electric locomotives regularly assigned were the 0361–0366 class. Short-time load demands, when operating the two trains in question, reach a peak of 7,600 horsepower, more than twice the continuous rating. These peak demands come about through frequent stops on a fast schedule for half of the run in each direction.

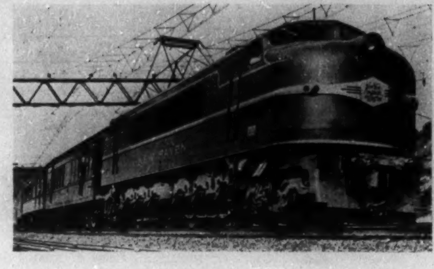


Figure 6. New York, New Haven, and Hartford Railroad Company locomotive 0361 upon which resistor transition equipment is operating

These two trains therefore were selected for the initial test runs. Other trains were handled as the locomotive was used in the regular 0361–0366 pool.

Two results of the resistor transition were immediately apparent. One was the virtual elimination of the usual barking of contactors on heavy current during acceleration. The second was the smoothing out of the tractive effort increments on acceleration, resulting in less tendency for the locomotive drivers to slip. This was remarked upon almost immediately by those operating the locomotive.

At first, the resistor transition apparatus was cut out except when a road foreman or others familiar with the equipment were riding the locomotive. In a short time, however, it became apparent that it could satisfactorily be left cut in, depending in the event of trouble on the thermostats or the instructions issued to the crews for cutting it out. The latter was a simple process, involving only the opening of a small double pole, single-throw control knife switch which made the transition contactors inoperative and returned the locomotive to normal operation.

Experience in regular operation since this time has developed practically no trouble, and the equipment has been continually in service and cut in, except for several instances of thermal relays tripping, probably due to enginemen operating in between notches on the master controller, and one period of approximately two weeks due to an error in judging the position of the timing relay contacts. The eight months of use have demonstrated that the resistor transition equipment is reliable in its operation without any more attention than is given other electric apparatus on the locomotive.

The duty on tapping contactors has been lessened. Unfortunately, due to operating conditions, there was no opportunity to determine quantitatively how much benefit was obtained, but qualitatively much benefit was observable. This means that aside from the operating advantages already described, there was a reduction in maintenance of tapping contactors, which is an important item on a-c electric locomotives. Against this, however, must be placed the additional maintenance of the resistor transition equipment, particularly the contactors and the segments and fingers on the master controllers.

Another feature of the equipment is that a certain amount of care must be taken by the engineman when operating the controller. The control-circuit scheme is based on a train of events initiated by an impulse from the controller, and subsequent impulses must be spaced far enough apart in time so that the initial train of events has been completed, or else the most desirable operation is not obtained. When accelerating from standstill, a short pause normally occurs between each two notches, and the operation is always correct, but in notching on or off too fast when up to speed, the resistor transition may be only partly effective because of lack of time between notches. This is only of interest from the standpoint of duty on preventive coils and tapping contactors; tractive effort considerations are not critical under these conditions.

Conclusions

While the resistor transition equipment was applied to a New Haven passenger locomotive as representing one of the latest type of a-c locomotives in service, it would seem that the application would find its greatest usefulness in freight locomotives where smooth-starting tractive-effort characteristics are practically a necessity, and where starting duty is both prolonged and severe.

In general, the resistor transition scheme, developed and applied as described, has given gratifying results in its apparent improvement in the acceleration of the locomotives to which it has been applied, through the eduction in the arcing duty on the tap switches as evidenced by the almost complete lack of any arc noise and the very much smoother operation of the locomotive when notching.

Although the resistor transition has a beneficial effect on tap-switch surges after closure of the higher tap switch during a transition, because of the fact that it will prevent saturation, particularly of the large preventive coil, it is still necessary to analyze carefully each case to insure that excessive tap-switch surges are not obtained with the given values of series air-core reactance in the small coils. The fact that the transition resistor is still energized after the next higher tap switch has closed means that the current flowing through the resistor is superimposed upon the small preventive-coil exciting current, with the result that the surge current may actually not be greatly different from what it would have been, had a resistor not been connected to the circuit. However, with some combinations of equipment, the presence of the resistor will result in a definite reduction in the surge, and each case must therefore be analyzed on the basis of its own merits.

A further study of the scheme and its application would seem to be warranted for heavy duty a-c freight and passenger locomotives.

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Electric Equipment for Large Electrochemical Installations

T. R. RHEA
ASSOCIATE AIEE

H. H. ZIELINSKI
MEMBER AIEE

Synopsis: The increased demand for aluminum, magnesium, chlorine, copper, and zinc for war purposes has made the electrolytic processes for these materials the largest consumers of electric energy in this country.

The most frequently encountered d-c and voltage requirements of these four principal electrolytic processes are discussed. An illustrative current-time and voltage-time characteristic curve for starting a chlorine cell line is shown, and the requirements that such characteristics impose on the electric equipment are discussed. The current-voltage characteristics of aluminum, magnesium, chlorine, and zinc cell lines are also shown.

A typical installation of conversion equipment for an electrolytic process plant is given, and the reasons for selecting the particular types of electric equipment and its physical and electrical arrangement are discussed.

As part of this discussion, there are included characteristic curves of rectifiers showing the effect of ignition control on power factor for 6-, 12-, and 36-phase combinations. A table giving a "rule-of-thumb" relationship between the number of phases and kilowatt limits which have been found in practice to provide operation reasonably free from telephone interference is included. There is also given a tabulation of phase shifter combinations, by means of which multiphase operation can be obtained with various combinations of standard six-phase rectifier transformers.

THE electrochemical industry has very recently become one of the most important consumers of electric energy in the United States with increasing production of aluminum and magnesium and, in the near future, will become an even more important user. The "Big Four" of the electrochemical industry—aluminum, magnesium, chlorine, and the sister metals, copper and zinc—have now installed over 2,500,000 kw of conversion apparatus of all types in this country, and the end of the year 1942 will see an additional million kilowatts installed in these four branches of the electrochemical industry.

This conversion equipment operates at almost 100 per cent load factor and converts power at a yearly rate of 25,000,000,000 kilowatt-hours, or nearly one quarter of the total energy used for all industrial purposes. This percentage may at the end of this year well increase to perhaps 40 per cent of the total industrial power.

There are many other electrochemical processes of less importance. Perhaps the next largest in installed conversion capacity produces fused sodium chloride with approximately 60,000 kw. Following this in importance are processes for production of hydrogen, manganese, potassium perchlorate and sodium chlorate, and other similar processes. The application problems are similar to those of the four major industries.

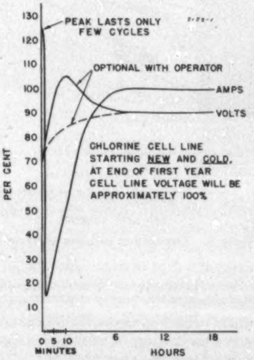

Figure 1. Chlorine cell-line characteristic

While these figures include conversion apparatus of all types (rectifiers, rotary converters, and motor generator sets), the rectifier is now the prevalent choice of the industry for converting such large amounts of power into direct current. Since 1937 very few large electrolytic installations using rotating conversion equipment were made in this country or in central Europe. During the last two or three years, the ignitron-type rectifier has been chosen for most of the installations in this country, although the multianode type of rectifier is still the choice of foreign designers.

The application engineer dealing with the electrochemical industry is at a certain disadvantage compared with application engineers for other industries, inasmuch as the chemical industry has published very little data concerning their electrical requirements. This may be attributed to the fact that electrochemical processes and techniques are often of a highly secret nature, not only in time of war, but also under peacetime conditions when the process is a matter of competitive endeavor. The electrical-application engineers would welcome more complete and precise knowledge of the electrical characteristics of the various electrolytic cells, presented not from the point of view of the chemist, but from the point of view of the electrical engineer. A few typical characteristics are discussed below, but this subject needs considerably more analysis.

Electrochemical Process Characteristics

ALUMINUM

A typical electrolytic cell line or "pot" line for the production of aluminum consists of a number of cells connected in series and is supplied with a normal direct current of 60,000 amperes at perhaps 650 volts. Such a line may require very little adjustment in d-c voltage except for minor variations of cell resistance and of the counter electromotive force of the cells. Adjustable voltage is however required, if the number of cells is decreased below normal, or for baking out a new or "green" cell line. The variation of the a-c supply voltage may require means of maintaining pot-line current and voltage at a constant value. Means for accomplishing such voltage regulation will be discussed later. Under ordinary operating conditions, after such a line is once put into operation, it will run for months or even years, with a voltage and current variation of not more than five or ten per cent.

Another typical aluminum cell line with cells of the so-called Soderberg type has a normal capacity of 32,000 amperes at 650 volts. With this type of cell more frequent adjustment in the d-c voltage is found very useful for the purpose of starting "green" cells or baking out cell linings.

MAGNESIUM

As far as the application of the electrical conversion equipment is concerned, a magnesium cell-line is very similar to the

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T. R. RHEA and H. H. ZIELINSKI are both engineers in the chemical section of the industrial engineering department of General Electric Company, Schenectady, N. Y.

aluminum line. The magnesium cell line may have a normal d-c rating of 60,000 amperes at 600 volts, although a large installation is now in the process of erection which, based on European practice, will use a large number of 20,000-ampere 350-volt cell lines. Another installation is using 32,000-ampere 600-volt lines.

Wider adjustment in the d-c voltage is required on a magnesium cell line as compared with one for aluminum, because a new cell line is usually started with only a few cells and is built up over a period of several weeks by adding new cells in series. Both in this country and abroad, installations have been made with a wide range of adjustable d-c voltage, even though normal operation is at practically constant voltage.

CHLORINE

Typical chlorine cell lines use from 7,500 to 10,000 amperes at 500 to 700 volts, depending upon the type of cell used or the number of cells connected in series. Some cells are designed for 1,800 to 2,000 amperes, and four or five of these may be connected in parallel on a common d-c bus. Usually each 7,500- or 10,000-ampere cell line is supplied from one rectifier unit, without interconnection with other cell lines on the d-c side except in case of emergency. This arrangement provides flexibility to meet the day-to-day variations in cell-line requirements. Such lines are operated at essentially constant d-c voltage. Figure 1 shows typical voltage- and current-time curves for a chlorine line which is placed into operation for the first time.

It should be pointed out that these curves are not based on tests, as the authors might desire, but on previously published information and on theoretical considerations.

The Figure 1 shows an initial instantaneous peak rising to perhaps 125 per cent after the cell line is first thrown on full voltage and before the counter electromotive force or battery action of the cell is established, a phenomenon which happens within a few cycles. The sodium-chloride solution initially has a high resistance, while it is cold and dilute. For the next six or eight hours the current rises as the resistance decreases with increasing concentration of the solution.

The initial voltage reduction to 60 or 70 per cent required of the conversion equipment to keep down the initial inrush of current is well within the capability of ignition control of a rectifier. Minor adjustments in the voltage required from day to day are also usually made by means of ignition control. Larger d-c voltage changes may be obtained with taps on the rectifier transformer as will be discussed later.

It should be noted that the characteristics of the individual cells are somewhat obscured if a number of lines are operated in parallel.

COPPER AND ZINC

A typical copper or zinc cell line is quite different from the chlorine line as regards the conversion equipment. A zinc line may require about 10,000 amperes at 500 to 600 volts, depending on the number of cells connected in series. Foreign installations have been made using voltages up to 800 or 850 volts. For normal operation the cell line is operated at substantially constant d-c voltage. Usual applications require some transformer taps for d-c voltage adjustment on a cell line as the number of cells in series are changed.

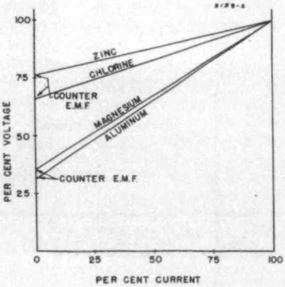

Figure 2. Electrolytic cell-line characteristic

Since, however, the electrolytic production of copper or zinc is a batch process, where a cell is periodically taken out of service for the purpose of stripping copper or zinc from the electrodes, the cell-line voltage must at the same time be lowered to such a low value that the disconnection of one cell may be made at slightly above zero current. The voltage reduction required to accomplish this is approximately 25 per cent, usually obtained by ignition retard.

Figure 2 shows a typical voltage-current characteristic of a zinc line. It is easily understood that the removal of one cell from the circuit by means of short-circuiting this cell should, from the standpoint of operation, be done as quickly as possible in order to revert to full current and full production without too much loss of time.

It is important that some cell current be maintained, at least at a low value, at all times to prevent the copper or zinc already deposited from going back into solution.

Figure 2 also shows cell-line characteristics for other cell lines. Chlorine has a counter electromotive force of approximately 66 per cent. For alumina dissolved in cryolite, the authors estimate a counter electromotive force of about 30 per cent based on the theoretical voltage of dissociation of 1.38 volts per cell and the approximate total voltage drop of 5.9 volts per cell. The counter electromotive force of fused magnesium chloride based on similar data is shown to be approximately 35 per cent. The authors have no accurate data on the length of time for which this counter electromotive force persists after an interruption in the current supply.

Some cell lines have their mid-point grounded through a resistor for the purpose of lowering the cell potential to ground. Such installations usually have a relay and alarm system in the ground connection to notify the operator of accidental grounds on the cell circuit. Grounding the mid-point of the cell line seems to be common practice abroad but is only occasionally used in this country. The disadvantages of its use are slightly more electrical losses to ground because of current bypassing some cells.

Selection of Electric Equipment for Electrochemical Processes

RECTIFIERS VERSUS ROTATING APPARATUS

The mercury-arc rectifier has become so well established in the electrochemical field that it is hardly necessary to dwell at great lengths on the advantages it has over rotating apparatus. However, it is desirable to review very briefly some of these advantages.

The space requirements for the installation of rectifiers are quite moderate, and no reinforced foundations are needed such as are required for rotating apparatus.

In the larger units at 600 volts direct current, the rectifier-conversion equipment may be installed for approximately $35 to $40 per kw. This includes the incoming 13.8 kv 60-cycle a-c switchgear, autotransformers, rectifier apparatus, cable and copper bus bars for the main d-c bus, building, and installation labor. This approximate installation cost figure would cover a layout as shown in Figures 5A and 5B.

An especially important consideration in wartime is the fact that the amount of particularly scarce materials, such as copper and steel, is considerably less in a rectifier installation than in rotating apparatus. Incidentally, it is for this reason that the rectifiers have for some time been more widely used in Europe than they have been in this country.

The efficiency of the modern ignitron-rectifier equipment in the voltage range around 600 volts is in excess of 95 per cent, including the transformer losses and the power consumed by the rectifier auxiliary equipment, such as pumps and ignition apparatus.

The efficiency of the conversion equipment is of special importance in the electrochemical industry, because these processes operate at close to unity load factor, and the electric energy which the conversion equipment supplies over a year's time may cost as much or more than the cost of the conversion equipment itself.

An operating advantage of the rectifier installation over a rotating-apparatus installation is particularly emphasized by the problem of restarting a large-capacity cell line after a complete power failure or shutdown. Assuming that such a cell line is supplied from six 10,000-ampere rotary converters, it is necessary after a power failure to restart and resynchronize all of the rotary converters with the d-c breakers open. Since it is impossible to close all of the d-c breakers simultaneously, one of the rotary converters will assume the entire load, its breaker will trip off on overload, and succeeding breakers will follow.

Table I. Phase Shifters Required for Multiphase Operation

Rectifier Transformers		Phase-Shifting Auto-transformers Required	Number of Phases Operation
Number of 6-Phase Units	High-Voltage Connections		
1	Delta or wye	None	6
2	1 delta 1 wye	None	12
3	2 delta 1 wye	2 10-degree	18
4	2 delta 2 wye	2 15-degree	24
5	3 delta 2 wye	2 12-degree 2 6-degree	30
6	3 delta 3 wye	4 10-degree	36
7	4 delta 3 wye	2 4.29-degree 2 8.57-degree 2 12.86-degree	42
8	4 delta 4 wye	2 15-degree 4 $7^1/_2$-degree	48

To meet this problem with rotary converters, a large cell-line breaker is resorted to, which may be rated 60,000 amperes continuously. By using this rather unwieldy device, all of the rotary converters may be paralleled on the d-c bus with this cell-line breaker open and then the breaker closed.

A rectifier application easily circumvents this problem. With rectifiers, all d-c and a-c breakers are closed with the ignition circuit de-energized. The pot-room load is picked up by energizing the ignition circuits of all rectifiers simultaneously. No cell-line breaker is needed. It is obvious that the time element, in reestablishing service after a power interruption, is very small indeed for a rectifier station as compared with a rotary converter installation.

Rectifiers provide a certain amount of d-c voltage adjustment by means of ignition retard, which allows quick and easy compensation for changes in the a-c supply voltage or for changes in the cell-line conditions; whereas with rotary converters, the use of boosters must be resorted to. It should be pointed out, however, that the continuous use of ignition control is not resorted to in practice beyond four or five per cent, since a wider use is accompanied by lowering of the power factor and an increase in the tendency of the rectifier to arc back.

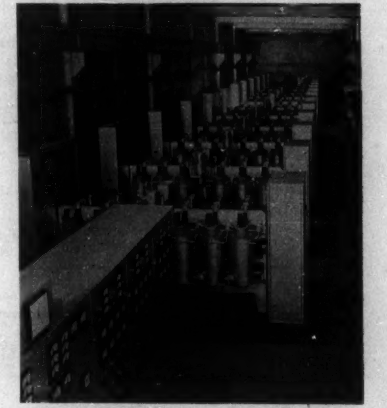
Figure 3. Large rectifier substation

All in all, it may be fairly stated that the many advantages of rectifiers over rotating apparatus makes their selection for a large electrochemical installation a foregone conclusion.

A considerable advance in the art of rectifier design is represented by the single-anode rectifier tank. As illustrated in Figure 3, these single-anode tanks or ignitrons are assembled in groups of 12 to supply 5,000 cathode amperes. This ignitron design has resulted in an increase of efficiency by a reduction of the arc drop to 20 volts or less; which, for such installations, means an over-all efficiency of about 95 per cent. The lower arc drop and the high efficiency result from the fact that the anode encased in a single tank can be placed closer to the mercury-pool cathode.

Because each anode is confined in a relatively small tank compared to the large multianode arrangement, with the ignitron it is possible to make much tighter vacuum seals, and therefore better to maintain the vacuum. Furthermore, if a tank leaks, or trouble develops with one tank, it is a relatively simple matter to replace this tank, whereas it is much more serious when a large multianode tank has to be taken out of service. For these reasons the single-anode ignitron rectifier is now the choice of the electrochemical industry, foreign countries excepted.

SELECTION OF RECTIFIERS

Figure 4 shows the general arrangement of an electric system for three 60,000-ampere 600-volt electrolytic cell lines. While this sketch does not represent in detail an actual installation, nevertheless all of the features shown are commonly installed, and the sketch will serve as a basis for discussion of the various reasons why the equipment shown was selected.

Each 60,000-ampere cell line operates as a unit. Therefore, the largest available size rectifier units are chosen, in order to obtain maximum economy in first cost. The rectifier transformers are selected so as to supply two rectifier units from one transformer, or a total of 10,000 d-c cathode amperes. This is about the upper limit. The maximum of 10,000 cathode amperes d-c per rectifier transformer has been found both in this country and abroad to be a conservative upper limit because of large copper sizes to be handled, stresses in the transformer during arc-back, and for other design reasons. Thus the logical choice of rectifier units for this case is the 5,000-ampere, 12-anode ignitron, similar to those shown in Figure 3.

In view of its relative simplicity of design and sturdiness of construction, the six-phase double-wye or quadruple-wye secondary connection is most widely used, both here and in foreign countries. Some 12-phase rectifier transformers have, of course, been used for small installations. As shown on Figure 4, the transformer selected has quadruple-wye secondary connections. Two wye, with their interphase winding, supply one 5,000-ampere rectifier, and two duplicate wye, with interphase winding, supply the other rectifier.

Number of Phases

As the six-phase, 10,000-ampere cathode d-c rectifier transformer is selected, there are now required six such units to supply the 60,000-ampere pot line. This is a total of 36,000 d-c kw. Investigations of rectifier wave shape and its influence on telephone communications indicate that the telephone influence is, to a very large extent, dependent on the characteristics and the total installed capacity of the a-c supply system, to which the rectifier installation is connected, and also on the number of phases selected. It has been fairly well established that such an amount of power operating at six phase would cause widespread telephone interference, if no co-ordinative measures are taken. Therefore, the number of phases should be selected with reference to the ratio of the total rectifier capacity and the total system capacity supplying the rectifier installation.

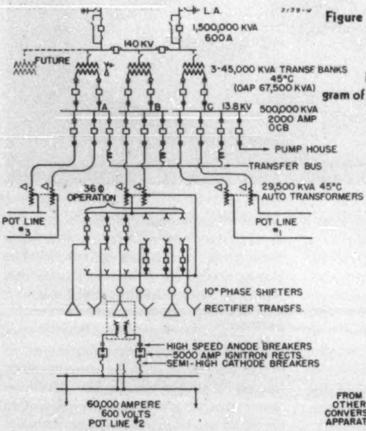

Figure 4 (left). Electric system for large electrochemical installation

Figure 4A (below). Simplified diagram of rectifier with arc back (X) on one anode

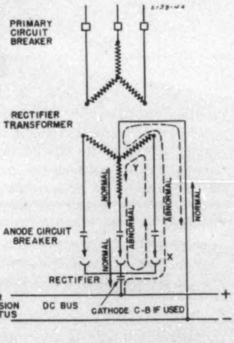

Table II contains a "rule of thumb" of rectifier kilowatt ratings and number of phases selected for 60-cycle rectifier operation. This table is, of course, subject to interpretation and should be used with extreme caution, since, for instance, there have been some cases of telephone interference caused by 200-kw rectifiers at the end of a long feeder. It is, however, generally applicable to rectifier installations on relatively large power systems; and so far as the authors know, there have been no cases of telephone interference caused by rectifiers in industrial plants within the limits shown in Table II.

The total kilowatts and the number of rectifier transformers on each pot line indicate in accordance with Tables I and II that the installation here discussed should use 36-phase relationship. Since there are six six-phase units, the rectifier tubes will fire in 36-phase relationship, if the primary voltages are shifted by ten degrees for each rectifier unit.

Table I gives a convenient reference for the number of phase shifters and the degree of phase shift required for combinations of six-phase transformers for multiphase operation. The use of 18-phase operation is inadvisable, because its principal harmonics are the 17th and 19th, having the greatest audio-sensitivity. The 36-phase relationship eliminates theoretically all harmonics below the 35th.

It has generally been found impractical to make sufficiently accurate estimates of the telephone influence in advance of a rectifier installation, because of the many variables and unknowns, which allow only approximate estimates of the telephone influence. Therefore, it has become the practice, supported by experience, to evaluate only approximately the telephone influence and choose the number of phases in some such "rule-of-thumb" fashion as shown in Table II.

If it should ever become necessary in a specific case, remedies such as increasing the number of phases or a-c line filters can be applied after the installation has been made.

It should be clearly borne in mind that the foregoing discussion refers to a 60-cycle supply. Since telephone influence is sensitive to certain harmonics, Table II does not apply to 25-cycle systems. An installation with the same number of kilowatts should have a little over double the number of phases for a 25-cycle system as it has for a 60-cycle system.

In addition to the reduction in telephone influence, the multiphase operation offers definite economic advantages, since the increase in the number of phases improves the power factor as indicated in Figure 6. This figure is based on design data of an actual installation whose basic unit is the six-phase transformer. It can be seen that when the number of phases is raised from 6 to 36, the power factor improves from 92 to 95 at zero ignition retard. The increase in the number of phases also reduces the losses.

Instead of using basic six-phase transformer units, it is possible to use basic 12-phase transformer units. Such 12-phase transformers are much more commonly used in Europe than in this country, although they may be considered practical devices and have a definite field of application for smaller installations. All interphase transformers, indicated in Figure 4, are usually built in the common transformer tank.

The introduction of the phase-shifting autotransformer, as shown in Figure 4, results in some additional reactance in the circuit which would cause it to take slightly less load than a circuit without phase shifter. However, such unequal load distribution is easily compensated by a slight amount of ignition retard on the unit without phase shifter, and it is not considered necessary to insert any external reactance, although this is the practice in foreign countries.

The rating of rectifier transformers for chemical installations is on a basis of 45 degrees centigrade temperature rise for continuous full load. This conservative rating for the transformers has universal acceptance in this country for electrochemical service, because the load factor is almost 100 per cent, and such installations may run for years under this full load condition. The 45 degrees centigrade temperature rise greatly reduces the maintenance on the oil and also provides some inherent spare capacity for emergencies, when it may be necessary to run the cell line on a reduced number of rectifiers, while one unit is undergoing maintenance.

With conservative design margins in both transformers and rectifiers, and with the high degree of reliability of modern rectifiers, such an installation as shown in Figure 4 would not normally require additional spare capacity. If the cell line is run with five instead of six units the overload is 20 per cent. It is generally practical, although not always desirable, to reduce the cell-line load while the single unit is off the line for maintenance. If it is essential to run the cell line with 100 per cent current at all times, a 10,000-ampere spare unit should be provided.

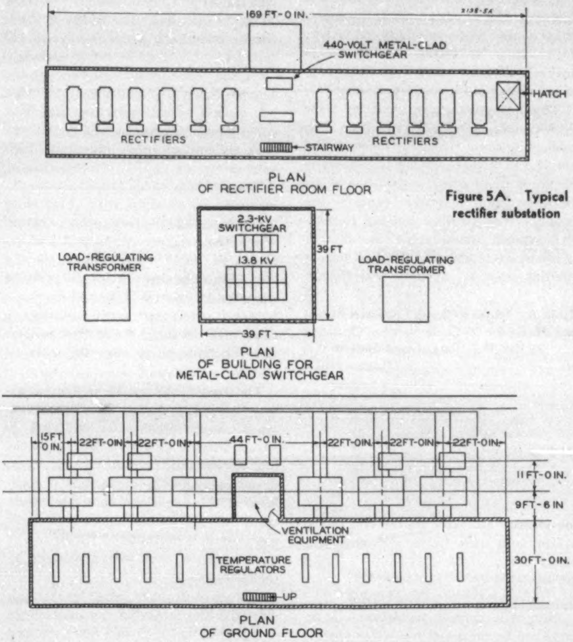
169 FT-0 IN.
2138-5A
440-VOLT METAL-CLAD SWITCHGEAR
HATCH
RECTIFIERS
STAIRWAY
RECTIFIERS
PLAN
OF RECTIFIER ROOM FLOOR
2.3-KV SWITCHGEAR
13.8 KV
39 FT
39 FT
LOAD-REGULATING TRANSFORMER
LOAD-REGULATING TRANSFORMER
PLAN
OF BUILDING FOR
METAL-CLAD SWITCHGEAR
15 FT 0 IN.
22FT-0IN.
22FT-0IN.
44 FT-0 IN.
22FT-0IN.
22FT-0IN.
22FT-0IN.
11 FT-0 IN.
9FT-6 IN
30FT-0IN.
VENTILATION EQUIPMENT
TEMPERATURE REGULATORS
UP
PLAN
OF GROUND FLOOR

Figure 5A. Typical rectifier substation

D-C Voltage Control

The installation illustrated by Figure 4 is assumed to require operation at d-c voltages as low as 100 for starting up a new cell line with only a few cells. Furthermore, the a-c voltage supply is assumed to vary five or six per cent from day to day, a condition which would cause the cell-line current to vary a slightly greater percentage. Both these conditions are frequently met in actual operation.

It is quite inadvisable to reduce the voltage from 600 down to 100 volts by ignition control. Figure 6 indicates that voltage reduction by ignition control, even as little as 16 per cent, reduces the power factor from 95 per cent to 82 per cent. Furthermore, operation of mercury-arc rectifiers with a large amount of ignition retard, for a long period of time, will increase the frequency of arc-backs. Experienced rectifier operators, therefore, use as little ignition retard as possible, and the application of ignition control should be limited to such cases where the voltage reduction is of only temporary character, as, for instance, when starting a cell line, where ignition retard of 30 to 40 per cent is permissible for a few minutes.

The other method of d-c voltage control, which uses a change of rectifier-transformer voltage, is not connected with these drawbacks. If only five or ten per cent change of d-c voltage is required over a long period of time, as would be the case for a chlorine line, this change should be obtained by taps in the rectifier-transformer high-voltage winding. These taps would be changed with the transformer de-energized, as such changes are sufficiently infrequent to make an interruption for this purpose acceptable.

However, with the change of d-c voltage required in the case of the installation here discussed, the only practical way of accomplishing this change of a-c voltage is through the use of an autotransformer with no-load taps. In this particular case the autotransformer would be supplied with seven no-load taps from 100 to 600 d-c volts rectifier output. Since these rather large changes in d-c voltage are required only once in several weeks, as a number of cells are added or removed from the circuit, changing these taps with the cell line de-energized imposes no serious hardship on the cell-room operator.

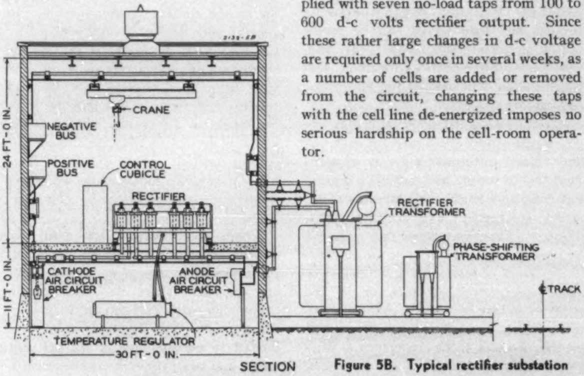
2138-5B
CRANE
NEGATIVE BUS
POSITIVE BUS
CONTROL CUBICLE
RECTIFIER
24 FT-0 IN.
11 FT-0 IN.
CATHODE AIR CIRCUIT BREAKER
ANODE AIR CIRCUIT BREAKER
TEMPERATURE REGULATOR
30 FT-0 IN.
SECTION
RECTIFIER TRANSFORMER
PHASE-SHIFTING TRANSFORMER
℄ TRACK

Figure 5B. Typical rectifier substation

In addition to the no-load taps, the autotransformer should in this case have load-ratio control to provide a plus or minus 60 d-c volts adjustment within the range of any of the seven taps of the no-load tap changer. This results in extremely flexible control and permits the operator to compensate for fluctuations in the incoming a-c power supply and also to bridge the gap between the no-load taps. The total range of 120 d-c volts load ratio control has been covered with 16 and 32 steps. Load-ratio control is also quite useful to the operator in maintaining constant cell-line current, as cell conditions change, and operators, who have this tool available, attest to its usefulness for this purpose.

The autotransformer with these features is also a tool for reducing the d-c voltage for the purpose of baking out a "green" cell line or for short-circuiting the d-c bus and simultaneously baking out all rectifier units themselves when they are first installed.

An exactly similar result can be obtained by the use of an induction voltage regulator when due attention is paid to the shifting of the primary voltage supply with respect to the ignition-system voltage. Induction voltage regulators are somewhat more expensive but are frequently used abroad.

With such flexible tools for voltage control available, it is not necessary to use any considerable amount of ignition control, except to balance the load between the various rectifier units.

The load-ratio control feature of the autotransformer makes possible automatic kilowatt input control for the pot line. This feature is not required by the cell-room operator, who will generally take all the kilowatts he can obtain, but by consideration of power contract formulas. By using constant kilowatt control, the load factor can be maintained at better than 98 per cent, and, with some power contract formulas, this feature alone will pay for the autotransformer very quickly.

It will be noted from Figure 4 that two autotransformers are chosen for each pot line. Each autotransformer is of sufficient size to supply with normal temperature rise four rectifier transformers and, in an emergency, five rectifier transformers. This spare capacity is required for reliability. When one autotransformer is out of service for maintenance of load-ratio control contacts, or in the event of failure of a cable connection to the main bus, the cell line can be operated for the duration of the emergency at five-sixths capacity.

Autotransformers, which are usually wye-connected, often have a tertiary stabilizing delta winding, as indicated in Figure 4. This tertiary winding will be of a certain minimum size as dictated by design consideration. If designed for a standard voltage commonly used to supply auxiliary plant load, the tertiary winding can be made to serve a double purpose. In a particular case, each tertiary winding was designed for 3,000 kva at 2,400 volts. This provides a very economical source of plant power, since no additional 13.8-kv high-voltage switching is required to make it available, and very little additional copper in the tertiary winding.

The autotransformer winding, being 13.8-kv wye-connected, provides a convenient means of grounding the neutral of the 13.8-kv system. While not shown by Figure 4, in an actual installation all six of the autotransformer neutrals are brought through disconnecting switches to a common grounding bus; and this bus is connected to ground through a current limiting resistor. Such neutral grounding greatly facilitates the relaying of the system and adds to its reliability and safety.

Table II. "Rule-of-Thumb" Kilowatt Ratings and Phases for 60-Cycle Rectifier Operation on Relatively Large Power Systems

Kilowatts	Phases
1,000– 2,000	6
2,000– 6,000	12
6,000–20,000	24
20,000–30,000	30
30,000–50,000	36
50,000 and up	48–72

AUTOMATIC CURRENT REGULATION

Automatic current regulation can be furnished by means of regulators operating on the rectifier ignition circuits. In general, such devices are not required for electrochemical installations, because the conditions are sufficiently constant, and they have been applied only to meet special conditions. They may be justified when the a-c system voltage varies, and it is desired to keep the d-c pot-line current constant. They are also of value where the rectifier must parallel with other conversion units of dissimilar characteristics.

Where such devices are used, the amount of ignition control is limited, and the undesirable effects on power factor are avoided. Where they are used in conjunction with load-ratio-control autotransformers, the ignition control is limited to perhaps five or six per cent range, and when these limits are reached, the load-ratio-control autotransformer taps are automatically changed by the regulator.

D-C SWITCHGEAR

High-speed d-c switching, particularly where more than one rectifier supplies the same load, is considered a highly desirable application requirement. By high-speed switching is meant a d-c circuit interrupting device which will begin to open in one-half cycle or less, and will therefore limit the peak current, and will completely interrupt it in approximately one cycle. The primary oil switch in the supply circuit to the rectifier transformer will have a normal operating time from six to eight cycles and would permit arc-back currents to reach undesirably high values.

Two methods of high-speed d-c switching are in common use: The older method uses a high-speed cathode breaker which trips on reverse current and removes the faulty rectifier from the d-c bus. Since the short circuit still exists on the secondary circuit of the rectifier transformer, it is necessary to attempt either to extinguish the arc in the rectifier by ignition blocking, or to open the main oil switch.

The second method of applying high-speed switching is the use of the high-speed anode breaker as indicated in Figure 4 and Figure 4A. The use of the high-speed anode breaker is an important development in the application of rectifiers, and, while there have been some papers written on this subject, the authors feel that it is desirable to review briefly the reasons for their wide acceptance by the operators.

The use of an anode circuit breaker provides the best protection not only for the rectifier but for the process being supplied by direct current, since its use gives a greater guarantee of continuity of service. The high-speed anode circuit breaker has one pole placed in each anode circuit and replaces a single-pole high-speed cathode breaker.

Referring to Figure 4A, normal current flow at a given moment is indicated by the solid-line arrows. When an arc-back occurs, one of the anodes (the right-hand one, not supposed to be carrying current at that instant) allows current to flow in the reverse direction. It will be seen that an arc back therefore causes two short circuits, one on the d-c bus indicated by the "abnormal" dotted arrow marked *X*, and the other on the a-c system indicated by the "abnormal" dotted arrow *Y*.

It is desirable to remove these short circuits quickly. An interrupter in the anode circuits removes the short circuits from the a-c and the d-c systems simultaneously.

The high-speed anode circuit breaker opens in response to the reverse direction of current in the anode. The current peak is limited to one-half cycle or less and the current completely interrupted in approximately one cycle.

In comparison, the high-speed cathode circuit breaker would only interrupt the feedback from the d-c bus into the faulty anode, but some additional means has to be used to remove the short circuit from the a-c system, either by opening the primary oil circuit breaker or by attempting to suppress the arc in the rectifier by ignition control, while the use of the high-speed anode circuit breaker goes directly to the source of the trouble and quickly and promptly interrupts the anode circuit. It should be noted that the use of an anode circuit breaker is not a substitute for a primary oil circuit breaker, but only a supplement of it. Transformer fault protection and normal switching generally require the use of primary circuit breakers when such large transformer unit is connected to an a-c bus.

Some of the specific advantages of high-speed anode circuit breakers, particularly in those cases where more than one rectifier supplies a common d-c bus, are:

(*a*). The transformer is protected against the high currents which may result when a rectifier arcs back. Such high currents impose very severe stresses on the transformer.

(*b*). The other rectifiers are protected against supplying large d-c currents to the faulty rectifier. Such protection against heavy currents reduces materially the tendency for the normal rectifiers to arc back "sympathetically."

(*c*). The remaining normal anodes on the same rectifier are not called upon to supply heavy currents to the faulty anode, and their tendency to arc back, either immediately or in the future, is greatly reduced. Arc-backs beget arc-backs.

(*d*). The use of anode breakers, with high-speed reverse-current trip on each pole, greatly simplifies the protective control system. There are no additional relays nor wiring to go wrong.

(*e*). When one rectifier transformer supplies two rectifiers, as is usually the case in electrochemical work, the use of high-speed anode circuit breakers will remove one of them on arc-back and permit the other to continue in uninterrupted service.

The principle of the high-speed anode circuit breaker has been well proven in service. It is so fundamentally a correct device for the protection of mercury-arc rectifiers that future electrochemical application without them will be rare indeed.

It will be noted from Figures 4 and 4A that a semihigh-speed cathode breaker is indicated. These semihigh-speed cathode breakers provide overload protection in the forward direction, since high-speed anode breakers which trip on reverse current are not particularly well suited to trip on straight overloads in the normal direction. The semihigh-speed cathode-breaker overload trip is given a slight time delay to permit the breaker to stay in until the high-speed anode breaker has cleared the arc-back. The semihigh-speed cathode breaker is also useful for normal emergency trip of the entire cell line.

Station Control

The station operator's control devices for controlling a station similar to that shown in Figures 3 and 4 are concentrated on an operator's control board.

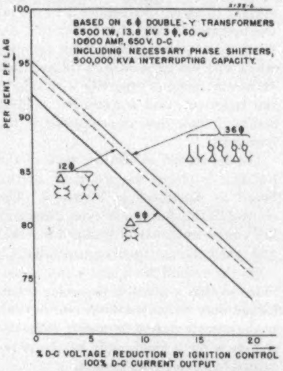

Figure 6. Power-factor characteristics for varying amount of grid control

The operator's functions in energizing such a cell line or taking it off the line are relatively simple. For energizing the cell line, the auxiliary circuits for water and vacuum pumps are energized, and then the a-c circuit breakers are closed. The anode and cathode breakers are next closed with the ignition circuit de-energized. The ignition circuit is then energized, permitting the rectifiers to take load. As previously mentioned, the d-c breakers cannot be closed with the ignition circuit energized, because the first breaker closed will trip again due to overload.

For some types of cell lines, and under some conditions, the operator may wish to start the cell line with the lowest possible d-c voltage permitted by ignition control, and energize the ignition circuit only after it has been retarded. All of these operations are performed at the operator's control board shown in Figure 3.

A cell line can be de-energized by simply tripping the cathode breakers. If one of the cathode breakers should lag materially behind the others, it will be forced to interrupt the entire cell-line current which represents an exceedingly heavy duty. If the operator desires and has the time, he may reduce the current interrupted by these breakers by first reducing the voltage and current on the entire cell line through the ignition control.

The power to cell lines should not be interrupted by de-energizing the ignition circuit. If this is attempted, the entire cell-line current somewhat attenuated will be finally transferred to a single anode. This is a serious overload for the particular anode and may cause serious damage to it. Even if all the cathode breakers are tripped simultaneously, they will open at slightly different moments. However, the magnetic energy stored in the cell-line circuit is dissipated quickly and without harm in the arc chutes of the cathode breaker.

Another method of de-energizing the cell line is to open the main a-c feeder breakers, a method which would correspond to the case of a complete power failure. Except for increased maintenance on the breaker, such interruption will not have any particularly bad effects, because the energy flow from the system is interrupted, and the current is transferred finally to the last conducting anode, only after it has died down to a relatively low and safe value.

Such stations as those illustrated by Figures 3 and 4 are continuously attended. The operations under normal circumstances are relatively simple and are usually confined to the observation of the instruments and the rectifiers. Where no automatic current nor voltage regulation is used, it is necessary to adjust the voltage from time to time by operating the tap changer or load-ratio control of the transformer, and perhaps occasionally to balance the load between the rectifiers by means of a slight amount of ignition control.

Temperature-Regulating Equipment

The losses in the rectifiers are usually removed by water cooling. There are a few installations of air-cooled rectifiers in Europe but no important installations in this country. In water-cooled rectifiers, the coolant is usually recirculated through some form of heat exchanger. Where the raw water supply is sufficiently cool, clean, and abundant, these heat exchangers are generally water-to-water. Where the water supply is less favorable, these heat exchangers may be water-to-air.

The temperature-regulating heat exchangers as shown in the typical station layout of Figure 5 are usually located directly beneath the rectifier and the coolant continuously circulated between the rectifier and the heat exchanger. For most efficient rectifier operation, the temperature of this coolant is maintained at approximately 47 degrees centigrade. Thermostatically controlled valves on the heat exchanger maintain this temperature by automatically controlling the amount of raw cooling water flowing through the heat exchanger.

The recirculated liquid coolant is usually distilled water and may be treated with sodium dichromate to inhibit corrosion. These water-to-water heat exchangers can be manufactured for cooling water temperatures as high as 35 degrees centigrade (95 Fahrenheit). For a 36,000-kw rectifier installation considered in Figure 4, there would be required 420 gallons per minute of 85 degrees Fahrenheit raw cooling water.

In water-to-air heat exchangers, the coolant is also continuously circulated between the heat exchanger and the rectifier. A by-pass valve, thermostatically controlled, will pass more or less of the recirculated coolant through the heat exchanger while the fans run continuously.

Heat exchangers, when sodium-dichromate corrosion inhibitor is used, are at cathode potential and are therefore usually insulated from ground. Rubber hose is used to connect it to the rectifier and to the raw water supply.

The use of water-to-air heat exchangers, because of blower losses, may lower the station efficiency perhaps one tenth of one per cent, but since these blowers operate at full speed only a few months in the summer time, this is not an important consideration. The choice of water-to-water or water-to-air heat exchangers should only be considered from the standpoint of expense and quality of the raw water supply available. The installed cost of the water-to-water heat exchangers is considerably less than water-to-air, if the raw water is delivered to the building at no cost.

STATION ARRANGEMENT

A typical station arrangement for a large electrochemical installation is shown in Figures 5A and 5B.

The transformers are installed outdoors and are usually of the oil-insulated self-cooled type.

The temperature-regulating heat exchangers are installed on the first floor of the main rectifier building directly below each of the rectifiers to which they are connected. For such large installations this arrangement gives the best economy of building space.

The rectifiers are mounted on the second floor at about the same elevation as the transformer secondary connections. Bus connections to the individual rectifier anodes are made preferably below the rectifier assembly. The cathode connections to the cathode breakers are also made from below. In this particular illustration, the neutral or negative connection from the interphase transformer is carried across the station directly below the roof.

The d-c breakers may be installed either on the ground floor or on the rectifier floor. An argument in favor of installing them on the rectifier floor is that the operator is then able to see that the breakers on a particular rectifier are open when work has to be done on this rectifier. However, space is generally available in the basement, and a narrower building results if these devices are placed in the basement.

On the second or rectifier floor of the building, is placed the operators' control board as illustrated in Figure 3. The control boards for two or even three pot-line equipments may be grouped together and under the control of one operator.

On the ground floor, aisle space is provided so that a portable degassing transformer may be moved from one rectifier to the other for initial degassing, or for degassing after a single unit has been returned to service after repairs.

The control cubicles containing the control devices for the pump and other auxiliaries for each rectifier are installed on the end of each rectifier.

The a-c switchgear in this particular illustration of Figure 5A is shown located in a separate building with the two autotransformers adjacent thereto.

It should be noted that this installation is typical of many electrochemical installations in that all of the 13.8-kv power cable enters the transformers and switchgear through pot heads, and there are no exposed 13.8-kv bushings in the system. This protection is a matter of some importance to those electrochemical installations where corrosive atmospheres or conducting dust is present.

A-C POWER SUPPLY

Reliability of power supply for electrochemical installations is considered essential, since loss of power even for a short time may be very costly, in addition to being naturally objectionable from the standpoint of the operator. In the case of chlorine cell lines, loss of power requires the immediate stoppage of auxiliary gas pumps and of the feed to the cells; otherwise chlorine gas may be liberated in the cell room with undesirable effects on the operators and the surrounding neighborhood. With aluminum and magnesium plants, the loss of power for an hour or more may cause the cells to freeze. In general, loss of power may mean an interruption of production for a much longer period than the duration of such power interruption and may require a costly and slow restarting of the process.

Therefore, the electric equipment and its power supply, which are the heart of this process, must be of the most reliable type and must include such spare capacity as may be indicated by experience. Even the best or most efficient or most reliable electric equipment will have an installed cost not usually exceeding 30 per cent of the total installed cost of the entire electrolytic plant which it supplies.

As shown in Figure 4, two autotransformers supply each pot line so that the reliability of power supply is insured, at least on a reduced basis if one autotransformer or its cable feed is lost. Each autotransformer has a continuous rating of two thirds of a pot line.

By proper separation of circuits, even these large electrochemical installations may be kept within a 500,000-kva short-circuit capacity breaker. Normal distribution voltage in well over 90 per cent of the electrochemical installations in the United States is 13.8 kv.

The double bus systems shown on the output side of the autotransformers permit any of the six main rectifier transformers to be connected to either of the autotransformers. While not indicated in Figure 4, the double bus system permits the autotransformers to be connected to different bus sections with no interconnections between feeders. This would be particularly important, if each autotransformer is fed from a different bus section in the powerhouse, and these powerhouse bus sections are tied together through a synchronizing or transfer bus. The fact that there is no interconnection on the output side of the autotransformers avoids any possibility of by-passing the synchronizing bus.

The outdoor substation supplying the three pot lines should logically have 45-degree transformers, to be consistent with the rectifier transformer practice. In the arrangement shown in Figure 4, the main stepdown transformers are provided with fans, so that, in the event of the loss of one bank, the fans can be started on the remaining two banks, and power transferred to them through the transfer bus.

The transfer bus is normally disconnected and the bus sections A, B, and C are independent, thus localizing the effects of short circuits and other disturbances.

Because of the current-carrying capacity required on the main outdoor transformer bank, Figure 4 indicates that the delta for the secondary windings of these transformers is made on the metal-clad bus by using two 2,000-ampere, triple-pole breakers. This is a method frequently resorted to in order to obtain the necessary current-carrying capacity in metal-clad switchgear.

The system should, of course, be supplied with two-incoming high-voltage lines, in accordance with the standards of reliability required. Since interruptions on the incoming high-voltage lines are the most frequent, the cost of high-voltage circuit breakers and protective relaying is justified.

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Factors Affecting the Mechanical Deterioration of Cellulose Insulation

F. M. CLARK
ASSOCIATE AIEE

Synopsis: The rate of mechanical deterioration of cellulose insulation is dependent on the conditions of its use. Those factors of major importance are the temperature applied and the presence of oxygen and moisture. Moisture even in small amounts greatly affects the mechanical stability of the cellulose insulation. In general, the mechanical life of the insulation is reduced by half for each doubling in water content. Deterioration promoted by oxidation is most effective at temperatures below 120 degrees centigrade and is accelerated by the presence of moisture.

The rate of deterioration for substantially dry insulation at temperatures above 120 degrees centigrade is dependent upon its previous history. Intermittent exposure to high temperature effects are additive. The "eight-degree-centigrade rule," indicative that the rate of mechanical deterioration is doubled for each eight-degree-centigrade increase from a base temperature of 120 degrees centigrade or higher, applies most closely for practical use when the insulation under study has lost more than 50 per cent of its tensile strength.

CELLULOSE insulation is widely used as the dielectric in high-voltage electric apparatus. Like mineral oil with which it is usually associated, cellulose in commercial practice is subject to chemical change to an extent which is dependent upon the conditions of its use. The deteriorating effects of temperature and oxygen are generally recognized and have been the subject of exhaustive research and discussion. The effect of moisture, as described in this paper, has not been carefully evaluated in previous studies of cellulose stability, despite the fact that it appears to play a dominant role in the chemical and mechanical changes exhibited by the cellulose when exposed to temperatures within the range encountered in the commercial use of transformers and other high-voltage electric apparatus.

As a material for insulating electric apparatus cellulose is used in a variety of forms. In its widest application cellulose is used in the form of paper sheets or tapes for the insulation of conductors which may vary greatly in size and shape. In some applications it is subjected to relatively low electric stresses and serves chiefly as a spacing material for the separation of adjacent conductors. In other applications it is subject to severe electric stress which in the case of capacitors and similar apparatus may be as high as 300 or 400 volts per mil. In most of its uses the cellulose is impregnated with mineral oil or varnish or both. It may, on the one hand, be found in apparatus operating under conditions allowing more or less free access of air or, on the other hand, in apparatus where contact with air or other source of oxygen is substantially eliminated by the use of hermetically sealed containers or by other means. In all of its applications, however, it is essential that chemical deterioration of the cellulose, resulting in the loss of dielectric efficiency and mechanical strength, shall be completely eliminated or carefully controlled in order that the insulation may be able to withstand the severe electric and mechanical stresses set up, not only in the normal use of the apparatus but also under short circuit or overload conditions.

One of the most difficult of all engineering problems is the translation of laboratory "life" data into terms of practical usage. The present paper concerns itself entirely with laboratory data correlating those factors of time, temperature, moisture, and oxidation which have been found to be important in affecting the mechanical stability of cellulose insulation. The results obtained are reported in order to assist the engineer in his efforts to use materials efficiently. The relative importance of the deteriorating factors investigated can only be finally determined from a study of the results obtained under the conditions of commercial use.

Objects of Investigation

In a previous paper[1] it has been related that the chemical and mechanical deterioration of cellulose is a complicated phenomenon greatly influenced by the decomposition products formed as a result of the chemical changes involved. Among those products are organic acids, water, hydrocarbons and their derivatives, and gases such as the oxides of carbon. It has been demonstrated that once initiated the chemical changes involved are accelerated to a degree dependent on the retention of the decomposition products. The present paper has as one of its objects the evaluation of the influence exerted by the presence of moisture on the mechanical stability of cellulose when aged under conditions which do not allow the escape of the deterioration products formed.

It has already been shown[2] that cellulose sheets or tapes will deteriorate mechanically when heated even in the absence of oxygen. The pyrochemical effect as contrasted to the effects of severe oxidation become pronounced as the temperature is raised above 120 degrees centigrade. Cellulose when heated passes through a "stable period" during which the mechanical properties are maintained. The presence of oxygen is chiefly effective in reducing the duration of this "stable period." It is a further object of this paper to demonstrate the effect of moisture on the duration of this "stable period."

Cellulose insulation as in transformers is subject to wide variations in temperature. Under overload conditions localized temperatures are obtained far in excess of those normally associated with the daily use of the apparatus. Engineering practice is to assume that the effects of such abnormal temperatures are additive. It is another object of this paper to examine the validity of such an assumption.

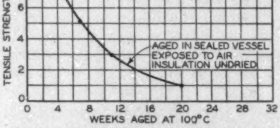

Figure 1. Showing the effect of testing conditions on the mechanical stability of 0.003-inch Manila insulating paper when aged under mineral transformer oil at 100 degrees centigrade

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F. M. Clark is technical assistant engineer in the laboratory of General Electric Company, Pittsfield, Mass.

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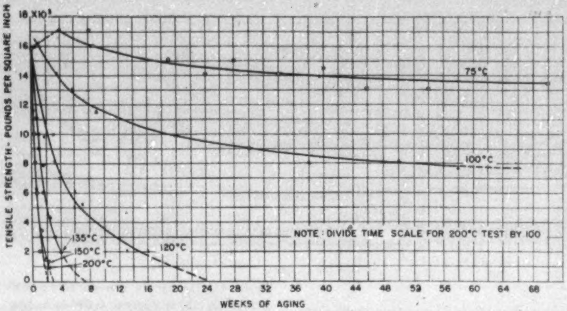


Figure 2. The tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila insulating paper as affected by aging in sealed containers under oil, the surface of which was in contact with oxygen gas

Chemical reactions involving organic materials are accelerated by heat. In a general sense, it is accepted that such chemical reactions are increased twofold for each ten-degree-centigrade increment in temperature. Montsinger in previous publications[3,4] has called attention to the importance of properly evaluating the influences of temperature as a criterion of the ability of transformer insulation to withstand the effects of temporary overloads encountered in the use of transformers. He has suggested the "eight-degree rule," indicative that the rate of the mechanical deterioration of cellulose doubles for each eight-degree-centigrade rise in temperature. It is a final object of this paper to demonstrate the rate of cellulose deterioration as affected by temperature increase under widely different conditions of possible use.

Testing Technique

Experience in the study of cellulose deterioration at elevated temperature demonstrates the necessity of closely controlling all those factors which have long been recognized as of fundamental importance in the study of sludge formation in mineral transformer oil.[5,6] The temperature must be carefully controlled. In order to obtain duplicate test results, the temperature should be controlled to ±0.5 degree centigrade.

The testing receptacle in which the cellulose is aged must be carefully selected if reproducible results are to be obtained. Access of air or other gases must be controlled. The ease of escape of products formed as a result of the deterioration must be clearly described and evaluated in terms of practical usage. The widely different results (Figure 1) accompanying a change in the conditioning treatment of the insulation or in the design of the vessel used for the aging only serve to emphasize the confusion which may arise when details of this type are ignored.

In the "sealed-tube" tests hereinafter described, the tube is of Pyrex glass and measures 200 millimeters in length and 25 millimeters in diameter. When sealed it has a volume of 59 milliliters. The conditioned oil and the oil-immersed paper insulation fill the tube to the 50-milliliter mark, the remaining 9 milliliters being filled with the stated gas conditioned as described. The paper used is cut before conditioning in strips, each one-half inch wide and ten inches long. The paper used throughout this study is 0.003-inch Manila cable paper. Ten strips of such paper, conditioned as described, are used in each test. This gives a ratio of approximately one-quarter pound of paper per gallon of oil. The tensile strength of the paper is determined in a room maintained at 65 per cent relative humidity, 70 degrees Fahrenheit. The oil-impregnated papers are tested immediately after having been taken from immersion in the oil. The tensile strength is determined in a Schopper testing apparatus. The results reported are the average of ten individual tensile strength tests.

The mineral oil used throughout this paper is a typical American transformer oil (58 seconds Saybolt Universal viscosity at 37.8 degrees centigrade) and is manufactured from Gulf Coast crude. Before use it is completely degassed and dried. During test it is saturated with the gas with which it is in contact, as is described in each particular instance.

Moisture Determination

The problem of determining the moisture content of cellulose sheets has been recognized as difficult and subject to variation because of the instability of the cellulose itself. Urquhart and Williams[7] have determined that oven drying at 110 degrees centigrade are as satisfactory as vacuum drying over phosphorous pentoxide at 15 degrees centigrade. Such methods may be well suited for roughly determining the water content of cellulose containing large amounts of moisture, such as is present in ordinary paper exposed to atmospheric conditions, but they fail to give accurate test values when the water content of the paper is materially reduced.

A satisfactory method of test for determining the water content of insulating papers has been established as follows:

Approximately 50 grams of paper, preferably cut into pieces about three by three-fourths inches in size (less weight of paper if suspected to be high in water content, more weight if suspected to be of low water content) is immersed quickly in 500 milliliters of dry liquid such as mineral transformer oil contained in a thoroughly dry 1,000-milliliter round bottom flask. Dry nitrogen gas is passed through the liquid at the rate of 100 milliliters per minute with a fritted gas washing tube to obtain the best dispersion. The temperature of the liquid is raised to 110 degrees centigrade plus or minus two degrees. The gas after passing from the liquid-paper suspension is passed through two liquid air-cooled traps connected in series where the moisture and other volatile materials are condensed. The outlet for the nitrogen gas is protected by a conventional drying tube to prevent suck back of atmospheric moisture. The nitrogen gas is passed through the test flask for three hours after which the water collected in the liquid air traps is quantitatively determined. The nitrogen gas treatment is repeated in three-hour steps at 110 degrees centigrade until all water has been driven from the paper under test. The quantitative test for the water collected is carried out as follows:

Ten milliliters of an acetyl chloride solution in dry benzene (118 grams per liter of solution) are added to a dry 250-milliliter glass stoppered flask and cooled in an ice bath. Two milliliters of dry pyridene are added dropwise to the acetyl chloride solution with continual shaking. A white precipitate of acetyl chloride-pyridene complex is formed.

The frozen condensate in the traps is dissolved in 25 cubic centimeters of dry acetone and added to the cold acetyl chloride-pyridene complex. The acetone is added in 5-5-2.5-2.5 milliliter portions to the first trap and in 5-2.5-2.5 milliliter

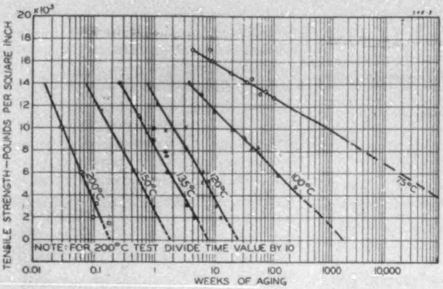


Figure 3. The tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila insulating paper as affected by aging in sealed glass containers under oil, the surface of which was in contact with oxygen gas

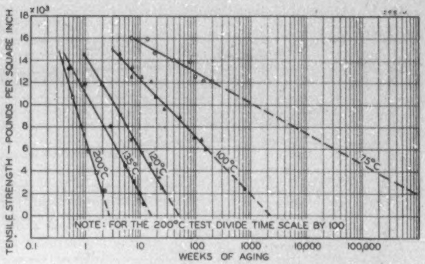


Figure 4. The tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila insulating paper as affected by aging in sealed glass containers under oil, the surface of which was in contact with nitrogen gas

portions to the second trap. The stoppered flask is then removed from the ice bath and allowed to stand at least 15 and not more than 30 minutes with occasional shaking out of contact with direct sunlight. Four milliliters of dry ethyl alcohol are added and the mixture is well shaken. Sixteen milliliters more of dry ethyl alcohol are then added and after being shaken and allowed to stand for five minutes, the mixture is titrated with 0.5 normal sodium hydroxide to a phenol phthalein end point. A blank test is run with all conditions identical in order to correct for residual moisture in the reagents and apparatus. The amount of moisture in the paper, expressed in terms of its dry weight is then calculated.

Using this procedure definite and reproducible test results have been obtained. Care must be taken, however, to avoid decomposition and subsequent removal of the "water of constitution" from the cellulose sheet. If the analysis be carried beyond the point at which the water evolution from the paper drops to zero, subsequent heating will result eventually in some decomposition of the cellulose.[1] The formation of water as a result of this decomposition will obscure the real moisture content determination.

The determination of the moisture content of a cellulose sheet is difficult and fraught with many technical pitfalls. The method described gives results which appear lower than those heretofore reported for mineral oil-treated cellulose insulation.[11] Pending further experimental data, the moisture contents described in subsequent paragraphs might well be considered as "relative" rather than as fixed and absolute values. Such a precaution in no wise diminishes the importance of the studies demonstrating the marked effect of moisture on the mechanical life of the cellulose insulation.

Expression of Test Results

Because of its greater degree of reproducibility, the tensile test has been adopted as the gauge of mechanical strength of the cellulose sheet and its rate of deterioration. Typical time relationships are illustrated in Figure 2. Because of the difficulty in clear presentation, however, such relations are not generally used in this paper. The semilogarithmic relationship between the tensile strength and the duration of aging have been found to be better suited for clear presentation (Figure 3).

The Effect of Temperature

There are many technical processes which employ cellulose products at high temperature. This has led to the initiation of many researches concerned with the stability of cellulose products over short periods of heating. The general conclusion reached has been that cellulose suffers some decomposition at temperatures above 150 degrees centigrade. The problem of the insulation engineer however is the problem of time and temperature. The stability of cellulose during short periods of heating[1,2] are frequently misleading. Knecht[3] has shown that, when cotton is heated for 336 hours at 80 degrees centigrade, chemical changes occur which endow the cellulose with reducing powers. It is the effect of these often overlooked chemical changes, culminating in mechanical deterioration, with which this paper is concerned. The time of heating is obviously of importance.

The effect of time and temperature over the range from 75 to 200 degrees centigrade on the tensile strength of vacuum-dried and oil-impregnated 0.003-inch Manila paper is illustrated in Figure 2. During the aging runs the insulation was immersed in dry oil in sealed Pyrex glass tubes, the surface of the oil being in contact with oxygen as already described. The semilogarithmic relation of tensile strength and time of heating is illustrated in Figure 3. Figure 4 illustrates similar data for vacuum-dried, oil-impregnated 0.003-inch Manila paper, the surface of the oil in which the paper is immersed being in contact with dry nitrogen gas. The marked effect of temperature on the mechanical stability of Manila paper is illustrated in Figure 5 which shows the "50 per cent life" in tensile strength over

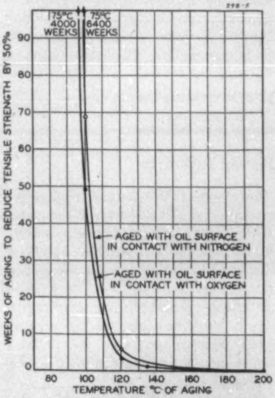


Figure 5. Showing the marked effect of temperature increase above 75 degrees centigrade in the mechanical life of vacuum-dried and oil-impregnated 0.003-inch Manila insulation paper when aged in sealed glass containers under oil, the surface of which was in contact with oxygen or nitrogen gas as indicated

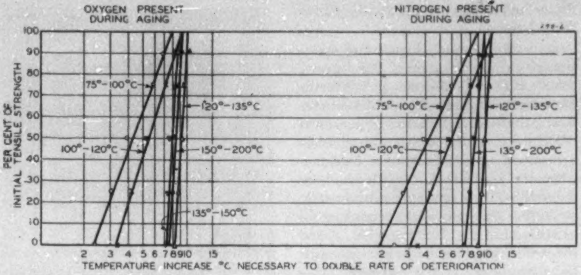


Figure 6. The rate of mechanical deterioration of vacuum-dried and oil-impregnated 0.003-inch Manila paper becomes more sensitive to temperature increase as the deterioration progresses

The effect is most pronounced when the temperature of test is below 120 degrees centigrade

the temperature range from 75 to 200 degrees centigrade. It is clearly evident that as the temperature rises above 75 degrees centigrade the deterioration proceeds at a rapidly increasing rate. It is the proper evaluation of this rapidly increasing rate of deterioration which is of fundamental importance in the application of cellulose insulation in high-voltage electric apparatus.

Inspection of the data presented in Figures, 2, 3, 4, and 5 at once leads to the conclusion that the rate of mechanical deterioration of Manila insulating paper cannot be expressed in terms of any single temperature expression. Figure 6 plots as a semilogarithmic expression the relation between the deteriorating mechanical strength and that temperature rise which is necessary in order to produce a twofold increase in the mechanical deterioration over the temperature range shown. For temperatures in the range from 75 to 100 degrees centigrade the effect of temperature change is extremely marked, whether the deterioration be produced in the presence or absence of oxygen. In this range, an increase in temperature of but 2.4 degrees centigrade will reduce the total life of the insulation by half. With temperatures of 120 degrees centigrade and higher, however, the deterioration is less sensitive to temperature increase. To double the rate of deterioration for these higher temperatures (reduce the total life by half), an increase ranging between approximately seven and ten degrees centigrade is necessary. The relation of the temperature of the aging to that temperature increase necessary to produce a twofold rate of deterioration when the tensile strength has been reduced to 50 per cent and zero per cent of its initial value is illustrated in Figure 7. The so-called "eight-degree rule" suggested by Montsinger is a good practical compromise for the higher temperatures of use (120 degrees centigrade and higher) when the tensile strength has been reduced to less than about 50 per cent of the initial value.

The Effect of Oxygen

The problem of evaluating the practical effect of oxygen on the mechanical strength of cellulose insulation during the normal use of a transformer or similar apparatus is difficult, because of the wide differences in transformer designs. The transformer designs of greatest importance for consideration, however, are the hermetically sealed type containing a gas cushion and the conservator type. Each of these has a limited supply of available oxygen. In the conservator transformer the amount of oxygen is limited to that carried into the transformer as an air solution in the mineral oil. In the hermetically sealed transformer the amount of oxygen is limited to that present in oil solution and in the gas cushion when the transformer is sealed. When this gas cushion consists of nitrogen gas, the amount of oxygen available within the transformer is negligible if the mineral oil is properly deaerated before the transformer is sealed.

In the laboratory study of the effects of oxidation on the mechanical strength of the cellulose insulation, an attempt has been made to exaggerate the hermetically sealed transformer design. The sealed tubes have been filled with oil and the oil-immersed insulation to 85 per cent of the volume. The gas cushion occupies the remaining 15 per cent of the volume. These are the approximate relationships of oil to gas volumes in the average hermetically sealed transformer. The effect of oxidation has been exaggerated by the use of pure oxygen in the gas space. Pure oxygen has been used to exaggerate the oxidation possibilities, since in a previous publication[2] the presence of air has been demonstrated to be of negligible oxidation effect within the duration of the laboratory tests made. The use of this type of test arrangement is considered to be more severe than the conditions which are met in either the conservator or the hermetically sealed type of oil-filled transformer.

It has been suggested[10,12] that the effect of oxygen on the mechanical deterioration of oil-treated cellulose insulation involves consideration of the acids formed as a result of the oxidation of the oil itself. In a general sense this is true. Therefore, since the most corrosive acids formed both from the oil oxidation and from the oxidation of the cellulose are volatile at the temperature of the tests described, sealed containers have been used for the aging study. This insures that all products of decomposition and oxidation, whether from the oil or from the cellulose, are retained within the test receptacle. From the practical standpoint this procedure is justified, since the mechanical deteriora-

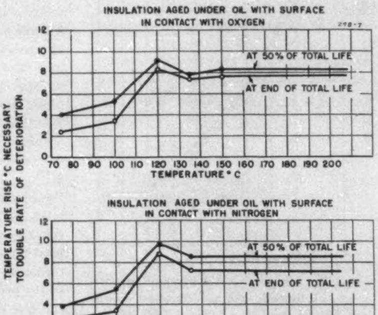


Figure 7. The "eight-degree rule" correlating temperature increase and mechanical deterioration of vacuum-dried and oil-impregnated 0.003-inch Manila paper applies with practical accuracy when the paper has deteriorated to at least 50 per cent of its initial value at temperatures of 120 degrees centigrade and higher

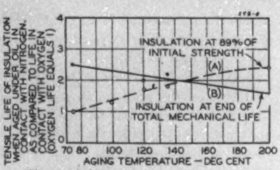


Figure 8. The elimination of oxygen increases the initial stability of vacuum-dried and oil-impregnated 0.003-inch Manila paper over the temperature range 75–100 degrees centigrade

The total life of the insulation, however is progressively less affected by the elimination of oxidation as the temperature of the aging is increased

tion reported therefore includes consideration of the accumulated effects of all the oxidation and decomposition that occurs.

In a previous publication[2] it was concluded that the "single effect of oxidation was restricted to the initial periods of treatment at temperatures lower than 120 degrees centigrade. At higher temperatures pyrochemical decomposition occurs . . ." The data presented extend and support this conclusion and indicate that the major effect of oxidation is produced when the insulation is exposed to the lower testing temperatures. At 75 degrees centigrade the deterioration due to oxidation becomes more pronounced as the mechanical aging progresses. At higher temperatures the pyrochemical decomposition of the cellulose tends to reduce the effects of oxidation.

Figure 8 illustrates the effect of oxidation on the deterioration of the vacuum-dried and oil-impregnated 0.003-inch Manila paper for two degrees of deterioration. Curve *A* illustrates the increased life of the insulation which is obtained as a result of the elimination of oxidation when the mechanical strength has been reduced to 89 per cent of its initial tensile strength. This value corresponds roughly to the end of the "stable period." The effect of oxygen elimination increases with the temperature of the exposure. This supports the conclusion that the duration of the "stable period" is reduced by oxidation processes. When, however, the total life of the insulation is considered (tensile strength zero), curve *B* supports the conclusion that the occurrence of pyrochemical decomposition during the later stages of the cellulose aging becomes more pronounced with the higher testing temperatures and reduces the beneficial effects normally associated with the elimination of oxygen.

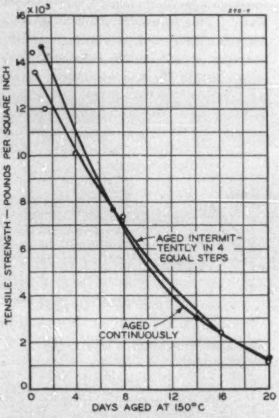


Figure 9. The mechanical aging of vacuum-dried, oil-treated, and oil-immersed 0.003-inch Manila insulating paper at 150 degrees centigrade in the presence of nitrogen gas is an additive function of the total period of heating when intermittently applied

Intermittent Aging

The total aging of cellulose insulation when exposed intermittently to high temperatures necessarily includes the deterioration which occurs during the heating and cooling periods. To determine whether the intermittent application of high temperature is strictly additive, the insulation contained in the sealed glass tubes was heated as rapidly as possible by immersion in an oil bath maintained continuously at 150 degrees centigrade, and cooled rapidly by transfer to an oil bath maintained continuously at 25 degrees centigrade. This reduced the effect of the heating and cooling periods to a minimum. The effect of such intermittent heating and cooling on the tensile strength of vacuum-dried, oil-impregnated, 0.003-inch Manila insulating paper aged under mineral transformer oil at 150 degrees centigrade is illustrated in Figures 9 and 10. The intermittent aging relation was obtained by heating the insulation at 150 degrees centigrade for four equal periods of time. Between these heating periods the insulation was maintained at 25 degrees for 24 hours. In the absence of oxygen, the effect of intermittent heating has been found to be strictly additive within the limits of experimental error (Figure 9). In the presence of oxygen (Figure 10) the additive effect again appears to be within the limits of experimental error until the insulation has lost approximately 50 per cent of its initial tensile strength. For a greater degree of deterioration the effect of intermittent aging appears less than additive, although the differences observed do not appear of major significance. Pending further investigation, it is concluded that the results obtained justify the engineering practice which considers the effects of intermittent heating at high temperature to be additive.

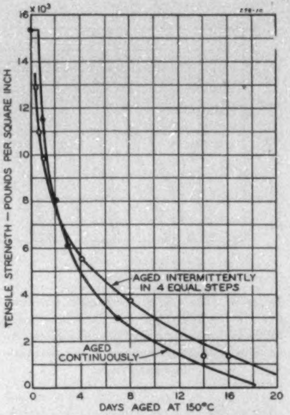


Figure 10. The effect of intermittent heating at 150 degrees centigrade on the mechanical strength of vacuum-dried, oil-treated, and oil-immersed 0.003-inch Manila insulating paper in the presence of oxygen

The Effect of Moisture on the Tensile Strength

Inspection of the data presented (Figure 6) correlating the effect of temperature on the rate of mechanical deteriora-

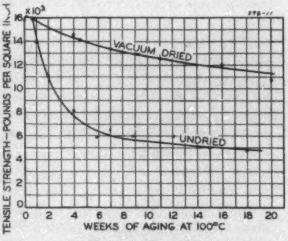


Figure 11. Showing the effect of moisture retained by cellulose in its normal condition on the mechanical stability of 0.003-inch Manila insulating paper during an aging run at 100 degrees centigrade in sealed glass tubes

During the aging the insulation is held under mineral transformer oil, the surface of which is in contact with nitrogen gas

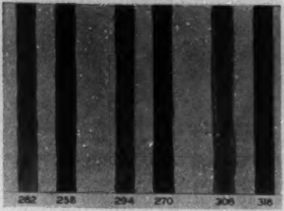


Figure 12. Illustrating the color darkening which accompanies the mechanical deterioration obtained when 0.003-inch Manila paper is aged in sealed tubes for two weeks at 120 degrees centigrade in the presence of moisture

Samples 282, 294, and 306—paper aged after having been vacuum dried and impregnated with mineral transformer oil, the surface of which is in contact with oxygen, nitrogen, and air respectively

Samples 258, 270, and 318—paper aged immersed in mineral transformer oil without previous drying, the oil being in contact with oxygen, nitrogen, and air respectively

tion leads to the observation that as the deterioration progresses it becomes more sensitive to temperature change. Thus, although in the early stages of heating, the paper deterioration increases approximately twofold for each seven to ten degrees centigrade rise in temperature, when the tensile strength has been reduced to as low as 25 per cent of its initial value, the rate of deterioration doubles for an increase of only about 2.4 to 9 degrees centigrade, depending on the testing temperature. This acceleration of the deterioration has been traced[1] to the effect of decomposition products retained in the body of the cellulose. Among such decomposition products is water. Because of the ability of water through ionization and otherwise to activate many of the products (acids and acidic materials) formed when cellulose undergoes deterioration, it is not unexpected to find that the mechanical strength of cellulose insulation is affected by the presence of moisture in even small amounts. Figure 11 illustrates the greater stability in the tensile strength of 0.003-inch Manila insulating paper which is obtained when the insulation is freed of its moisture before exposure to high temperature. In one instance the paper is heated under oil at 100 degrees centigrade without vacuum drying. In the other instance the paper is first vacuum dried to a low moisture content. The greater mechanical instability of the undried paper is accompanied by other visual evidence of greater chemical change. In contrast to the dried paper, the undried insulation after aging is darkened, reacts acidic, and smells strongly of a caramel odor, characteristic of overheated cellulose. Figure 12 illustrates the marked darkening in color which accompanies the greatly accelerated mechanical deterioration in the cellulose sheet when aged in the presence of moisture, even though the possibility of oxidation be eliminated by the use of nitrogen gas.

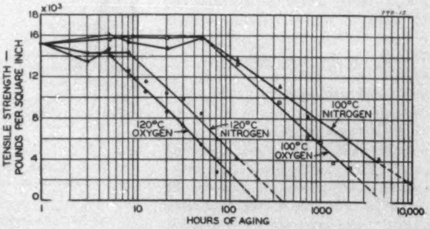
The data of Figure 11 are obtained as a result of an aging run in sealed glass vessels. It is suggested that the extreme variations reported in Figure 1 are due to the difference in the design of the aging vessel. On the one hand, the moisture and other volatile products originally present or formed during the test were allowed easy escape since the vessel was open to the air. On the other hand, the moisture and volatile products were completely retained in the sealed tube during the aging run. The wide difference in the test results reported in Figure 1 and in Figure 11 illustrates the wide variation which can be expected when the insulation is aged without careful attention to all of the conditions affecting the chemical stability of the cellulose. Not the least of these conditions is its moisture content.

The Effect of Moisture on the Rate of Deterioration as Affected by Temperature and Oxidation

The presence of moisture in cellulose insulation is of such importance in its effect on the mechanical stability at high temperature that it tends to modify the relationships set up for the dried insulation. To illustrate this, 0.003-inch Manila insulating paper containing 1.5 per cent moisture has been immersed in oil and aged at 100 and 120 degrees centigrade in sealed glass tubes of the type already described, the surface of the oil being in contact with oxygen or nitrogen gas. The results obtained are illustrated in Figure 13.

It has already been shown in Figure 8 that with dry insulation the presence of oxygen produces its maximum effect on the total life of the insulation when the aging is carried out at the lower testing temperatures. At 75 degrees centigrade the total life is increased about 2.5 times when an inert gas such as nitrogen is substituted for oxygen, thereby eliminating substantially all of the deterioration attributed to oxidation. When, however, there is a substantial amount of moisture present, its deteriorating influence accentuates the effect of oxidation. Figure 14 based on the data of Figure 13 demonstrates that in the presence of moisture the elimination of the oxidation reaction at 100 degrees centigrade has a more pronounced effect than is associated with elimination of oxygen on the life of the dried paper insulation.

Figure 13. The aging of 0.003-inch Manila insulating paper containing 1.5 per cent moisture



In like manner, the effect of temperature increase on the aging of Manila insulating paper is materially modified by the presence of moisture. The deterioration becomes less sensitive to temperature change. This is illustrated in Figure 15.

Quantitative Effects of Moisture

The quantitative study of the effects of moisture on the mechanical aging of cellulose is made difficult by the fact that the equilibrium which exists between the amount of water retained by the cellulose and that present in the mineral transformer oil in which it is immersed shifts with changes in temperature.[9] The higher the temperature, the less water is retained by the cellulose. In the data relating to the water content of the cellulose in this and subsequent sections, the value given is the total water content of the oil and cellulose insulation within the sealed container at 25 degrees centigrade. At this temperature under the experimental conditions existing, substantially all of the water present is held by the cellulose and is so reported.

In order to eliminate the effects of oxidation which vary with the temperature of aging and the moisture content of the cellulose insulation, a typical set of test results, describing the deterioration of oil-impregnated and oil-immersed 0.003-inch Manila paper aged at 120 degrees centigrade in the presence of nitrogen, is selected to demonstrate the influence of

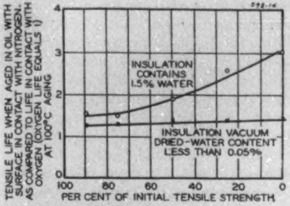


Figure 14. The importance of oxidation in promoting the mechanical deterioration of 0.003-inch Manila insulating paper is materially increased when the aging of the oil-treated and oil-immersed insulation is carried out in sealed tubes in the presence of moisture

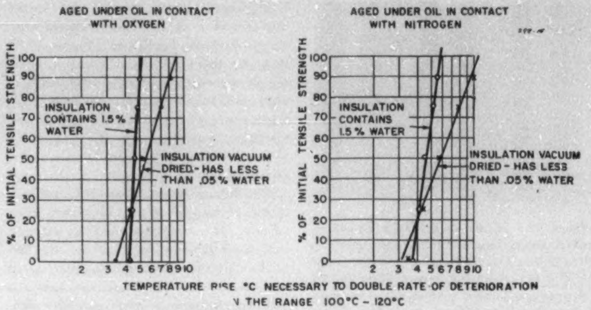


Figure 15. The presence of moisture in incompletely dried, oil-treated and oil-immersed 0.003-inch Manila insulating paper exaggerates the effect of temperature increase from 100 to 120 degrees centigrade during the major part of the mechanical life

The greater initial resistance to temperature increase characteristic of the well-dried insulation, however, is reduced as the result of water formation as the deterioration progresses

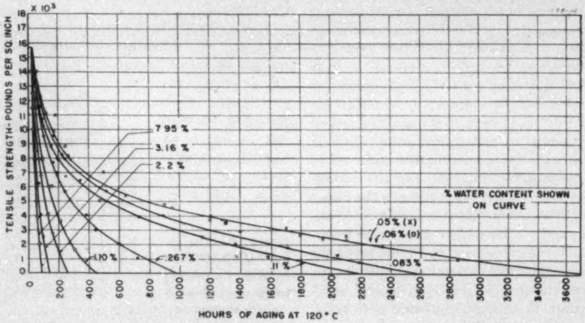
moisture. The insulation was first vacuum dried at 100 degrees centigrade for a period of time ranging from less than one hour to more than 100 hours, after which it was impregnated with gas-free, dry mineral transformer oil and sealed at once in glass vessels partially filled with the oil and oil-immersed insulation as already described. The gas with which the oil was saturated and with which the oil surface was in contact was dry nitrogen. The water content of the assembly was determined at the start of each aging run. The effect of moisture was marked. The total life of the insulation was reduced from a total of 3,700 hours (22 weeks), characteristic of the well-dried insulation, to a life of less than 100 hours (0.6 week), when the paper was substantially undried and contained about eight per cent moisture. Figure 16 represents the relationship found to prevail in a typical series of test results obtained simultaneously. As illustrated in Figure 17, there exists a logarithmic relation between the water content of the cellulose insulation and that period of time necessary to produce a given amount of mechanical deterioration in the cellulose sheet when aged at 120 degrees centigrade under the conditions applied.

In a previous paragraph it has been demonstrated that the effect of oxidation during the mechanical aging of cellulose insulation materially shortens the "stable period" during which the insulation can be heated at high temperature without evidence of mechanical deterioration. The effect of the initial water content of the cellulose of this "stable period" has been found to be negligible. In Figure 16 the stable period is shown to be approximately 40 hours, irrespective of the initial water content of the Manila insulating paper. Since oxidation is effective in determining the length of this "stable period" it is at once obvious that other testing factors which affect the oxidation reaction are of importance in determining its duration.

Because of the importance of mechanical stability in many of the dielectric applications of cellulose in electrical apparatus, the marked influence of moisture on the mechanical life of the insulation assumes practical value. Cellulose is a hygroscopic material and rapidly absorbs moisture from the surrounding medium.[9] The absorption of this moisture is effective in determining the subsequent rapidity in mechanical deterioration. From the data of Figure 16 it is obvious that, when oxidation is eliminated, the absorption of even small amounts of moisture by the dry insulation will materially reduce its mechanical life. Conversely, it is obvious that the absorption of relatively large amounts of moisture will have less effect on the aging of the improperly dried insulation. The relationship is illustrated in Figure 18.

To double the rate of mechanical deterioration in oil-treated and oil-immersed Manila paper in the absence of oxidation, the amount of moisture addition necessary is dependent upon the degree of deterioration which is selected for study. The total life of the insulation is halved (the deterioration rate doubled) by the addition of water equal to approximately 100 per cent of that originally present in the insulation. Thus, with a water content of 0.05 per cent, the life of oil-treated and oil-immersed Manila paper in the absence of oxygen is approximately 4,000 hours. With the absorption of an equal amount of moisture to give a total water content of 0.10 per cent, the total life of the insulation at 120 degrees centigrade is reduced to about 2,000 hours. On the other hand, to reduce by half the total

Figure 16. The effect of moisture content on the mechanical life of oil-impregnated and oil-immersed 0.003-inch Manila insulating paper when aged at 120 degrees centigrade in sealed glass containers, the oil surface being in contact with nitrogen gas



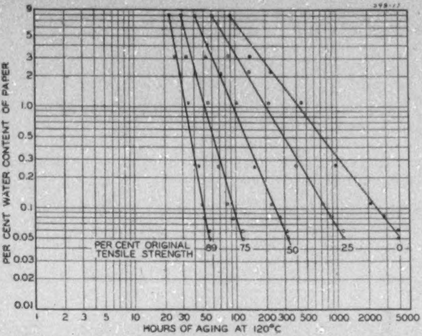


Figure 17. Under the conditions of test applying for the data of Figure 13, the water content of the cellulose insulation bears a logarithmic relation to the period of heating necessary to produce a given amount of mechanical deterioration

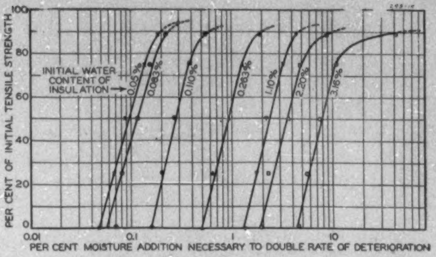


Figure 19. Illustrating the semilogarithmic relation which exists between the degree of mechanical deterioration of oil-treated and oil-immersed 0.003-inch Manila insulating paper and the amount of moisture addition necessary to double the rate of deterioration (halve the life) when heated in sealed glass vessels, the surface of the oil being in contact with nitrogen gas

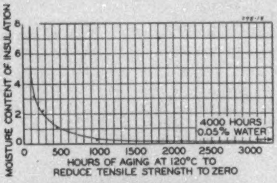


Figure 18. The elimination of moisture from cellulose insulation before being subjected to high temperature produces a rapidly increasing life as the moisture content is reduced below three per cent

Data based on the ultimate life values illustrated in Figure 16

life of the same insulation containing 3.16 per cent water from its value of 140 hours aged at 120 degrees centigrade, the addition of approximately 4.4 per cent more water is necessary. The relationship between the degree of mechanical deterioration of the oil-treated and oil-immersed 0.003-inch Manila insulating paper and the amount of moisture necessary to double the rate of deterioration (halve the life) in the absence of oxidation is shown in Figure 19.

Figure 19 is of importance. It demonstrates that as the point of mechanical strength selected for comparison approaches the original tensile strength of the insulation, the effect of water additions is reduced in the absence of oxygen, that is, greater water additions are necessary to reduce the life by half. This supports the observation that the "stable period," the length of time that the material can be heated without apparent mechanical change, is not greatly affected by changes in the water content of the insulation. Figure 19 also indicates that the effect of water additions becomes more pronounced as the degree of deterioration of the insulation progresses. This supports the observation that those reagents active in promoting mechanical deterioration include acidic products formed as a result of the initial chemical changes, the action of which is made more severe because of the presence of moisture.

Summary Conclusions

The general conclusions which are drawn from this investigation are:

1. The rate of mechanical deterioration in cellulose insulation depends on the conditions of its use. Among those factors of major importance are the temperature applied and the presence of oxygen and moisture.

2. As the temperature is raised above 75 degrees centigrade, the mechanical deterioration of cellulose insulation proceeds at a rapidly increasing rate.

3. Oxidation is most effective on the total mechanical life of the cellulose when the insulation is aged at temperatures below 120 degrees centigrade.

4. The effect of intermittent heating at 150 degrees centigrade on the mechanical deterioration of cellulose insulation is additive.

5. The mechanical life of a cellulose sheet is radically reduced when the water content of the dry insulation is increased in amount up to approximately 0.5 per cent of its dry weight. In general terms, the mechanical life of cellulose insulation is reduced by half for each 100 per cent increase in water content.

6. The effect of oxidation on the mechanical life of cellulose insulation becomes more pronounced in the presence of moisture.

7. The rate of deterioration for the substantially dry insulation as affected by temperature increase above 120 degrees centigrade is dependent upon its previous history. The "eight-degree rule," indicative that the rate of mechanical deterioration is doubled for each eight-degree-centigrade increase from a base temperature of 120 degrees centigrade or higher, applies most closely for practical use when the insulation under study has lost more than 50 per cent of its initial tensile strength.

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Steady-State Theory of the Amplidyne Generator

TROY D. GRAYBEAL
ASSOCIATE AIEE

Synopsis: The fundamental steady-state theory of the amplidyne generator is presented in this paper together with methods for calculating the characteristics of the generator from the machine constants. Experimental data obtained on a test machine are compared with calculated results to show the accuracy of the methods and to substantiate the theory. The effects of such factors as brush losses, commutation, and overcompensation and undercompensation on the operation of the generator are explained. Overcompensated and undercompensated machines are treated from the standpoint of both steady-state operation and electrical stability. Methods are given for the determination of the machine constants from open-circuit and short-circuit tests performed on the machine. This method offers the advantage that the machine constants so determined apply for actual conditions of operation and need not be modified to include the effects of such factors as brush and commutation losses, eddy currents, and hysteresis.

THE amplidyne generator which has found wide industrial application in various types of control apparatus is closely related from the standpoint of operating principle to the Rosenberg generator and the metadyne, differing principally in the manner in which it is used. The Rosenberg generator is a constant current source of d-c electrical energy; the metadyne in its usual form is a machine for converting constant potential d-c energy into constant current d-c energy, while the amplidyne generator is a dynamoelectric power amplifier.

One of the outstanding features of the Rosenberg generator is that the open-circuit output voltage varies with the square of the speed for constant field excitation.[2] Therefore, the polarity of the machine is independent of the direction of rotation. When the terminal voltage is maintained constant, by a storage battery, for instance, the output current is essentially independent of the speed for very wide limits of speed variation. Because of these particular properties, this generator in conjunction with storage batteries has been widely used for train lighting service.[1,3,4]

Although the Rosenberg machine was first described in 1905,[1] apparently little was known as to the variety of characteristics this type of machine would assume if other coils in addition to the primary field were used in providing excitation. An investigation of this nature, conducted by J. M. Pestarini in France and published in 1930,[5–7] led to the conception of the metadyne. As defined by Mr. Pestarini, the metadyne is a generalized d-c machine consisting of a d-c armature and commutator, any number of field poles each of which may be excited in any manner, and any number of brushes arranged to bear on the commutator in positions for which satisfactory commutation can be obtained. In accordance with this definition, all rotating d-c machines including the ordinary d-c generator are metadynes. In common usage, however, the term "metadyne" refers only to those several forms of d-c machine which convert constant potential energy into constant current energy. The "figure-eight-connected" metadyne was found suitable for electric locomotive control and was successfully applied to many of the electric railways in France[6] and later to electric traffic systems in England.[8,9] Modifications of this general scheme have been used in this country for control of the locomotives of modern streamlined trains.

Investigation by the General Electric Company of possible applications of different forms of the metadyne to the control of industrial apparatus led to the development of the amplidyne generator.[12–14] This machine, which falls into the general class of metadynes designated as "cross-connected," represents a generalization of the Rosenberg generator in which the field poles are excited by several coils supplied from different sources either internal or external to the machine.

The amplidyne generator is used principally as an element of control mechanisms and plays a part similar to that of the ordinary vacuum tube. It absorbs weak current or voltage signals, amplifies them, and delivers them to the next unit of the control mechanism at a greatly increased power level. This machine offers the principal advantage that it may be built at very reasonable cost to deliver large power outputs. On the other hand, it is limited in application because of its comparatively slow speed of response. The time lag between a change in the input signal and the corresponding change in the output is comparatively large, because the machine consists entirely of highly inductive circuits. Its chief field of application is in control mechanisms operating on d-c or very low-frequency a-c signals. Essentially, the maximum available output voltage varies inversely with the square of the frequency of the applied signal.

The amplidyne generator is applicable to both "open" and "closed-cycle" control. The greater field, however, is in closed-cycle-control mechanisms because of certain inherent differences between the two types, as well as the more general use in recent years of closed-cycle control. "Open" control is that type of automatic control in which the quantity (time, voltage, displacement, and so forth) which actuates the control mechanism is essentially independent of the control operation. A simple example of this type is the automatic-starting compensator used for starting induction motors, synchronous motors, and so forth. These devices initially connect the machine terminals to a reduced voltage supply. By means of a time relay full line voltage is applied to the machine after normal speed is attained. This mechanism is a time-operated open-control device since it allows a definite time independent of acceleration for the starting period. "Closed-cycle" control is that type of automatic control in which the quantity which actuates the control mechanism is affected by the control operation. A good example of this

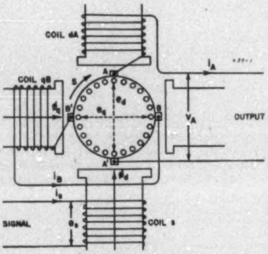

Figure 1. Essential elements and connections of the amplidyne generator

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TROY D. GRAYBEAL is instructor in electrical engineering, University of California, Berkeley, Calif.

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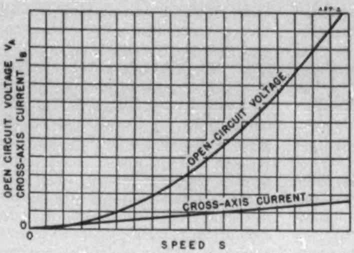
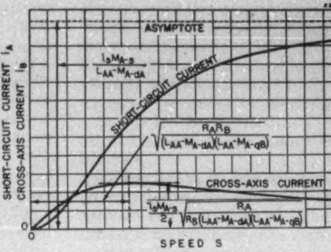


Figure 2 (left). Calculated curves illustrating the nature of the open-circuit characteristics of the amplidyne generator

Figure 3 (right). Calculated curves illustrating the nature of the short-circuit characteristics of an undercompensated amplidyne generator



type is automatic ship steering. Here, the angle between the ship compass and the hull is used to control the rudder, and this angle in turn is affected by the rudder operation.

In the first type of control the source of the controlling quantity usually has sufficient power to operate the control mechanism directly. In the second type the source of control usually is some form of measuring instrument which seldom can deliver any appreciable amount of power. Therefore, the original signal must be amplified in order that sufficient power be available to drive the control mechanism. The amplidyne generator provides a very satisfactory means of obtaining the necessary power amplification when a d-c control signal is used, and when a large amount of power is required to drive the control mechanism.

Closed-cycle-control mechanisms usually operate on the difference of two quantities, that is the difference between the desired quantity and the quantity which actually results from the output of the control mechanism. In the ideal case this difference would be zero, but this result is never obtained in practice. Comparison with the ideal, however, furnishes a standard by which the actual operation of the control mechanism may be judged.

In studies of the operation of a control mechanism the over-all behavior of the complete system including all main and control units must be analyzed, and the individual units so designed and adjusted that stable operation of the entire system is obtained. At the same time, the operation must be compared with that of the ideal in order that actual working tolerances may be kept to a satisfactory minimum. It is essential, therefore, that the operation of each individual unit, particularly in the control system, be expressible in simple quantitative terms. The load-voltage characteristic equation of the amplidyne generator (equation 14 of the appendix) defines the over-all operation of this machine and may be used in such analyses. It is valid for any type of load supplied by the generator, provided that the range of operation on the magnetization curve is sufficiently low that saturation effects are negligible. In most control applications this condition would be fulfilled, since a linear relationship between the signal and the output from the amplifying unit is usually desirable.

In instances where satisfactory operation is not attained for one reason or another, a comprehensive knowledge of the behavior of the various units comprising the control mechanism is necessary in order that proper adjustments or modifications may be made. Other characteristics of the amplidyne generator such as the variation of voltage and short-circuit current with speed and the effects of various coils on these characteristics provide this information. Overcompensation and undercompensation are necessary considerations, because these factors influence the stability of the machine. Generally speaking, overcompensation results in a higher output for the same signal input but causes inherent tendencies toward instable operation.

The essential electric elements of the amplidyne generator are an ordinary d-c armature and commutator, two complete sets of brushes spaced 90 electrical degrees apart, and windings connected as shown in Figure 1. The signal to be amplified, usually direct current, is applied to coil *s*. Current in this coil gives rise to a flux in the main axis of the machine, that is in the axis in line with the brushes AA'. Motion of the armature conductors in this flux generates a voltage in the cross axis, that is between the brushes BB'. Since these brushes are connected electrically, a current flows which produces a flux in the cross axis by virtue of armature reaction. The coil qB may be omitted in an actual machine but is included in the figure, since commutating poles used to secure good cross-axis commutation produce an effect on the machine constants similar to that of the coil qB. If present, this coil may be connected so as to add to or subtract from the magnetomotive force produced by the current flowing in the armature coils. The armature rotating in the cross-

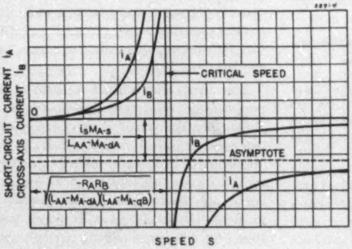


Figure 4 (left). Calculated curves illustrating the nature of the short-circuit characteristics of an overcompensated amplidyne generator

The machine is unstable even in the steady state for any speed above the critical speed

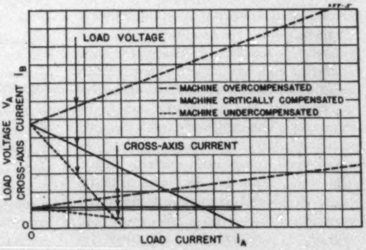


Figure 5. Calculated curves illustrating the nature of the load characteristics of the amplidyne generator and the effect of different degrees of main-axis compensation on these characteristics

axis flux gives rise to a voltage in the main axis which appears between the brushes *AA'*. This is the source of the output voltage of the machine. The purpose of the coil *dA* is to compensate for armature reaction in the main axis.

Open-Circuit Characteristics

The open-circuit or no-load characteristics of the amplidyne generator are expressed by equations 8, 9, and 10 of the appendix. The signal current is a function only of the signal voltage and the resistance of the signal coil. The cross-axis current varies linearly with the speed, and the output voltage varies with the square of the speed. The nature of these characteristics is illustrated by the calculated curves of Figure 2. The cross-axis current and the output voltage on open circuit are independent of the number of turns in the compensating coil *dA*.

The magnitude of the open-circuit voltage of the machine is governed by the mutual inductance between the signal coil and the armature and by the two cross-axis constants L_{AA} and M_{A-qB}. With other factors constant, doubling the number of turns in the armature coils increases the output voltage eight times if the coil *qB* is not present. In order to obtain a high output voltage, the cross-axis circuit resistance must be low as this factor appears in the denominator of the open-circuit voltage equation. The voltage acting in the cross axis is usually quite small, of the order of a few volts, so that brush drop represents a considerable portion of the total cross-axis circuit resistance. It is imperative, therefore, that material of very low contact resistance be used for the cross-axis brushes.

Short-Circuit Characteristics

The short-circuit characteristics of the amplidyne generator are expressed by equations 11, 12, and 13 of the appendix. The signal current depends only upon the signal voltage and the resistance of the signal coil. The nature of the two current characteristics depends upon the degree of compensation of the machine, that is upon the relative number of turns in the compensating coil *dA*. These characteristics are illustrated for an undercompensated machine by the calculated curves of Figure 3. The cross-axis current i_B has an initial slope the same as that of the open-circuit cross-axis current curve. The slope, however, decreases with increase in speed becoming zero when the condition $S\,(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB}) = R_AR_B$ is satisfied. As the speed increases further, the cross-axis current decreases and approaches zero asymptotically.

The short-circuit current i_A, beginning with an initial slope of zero, increases with the speed approaching asymptotically a definite finite value which depends upon the degree of compensation of the machine. As the ratio $R_AR_B/(L_{AA}-M_{A-qB})$ is made smaller by careful design of the machine, the short-circuit current approaches more closely the asymptotical value for any given maximum speed.

The asymptotical value of the short-circuit current, as obtained from the limit of equation 13 as S becomes infinite, is given by the expression $M_{A-s}i_s/(L_{AA}-M_{A-dA})$. With the compensating coil *dA* connected in the usual manner with regard to polarity, the value of the asymptote becomes larger and larger as turns are added to the compensating coil. When the number of turns are such that M_{A-dA} is equal to L_{AA}, the asymptote disappears, and the short-circuit current varies directly with the square of the speed of the machine. This condition may be used to define the boundary between overcompensation and undercompensation in the main axis. If the number of turns in the compensating coil are further increased so that M_{A-dA} is greater than L_{AA}, the value of the asymptote becomes negative, and the speed-current curve has a discontinuity at the point where $S^2(M_{A-dA}-L_{AA}) \times (L_{AA}-M_{A-qB}) = R_AR_B$. The operation of an overcompensated machine is stable in the steady state for speeds below the critical speed represented by this condition. At this critical speed the operation becomes unstable, and the short-circuit current increases without limit. For higher speeds, theoretically, the current reverses direction and decreases asymptotically to the value given by the asymptote expression. The curves for this condition illustrated in Figure 4 were calculated on the basis of the same constants as the curves of Figures 2 and 3 except that the value of the constant M_{A-dA} was increased approximately 20 per cent. This value represents a machine approximately six per cent overcompensated; that is, there are six per cent more turns in the compensating coil *dA* than those required for critical compensation as defined from the relation $L_{AA} = M_{A-dA}$.

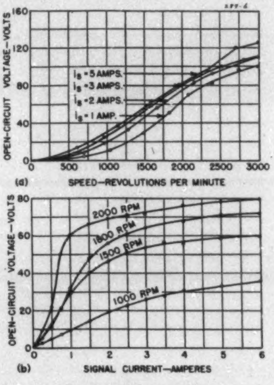


Figure 6. Experimental curves illustrating the effect of hard carbon brushes

For satisfactory operation the speed would have to be restricted to a maximum of 1,000 rpm and the signal current to 2.5 amperes. Brush drop resulting from higher speeds and signal currents causes excessive distortion as indicated by the crowding of the curves of (a) and the nonlinearity of the curves of (b). Compare with the curves of Figure 7 which were obtained from the same machine using graphite brushes

Load Characteristics

The load characteristics of the amplidyne generator are expressed by equations 14 and 15 of the appendix. For a given signal current and speed the curves are straight lines. If the machine is undercompensated, the lines are drooping, that is of negative slope, at any constant speed. If overcompensated, the lines are drooping at low speeds and are rising, that is of positive slope, at high speed. The load-voltage load-current characteristic becomes horizontal when the relationship $S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB}) = -R_AR_B$ is satisfied. If the machine is critically compensated, the curve is drooping, but the slope is independent of the speed of the machine. The effect of different degrees of compensation on the load characteristics is illustrated by the curves of Figure 5, which were calculated for a three per cent overcompensated, a critically compensated, and a three per cent undercompensated machine respectively. For all three sets of curves, the speed is the same and is greater than the critical speed at which instability results for the overcompensated machine on short-circuit.

Low cross-axis circuit resistance is necessary for high output from the machine since the factor R_B appears in the denominators of all terms. If R_B is reduced to half its original value, both intercepts of the load-voltage load-current curve are doubled, and the maximum available power output is increased four times for the same signal current and speed. The maximum available power output is equal to one-fourth the product of the two intercepts of the load-voltage load-current characteristic curve.

A critically compensated machine is stable in the steady state, regardless of the speed or the type of load. An overcompensated machine becomes unstable and builds up as a series generator for certain conditions, depending upon the speed of the machine, the degree of overcompensation, and the type of load supplied. If the machine supplies a constant impedance load, stable operation in the steady state occurs as long as the condition $(R_A+R_L)R_B>S^2(M_{A-dA}-L_{AA})\times(L_{AA}-M_{A-qB})$ is fulfilled. The resistance of the load impedance is R_L. Thus, in the practical application of an overcompensated machine, instable operation may result if the load resistance is too low or if the speed of the machine is too high.

A critically compensated or overcompensated amplidyne generator may display a form of transient instability characterized by the output voltage, and also by the output current, building up to a very high value before coming to the final steady-state value when a sudden increase of load occurs. Transients of this character arise from the magnetic coupling between the compensating coil, the main-axis armature circuit, and the signal coil. In general, the compensating coil is more closely coupled with the signal coil than is the armature, because of the air gap. For critical compensation, the compensating coil must contain a few more turns than the equivalent number of turns on the armature. Thus, a sudden change of current in the load circuit will induce a current in the signal circuit of such direction as to increase the strength of the main-axis flux. This, in turn, causes a greater output voltage and greater output current from the machine. In some cases this action may be cumulative and result in free oscillation. In order to eliminate the possibility of transients of this character, the number of turns in the compensating coil *dA* must be reduced to the point where the transient components of current induced in the signal coil by sudden changes in the load current give rise to effects which oppose the original change of load current. This condition is fulfilled when M_{dA-s} is less than M_{A-s}.

To obtain the maximum output for a given maximum speed and signal current, the degree of compensation of the machine should be as high as possible. In order that the condition of critical compensation may be more closely approached without introducing tendencies toward instability, the compensating coil *dA* should be arranged with respect to the signal coil *s* so as to allow a lower coefficient of coupling between the two. This coefficient of coupling theoretically should be the same as that between the compensating coil and the armature.

Experimental Data and Results

In order to check experimentally the theory and equations presented in this paper, an amplidyne generator was constructed from a General Electric type *RI*, three-horsepower, induction motor. The armature of this machine was used without modification since it was wound with a four-pole wave winding. The stator was rewound with a four-pole distributed winding, part of the slots being used for an interpole winding in order that satisfactory commutation could be obtained at the cross-axis brushes. The compensating coil *dA* consisted of 14 turns per coil, four coils per pole, with taps brought out so that 9, 10, 11, 12, 13, or 14 turns per coil could be used. The signal coil was wound with five turns per coil, four coils per pole. The interpole winding consisted of 50 turns surrounding each of the four stator teeth located in the cross-axis neutrals. While this design is not particularly efficient from the practical standpoint because of insufficient space for copper in the stator slots, it provides a satisfactory means for an experimental check of theory.

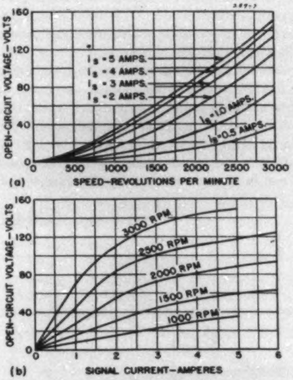


Figure 7. Experimental open-circuit characteristic curves of an amplidyne generator equipped with soft graphite brushes in the cross axis

In making adjustments for satisfactory operation of the machine, it was found that the higher taps of the compensating winding gave so much overcompensation that they could not be used. The machine would build up as a series generator to a very high value of armature current when the machine was loaded. On the highest tap the machine would oscillate at a frequency of approximately 80 cycles per second for very low values of load resistance.

Physically, the amplidyne generator may be considered a two-stage amplifier. The first stage is represented by the voltage generated in the cross-axis circuit by the flux produced in the main axis by the signal coil. The second stage is represented by the voltage generated in the output circuit by virtue of the flux established in the cross-axis circuit by the cross-axis current. When load current flows in the output circuit a component of flux, in addition to that produced by the signal coil, is produced in the main axis. This component adds to the flux of the signal coil if the machine is overcompensated and subtracts if the machine is undercompensated. Thus, an overcompensated amplidyne generator is in effect a regenerative amplifier. It is unstable if too much of the output is fed back into the first stage of the machine by virtue of too many turns in the compensating coil. On the other hand, an undercompensated amplidyne generator is a degenerative amplifier and is quite stable. For this reason the tests were made with the compensating coil *dA* connected with nine turns per coil, giving a slightly undercompensated machine.

Hard carbon brushes were entirely unsatisfactory, as illustrated by the curves of Figure 6, because of the comparatively low voltages generated in the cross-axis circuit. Soft graphite brushes gave very satisfactory operation, but in this case the adjustment of the interpole shunt was quite critical. Best conditions of cross-axis commutation were obtained with a diverter which would by-pass approximately ten per cent of the total cross-axis current.

The characteristics of the test machine after the necessary adjustments were made to secure satisfactory operation are illustrated by the curves of Figures 7, 8, and 9. According to theory, the curves of Figure 7a should be parabolas, while the curves of the other five figures should be straight lines. In actual operation of the machine several factors such as magnetic saturation and brush drop cause the curves to deviate from the theoretical form.

The total ampere turns acting to produce flux in the space between brushes *A* and *B* and between brushes *A′* and *B′*

(see Figure 1) is proportional to the *sum* of the ampere turns acting in the cross axis and the ampere turns acting in the main axis. The total ampere turns acting to produce flux in the space between brushes B and A' and between brushes B' and A is proportional to the *difference* between the ampere turns acting in the cross axis and the ampere turns acting in the main axis. Thus, the total flux and the degree of magnetic saturation are greater in the air gap between brushes A and B and between brushes A' and B' than between brushes B and A' and between brushes B' and A. The total voltage generated in the cross-axis circuit is proportional to the total flux distributed around the armature between brushes B and B', and the total voltage generated in the output circuit is proportional to the total flux distributed around the armature between brushes A and A'. Since the same components of flux are involved in the generation of both of these voltages, the effects of magnetic saturation appear simultaneously in both the cross-axis and main-axis circuits.

In Figure 7b, the 1,000-rpm curve is essentially linear for signal currents up to four amperes, and the 2,000-rpm curve for signal currents up to two amperes. At higher signal currents magnetic saturation caused a decrease in the machine inductances with the result that the curves fall below the points represented by the initial linear relationship.

Since the measured resistance of the cross-axis circuit was slightly greater than one ohm, the change of the resistance of the graphite brushes with current was sufficient to cause a noticeable effect. This accounts for the upward turn of the 0.5-ampere and 1.0-ampere curves of Figure 8a near 2,500 rpm. For the 2-ampere curve the effects of magnetic saturation were appreciable for speeds above 2,200 rpm (as illustrated by the corresponding curves of Figure 8b). The slight decrease in the generated cross-axis voltage at high speed due to saturation was offset by the decrease in brush resistance. The resulting curve of cross-axis current was linear.

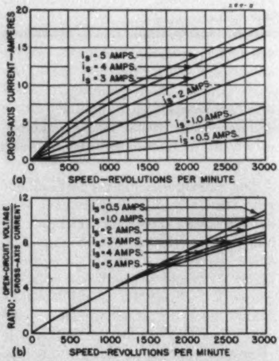


Figure 8. Open-circuit speed characteristics of the first and second stages respectively of an amplidyne generator

Nonlinearity of the curves of (b) are an indication of magnetic saturation of the machine

Characteristics of the Machine From Open-Circuit and Short-Circuit Tests

In order to calculate the operating characteristics of the amplidyne generator, machine constants must be used which are valid for actual conditions of operation. The values of the constants are influenced by magnetic saturation, by brush losses and commutation, and by hysteresis and eddy current losses. Therefore, any set of constants obtained from direct measurements of the impedances or inductances must be modified to include these factors. Such modifications are difficult to make accurately, since the effects of the losses on the machine constants cannot be expressed in simple quantitative terms. The necessity for making modifications can be eliminated to a large extent if the constants of the machine are calculated from test data from open-circuit and short-circuit tests performed on the machine.

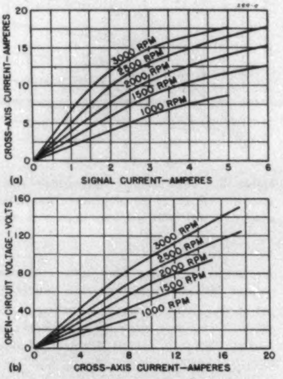


Figure 9. Open-circuit saturation curves of the first and second stages respectively of an amplidyne generator

Nonlinearity of the curves of (b) results from magnetic saturation of the machine, while nonlinearity of the curves of (a) result from magnetic saturation and brush drop at the cross-axis brushes

The two constants B and C are calculated from open-circuit test data in accordance with the following equations which result from equations 19 and 20 of the appendix:

$$\left.\begin{aligned}B&=i_B/Si_s\\C&=V_A/Si_B\end{aligned}\right\}\text{ Open-circuit test data used in these equations}$$

The constants A and R_A are calculated from short-circuit test data in accordance with the following equations which result from equations 21 and 22 of the appendix:

$$\left.\begin{aligned}R_A&=CSi_B/i_A\\A&=(Bi_s/i_A)-(R_A/CS^2)\end{aligned}\right\}\text{ Short-circuit test data used in these equations}$$

These four constants together with equations 19 to 23 are sufficient for the calculation of any of the steady-state characteristics of the amplidyne generator. The speed S in the equations above may be expressed in revolutions per minute instead of radians per second. In this case the speed S in equations 19 to 23 derived from these four constants must also be expressed in revolutions per minute.

The accuracy of this method for determining the characteristics of the amplidyne generator is shown by the curves of Figures 10 and 11, which were calculated on the basis of constants determined from open-circuit and short-circuit test data. For comparison actual test values are indicated by small circles and triangles plotted on the same axes.

In Figure 10a, the test points of the open-circuit voltage and cross-axis current curves for one-ampere signal current

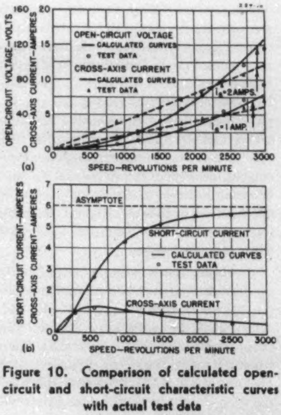


Figure 10. Comparison of calculated open-circuit and short-circuit characteristic curves with actual test data

fall above the calculated curves for speeds above 2,200 rpm. This discrepancy is due to the decrease in the resistance of the cross-axis brushes with increase in cross-axis current at the higher speeds. For the two-ampere signal current curve of cross-axis current, the change in brush resistance with current was offset by saturation, with the result that the test points agree very closely with the calculated curve. The test points of the corresponding open-circuit voltage curve fall below the calculated curve for speeds above 2,200 rpm because of saturation effects in the second stage of the machine.

Conclusions

Commutation and brush effects, particularly in the cross-axis circuit, have a very pronounced effect upon the operation of the amplidyne generator. Bad commutation and brush losses cause a considerable decrease in the available output. In order that the machine operate satisfactorily, the use of proper brush material and the realization of good commutation are imperative. To insure good commutation interpoles may be used to excellent advantage. The brushes in the cross-axis circuit must be of low resistance and produce as small a contact drop as possible.

The characteristics of the machine may be calculated on the basis of open-circuit inductances and resistances. The constants used for calculations must correspond with actual conditions of operation; but the main- and cross-axis coils may be treated as magnetically independent. The speed voltages appear in the opposite axes from the fluxes which produce them. The effects of magnetic saturation appear in both the main- and cross-axis circuits simultaneously. Saturation effects are difficult to include in calculations, because the degree of saturation changes with the speed and with the load.

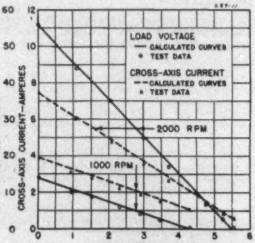


Figure 11. Comparison of calculated load characteristic curves with actual test data

Constants for the calculation of the steady-state characteristics of the amplidyne generator may be determined from open-circuit and short-circuit tests performed on the machine. This method offers the advantage that the values so obtained apply for actual conditions of operation. For purposes of design the characteristics may be predicted on the basis of the inductance coefficients, which may be obtained by the methods used for the more standard electrical machines by calculation of the various flux linkages involved. This method, however, is subject to considerable error because of the uncertainty of many factors, principally those relating to commutation, brush drop, and the associated losses.

The output load-voltage characteristic of an undercompensated machine is drooping (of negative slope) for all conditions of operation. The corresponding characteristic of an overcompensated machine may be either drooping or rising (of positive slope), depending upon the speed and the degree of overcompensation. Low speed together with a low degree of overcompensation gives a drooping characteristic which becomes horizontal and then rising if either of these two factors is sufficiently increased. A machine having a rising characteristic is unstable, even for steady-state operation, if the load resistance is less than a definite critical value which depends upon the slope of the characteristic curve.

Transient instability may result in an overcompensated or in an undercompensated machine, if the mutual inductance between the compensating coil and the signal coil is greater than the mutual inductance between the signal coil and the armature. Transient instability arising from this cause is characterized by the load current and the load voltage building up to very high values before coming to their final steady-state values when a sudden increase of load occurs. With a sudden decrease of load, the opposite effect takes place. In some instances this action may be cumulative and may result in free oscillation of the machine. If the compensating coil is arranged so that a fairly high leakage flux exists between it and the signal coil, a greater output is available from the machine without loss of stability.

The open-circuit voltage varies directly with the square of the speed, while the short-circuit current approaches asymptotically a definite value which depends upon the signal current and the design of the machine. For maximum power output and power amplification the speed should be as high as possible.

The available power output and also the power amplification become infinite for d-c operation of the machine at constant speed as the resistance of the cross-axis circuit approaches zero. As the resistance of the signal coil approaches zero, the power supplied by the signal source approaches zero, and the power amplification again becomes infinite. Since both of these resistances may be made quite low, the power amplification of an amplidyne generator operating on direct current is very high. The machine used in obtaining data for this paper gave a maximum value of approximately 300. This corresponds with an ordinary d-c generator having a field loss of one third of one per cent. By use of a specially designed stator core which would allow more copper in the signal coil and interpole windings, a power amplification of at least ten times this value could easily be obtained.

Appendix. D-C Operation of the Amplidyne Generator

If a d-c signal is applied to coil s (Figure 1), the steady-state behavior of the machine is expressed by the following equations:

$$e_s = i_s R_s \qquad (1)$$

$$e_q = i_B R_B = KS\phi_d 10^{-8} \qquad (2)$$

$$e_d = i_A R_A + V_A = KS\phi_q 10^{-8} \qquad (3)$$

If the speed of the machine is measured in electrical radians per second, the constant K is the equivalent number of series armature turns between opposite brushes, that is the number of series turns which would give the same number of flux linkages if each of these turns linked all the flux in either axis. The expressions $K\phi_d$ and $K\phi_q$ are the total number of armature flux linkages in the main and cross axes of the machine respectively. The total flux which is effective in producing speed voltage in the output circuit, that is ϕ_d, includes the mutual flux from coils dA and s as well as the leakage flux of the armature. Likewise, the total flux which is effective in producing speed voltage in the cross-axis circuit includes the mutual flux from coil qB as well as the leakage flux of the armature. In terms of open-circuit self- and mutual inductances the flux linkages in the main and cross axes are:

$$K\phi_d = N_A\phi_d = (M_{A-s}i_s - L_{AA}i_A + M_{A-dA}i_A)10^8 \qquad (4)$$

$$K\phi_q = N_A\phi_q = (L_{AA}i_B - M_{A-qB}i_B)10^8 \qquad (5)$$

The negative sign is used before the mutual inductance M_{A-qB}, because the coil qB is assumed connected so as to compensate partially for armature reaction in the cross axis. This sign should be positive if the coil is connected the other way. Making use of these relations, equations 2 and 3 may be written:

$$i_s S M_{A-s} - i_B R_B - i_A S(L_{AA} - M_{A-dA}) = 0 \qquad (6)$$

$$i_B S(L_{AA}-M_{A-qB})-i_A R_A=V_A \tag{7}$$

The open-circuit characteristics of the machine are obtained from the solution of these equations with the load current i_A equated to zero:

$$i_s=e_s/R_s \tag{8}$$

$$i_B=i_s S M_{A-s}/R_B \tag{9}$$

$$V_A=i_s S^2 M_{A-s}(L_{AA}-M_{A-qB})/R_B \tag{10}$$

The short-circuit characteristics of the machine are obtained from equations 1, 6, and 7 with the load voltage V_A equated to zero:

$$i_s=e_s/R_s \tag{11}$$

$$i_B=\frac{S M_{A-s} R_A}{S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})+R_A R_B}\,i_s \tag{12}$$

$$i_A=\frac{S^2 M_{A-s}(L_{AA}-M_{A-qB})}{S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})+R_A R_B}\,i_s \tag{13}$$

If the machine is supplying a constant resistance load, the voltage V_A in equation 7 is replaced by $i_A R_L$ in which R_L is the resistance of the load. The solutions for the three currents are exactly the same as the short-circuit characteristics except that the resistance of the main-axis circuit becomes (R_A+R_L) instead of R_A. The load voltage is found from the relation $V_A=i_A R_L$ after the currents are obtained.

The load characteristics of the machine are obtained from equations 6 and 7 by elimination of i_B and V_A respectively:

$$V_A=\frac{-[S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})+R_A R_B]}{R_B}i_A+\frac{S^2 M_{A-s}(L_{AA}-M_{A-qB})i_s}{R_B} \tag{14}$$

$$i_B=\frac{-S(L_{AA}-M_{A-dA})}{R_B}\,i_A+\frac{S M_{A-s} i_s}{R_B} \tag{15}$$

These latter two equations are valid regardless of the type of load supplied by the machine. Equation 14 expresses the relation between the three variables V_A, i_A, and i_s, and may be used to define the overall operation of the amplidyne generator in any control circuit in which it is connected, provided the machine operates low enough on the magnetization curve so that saturation effects may be neglected.

The equations given above may be simplified by the following substitutions:

$$A=(L_{AA}-M_{A-dA})/R_B \tag{16}$$

$$B=M_{A-s}/R_B \tag{17}$$

$$C=(L_{AA}-M_{A-qB}) \tag{18}$$

In terms of these constants the characteristic equations are:

Open-circuit characteristics:

$$i_B=BSi_s \tag{19}$$

$$V_A=BCS^2 i_s \tag{20}$$

Short-circuit characteristics:

$$i_B=\frac{BR_A S}{ACS^2+R_A}\,i_s \tag{21}$$

$$i_A=\frac{BCS^2}{ACS^2+R_A}\,i_s \tag{22}$$

Load characteristics:

$$V_A=-(ACS^2+R_A)i_A+BCS^2 i_s \tag{23}$$

$$i_B=-ASi_A+BSi_s \tag{24}$$

List of Symbols

e_d—Generated voltage in the main-axis or output circuit
e_q—Generated voltage in the cross-axis circuit
e_s—Voltage applied to the signal coil
L_{AA}—The self-inductance of the armature between either the main-axis brushes or between the cross-axis brushes
M_{A-dA}—The mutual inductance between the armature and the compensating coil dA
M_{A-qB}—The mutual inductance between the armature and the cross-axis coil qB
M_{A-s}—The mutual inductance between the armature and the signal coil s
M_{dA-s}—The mutual inductance between the compensating coil dA and the signal coil s
R_A—Resistance of the main-axis circuit internal to the machine. Includes the resistance of the armature, the main-axis brushes, and the compensating coil dA
R_B—Resistance of the cross-axis circuit. Includes the resistance of the armature, the cross-axis brushes, and the cross-axis coil qB
R_L—Resistance of the load impedance
R_s—Resistance of the signal coil s
S—Speed in electrical radians per second
V_A—Output or load voltage
ϕ_d—The total flux linking the armature in the main axis
ϕ_q—The total flux linking the armature in the cross axis

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Analysis of Short Circuits for Distribution Systems

CHARLES F. DALZIEL
MEMBER AIEE

Synopsis: This paper presents a discussion and analysis of short circuits for low-voltage distribution systems. Many distribution systems are supplied from transformers connected in delta on the low-voltage side with the mid-tap of one secondary winding, or with one corner of the delta, connected to ground. As a result, short circuits on these systems often involve failure to ground, and an accurate analysis for these faults is of value. An original analysis of power-leg-to-ground and light-leg-to-ground short circuits is presented together with formulas for line-to-line and three-phase faults. A study of short circuits on the secondary circuits of single-phase transformers supplied from three-phase systems is also included, thereby giving a comprehensive treatment of distribution short circuits. The paper also includes a discussion of the effects of resistance in limiting short-circuit currents and the voltage rise of the secondary neutral above ground potential caused by transformer failure and resistance to ground.

DISTRIBUTION transformer banks supplying 240-volt three-phase and 120/240-volt single-phase loads generally have the mid-point of one secondary winding connected to ground. This is done to protect the low-voltage distribution systems against high voltage in the event of transformer failure and to provide a ground for the low-voltage systems. An accurate analysis of short circuits and related problems on these systems is important in the selection and setting of protective equipment and in reduction of personal and fire hazards. The ratings of these transformer banks are from about 6 to 1,000 kva. The most common short circuit in conduits and machines is failure to ground. In addition to the unsymmetrical conditions imposed by a fault to ground, special considerations are required, since it is assumed that the transformer secondaries are connected in delta and the ground return is furnished by a connection to the mid-point of one secondary winding. Other connections are in common use. Three-phase four-wire circuits may be supplied from transformers with the low-voltage secondary windings wye-connected with the neutral grounded, and transformers without mid-taps supplying three-phase three-wire 240 volts often have one corner of the secondary delta connected to ground. This paper presents an analysis of power-leg-to-ground and light-leg-to-ground instantaneous symmetrical short-circuit currents and a discussion of short-circuit currents on distribution systems resulting from high-voltage transformer failures. Consideration of three-phase and line-to-line low-voltage short circuits and low-voltage short circuits on single-phase distribution transformers is also included.

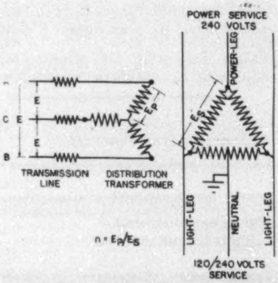

Figure 1. Typical distribution transformer with mid-point of one secondary winding grounded

The schematic diagram of Figure 1 gives the terminology and connections in common use for obtaining 240-volt three-phase three-wire and 120/240-volt single-phase three-wire services from a single transformer bank. For cases where the power load predominates, such as in industrial plants, the three transformers are ordinarily of the same capacity. Where single-phase loads predominate, for example, in rural installations, the two transformers used to furnish the power-leg are usually the same size, but they may be smaller than the third unit. In this study it is assumed that the three transformers are identical. However, in solving practical problems differences in capacity may be accurately taken into account for power-leg-to-ground short circuits, and approximations may be introduced for light-leg-to-ground faults.

Effect of Resistance in Limiting Low-Voltage Short-Circuit Currents

Although resistance may produce a negligible effect in limiting short-circuit currents on high-voltage systems, it may have a considerable effect when faults occur on low-voltage circuits.[1] It is evident that the higher the voltage, the higher are the ohmic values of equipment impedance. Therefore, impedances having about the same ohmic values regardless of voltage rating may produce a negligible effect on high-voltage short-circuit currents when in combination with high-impedance equipment, but they may be very important on low-voltage circuits when connected in series with other low-impedance apparatus.[2] For 120- or 240-volt circuits resistance is often the predominating current-limiting factor, and omission of it (in the interest of simplicity) may lead to serious error. The importance of resistance is illustrated in Figure 2, wherein line-to-ground fault current* is plotted against ground or fault resistance for a few selected voltages. Resistance is included in both analysis and examples of the present discussion.

Power-Leg-to-Ground Short-Circuit Analysis

TRANSFORMERS CONNECTED IN WYE-DELTA

It is apparent from inspection of Figure 3, that a power-leg-to-ground short circuit is a special type of single-phase fault. The voltage effective in producing the fault current is $\sqrt{3}E_s/2$. The fault current is equal to the sum of two equal low-voltage secondary currents which are inphase with each other. Neglecting the effects of load and transformer exciting current, the instantaneous symmetrical secondary current is given by:

$$I_s = \frac{\sqrt{3}E_s/2}{z_{TL}+z_{(S-P)}+0.5z_{(S/2-S/2)}+2z}$$

in which

E_s = the voltage of the secondary winding on open circuit, in volts

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CHARLES F. DALZIEL is associate professor in the department of electrical engineering and supervisor of engineering science and management, war training, University of California, Berkeley, Calif.

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* Refer to standard references for analysis of ordinary unsymmetrical short circuits (for example, reference 3 or 4).

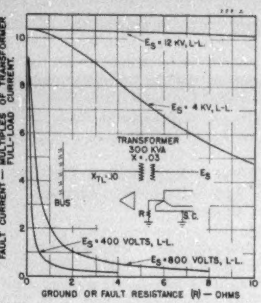


Figure 2. Effect of resistance in limiting short-circuit currents

z_{TL} = the impedance of the transmission line and other series equipment in ohms referred to the secondary voltage

$z_{(S-P)}$ = the impedance of the low-voltage secondary winding to the high-voltage primary winding in ohms (see Figure 3C)

$z_{(S/2-S/2)}$ = the impedance of one-half the low-voltage secondary winding to the other one-half low-voltage secondary winding in ohms (see Figure 3C)

z = The impedance of the external low-voltage circuit to fault, including arc, conductor, and conduit impedances, the resistance to ground, and the impedance of the ground return circuit in ohms

The fault current is:

$$I_F=2I_s=\frac{\sqrt{3}E_s}{z_{TL}+z_{(S-P)}+0.5z_{(S/2-S/2)}+2z}\qquad(1)$$

Let

E = the transmission-system voltage line-to-line, in volts

$n=E_p/E_s=E/\sqrt{3}E_s$, the transformer turn ratio

Then

$E_s=E/n\sqrt{3}$

and

$$I_F=\frac{E/n}{[Z_{TL}+Z_{(P-S)}]/n^2+0.5z_{(S/2-S/2)}+2z}\qquad(2)$$

where

Z_{TL} = the impedance of the transmission system in high-voltage ohms. This assumes that the reactance of one wire to neutral is the same for both single-phase and three-phase balanced currents. This is true for circuits with equilateral spacing or completely transposed circuits. It is unlikely that the distribution feeder would be either equilaterally spaced or completely transposed between the source and a particular transformer installation, and for greatest accuracy the actual single-phase reactance between the two high-voltage primary line conductors carrying current should be used

$Z_{(P-S)}$ = the impedance of the high-voltage primary winding to the low-voltage secondary winding in high-voltage ohms. See Figure 3C.

The primary line currents are obtained by inspection of Figure 3B:

$$I_A=I_B=I_s/n=I_F/2n,\text{ and }I_C=0\qquad(3)$$

TRANSFORMERS CONNECTED IN DELTA-DELTA

The analysis for the case in which the transformers are connected in delta-delta is similar to that of the preceding derivation with the exception of the modifications required due to the high-voltage delta connection. See Figure 4.

$$I_F=2I_s=\frac{\sqrt{3}E_s}{3z_{TL}+z_{(S-P)}+0.5z_{(S/2-S/2)}+2z}\qquad(4)$$

For this case $n=E_p/E_s=E/E_s$. In terms of the transmission voltage and actual transmission impedances in high-voltage ohms, the fault current is given by

$$I_F=\frac{\sqrt{3}E/n}{[3Z_{TL}+Z_{(P-S)}]/n^2+0.5z_{(S/2-S/2)}+2z}\qquad(5)$$

From inspection of Figure 4B

$$I_B=I_C=I_s/n=I_F/2n,\text{ and }I_A=2I_B=I_F/n\qquad(6)$$

Light-Leg-to-Ground Short-Circuit Analysis

TRANSFORMERS CONNECTED IN WYE-DELTA

The light-leg-to-ground short-circuit analysis is developed from the method of symmetrical components as follows: The schematic diagrams of Figure 5 are based on the assumption that the system is entirely symmetrical. The dissymetry imposed by the terminal conditions which represent this type of fault is inserted in the analysis at the appropriate point.

Let

$n=E_p/E_s=E/\sqrt{3}E_s$, the transformer turn ratio

z_{TL}' = the impedance of the transmission line and other series equipment in ohms referred to one-half the secondary voltage, that is,

$$z_{TL}'=Z_{TL}\Big/\left[\frac{E_p}{E_s/2}\right]^2=Z_{TL}/(2n)^2\quad\text{where}$$

Z_{TL} = the impedance of the transmission line and other series equipment in high-voltage ohms

$z_{(S/2-P)}$ = the impedance of one-half the low-voltage winding to the high-voltage primary winding in ohms, and

$z_{(S/2-P)}=Z_{(P-S/2)}/(2n)^2$*

$z_{(S/2-S)}$ = the impedance of one-half the low-voltage winding to the entire low-voltage winding in ohms (see Figure 5D)

z = the impedance of the external low-voltage circuit to fault, including arc, conductor, and conduit impedances, the resistance to ground, and the impedance of the ground return circuit, in ohms

The positive- and negative-sequence impedances to fault are obtained from inspection of Figure 5B. All impedances are actual ohms, or expressed in ohms referred to one-half the secondary voltage on open circuit. Z_{2F} is taken equal to Z_{1F} for determination of instantaneous symmetrical short-circuit currents. The proper generator impedances may be included in the term Z_{TL} when transient or sustained currents are desired.

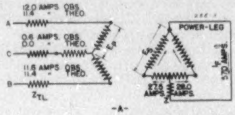
The positive-sequence impedance to fault is

$$Z_{1F}=z_{TL}'+z_{(S/2-P)}+z$$

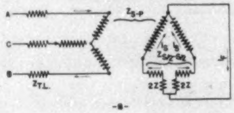
The zero-sequence impedance to fault is obtained from Figure 5C.:

$$Z_{0F}=z_{(S/2-S)}+z$$

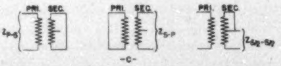
The equations for a line-to-ground



A. Schematic diagram with experimental values inserted



B. Equivalent circuit for analysis



C. Determination of transformer impedances

Figure 3 Power-leg-to-ground short-circuit analysis

Transformers connected in wye-delta

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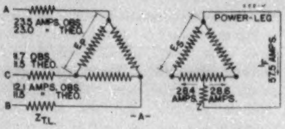
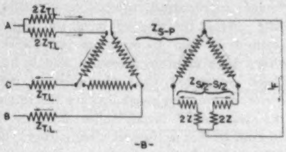
* The first subscript denotes the winding upon which the impedance measurements are made, or the side to which they are referred. The second subscript refers to the winding short-circuited for the impedance test. See Figure 5D.



A. Schematic diagram with experimental values inserted



B. Equivalent circuit for analysis

Figure 4. Power-leg-to-ground short-circuit analysis

Transformers connected in delta-delta

short circuit are (see reference 3 or 4 for details):

$$I_1 = I_2 = I_0 = I_F/3, \text{ and } \quad I_F = \frac{3E}{Z_{1F}+Z_{2F}+Z_{0F}}$$

The voltage effective in producing fault current is $E_s/2$. Substituting the above expressions for the impedance to fault and the voltage effective in producing the fault current gives

$$I_F = \frac{3E_s/2}{2[z_{TL}'+z_{(S/2-P)}]+z_{(S/2-S)}+3z} \qquad (7)$$

In terms of the transmission voltage and actual transmission impedances in high-voltage ohms, the above becomes

$$I_F = \frac{\sqrt{3}E/2n}{2[Z_{TL}+Z_{(P-S/2)}]/(2n)^2+z_{(S/2-S)}+3z} \qquad (8)$$

The primary currents are obtained from inspection of Figure 6:

$$I_C = (I_1+I_2)/2n = 2/3(I_F/2n) = I_F/3n, \text{ and } \quad I_A = I_B = I_F/6n \qquad (9)$$

TRANSFORMERS CONNECTED IN DELTA-DELTA

The analysis for the case in which the transformers are connected in delta-delta is similar to the foregoing with exceptions which are apparent from inspection of Figures 7 and 8.

$$Z_{1F} = Z_{2F} = z_{TL}'+z_{(S/2-P)}+z$$

For this case

$$z_{TL}' = Z_{TL} \Big/ \left[\frac{E_P/\sqrt{3}}{E_s/2}\right]^2 = 3Z_{TL}/(2n)^2$$

$$Z_{0F} = z_{(S/2-S-P)}+z$$

in which

$z_{(S/2-S-P)}$ = the impedance of one-half the low-voltage secondary winding to the entire low-voltage winding and high-voltage winding in ohms. See Figure 7D.

Following the procedure of the previous derivation:

$$I_F = \frac{3E_s/2}{2[z_{TL}'+z_{(S/2-P)}]+z_{(S/2-S-P)}+3z} \qquad (10)$$

Or, in terms of the transmission voltage and actual transmission impedances in high-voltage ohms

$$I_F = \frac{3E/2n}{2[3Z_{TL}+Z_{(P-S/2)}]/(2n)^2+z_{(S/2-S-P)}+3z} \qquad (11)$$

The primary line currents are obtained from inspection of Figure 8. The zero-sequence currents circulate in both delta-connected windings, and the relative division could be worked out by using the methods developed for analyzing three-winding transformers. However, since the zero-sequence currents cannot pass from the delta into the high-voltage lines, this refinement is not necessary, and the line currents are determined directly from the positive- and negative-sequence currents as indicated in the figure.

$$I_B = I_C = I_F/2n, \text{ and } I_A = 0 \qquad (12)$$

Three-Phase Short-Circuit Analysis

TRANSFORMERS CONNECTED IN DELTA ON LOW-VOLTAGE SIDE

Although three-phase short circuits are of infrequent occurrence on distribution circuits, they occasionally occur on open bus structures or in other equivalent locations. The fundamental equation for three-phase short-circuit current is

$$I_{3\phi} = \frac{E_s/\sqrt{3}}{z_{TL}''+z_{(S-P)}'+z'} \qquad (13)$$

in which

z_{TL}'' = the impedance of the transmission line and other series equipment in ohms referred to the secondary voltage to neutral. For transformers connected in wye-delta

$$z_{TL}'' = Z_{TL} \Big/ \left[\frac{E_P}{E_s/\sqrt{3}}\right]^2 = Z_{TL}/3n^2$$

For transformers connected in delta-delta

$$z_{TL}'' = Z_{TL} \Big/ \left[\frac{E_P/\sqrt{3}}{E_s/\sqrt{3}}\right]^2 = Z_{TL}/n^2$$

$z_{(S-P)}' = z_{(S-P)}/3$, the equivalent wye, or line-to-neutral impedance of the transformer bank, in low-voltage ohms.

z' = the line-to-neutral impedance of the external circuit to fault in low-voltage ohms including conductor impedance and fault impedance, that is, the impedance of one of three assumed equal arcs to their common meeting point.

Line-to-Line Short-Circuit Analysis

TRANSFORMERS CONNECTED IN DELTA ON LOW-VOLTAGE SIDE

Line-to-line short circuits frequently occur on exposed wires and in open bus structures. This type of fault is probably most frequent on distribution systems where one corner of the secondary delta is connected to ground. Analysis is made by using the method of symmetrical components.[3,4] In terms of the open-circuit secondary voltage, the low-voltage fault current is

$$I_{L-L} = \frac{E_s}{2[z_{TL}''+z_{(S-P)}']+z} \qquad (14)$$

in which the quantities have been previously defined. For this case z equals the total external impedance of the external circuit to fault in low-voltage ohms (in contrast to z' used for the three-phase analysis).

Short Circuits on Single-Phase Transformer Secondary Circuits

TRANSFORMERS SUPPLIED FROM TWO HIGH-VOLTAGE LINES

The problem of determining short-circuit currents on single-phase transformer secondary circuits is so important that it deserves mention. If the transformer and total external impedance to fault is referred to the high-voltage side, the equation is obtained from elementary methods by inspection:

$$I_{HT1\phi} = \frac{E}{2Z_{TL}+Z_{(P-S)}+Z} \qquad (15)$$

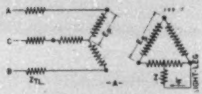
in which

$Z = n^2 z$, the impedance of the external circuit to fault in equivalent high-voltage ohms

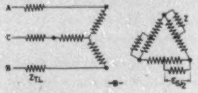
Or, the low-voltage secondary current equals:

$$I_F = \frac{E_s}{2Z_{TL}/n^2+z_{(S-P)}+z} \qquad (16)$$

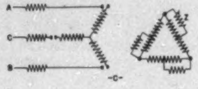
Equations 15 and 16 give correct results for symmetrical subtransient fault currents. The line-to-line fault analysis developed by using the method of symmetrical components is a better approach from the strictly technical viewpoint. This gives the same equations but has the advantage that the proper generator constants may be included in the transmission-system impedance term and



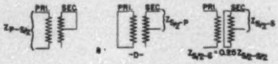
A. Schematic diagram



B. Positive-and negative-impedance network



C. Zero-impedance network



D. Determination of transformer impedances

Figure 5. Light-leg-to-ground short-circuit analysis

Transformers connected in wye-delta

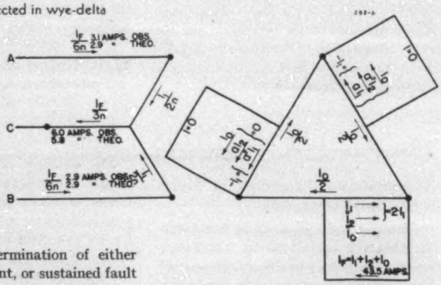


Figure 6. Detailed current distribution for a light-leg-to-ground short circuit

Transformers connected in wye-delta. Experimental values inserted

thereby permit determination of either subtransient, transient, or sustained fault current.

TRANSFORMERS SUPPLIED FROM ONE HIGH-VOLTAGE LINE AND NEUTRAL

Analysis for this case is similar to the preceding analysis, and from the method of symmetrical components

$$I_{HT1\phi}=\frac{3E/\sqrt{3}}{2Z_{TL}+Z_{0TL}+3Z_{(P-S)}+3Z} \qquad (17)$$

or the low-voltage secondary current equals

$$I_F=\frac{3E_s}{(2Z_{TL}+Z_{0TL})/n^2+3z_{(S-P)}+3z} \qquad (18)$$

in which Z_{0TL} = the zero-sequence impedance of the transmission system in high-voltage ohms.

The equations for line-to-ground faults on three-wire systems supplied from single-phase transformers having the mid-tap of the low-voltage secondary winding connected to ground are the same as the above with the following modifications: The voltage effective in producing short-circuit current is now $E_s/2$, the ratio of transformation is $2n$, and the transformer impedance is $z_{(S/2-P)}$.

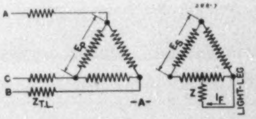
Short-Circuit Currents on Distribution Systems Resulting From High-Voltage Transformer Failures

During the initial stages of an internal failure between the high-voltage and low-voltage windings of a transformer, that is, before any of the conductors have been opened due to arcing, the high-voltage fault current flowing through the low-voltage secondary windings induces compensating currents in the high-voltage windings which circulate back through the high-voltage circuits. Also, under these conditions the transformer saturates because of high-voltage impressed on the low-voltage windings. Saturation neglected the requirement of equal and opposing ampere turns is met, and the current limiting effect of the transformer is due to leakage impedance. Actually, because of saturation, the effective impedance may be somewhat less than the leakage impedance. For purposes of illustration consider a three-phase distribution transformer supplied from a transmission line of reasonable length and assume that short circuits occur between each high-voltage terminal and the corresponding low-voltage terminal. The impedance limiting the fault current is equal to the transmission-system impedance up to the transformer terminals in high-voltage ohms, plus the impedance of the transformer in low-voltage ohms. Thus, the additional impedance opposing the high-voltage short-circuit current flowing in the secondary winding equals the transformer impedance in high-voltage ohms divided by the ratio of transformation squared (that is, n^2).

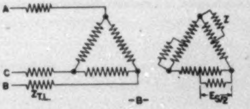
For most practical distribution systems n^2 is large, and the additional current-limiting effect due to the transformer is small. For 2,400 delta 240-volt transformers $n^2=100$, and the additional impedance is one per cent of the transformer leakage impedance (saturation neglected).

Where the windings have been opened, and compensating currents cannot flow, the current-limiting effect of the transformer is due to exciting impedance. Very high saturation results, and it has been suggested that the exciting impedance may approach leakage reactance in magnitude. Analysis for this case is similar to that in the preceding paragraph. The saturated exciting impedance in high-voltage ohms is reduced by the square of the turn ratio, and the effect of the transformer may be neglected as a first approximation.

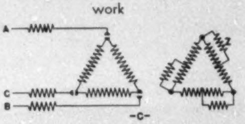
For unsymmetrical transformer failures, such as a breakdown near the end turns or between the high- and low-voltage bushings, the effect of transformer impedance is likewise small. The voltage effective in producing the fault current equals the vector sum of the voltage of the high-voltage system and the induced voltage of the secondary winding. In cases where the ratio of transformation is small, this effect may



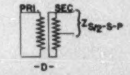
A. Schematic diagram



B. Positive- and negative-impedance network



C. Zero-impedance network



D. Determination of transformer impedances

Figure 7. Light-leg-to-ground short-circuit analysis

Transformers connected in delta-delta

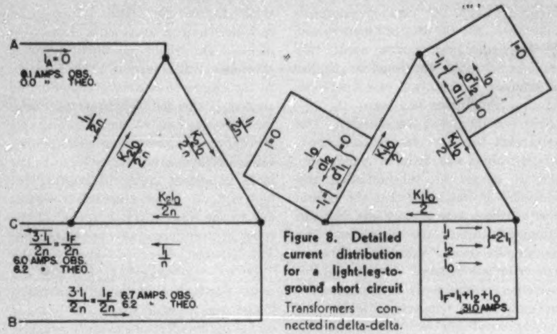


Figure 8. Detailed current distribution for a light-leg-to-ground short circuit

Transformers connected in delta-delta. Experimental values inserted

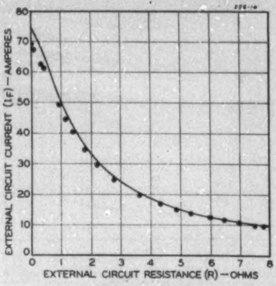


Figure 10. Power-leg-to-neutral experimental short circuits

Transformers connected in delta-delta

be important. However, for cases where the ratio is 10/1 or greater, this effect is also small and may be neglected for approximate work. An internal failure between the windings introduces additional complications, but it is unlikely that the fault current would be greater than that caused by breakdown near the extremities of the windings. Since the effects due to the transformer are relatively small, they may be neglected in estimating the high-voltage short-circuit current flowing to ground in low-voltage distribution circuits due to high-voltage transformer failures. Figure 13 gives a few of the laboratory connections used in the study of high-voltage transformer failures. The arrows represent the detailed current distribution and indicate the compensating primary currents induced by the high-voltage fault current flowing in the low-voltage secondary windings.

It is believed that the total high-voltage short-circuit current produced in low-voltage distribution circuits due to a distribution transformer failure can be determined with an accuracy sufficient for most practical purposes by neglecting the effects of the transformer itself. Additional impedances, such as the resistance of ground return circuit, must be included. It is obvious that the errors due to the approximation decrease as the impedance of the transmission system up to the transformer is increased, and conversely. It is believed this procedure is satisfactory for cases in which the feeder impedance is at least equal to 50 per cent of the transformer leakage impedance in high-voltage ohms. This analysis permits study of the effects of grounding resistances for distribution systems. It gives an approximate method of estimating the voltage to which a distribution system may rise above ground because of transformer failure, and it is important in the study of protective measures and shock hazard. Experience shows that the number of high-voltage failures in modern distribution transformers are relatively small in comparison to the number in service. However, breakdowns occasionally occur, and a mathematical approach to the problem is of value.

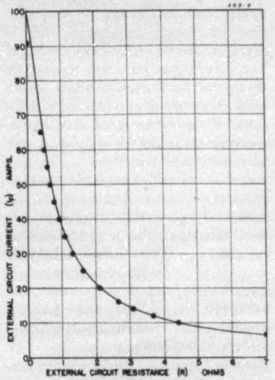


Figure 9. Power-leg-to-neutral experimental short circuits

Transformers connected in wye-delta

The total fault current in the low-voltage distribution circuit due to a high-voltage transformer breakdown is obtained from the conventional line-to-ground analysis, neglecting the effect of the transformer:

$$I_G = \frac{\sqrt{3}E}{2Z_{TL} + Z_{0TL} + 3z} \qquad (19)$$

The voltage rise of the secondary neutral above ground potential due to this fault current flowing through the impedance to ground equals:

$$E_{LT\ \text{neutral}} = \frac{\sqrt{3}Ez}{2Z_{TL} + Z_{0TL} + 3z} \qquad (20)$$

in which z represents the grounding impedance (that is, resistance of driven ground in ohms), and other quantities as defined previously.

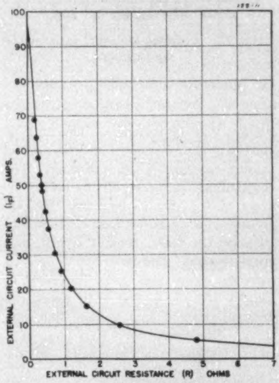


Figure 11. Light-leg-to-neutral experimental short circuits

Transformers connected in wye-delta

Experimental Results

Experiments were made on a laboratory model consisting of three reactors connected to simulate a short transmission line and four ten-kilovolt-ampere transformers having two tapped primary and secondary windings, rated 500/100 volts. The experimental points and theoretical curves for power-leg-to-neutral and light-leg-to-neutral short circuits are given in

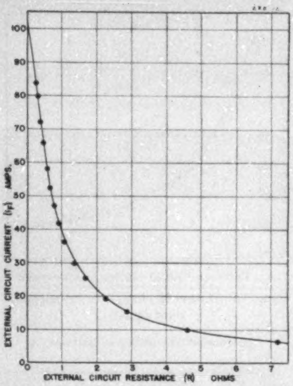


Figure 12. Light-leg-to-neutral experimental short circuits

Transformers connected in delta-delta

Figures 9 to 12 inclusive. The fault or current in the external circuit was plotted against external circuit resistance. The calculations included all known resistances of the circuit, including the resistance of a water-box load connected to simulate the fault, conduit return, and arc resistance. Theoretical and observed currents have also been inserted in Figures 3A, 4A, 6, and 8, to check the determination of high-voltage line currents. The transformers were connected 500 to 200 volts for these tests, and for Figures 11 and 12 three transformers were used. For the tests of Figures 9 and 10 four transformers were used; two transformers were connected in parallel to simulate the practical case where the lighting transformer had twice the capacity of the power transformers. The agreement between observed and theoretical values was believed satisfactory and is offered as substantiating the analysis. It was noted that the experimental values were slightly less than the computed curves, the discrepancy increasing for smaller values of resistance. The error was believed to be due to the resistance of the many leads and split-terminal socket connections which obviously could not be included in the analysis, to the fact that the system was not entirely symmetrical since the transformers and reactors did not have identical impedances respectively, and to slight unbalances of the supply voltages. The poorest correlation was obtained for the power-leg-to-neutral short circuit with the transformers connected in delta-delta. It should be noted that the discrepancies were on the safe side, as the measured values were low.

Several experiments were made to check the conclusions given in the discussion of high-voltage transformer failures. Short circuits with the transformers energized at reduced voltage were made to simulate primary to secondary failures using three transformers connected 1,000/100, 1,000/200, and 500/500. Typical results for various fault locations for the 1,000/100-volt connection are given in Figure 13. If the winding currents are multiplied by factors proportional to the turn ratio, primary and secondary ampere turns are in close agreement. The slight discrepancies from theoretical values are attributed largely to the effects of saturation and exciting current. This substantiates the statement that as long as compensating currents flow, transformer leakage impedance rather than exciting impedance, is the most important factor in limiting the fault current. As was expected, the effects due to the transformer decreased as the ratio of transformation was increased. The greatest observed error for the 1,000/100-volt connection was five per cent. For these tests the artificial transmission-line impedance was equal to 43 per cent of the transformer leakage impedance, and the ratio of Z_{0TL} to Z_{1TL} was varied between 1.0 and 5.5/1. From this it was concluded that the effects of the transformer may be neglected in approximate determination of the total short-circuit current in low-voltage circuits of practical distribution circuits because of high-voltage transformer failures for ratios of transformation of 10/1 or greater.

Application

For purposes of illustration, the results of this investigation were applied to a typical installation consisting of three 100-kva 2,400/240−120-volt distribution transformers supplied from an infinite bus over a four-kilovolt feeder. The feeder was three-conductor 4/0 copper having the common horizontal spacing of 23-30-53 inches, and $Z_{TL}=0.303+j0.622$ ohm per mile. It was assumed that $X_{0TL}=3.5X_{TL}$. The following constants were obtained from tests on one 100-kva unit.

$z_{(S-P)}=0.00691+j0.01895$ ohm or $0.012+j0.0329$ per unit

$z_{(S/2-P)}=0.00277+j0.00528$ ohm or $0.01925+j0.0366$ per unit

Figure 13. Study of effect of transformer in limiting short-circuit current for typical high-voltage transformer breakdowns

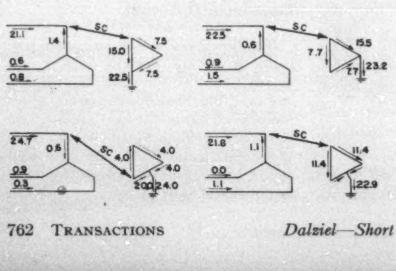
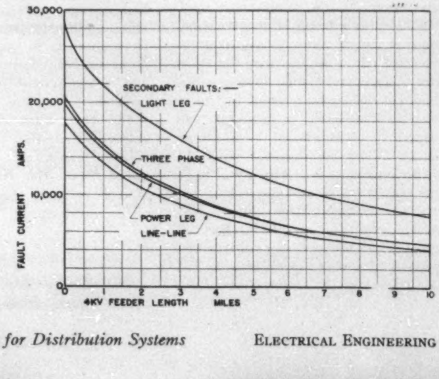


Figure 14. Short-circuit currents for 300-kva, 2,400-wye/240-120-volt transformer and four-kilovolt feeder

Fault resistance assumed zero



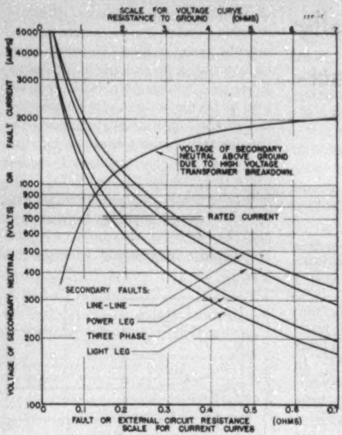


Figure 15. Short-circuit currents and voltage of secondary neutral for 300-kva 2,400-wye/240-120-volt transformer and 2.5 miles of four-kilovolt feeder

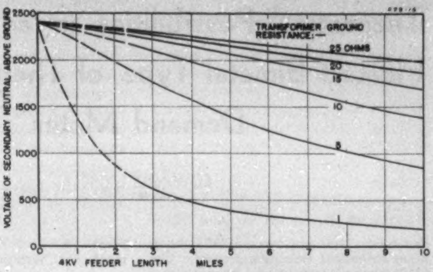


Figure 16. Voltage of secondary neutral above ground due to high-voltage transformer breakdown for 300-kva 2,400-wye/240-120-volt transformer and four-kilovolt feeder

$$z_{(s/2-s/2)} = 0.00355 + j \text{ nil} \quad \text{ohm or}$$
$$0.0246 + j0.000 \text{ per unit}$$

The transformers were assumed connected in wye-delta with the mid-point of one secondary winding connected to a ground having a variable resistance (R) ohms.

Figure 14 gives results for three-phase line-to-line power-leg-to-ground and light-leg-to-ground short circuits for various lengths of feeder up to ten miles. The fault or ground resistance was assumed zero for this case. It was noted that the light-leg-to-ground fault developed the greatest current, whereas the three-phase and power-leg short-circuit currents were approximately equal. Line-to-line short-circuit currents were the smallest and were equal to the three-phase value multiplied by 0.866.

The effects of fault resistance and resistance to ground are shown in Figure 15. For this case the feeder length was assumed to be $2^1/_2$ miles, and the fault currents were determined for external circuit resistances of 0 to 0.7 ohm. The power-leg-to-ground fault current decreased less rapidly than the light-leg fault current, the latter decreasing from its predominating value for zero resistance to values consistently below that of the other curves. It was interesting to note that if the line-to-line short-circuit current values are sketched in as a function of one-half the fault resistance, the curve practically coincides with the light-leg fault-current curve over the range covered in the figure. The important point brought out by the curves is the large current-limiting effect due to relatively small values of resistance.

The relatively large effect of small impedances in limiting low-voltage short-circuit currents has economic importance. The impedance of current transformers, resistance of joints, and the reactance due to bends in busses and connections may result in a material decrease in the interrupting duty of auxiliary circuit breakers or fuses in comparison with the main circuit breaker. Consideration of these factors in choosing the location of circuit breakers may point to improvements in plant layout with significant savings for large capacity low-voltage installations. Another aspect of this problem concerns the positive operation of circuit breakers or fuses. The protective devices would probably be set to clear for 150 to 250 per cent rated current. Entering the curves with these current values indicates that an external circuit resistance in excess of only 0.06 to 0.10 ohm would prevent proper clearing of faults. The advantage of several subfeeders with low trip values instead of one or two main circuits with high trip values or large fuses is self evident. In cases where high current-rating fuses are required, some form of ground protection (at the main circuit breaker) may be desirable.

The curve showing voltage rise of the low-voltage neutral above ground, due to a high-voltage transformer breakdown, was computed for resistances to ground of 0 to 7.0 ohms. It was found that the fault current would be limited to the rated low-voltage current of the transformer for a ground resistance of about 1.0 ohm. Hence, it was concluded that most transformer failures would be cleared only by the protective devices on the high-voltage side. During the time required to actuate the protective device, the voltage of the distribution system will be subjected to relatively high potential above ground. For example, the voltage above ground reaches 1,500 volts (the minimum dielectric strength of code rubber when new) for a ground resistance of three ohms, whereas a grounding resistance of five to seven ohms provides small protection indeed. The voltage rise of the secondary neutral above ground due to transformer failure for various resistances to ground is plotted as a function of four-kilovolt feeder length in Figure 16.

The results of the study emphasize the importance of securing low resistance to ground and low return-circuit resistances for distribution systems. It is realized that it is often difficult if not impossible to obtain driven or buried grounds having resistances low enough to provide satisfactory protection for low-voltage distribution circuits. This is particularly important for 240-volt three-phase three-wire installations where the mid-tap of one transformer is grounded and the neutral is not run into the plant. The author knows of one such installation consisting of a large transformer bank supplied over a regulated four-kilovolt feeder only a relatively short distance from the substation. The single driven ground at the transformer structure measured about 100 ohms. Although the plant conduits and equipment were apparently well grounded to the water system, little protection was actually afforded against either transformer breakdown or short circuits involving a ground return within the plant. Although the above remarks refer to the examples chosen for study, somewhat similar conclusions might result from analysis of many commercial installations.

Theoretical Possibilities in an Internally Heated Bimetal Type of Thermal Watt-Demand Meter

EDWARD LYNCH
MEMBER AIEE

Synopsis: There are two generally recognized methods of measuring demand. The block-interval type has been used extensively where great accuracy is desirable, a simple explanation necessary, and a quick testing method essential, but the "logarithmic" type has received increased attention in recent years, and a greater dissemination of knowledge of its characteristics is desirable.

A mathematical expression for "logarithmic" average is given which can apply to a variable load as well as to a uniform load. It is shown that a meter with a simple exponential time-deflection characteristic will indicate this "logarithmic" average, but that meters with characteristics obtainable in present commercial thermal meters will theoretically differ from it except when used with certain loads.

The accuracy of commercial meters has increased in recent years. So besides indicating that the various types of characteristics available in commercial meters have apparently been satisfactory on normally encountered loads, it is shown that sturdiness and high torques are desirable features to sustain these accuracies.

The internally heated thermal watt-demand principle is discussed, and the conclusion is reached that by its use high torque can be obtained using the time tested bimetal type of meter. In addition, the rapid diffusion of heat in the bimetal where it is generated theoretically allows shorter "heat-up" time when the meter is being tested and results in a time-deflection characteristic much closer to the ideal simple exponential.

DEMAND measurements when combined with watt-hour-meter readings have been recognized as giving a practical basis for electricity charges.[1,2] In the United States demand meters operating on the block-interval principle have been extensively used, and their operating principle and characteristics are well understood.

There is another recognized method of measuring demand which has been used to a limited extent for the same purpose. It is the method utilized by thermal demand meters, and the resulting indication has been called the "logarithmic average" demand. A general explanation of meter design considerations which enter into this demand indication and the operating characteristics which they affect will be covered in the following discussion.

Time Deflection Characteristic

The block-interval meter indicates continual arithmetic average maximum demand. The thermal meter obtains its average in a different manner, and it indicates a slightly different quantity which can be designated as continuous logarithmic average maximum demand. On a constant load its reading changes rapidly at first and then more slowly until, in the case of a meter which reaches 90 per cent of its indication in 15 minutes, which by definition makes it a 15-minute demand interval meter, it would be within about one per cent of its ultimate in 30 minutes. From this characteristic we see that any load on the meter during the 30 preceding minutes may have a readable influence on the demand pointer indication, but that the longer time that has elapsed, the less effect this load has on the reading. The indicating pointer on these meters is therefore continuously changing its position as load changes occur according to what has been called a "logarithmic" average.

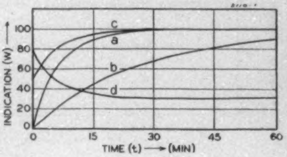


Figure 1. Curves of $W = W_1(1 - \epsilon^{-kt}) + W_0\epsilon^{-kt}$

	W_1	W_0	K
a	100	0	0.1535
b	100	0	0.0384
c	100	50	0.1535
d	30	80	0.1535

This average for a theoretical meter can be expressed as follows (see appendix B):

$$W = k\int_{-\infty}^{t} w\epsilon^{-k(t-t_1)}dt_1 \tag{1}$$

where

W = instantaneous deflection of pointer
k = a design constant
w = load (which may vary with time)
t = time at which deflection is W
t_1 = time at which load is w

On constant loads after about three demand intervals this "logarithmic" average is identical with the arithmetic average as given by a block-interval type of meter.

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EDWARD LYNCH is thermal device engineer, West Lynn works, General Electric Company, Lynn, Mass.

This study would hardly be complete without one or two constructive suggestions. The goal must be as low a resistance to ground as possible. The resistances of the ground circuit must be maintained sufficiently low to insure positive operation of the protective devices in case of transformer failure. In addition, it would appear that considerable protection would result if, in addition to providing the best ground that can be reasonably obtained at the transformer installation, the low-voltage neutral be carried along with the phase wires (regardless of whether or not it is to be used for power) and connected to the plant conduit system and grounded as effectively as possible on the customer's premises. The common neutral system of grounding would appear to provide an alternate method of accomplishing the same result.

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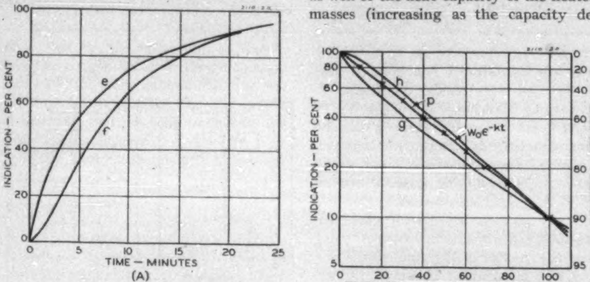
Simple Exponential Is Ideal

However, time-deflection characteristics of commercial thermal meters are not identical, and they differ from the characteristics of block-interval meters so that on fluctuating loads they can theoretically give slightly different readings. The following discussion of theoretical and practical time-deflection curves will indicate the desirability of a simple exponential characteristic for the thermal meter.



A. Load cut to zero after equilibrium temperature is reached

B. Constant load applied from zero

Figure 2. Time-indication curves of typical thermal watt-demand meters

Figure 1 shows typical theoretical time-indication characteristics of thermal meters when a constant load is applied. Curves *a* and *b* differ by the fact that the demand interval or time to reach 90 per cent indication is 15 minutes (k is 0.1535) for *a*, and 60 minutes (k is 0.0384) for *b*. Curves *c* and *d* have 15-minute demand intervals but start with an initial indication. Each curve can be expressed by the "exponential" equation:

$$W = W_1(1-\epsilon^{-kt}) + W_0\epsilon^{-kt} \qquad (2)$$

(See appendix A)

where

W = instantaneous deflection of pointer
W_0 = deflection of pointer at zero time
W_1 = ultimate deflection for load involved
$\epsilon = 2.718$
k = a design constant
t = time

(W, W_0, W_1 must be expressed in the same units, and k must be in units the reciprocal of the units for t.)

The well-known expressions $W = W_0\epsilon^{-kt}$ and $W = W_1(1-\epsilon^{-kt})$ for the transient return of the indication to zero when power is cut off, and the rise to ultimate deflection when a constant load is applied from zero deflection, can be seen to be special cases of the above more general equation. Equation 2 holds for conditions of constant applied load, whether or not the indication starts or ends at zero, and whether the indication increases or decreases. It is interesting to note that the instantaneous deflection is equivalent to the sum of two hypothetical indications, one of which starts at zero and rises to the ultimate deflection of the meter, and the other of which starts at the initial indication of the meter and reduces to zero, both following the locus given by equation 2.

It is also interesting to note that k, which determines the demand interval has a dimension equivalent to the reciprocal of time and is a function of the heat insulation of the heated parts (increasing as the insulation decreases), and a function as well of the heat capacity of the heated masses (increasing as the capacity decreases). This same k represents in magnitude the reciprocal of the time that would be necessary to attain proper deflection if there were no heat escape. It also represents the reciprocal of the time necessary to accomplish a change in indication represented by the following fraction of the total change:

The fraction is:

$$\left(1-\frac{1}{\epsilon}\right)=0.632 \text{ approximately}$$

If the load varies at a uniform rate (see appendix B), either increasing or decreasing, k has further physical significance because $1/k$ represents the time between the arrival at some particular watt load and the indication of that load by the meter, providing the rate has been uniform long enough (three demand intervals) for this time lag to become essentially constant. This time lag is independent of the rate of change of watts and the meter demand interval is directly proportional to it if we consider simple exponential characteristics.

Time-Deflection Curves in Meters

No commercial meter in the past has, to the writer's knowledge, attained exactly an indication given by equation 2. Curves *e* and *f* in Figures 2A and 2B represent typical time-deflection curves of actual meters which have been converted to equivalent demand intervals in *g* and *h* and are plotted decreasing from an initial value on semilog co-ordinates in order to easily compare them with the straight line *p* of $W_0\epsilon^{-kt}$.

An investigation of time-indication curves on any particular type of thermal meter indicates that there are small variations from meter to meter in the demand interval due to limitations in manufacturing control. We find also that in the same meter there are differences depending on the conditions of load and ambient temperature. We can reason from this that for a "15-minute demand interval" meter the actual demand interval should not be less than 15 minutes under any normal conditions of use in order not to read high on certain conditions of load, and we find practically all meters on the market which are considered satisfactory today for use as 15-minute demand interval meters fall within limits from 15 to 21 minutes interval.

When we consider differences in the curves in Figures 2A and 2B, we find another fundamental characteristic which although small might appreciably affect the meter readings in certain isolated conditions. This effect is easily demonstrated by applying a load which would give about full-scale deflection in 40 minutes and cut the load off at the end of five minutes. If we consider the "exponential" characteristic as shown in *p* as a standard of comparison, we find that a meter with a characteristic similar to *e* may read high by eight per cent of full scale, and a meter similar to *f* may read low by eight per cent at the instant the power is cut off. These should not be considered as errors as the definition of the time-deflection characteristic determines these differences. The meter following the *d* curve will continue to rise for a

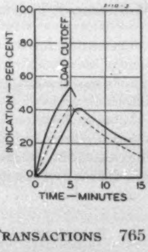


Figure 3. Time-indication curves for typical thermal watt-demand meters

Uniform load cut to zero before thermal equilibrium has been attained little time after the power is cut off but will not quite reach the maximum value reached by the "exponential" characteristic. Figure 3 shows this characteristic graphically. It will be noted that meters with the *f* characteristic lag behind changes in load, while meters with the simple "exponential" or the *e* type of characteristic will change direction immediately in the direction of the new load value.

Simple Exponential Curve Desirable

From this brief discussion we can reason that as far as thermal meters are concerned, subject, of course, to any considerations which rate engineers may find of importance, the simple "exponential" time-indication curve published as characteristic of thermal meters is desirable. It is desirable if an acceptable time-indication curve is to be defined, for it allows a relatively simple mathematical definition of the shape of the curve. And more important is the fact that it is the only characteristic which will indicate "logarithmic" average for all types of loads.

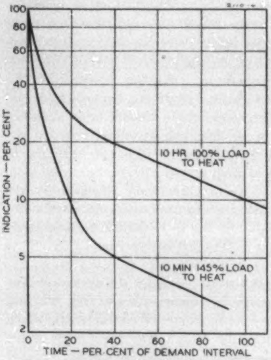


Figure 4. Time-indication curves of special thermal watt-demand meter

Cooling curves after loads as shown

Multibranch Thermal Circuits

The reason for the divergence of commercial-meter characteristics from the simple exponential has been attributed to diffusion.

Figure 4 shows performance curves of a 60-minute-interval thermal wattmeter illustrating an exaggerated condition of this diffusion which appeared in a paper presented by P. M. Lincoln before the Institute. The design constant, *k* was treated as a variable to account for the results. A slightly different treatment of the curves may be of interest and offers some advantages from a mathematical point of view in determining their shape.

The analogy shown in Figure 5 illustrates that multiple-branch thermal circuits may be expected to cool in a constant ambient according to the exponential:

$$W=W_0(A_1\epsilon^{-k_1t}+A_2\epsilon^{-k_2t}+A_3\epsilon^{-k_3t}\ldots+A_n\epsilon^{-k_nt}) \qquad (3)$$

We can use the following expression to predict changes in indication when any constant load is applied:

$$W=W_0(A_1\epsilon^{-k_1t}+A_2\epsilon^{-k_2t}\ldots+A_n\epsilon^{-k_nt})+W_1(1-A_1\epsilon^{-k_1t}-A_2\epsilon^{-k_2t}\ldots-A_n\epsilon^{-k_nt}) \qquad (3a)$$

In these equations A_1, A_2, k_1, k_2, and so forth are complicated functions of the storages and heat-transfer coefficients of the various branches. The form $W_0\epsilon^{-kt}$ referred to in various literature on thermal demand meters has been referred to above as a special case of equation 2, and equation 2 can be seen to be a special case of this more general equation 3a.

P. M. Lincoln has shown that the simplified expression is inadequate to explain the results of his tests unless we treat *k* as a variable. However, the results shown in Figure 4 can be predicted approximately from an equation such as 3, carried out to three terms as below:

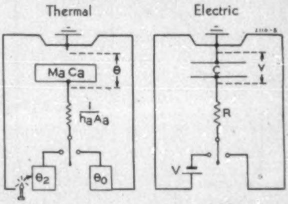
$$\text{Indication}=W_0(0.19\epsilon^{-0.921t}+0.51\epsilon^{-0.2302t}+0.3\epsilon^{-0.019t} \qquad (4)$$

This curve is plotted in Figure 6 where the first term is given as a dotted line, the second term as a dash line, and the third term as a dot and dash line, and the total as a full line. This curve checks reasonably with the per cent indication versus time curves (after ten-hour heating). Another group of curves (Figure 7) shows the heating curve for ten minutes at 145 per cent load and the following cooling curve when the load is removed, as given by constants in equation 4. Crosses indicate points calculated by considering the multiple-branch circuit as used in equation 4, and the circles represent points taken from Doctor Lincoln's meter data. The correlation is considered reasonably good for a first approximation of coefficients and exponents in the above equation. This allows us to think of meters of this nature as if they were the equivalent of multiple-branch thermal circuits with more than one constant *k*. Thus although such a meter reaches about 90 per cent of its final indication in 60 minutes, its final indication within reading limits will depend on the coefficient and power terms of one or more of the terms in equation 3. This may result in an extremely long time when considered from a testing point of view. In the above equation 4 for the 60-minute characteristic shown in Figure 6, k_1 corresponds to a demand interval of about 2.5 minutes and k_2 to an interval of about ten minutes and k_3 to an interval of about two hours. Doctor Lincoln has shown that meters with performance closer to the simple expression $W=W_0\epsilon^{-kt}$ can be built.

Time-Indication Curves of "Square" Scale Meters

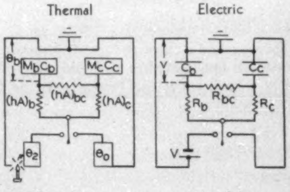
When we consider the time-indication curves of a demand meter with a "square"

A. Simple circuit



$$\theta=\theta_2\epsilon^{-\left(\frac{h_aA_a}{M_ac_a}\right)t} \qquad v=V\epsilon^{-\left(\frac{1}{RC}\right)t}$$

B. Multiple (two-branch) circuit



$$\theta_b=\theta_2[B_1\epsilon^{-b_1t}+B_2\epsilon^{-b_2t}] \qquad v_b=V[B_1\epsilon^{-b_1t}+B_2\epsilon^{-b_2t}]$$

B_1, B_2, b_1, b_2 are functions of M_bC_b, M_cC_c, $(hA)_b$, $(hA)_c$, $(hA)_{bc}$ or C_b, C_c, R_b, R_c, R_{bc}

Figure 5. Comparison of thermal and electric circuit

M = mass
c = specific heat
C = capacitance
h = heat transfer coefficient
A = area
R = resistance
θ = temperature difference
v = voltage
θ_2 = initial temperature difference
V = initial voltage
θ_0 = ambient temperature

scale such as those used for ampere demand, we encounter further characteristic differences. The time-deflection characteristic of the wattmeter (as a simple thermal circuit) in general form has been given in equation 2 for constant applied loads. The time-deflection characteristic of a "square" scale meter can be expressed similarly, but since the indication is not proportional to the deflection but is proportional to its square root, we find the expression for indication:

$$A=\sqrt{A_0^2\epsilon^{-kt}+A_1^2(1-\epsilon^{-kt})} \qquad (5)$$

where

A = instantaneous indication on a "square" scale (constant load)
A_0 = initial indication on a "square" scale
A_1 = final indication (constant load)

This reduces $A_0\epsilon^{-k/2\,t}$ when load is cut off. This has the same form as $W_0\epsilon^{-kt}$, and the cooling curve of time-indication will have the same general shape as the wattmeter, but since the exponent is $k/2$, it will take twice as long to cool to any percentage indication.

If the load starts at zero indication equation 5 reduces to

$$A=A_1\sqrt{1-\epsilon^{-kt}}$$

It can be seen that this does not have the same form as $W=W_1(1-\epsilon^{-kt})$, which is the equivalent on a uniform scale. These curves are shown graphically in Figure 8.

It may seem desirable to keep the shape of the time-indication curves of "square" scale and uniform scale meters on increasing loads (starting at zero) as nearly alike as possible. It has been proposed to decrease k for the ammeter to half the value for a wattmeter. This gives curve n shown in Figure 8 (as compared to curve j). It also gives a cooling curve m which is indeed slower (compared to i). Such a meter not only will differ in basic construction but also will theoretically differ on varying loads when compared to the uniform scale meter.

Another alternative suggested is to utilize the design and construction of the wattmeter, which requires changing the definition of demand interval so that the fraction of final indication of a "square" scale meter is the square root of that of a uniform scale meter. Thus, if the wattmeter is kept at 90 per cent for the demand interval definition, the ammeter would then be $\sqrt{0.90}=0.95$ or 95 per cent. Such a meter does not have the same characteristic as a uniform scale meter, and its reading will differ on varying loads when compared to the uniform scale meter. (See k and l of Figure 8.) However, if the basic characteristic of interest is the heating effect, then the characteristic of this meter favorably compares with that of a uniform scale wattmeter. Perhaps in such a case, the scale marking should be in terms of amperes squared.

It is interesting to note that differences of watt- and ampere-demand meter readings on normally encountered loads have been sufficiently small to allow the use of meters with many of the varying characteristics described above with satisfactory results.

Operating Characteristics

With a knowledge of the fundamental operation characteristic of thermal meters, let us consider how it is affected with time or with changing conditions of load power factor, ambient temperature, and so forth. One of the important considerations in reliability of a meter of this kind is mechanical stability in which sturdiness of construction and constant friction are important factors. These factors are especially significant in meters which are to be maintained with a minimum of skilled servicing and equipment. Most meters with maximum indicating pointers have friction clutches to hold one pointer in the position of its maximum indication. Experience over a long period of time has shown that there is a more or less definite range of optimum friction in such cases. This is due to the fact that over the range that is normally used, the friction becomes more constant, the greater the magnitude of the friction. However, at the higher values of friction, its magnitude is objectionable, and at the very low values its variation with time, temperature conditions, and so forth, is equally objectionable. A value from 30 gram-millimeters up, in a design which has been used for many years, has proved very satisfactory as far as consistency is concerned.

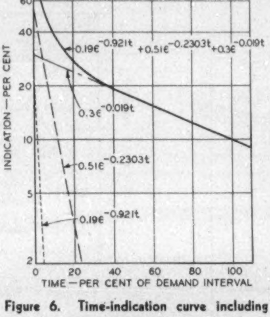


Figure 6. Time-indication curve including components

High Torque Desirable

To utilize frictions of this order of magnitude and still have them only approximately $1^1/_2$ or 2 per cent of the full-scale meter torque requires a meter torque of 1,500 to 2,500 gram-millimeters or a torque gradient of about 20 to 35 gram-millimeters per degree. Most thermal meters on the market today of the bimetal type do not utilize such high torques and rely on the lower friction torque remaining as constant as is required. The limiting features in obtaining higher torques are:

1. The amount of bimetal that is used, which is limited by other considerations such as space requirements, and so forth, and which has a definite influence on the demand interval of the meter.
2. The temperature at which the radiation or convection becomes an appreciable factor and the maximum temperature that the structure can stand.

To increase the operating temperature of the temperature-sensitive element without raising the temperature of the insulating parts of the heater requires better efficiency of heat transmission from the heater to the heated element. In recent years progress has been made toward greater efficiency and at the same time toward limiting convection and radiation by resorting to a bourdon tube actuating element with its bulb covered by a thermos bottle. This has gone a long way toward the achievement of high torque with low watts input. Inherent in a bourdon-tube design, however, is the hazard of leakage, one that must be eliminated by proper design, taking into consideration the long ranges of loads, power factors, and ambient temperatures over which modern meters are expected to function, and realizing the pressures that are set up in the tubes to meet these conditions. Added to this problem is one of choosing the best liquid for the bourdon tube. The problem is to obtain one which will not cause difficulty at lower temperatures and, at the same time, will have satisfactory temperature-expansion characteristics over the range required. With the use of demand meters in outdoor installations, this limiting feature assumes importance for temperatures below 32 degrees Fahrenheit and more especially, for temperatures as low as −40 degrees Fahrenheit, which although perhaps not common, might certainly be encountered in some installations. Many liquids are available and much experimental work has been done in this direction.[3]

Internally Heated Bimetal System

But when we consider difficulties of this nature together with others such as the hysteresis effects in bourdon-tube springs, and so forth, we realize the desirability of finding another method of attaining better heat-transmission efficiency which can be used with the time-tested bimetal spiral construction. Such a method is available if we heat the element by current through the bimetal itself. Here no loss is encountered, because the heat is generated where it is wanted. We thus get the highest possible efficiency of transmission and can limit radiation by means of controlled surfaces and convection by means of limited air spaces which, because of research work done in recent years, offers a practical solution.

If this arrangement is utilized to obtain the maximum efficiency of heat transmission possible, full-scale torques of the order of 2,000 grams-millimeters are readily obtained, and we can continue to use the time-tested bimetals as the activating element, where past experience and the vast amount of work that has been done by bimetal companies and meter manufacturers on heat treatment and the aging process insures stability of the bimetal characteristics. Moreover, uniformity of bimetal characteristics has recently been the subject of intensive investigation and has resulted in closer perfection of bimetal spirals with far more consistent characteristics than have been obtainable in the past.

Effect of Temperature Coefficient of Bimetals

One of the interesting phases encountered in taking advantage of the internally heated principle is occasioned by the fact that all suitable bimetals have a positive temperature-resistance coefficient. This has led to the following equations from a simple mathematical solution of Figure 10 giving the indication of such a meter:

$$W=4I_BI\cos\theta\left[\frac{R_1R_2}{R_1+R_2}\right]+I_B{}^2[R_1-R_2]-I^2\left[\frac{R_1R_2(R_1-R_2)}{(R_1+R_2)^2}\right]$$

Since

$$I_B=\frac{E}{R_1+R_2}$$

Therefore:

$$W=4EI\cos\theta\left[\frac{R_1R_2}{(R_1+R_2)^2}\right]+E^2\left[\frac{R_1-R_2}{(R_1+R_2)^2}\right]-I^2\left[\frac{R_1R_2(R_1-R_2)}{(R_1+R_2)^2}\right]$$

If:

$R_1=aR_2$ where a may be a variable as load conditions vary

$$W=4EI\cos\theta\left[\frac{a}{a+1}\right]+\frac{E^2}{R_2}\left[\frac{a-1}{(a+1)^2}\right]-I^2R_2\left[\frac{a(a-1)}{(a+1)^2}\right] \quad (6)$$

This general equation enables us to make a selection of a value for a when the meter is not excited and a value for the ratio of I to I_B which will give satisfactory values for W when $EI\cos\theta$ is kept constant, and ambient temperature, voltage, power factor, or load is changed through limits ordinarily met in service. These are two design constants which the design engineer can vary to obtain the optimum condition for this sort of meter.

When a is equal to 1.0, the last two terms of this equation disappear. However, if a differs slightly from 1.0 we can take advantage of the changes in these last two terms with temperature, power factor, and so forth, to obtain some slight compensation for small inherent errors.

An important bimetal characteristic which enables a simple mechanical construction to meet these requirements is the fact that the torque per degree temperature rise is independent of the length of a bimetal coil. We can therefore buck two bimetal coils on a single shaft to obtain complete temperature compensation at no load, and at the same time get the required ratio in resistances by using coils of different lengths. Modern bimetal-coil manufacturing procedure has made possible the satisfactory matching of coils of this nature.

Data on Actual Meter

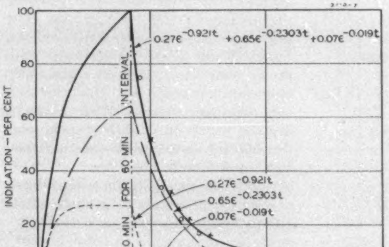
It is interesting to note how these observations are confirmed in a sample meter built to take advantage of this high efficiency heat generation thus giving a high torque bimetal meter. Figure 9 illustrates such a meter and Figure 10 gives its wiring diagram. The most important operation characteristics are tabulated in Table I and indicate the possibilities which may be utilized in development along these lines. Important is the fact that generation of heat in the bimetals allows such rapid diffusion of heat that there are possibilities that the necessary voltage heat-up before test can be shortened and that the time deflection curves actually will follow the desirable simple exponential curve.

Conclusions

A great deal must be discussed in elaborating on the various time-deflection characteristics obtainable in thermal watt-demand meters but experience has indicated that since demand is not an exact quantity, and since rate structures must vary for different classifications of load,

Figure 7. Time-indication curves

Constant load cut to zero before thermal equilibrium has been attained (showing components)



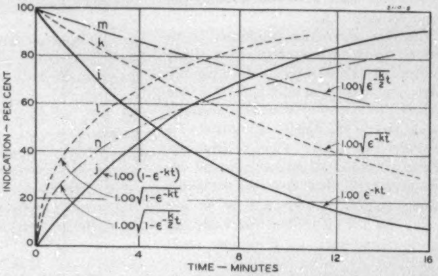


Figure 8. Time-indication curves

Two types of "square" scale meters compared to a uniform scale meter the various kinds of characteristics discussed above all seem to be satisfactory for most demand measurements.

Accuracy of thermal meters today is considerably better than it was several years ago, and sturdiness, together with high torque, are desirable qualities to sustain this greater accuracy.

This high torque is obtainable with the time-tested bimetal type of thermal wattmeter by taking advantage of the "internally heated" principle which gives maximum heat transfer efficiency and gives a time of response characteristic much closer to the ideal simple exponential than has been attained in commercial meters in the past.

The preceding data of a typical meter developed to give high torque by means of internally heated bimetal construction indicates that a superior all-around meter has been designed, and that this principle of operation is worthy of consideration as the basic design of thermal watt-demand meters.

Appendix A[6]

The general time-deflection response equation is developed here for a device indicating the difference in temperature of two similar bodies connected by a thermal shunt when constant heat inputs (or zero input) is applied to each body, the bodies starting at any initial temperature or temperature difference.

Let

H_1 = rate of heat applied to A (gram calories per second)
H_2 = rate of heat applied to B (gram calories per second)
h = thermal conductivity from A or B to ambient (gram calories per second per degree centigrade)
Q = thermal conductivity of shunt (gram calories per second per degree centigrade)
θ_{01} = initial temperature of A (degrees centigrade)
θ_{02} = initial temperature of B (degrees centigrade)
θ_1 = instantaneous temperature of A above θ_{01} (degrees centigrade)
θ_2 = instantaneous temperature of B above θ_{02} (degrees centigrade)
M = heat stored in A or B per degree centigrade (gram calories)

The conservation of energy equations become

$$H_1dt-Q(\theta_1-\theta_2+\theta_{01}-\theta_{02})dt=h(\theta_{01}+\theta_1)dt+Md\theta_1$$

$$H_2dt+Q(\theta_1-\theta_2+\theta_{01}-\theta_{02})dt=h(\theta_{02}+\theta_2)dt+Md\theta_2$$

Let

$$\theta_{01}-\theta_{02}=W_0 \text{ and } (\theta_{01}+\theta_1)-(\theta_{02}+\theta_2)=W \text{ and } \theta_1-\theta_2=W_2$$

$$d\theta_1+d\theta_2=d(\theta_1+\theta_2)=dW_2$$

The time-deflection response of thermal meters is proportional to the temperature difference W.

Then by subtraction and substitution

$$(H_1-H_2)dt-2Q(W_2+W_0)dt=h(W_2+W_0)dt+MdW_2$$

$$\frac{dW_2}{dt}+\frac{h+2Q}{M}W_2=\frac{H_1-H_2}{M}-\frac{h-2Q}{M}W_0$$

Let

$p=d/dt$, and $\mathbf{1}$ = Heaviside's unit operator (see reference 4)

$$\left[p+\frac{h+2Q}{M}\right]W_2=\left[\frac{H_1-H_2}{M}-\frac{h+2Q}{M}W_0\right]\mathbf{1}$$

$$W_2(t)=\left[\frac{H_1-H_2}{M}-\frac{h+2Q}{M}W_0\right]\times\left[\frac{1}{p+\frac{h+2Q}{M}}\right]\mathbf{1} \quad (1)$$

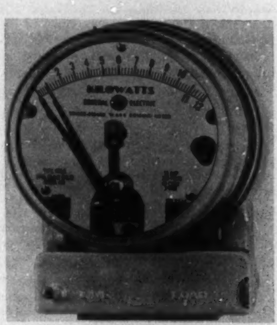


Figure 9. Single-phase watt-demand meter

15 amperes, 240 volts, 60-cycle, three-wire

From simple operational calculus

$$W_2=\left[\frac{H_1-H_2}{M}\right]\left[\frac{M}{h+2Q}\right]\times\left[1-\epsilon^{-\frac{h+2Q}{M}t}\right]-\left[1-\epsilon^{-\frac{h+2Q}{M}t}\right]W_0$$

By definition above

$$W=W_2+W_0$$

$$W=\left[\frac{H_1-H_2}{h+2Q}\right]\left[1-\epsilon^{-\frac{h+2Q}{M}t}\right]+W_0\epsilon^{-\frac{h+2Q}{M}t}$$

Let

$$\frac{H_1-H_2}{h+2Q}=W_1 \text{ and } \frac{h+2Q}{M}=k$$

Then

$$W=W_1(1-\epsilon^{-kt})+W_0\epsilon^{-kt} \quad (2)$$

Appendix B

The response of an object such as a theoretical thermal watt-meter is developed here for three conditions of loading:

(*a*). Where the load is any function of time.
(*b*). Where the load is constant.
(*c*). Where the load increases at a constant rate.

The response is of such a nature that it is directly proportional to the temperature of a mass where the mass is supplied with a heat input proportional to the load; and this input is utilized both in raising the temperature of the mass and in supplying the heat escape which is proportional to the temperature of the mass.

Let

h = thermal conductivity from mass to ambient (gram calories per second per degree centigrade)

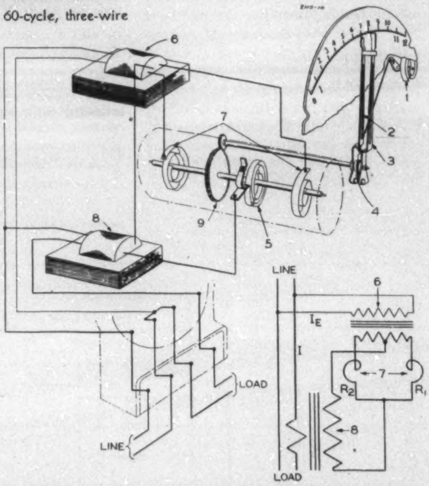


Figure 10. Wiring diagram for single-phase watt-demand meter

1. Calibration adjustment
2. Kilowatt indicator
3. Maximum - demand indicator
4. Friction clutch
5. Zero adjustment spring
6. Potential transformer
7. Thermostat metal
8. Current transformer
9. Drive gear

Table I. Characteristics of a 15-Ampere 240-Volt Three-Wire Single-Phase Thermal Watt-Demand Meter

(Typical Average Values for an Experimental Internally Heated Design)

Torque (full-scale)		2,200 g-mm
Scale length		Approximately 4 inches
Power factor influence (7.2 kw) referred to pf 1.00	0.5 pf lag	Less than 2%
	0.5 pf lead	Less than 2%
Frequency influence referred to 60 cycles		Negligible
Temperature influence (12.0 kw, 1.0 pf) referred to 25 C	+50 C	Less than 2%
	−20 C	Less than 2%
Voltage influence (10% change in voltage) referred to rated voltage		Less than 2%
Rising time-deflection curve shape		"Simple exponential" (Figure 2B, curve p)
Demand interval		Approximately 17 minutes
Watts loss	Potential (240 volts)	2.5
	Current (12 kw, pf 1.0)	9.0
Size		Standard single-phase watt-hour meter size

$hf(t_1)$ = rate of heat applied (gram calories per second)

θ = temperature (instantaneous) of mass which is proportional to meter deflection (degrees centigrade)

t = time (minutes)

W = angular deflection

The conservation of energy equation becomes

$$hf(t_1)dt = h\theta dt + Md\theta$$

Then

$$\frac{d\theta}{dt} = \frac{h}{M}[f(t_1)-\theta] = k[f(t_1)-\theta] \text{ where } k = \frac{h}{M}$$

Let

$\frac{d}{dt} = p$, and $\mathbf{1}$ = Heaviside's unit operator (see reference 4)

$$(p+k)\theta = kf(t_1)\ \mathbf{1}$$

$$\theta = \frac{k}{p+k}[f(t_1)]1 = k\left[\frac{1}{p}\right]\left[\frac{p}{p+k}\right][f(t_1)]\ \mathbf{1}$$

From Borel's theorem:

$$Y_1(p) = \frac{p}{p+k}1 = \epsilon^{-kt_1}$$

$$Y_2(p) = f(t_1)\ 1$$

$$\theta = k\int_0^t \epsilon^{-k(t-t_1)}f(t_1)dt_1 \qquad \text{(1a)}$$

This shows that the temperature θ of the indication W is proportional to the sum of all the instantaneous values of $f(t_1)$ each multiplied by a factor, $\epsilon^{-k(t-t_1)}$, which decreases exponentially as the time, t_1, of the instantaneous value of $f(t_1)$ is taken backwards from the time, t. The value t is the time of the instantaneous value of the indication W or temperature θ being considered.

(a). This function,

$$\kappa\int_0^t [f(t_1)][\epsilon^{-k(t-t_1)}]dt_1$$

can be designated by definition as the logarithmic average of $f(t_1)$ at any time, t, and is the value a simple theoretical thermal watt-demand meter indicates.

(b). If $f(t_1)$ is a constant such as W_1 after starting from zero indication, the expression becomes

$$W_1(1-\epsilon^{-kt})$$

When the transient ϵ^{-kt} becomes small, the meter will read W_1 or the equivalent of a block interval type of meter.

(c). But if $f(t_1)$ is directly proportional to time such as At the expression reduces to

$$At - \frac{A}{k} + \frac{A}{k}\epsilon^{-kt}$$

This indicates that after the transient $A/k(\epsilon^{-kt})$ becomes small (about three demand intervals), the meter will have approached a constant increase in deflection:

$$\frac{d\theta}{dt} \text{ when } \epsilon^{-kt} \text{ is small} = \frac{d}{dt}\left(At - \frac{A}{k}\right) = A$$

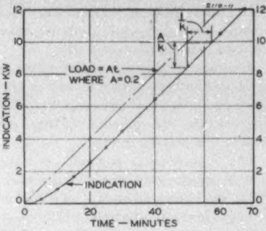


Figure 11. Indication lag of a thermal watt-demand meter on a constantly increasing load

x—points for curve

$$At - \frac{A}{k} + \frac{A}{k}\epsilon^{-kt}$$

where $A = 0.2$

$k = 0.135$

(demand interval = 17 minutes

Also since the deflection, if it followed watts input, instantaneously would be proportional to At.

And since the deflection, after the transient term becomes small, is $At - A/k$; therefore the reading will lag in deflection by A/k, and since the rate of change of deflection is A, the reading will lag in time by $1/k$.

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Practical Calculation of Circuit Transient Recovery Voltages

J. A. ADAMS
ASSOCIATE AIEE

W. F. SKEATS
MEMBER AIEE

R. C. VAN SICKLE
MEMBER AIEE

T. G. A. SILLERS
ASSOCIATE AIEE

WHILE there have been a number of papers on recovery voltage and its effect on the operation of circuit-interrupting devices, and although the transient recovery voltage and its characteristic are assuming a more important place in the design and application of circuit breakers with the growth and increasing capacity of power systems, there has not been available to the average power-company engineer a convenient method for determining this characteristic. The present paper offers such a method and also tabulates capacitance data for the more important circuit elements.

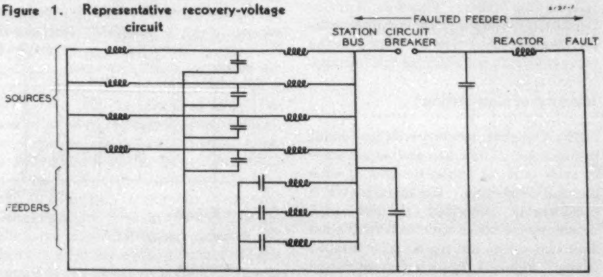
This method was developed in connection with a survey conducted by the Association of Edison Illuminating Companies to obtain a comprehensive picture of existing circuit recovery-voltage conditions. In order to review the characteristics for practically all breaker locations on the six power systems studied, it was necessary to develop a method that could be applied with minimum effort and time. The results of this survey are given in the companion paper, "Transient Recovery-Voltage Characteristics of Electric-Power Systems," by H. P. St. Clair and J. A. Adams.[11]

For different locations having the same voltage and short-circuit current duties, the transient recovery characteristic may vary with high-voltage values of approximately the same magnitude occurring over a time range of almost one hundred to one. From one end of this range to the other considerable differences may occur in the performance of some types of breaker. However, a moderate change in time to the first high peak usually produces a comparatively small change in breaker performance; a change in this time of ten to one, for instance, might produce a two-to-one change in arc length. In this situation it will be obvious that high accuracy is not required in the determination of the recovery transient, so that approximations which might be intolerable in some other applications become here quite permissible. This is very fortunate, for the circuits involved are far too complex for precise mathematical analysis and in many cases do not lend themselves well even to miniature system analysis. With a certain amount of approximation, however, these circuits may be simplified to the point where a mathematical solution is not too arduous. It is believed that the method outlined below will give results with an error of less than 20 per cent, which is considered quite reasonable. Tests have shown that in some cases much greater accuracy can be expected. Only sufficient discussion is given to explain the method of calculation and for a more complete analysis of the problem reference should be made to the various papers listed in the bibliography.

Definitions and Basis of Calculation

The circuit recovery voltage is the voltage that appears across the contacts of a circuit-interrupting device immediately after it opens the circuit. Just before interruption the voltage across the contacts is limited to the arc voltage, and at the instant the arc is extinguished the voltage attempts to recover to the generated value. Since most short circuits are limited principally by reactance, in the usual case of a symmetrical current wave, the generated voltage is near its peak value when the circuit is interrupted at current zero and, therefore, the voltage recovery is from practically zero to peak voltage. This recovery is prevented from taking place instantaneously by the capacitance of the circuit and, because of the circuit characteristics, it may oscillate at high frequencies to nearly double its final value. Usually the capacitances involved are very small, and the recovery occurs in a matter of microseconds. For

Figure 1. Representative recovery-voltage circuit



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J. A. ADAMS is senior engineer of the connections and control group in the engineering department, Philadelphia Electric Company, Philadelphia, Pa.; W. F. SKEATS is technical assistant to the works engineer, General Electric Company, Philadelphia, Pa.; R. C. VAN SICKLE is in the circuit breaker and protective devices engineering department of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., and T. G. A. SILLERS is switchgear engineer for Allis-Chalmers Manufacturing Company, West Allis, Wis.

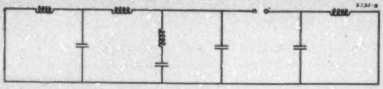


Figure 2. Simplified approximation of Figure 1

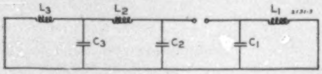


Figure 3. Simplified approximation of Figure 2

Table II. Capacitance of Circuit 3

		Microfarads
Leads to 2 transformers	2×0.584	=1.168
2 transformers	2×0.0048	=0.0096
2 generators	2×0.13	=0.26
2nd bus section (similar to first)		=0.02
4 lines		=4.0
		5.4576
		(Use 5.45)

successful interruption the dielectric strength between the contacts of the interrupting device must exceed at all times the transient recovery voltage.

The method of calculation gives the *circuit* transient recovery-voltage characteristic and does not take into account any modifying action that the breaker may have on the characteristic. On an actual circuit the recovery-voltage characteristic may be modified by one or more of the following factors:

1. Asymmetry of the current wave.
2. Decay of flux in the generators during short circuit.
3. Arc voltage drop in the fault.
4. Arc voltage drop in the breaker.
5. Conduction current in the breaker after current zero.

These factors usually reduce the severity of the recovery-voltage characteristic. In order to compare calculated with test results, it is necessary to account for these factors when analyzing oscillograms obtained on test.

The calculation of the transient recovery-voltage characteristic involves the determination of the natural frequencies and voltages of the circuits affecting the recovery voltage from the inductances and capacitances of these circuits, and the combination of these values to obtain the voltage wave across the open breaker contacts. It is often not sufficient to obtain only rate of rise to the first peak, as this peak may be relatively low in comparison with some later peak which may be more important from the standpoint of breaker interrupting ability. Therefore, it is important to obtain a fairly complete voltage characteristic. The envelope over the wave is usually sufficient for this purpose.

Method of Calculation

The complete recovery-voltage circuit includes the inductance and capacitance of each piece of apparatus and of each line and connection. The mathematics is considerably simplified if distributed capacitances such as occur in transmission lines and cables are replaced by lumped values. Figure 1 shows a representative circuit resulting from this simplification. This circuit might correspond to that for a feeder breaker with a reactor on the feeder side of the breaker and a fault immediately beyond the reactor. Between the breaker and the feeder reactor is a capacitance to ground which is contributed by half of the breaker, half of the reactor, and the conductor between them.

On the bus side of the breaker is the capacitance of the bus and connected equipment. There are shown lumped reactances corresponding to generator, transformer, and feeder reactors. Transformers themselves are also represented here since they constitute substantially lumped reactances with a comparatively small amount of capacitance. Beyond these lumped reactances will be found the relatively large capacitances of generators, transmission lines, and cables, and, finally, completing the circuit, the additional reactances of the sources themselves.

Table I. Capacitance for Circuit 1

	Micromicrofarads
Reactor	100
Breaker and bushing	170
3 bus insulators—3×12	36
7′ of connections—7×7	49
Total capacitance	355

The circuit shown in Figure 1 is too complex for convenient treatment and so by paralleling reactances and capacitances in the source circuit and also in the outgoing feeder circuit, it is reduced to the circuit of Figure 2. This involves an approximation which would not be permissible in very precise work but will, in general, be satisfactory for the present purpose.

In a similar manner the circuit of Figure 2 is reduced to that of Figure 3.

It is possible to go immediately from the circuit of Figure 1 to the circuit of Figure 3 by means of the following procedure.

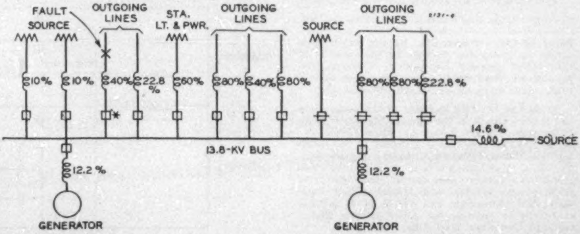
1. Determine the parallel reactance of all reactors connected to the bus except the one on the faulted line. Use the corresponding inductance as L_2.
2. Add together all the capacitances connected on the left side of these reactors. The resulting sum is C_3.
3. Determine the inductance corresponding to the reactance limiting short-circuit current on the bus. Subtract L_2 from this inductance. The result is L_3.

The transient corresponding to each of these circuits consists of a number of oscillations of different voltages, different frequencies, and different damping factors. Each of these oscillations considered by itself starts from zero and leaves a certain voltage across the breaker when it has died down, oscillating in the meantime between zero and twice that voltage value as shown in Figure 19. Figure 11 shows a transient in which a number of these oscillations are combined. The equation for such a transient is

$$E = e_1(1-\epsilon^{-a_1 t}\cos 2\pi f_1 t) + e_2(1-\epsilon^{-a_2 t}\cos 2\pi f_2 t) + e_3(1-\epsilon^{-a_3 t}\cos 2\pi f_3 t) + \ldots.$$

Figure 4. Station single-line diagram

*Breaker for which calculation is made



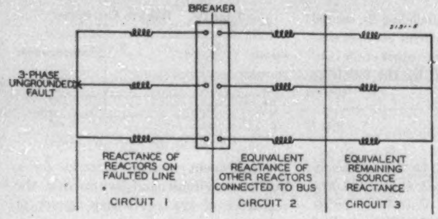


Figure 5. Reduction of system to three circuits

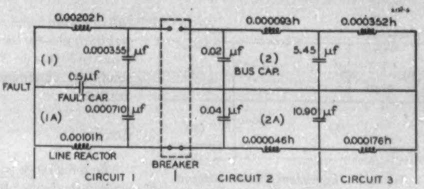


Figure 6. Equivalent circuit—three-phase

where e_1, e_2, e_3 . . . are the voltages associated with the various oscillations, f_1, f_2, f_3 . . . are the corresponding frequencies, and a_1, a_2, a_3 . . . are the respective decrement factors. Each of these oscillations may be considered to be associated with one inductance–capacitance pair (sometimes modified by the presence of the remainder of the circuit), and the voltages and frequencies are determined from the inductances and capacitances of these pairs.

Where an inductance and capacitance stand by themselves, as do L_1 and C_1 in Figure 3, the frequency associated with them is given by the well-known formula

$$f=\frac{1}{2\pi\sqrt{LC}}$$

and the voltage is equal to normal crest voltage times the ratio of the particular inductance concerned to the total inductance of the circuit. Where two adjacent circuits are coupled inductively or capacitively, however, they will react on each other, and this must be taken into account in determining frequencies and voltages. Boehne[3] has developed a method for doing this with two circuits so coupled.

Quite often with two adjacent circuits, the frequency of the first circuit is much higher than that of the second circuit, and the coupling capacitance is so high that it offers negligible impedance to current in the first circuit. In such a case fairly accurate results may be obtained by solving the two circuits independently. This is particularly helpful, where there are three or more adjacent circuits, in reducing the number to two so that Boehne's procedure can be applied.

Table III. Voltage Distribution

Circuit	Inductance	Per Cent Voltage	Volts
3A	0.000366 (L_R)	10.0	1,690
1A	0.000865 (L_S)	23.5	3,970
1	0.002020	54.5	9,210
3	0.000445	12.0	2,030
	0.003696	100.0	16,900

The decrement factors involve the resistances associated with the circuits. Since it is difficult to carry resistance values through these reductions, and fairly wide variation in decrement factor introduces comparatively small variation in the initial part of the curve, an approximate value for the decrement factor can be used for all circuits. In the survey referred to above, a factor which gave a decrement to 85 per cent of the initial value at the end of the first half-cycle was used for each oscillation. This factor gives a decrement to 20 per cent of the initial value in five cycles. This was based on observation of the decrement indicated in a number of cathode-ray oscillograms taken on actual circuit interruptions. Figure 19 shows a wave with this decrement.

This method must be considered to be a very much simplified one which will give results with reasonable accuracy in a great many cases. While it would be more accurate to consider both positive- and zero-phase-sequence values for all circuits, this will introduce additional complication which does not seem to be warranted except in special cases. Caution should be exercised, particularly where a higher degree of accuracy is desired, in simplifying circuits by the method suggested to avoid introducing unnecessary errors.

Inductances for circuits and equipment can be obtained from the reactance data used for short-circuit calculations. Capacitance data can be obtained by direct measurement when the circuit can be isolated, and when measuring equipment is available. However, if this is impractical, sufficient accuracy can be obtained by summation of the capacitances of the various circuit elements. Capacitances of equipment can be obtained from the manufacturers, the capacitances of cables and wires can be determined from standard formulas given in most handbooks, and those of copper connections in cells can be estimated by graphical methods.[8] Some data are given in the appendix for the more important circuit elements. It is extremely important that the capacitances of the higher-frequency circuits be estimated accurately, especially when the voltages associated with them are a relatively high proportion of the total voltage.

The following example illustrates the method of calculation. While there are various methods of simplification for circuits, only a few possibilities are illustrated. Various factors are brought out in the discussion of this example which have not been covered in the foregoing.

Example

This calculation obtains the transient recovery-voltage characteristic for the first phase of a breaker to open a three-phase ungrounded fault on a generating station feeder. The fault current is limited by reactors installed close to the breaker, with the result that the capacitance is low between the breaker and reactor, and the rate of rise of the transient recovery voltage for this portion of the circuit is extremely high. It will usually be found that circuits of this type give severe recovery-voltage characteristics.

The single line diagram of the station is shown in Figure 4 with breaker positions indicated and reactance values given on a

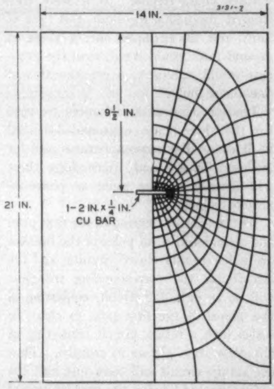


Figure 7. Flux plot for lead run in switch house

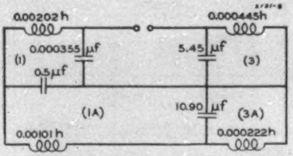


Figure 8. Equivalent circuit—reduced

100,000-kva 13,800-volt three-phase basis. In general, more severe recovery conditions are obtained when some circuits are disconnected, because this reduces the capacitance of the system. In analyzing the recovery characteristics of a system, it is advisable to consider probable operating connections for both normal and emergency conditions and to calculate the recovery characteristics for the one most likely to produce severe characteristics. This is analogous to the determination of short-circuit duty in applying circuit breakers.

The system can be reduced to three principal circuits limiting the fault current, Figure 5. These three circuits consist of:

1. A circuit from the fault to the breaker.
2. An equivalent circuit, the reactance of which is the parallel reactance of all reactors connected to the bus except the reactor on the faulted line.
3. An equivalent circuit representing the remainder of the system reactance.

The fault has been assumed to be a three-phase ungrounded short circuit, and the recovery characteristic is calculated for the first phase to clear, since this combination gives the most severe recovery-voltage characteristic. If it were considered impossible to obtain this type of fault, and, for example, only a phase to ground fault could occur, then the problem would reduce to a consideration of one phase only.

Phase-to-neutral capacitances are used for the three-phase ungrounded-neutral fault case. For most apparatus and for enclosed busses and connections these will usually be the same as phase-to-ground values.

At the time of clearing of the first pole, the second and third poles of the breaker are substantially closed circuits, and the circuit for the corresponding transient will be an outgoing circuit, consisting of the phase of the first pole to clear, in series with a return circuit consisting of the other two phases in parallel. Thus the return circuit will have just half the inductance and twice the capacitance of the outgoing circuit.

Introducing capacitances into the circuit of Figure 5 and paralleling the second and third phases, the circuit becomes that shown in Figure 6. The values of the constants are determined by the following method.

CIRCUIT 1

The inductance will be that of the reactor in the faulted line. On the 100,000-kva 13,800-volt three-phase base, the reactance is 40 per cent, which is equivalent to an inductance of 0.00202 henry.

The capacitance will be that of the connections between the breaker and the reactor, including one end of the reactor and one half of the breaker. The capacitances of equipment are obtained from the tables in the appendix.

The capacitance of the connection was determined by means of the flux plot, Figure 7. This is a scale drawing of the conductor and the inside of the concrete cell with lines of flux and equipotential surfaces drawn in. The capacitance is calculated from the following equation:

$$C = 2.7\frac{w}{l} \text{ micromicrofarads per foot}$$

where

w = number of tubes of flux = 34
l = number of cells per tube = 13

$$C = 2.7\frac{34}{13} = 7.06 \text{ micro-microfarads per foot}$$

The total capacitance for circuit 1 is obtained by summation of the capacitances of the various parts, as shown in Table I.

CIRCUIT 2

The reactance is equivalent to the parallel reactance of all reactors connected to the bus, except those on the faulted line. This parallel reactance is 1.83 per cent, equivalent to an inductance of 0.000093 henry.

The capacitance includes that of one half of the breaker on the faulted line, the connections from this breaker to the bus, the bus, and the connections from the bus to and including one half of the reactors. This is obtained by the method used for circuit 1 and is 0.02 microfarad.

CIRCUIT 3

The inductance of the equivalent source reactance can be obtained by determining the equivalent system reactance for a bus short circuit and subtracting the reactance of the equivalent reactor of circuit 2.

Table IV. Times to First Peaks

Circuit	Microseconds
3A	102
1A	67
1	2.6
3	155

From a short-circuit board study the bus fault current was determined to be 47,400 amperes, which is equivalent to a reactance of 8.8 per cent on a 100,000-kva 13,800-volt three-phase base. Subtracting the equivalent reactance of circuit 2

$$8.8 - 1.83 = 6.97 \text{ per cent}$$

which is equivalent to an inductance of 0.000352 henry.

The capacitance of this circuit is considered to be that from the reactors to the next large lumped reactance such as a transformer or generator. Actually in the case of a transformer there will be additional circuits beyond the transformer, but for practical purposes these usually need not be analyzed separately.

In this case the leads to the transformer banks consist of two 2,000,000 circular-mil lead-covered cables per phase which have a total capacitance of 0.584 microfarad.

The transformers are General Electric Company rated 20,000-kva 13,800–69,000-volt three-phase, and are delta-connected on the low-voltage side. From Figure 13, and the method explained in the appendix, the capacitance is 4,800 micro-microfarads.

The generators are General Electric Company, rated 75,000-kva 13,800-volt three-phase. Substituting in the equation given in the appendix:

$$C = 0.0187\frac{75}{\sqrt{13.8(1+0.08\times 13.8)}}$$
$$= 0.26 \text{ microfarad per phase}$$

One half of this capacitance is considered concentrated at the generator terminals.

The lines are 3×350,000 circular-mil cable with an average length of two miles and a capacitance of 0.5 microfarad per mile. Since the lines are short, the total capacitance is effective, which for the

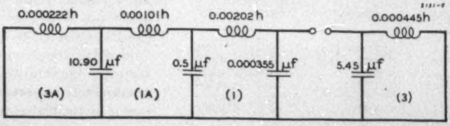


Figure 9. Equivalent circuit—rearranged

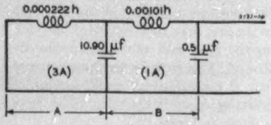


Figure 10. Circuits 1*A* and 3*A*

four lines is four microfarads. The capacitance of the station light and power section is negligible in comparison with that of the cables.

The total source capacitance is shown in Table II.

Fault Capacitance

Since the fault is ungrounded, there is a capacitance to ground at this point which consists of the three conductors of the open circuited line. Assuming the fault to be on a short line of 3,500 feet of 3× 350,000 circular-mil cable, the equivalent length of line to use for obtaining the fault capacitance can be obtained from the following equation which is explained in the appendix:

$$M=\left(\frac{\pi}{4}\right)^2\frac{L}{l}$$

where

L = inductance of the return circuit = 0.001232 henry

l = $^1/_3$ zero-phase-sequence inductance per mile of line in henries = 0.00067 henry

therefore

$$M=\left(\frac{\pi}{4}\right)^2\times\frac{0.001232}{0.00067}=1.14 \text{ miles}$$

Since M is greater than the actual length of line, the total capacitance to ground of the line is used. For 3×350,000 circular-mil cable the zero-phase-sequence capacitance is approximately one half the positive-phase-sequence capacitance,[9] or 0.25 microfarad per mile. The fault capacitance is three times the zero-phase-sequence value, or 0.5 microfarad for 3,500 feet of cable.

Equivalent-Circuit Reduction

The upper part of the equivalent circuit, Figure 6, represents the first phase of the breaker to open, and the lower part represents the other two phases in parallel. The voltage will be distributed around the circuit approximately in proportion to the inductances, and the frequencies will be determined by the inductances and capacitances.

This diagram is simplified as explained below. Some of the reductions are approximations, but it is believed that they are sufficiently accurate for practical purposes.

Table V. Recovery Voltage

Time Micro-seconds	Circuit 3A 1,690 Volts		Circuit 1A 3,970 Volts		Circuit 1 9,210 Volts		Circuit 3 2,030 Volts		e Volts
	π	V	π	V	π	V	π	V	
2.6	—	—	0.04	—	1.0	17,000	—	—	17,000
67	0.41	1,250	1.00	7,350	25.8	9,210	0.43	1,620	19,430
100	0.62	2,230	1.50	3,970	38.4	9,210	0.65	2,840	18,250
120	0.74	2,700	1.80	1,550	46.0	9,210	0.77	3,370	16,830
140	0.86	3,000	2.10	1,270	54.0	9,210	0.90	3,700	17,180
155	0.96	3,120	2.32	2,460	59.5	9,210	1.00	3,760	18,550
162	1.00	3,120	2.42	3,260	62.3	9,210	1.04	3,740	19,530
180	1.11	3,000	2.69	5,400	69.1	9,210	1.16	3,500	21,110
190	1.17	2,870	2.84	6,120	73.0	9,210	1.23	3,280	21,480

Since the voltage across circuit 2 will be small compared with that of circuits 1 and 3, the error in adding the inductance of this circuit to that of circuit 3 will be negligible. Also, the error in neglecting the capacitance of circuit 2 and the capacitance of the two paralleled phases of circuit 1 will be small. By making these changes the circuit reduces to that shown in Figure 8. Figure 9 shows this circuit rearranged in a more convenient form for analysis. Since circuit 3 is separated from the rest of the circuits by the open breaker contacts there will be no interaction between it and the other circuits, and, therefore, its frequency will be determined directly by its inductance and capacitance.

By inspection of circuits 1 and 1*A*, it is evident that the frequency of circuit 1 is high and also that the 0.5-microfarad capacitance of circuit 1*A* is large compared with the constants of circuit 1. Therefore, circuit 1 can be treated as an independent circuit. This may be proved by the following method if desired.

For two interacting circuits with similar characteristics, the voltages and frequencies can be obtained by the method developed by Boehne.[3] Applying this method to circuits 1*A* and 3*A*, shown separately in Figure 10:

$$n_0=\frac{L_B}{L_A}=\frac{0.00101}{0.000222}=4.55$$

$$m_0=\frac{C_B}{C_A}=\frac{0.5}{10.90}=0.046$$

From Figure 20

$K_S=2.3$

$$f_A=\frac{1}{2\pi\sqrt{0.000222\times10.9\times10^{-6}}}=$$
3,240 cycles per sec

$$f_B=\frac{1}{2\pi\sqrt{0.00101\times0.5\times10^{-6}}}=$$
7,100 cycles per sec

$f_S=2.3\times3{,}240=7{,}450$ cycles per sec

$f_R=\dfrac{7{,}100}{2.3}=3{,}100$ cycles per sec

The true frequencies (f_S and f_R) for these circuits, therefore, differ five per cent from the individual circuit frequencies (f_A and f_B).

Determining the voltage distribution by Boehne's method, using the magnitude curves, Figure 21:

$J_S=3.9$
$J_R=1.65$

As a check on these values, J_S+J_R should equal n_0+1

$J_S+J_R=5.55$
$n_0+1=5.55$
$L_R=1.65\times0.000222=0.000366$ henry
$L_S=3.9\times0.000222=0.000865$ henry

These new inductances are 30 per cent and 70 per cent, respectively, of the total inductance of the two circuits compared with 17 per cent and 83 per cent for the original inductances.

These new inductances are used to

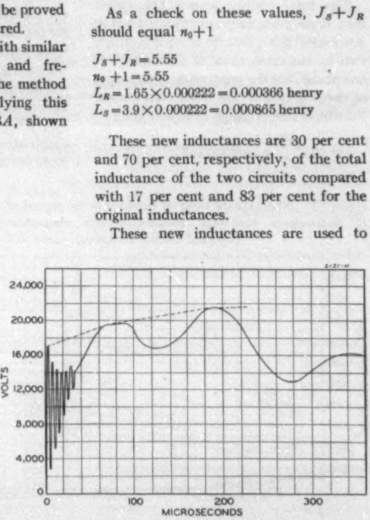


Figure 11. Recovery-voltage curve

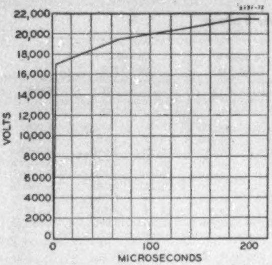


Figure 12. Envelope of recovery-voltage curve

determine the voltage distribution for circuits 1*A* and 3*A*.

FREQUENCIES

The circuit frequencies are determined as follows:

Circuit 3*A*—3,100 cycles per second, from f_R above
Circuit 1*A*—7,450 cycles per second, from f_S above
Circuit 1

$$\frac{1}{2\pi\sqrt{0.00202\times 0.000335\times 10^{-6}}}=194{,}000 \text{ cycles per sec}$$

Circuit 3

$$\frac{1}{2\pi\sqrt{0.000445\times 5.45\times 10^{-6}}}=3{,}240 \text{ cycles per sec}$$

VOLTAGE DISTRIBUTION

The total voltage around the circuit for a three-phase ungrounded fault will be 1.5 times the phase-to-neutral voltage, that is, the crest value of the voltage of one phase plus the crest value of the mean of the voltages of the other two phases. For the system voltage of 13,800 volts:

$$e=13{,}800\frac{\sqrt{2}}{\sqrt{3}}1.5=16{,}900 \text{ volts}$$

The voltage will be distributed in proportion to the inductances as shown in Table III.

RECOVERY-VOLTAGE EQUATION

Substituting the voltages and frequencies in the recovery-voltage equation gives the following for the transient recovery voltage of the circuit:

$$\begin{aligned} e={}&1{,}690(1-\epsilon^{-a_1t}\cos 2\pi 3{,}100t)+\\ &3{,}970(1-\epsilon^{-a_2t}\cos 2\pi 7{,}450t)+\\ &9{,}210(1-\epsilon^{-a_3t}\cos 2\pi 194{,}000t)+\\ &2{,}030(1-\epsilon^{-a_4t}\cos 2\pi 3{,}240t)\end{aligned}$$

FIRST PEAKS

The first peaks of each of these waves occur when

$$2\pi ft=\pi$$

therefore

$$t=\frac{10^6}{2f} \text{ microseconds}$$

For these circuits, the times to first peaks are shown in Table IV.

RECOVERY-VOLTAGE CURVE

The method for the determination of points on the recovery-voltage curve is illustrated in Table V. The values under the heading π are time expressed as a fraction of time to the first peak of the corresponding wave. The corresponding voltages are obtained by using these fractions as abscissae for the decrement curve, Figure 19, and multiplying the corresponding ordinates by the voltage for the circuit as given in Table III. The total voltage at any time is the sum of the voltages of the individual circuits.

The recovery-voltage curve out to the maximum peak is shown on Figure 11. The labor involved in determining this entire curve is considerable, however, and most practical purposes will be served if the envelope consisting of the curve up to the first peak and the dotted line joining the peak with subsequent peaks are shown. This requires the calculation of only a comparatively small number of points as shown in the table. The curve is shown in Figure 12.

The rate of rise of the recovery voltage may be considered to be the rate of rise to the first peak. For this example it is:

$$rr=\frac{17{,}000}{2.6}=6{,}500 \text{ volts per microsecond}$$

Table VI. Total Capacitance to Ground of Single-Phase Core-Type Transformers

Westinghouse Electric and Manufacturing Company

Kva	Capacitance in Micromicrofarads		
	115 Kv	161 Kv	230 Kv
5,000	2,550	2,750	2,350
10,000	2,450	3,650	2,750
15,000	3,400	3,650	2,950
20,000	3,650	3,650	3,050

The maximum rate of rise is the slope of the line passing through zero and tangent to the first peak. This is:

$$rr_{\max}=1.14\times 6{,}500=7{,}400 \text{ volts per microsecond}$$

As mentioned previously, the representation and simplification of this circuit exemplifies only one of a number of general methods. The representation of distributed constants by lumped constants sometimes requires more elements than at others, depending upon the circuit in which they are located. As the accuracy of the calculation depends upon the accuracy with which the circuit is repre-

Figure 13. Approximate trend of capacitance to ground of windings of single-phase core-type transformers with concentric windings (General Electric Company)

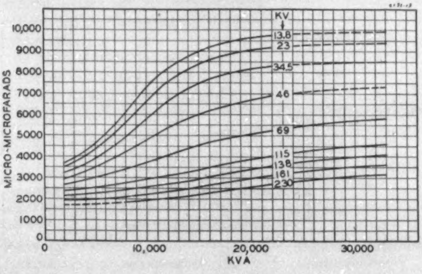
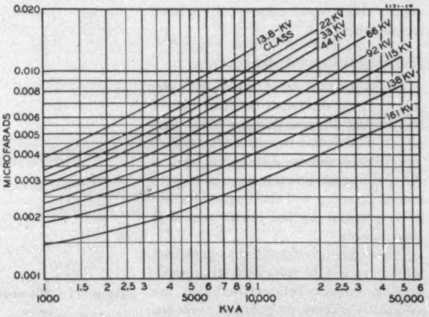


Figure 14. Capacitance of typical power transformers—high-voltage to low-voltage and ground, single-phase, 60 cycles—data from tests (Allis-Chalmers Manufacturing Company)



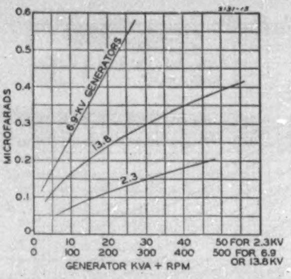


Figure 15. Effective capacitance to ground of salient pole generator stator windings—one-half capacitance of one phase to ground—all curves are the average of a large number of calculated curves (Westinghouse Electric and Manufacturing Company)

sented, as well as upon the assumptions made in simplifying it, both should be carefully studied.

Appendix. Capacitance Data

The following capacitance data are based on tests or computation by the manufacturer of the equipment unless otherwise noted.

Transformer Windings

General Electric Company. For single-phase transformers with concentric windings the total winding capacitance is given in Figure 13. These curves show only approximate trends. Individual transformers may deviate considerably from them. For capacitance of windings rated less than 13.8 kv, the 13.8-kv curve will apply.

The effective capacitance concentrated at each end of the transformer winding is one half of the curve value. The capacitance should be determined using the line voltage, whether the transformers are connected wye or delta, since the capacitance depends on the insulation thickness which is determined by the operating voltage of the transformer.

For a bank of transformers connected wye, one half of the capacitance of the transformer winding, as determined from the curve, should be used when considering each phase of the system. For a bank of transformers connected delta, the capacitance per phase is equal to the value obtained from the curve, because there are two transformers connected to each line.

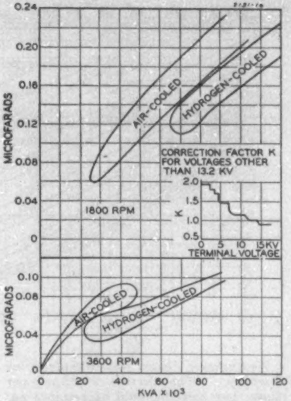


Figure 16. Effective capacitance to ground of turbine-generator windings—one-half capacitance of one phase to ground (Westinghouse Electric and Manufacturing Company)

Microfarad basis—13.2 kv; multiply capacitance by K for voltages other than 13.2 kv

For three-phase transformers, the capacitance per phase is approximately one half of the value given by the curve. The transformer kilovolt-ampere capacity to apply to the curves to obtain the capacitance is the three-phase kilovolt-ampere rating of the transformer.

For Pyranol transformers, the capacitance values are 1.6 to 1.7 times the curve values.

For a more complete discussion of these curves refer to "Equivalent Circuits of Transformers and Reactors to Switching Surges."[7]

Westinghouse Electric and Manufacturing Company. Table VI gives total capacitance to ground of single-phase core-type transformers. Table VII gives total capacitance to ground of single-phase shell-type transformers. The effective capacitance concentrated at each end of the transformer winding is assumed to be one half of the total capacitance.

Allis-Chalmers Manufacturing Company. The total winding capacitance for the high-voltage windings of various sizes of transformers are shown in Figure 14. The effective capacitance concentrated on each end of the winding is assumed to be one half the total value.

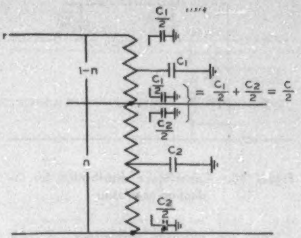


Figure 17. Capacitance distribution for autotransformers

Autotransformers

The capacitance of an autotransformer may be assumed to be distributed as shown in Figure 17.

C = total capacitance of autotransformer winding as determined from curve or tabulation (determine for transformer kilovolt-amperes, not circuit kilovolt-amperes)

n = ratio—low voltage to high voltage

$C_1 = (1-n)C$

$C_2 = nC$

Capacitance at high-voltage line end will be $C_1/2$

Capacitance at low-voltage end connected to tap will be $C/2$

Potential Transformers

Westinghouse Electric and Manufacturing Company. Effective values, that is, one half the measured winding capacitance, are shown in Table VIII.

Current Transformers

Average values for total winding capacitances are given in Table IX.

Induction Regulators

To determine the effective capacitance of induction regulators, it may be assumed that one half of each total winding capacitance is concentrated at each end of the winding as indicated in Figure 18.

C_P = total capacitance of primary winding

C_S = total capacitance of secondary winding

Table VII. Total Capacitance to Ground of Single-Phase Shell-Type Transformers

Westinghouse Electric and Manufacturing Company

	Capacitance in Micromicrofarads	
Kva	115 Kv	230 Kv
10,000	5,500	5,900
20,000	7,100	7,700

Table VIII. Effective Capacitance of Potential Transformers

Westinghouse Electric and Manufacturing Company

Kv Class	Ratio	Capacitance (Micro-microfarads)
25	23,000–115	180
34.5	34,500–115	175
46	46,000–115	185
69	69,000–115	310

Table IX. Total Capacitance of Current Transformers

Kv Class	Type	Capacitance Micro-microfarads
General Electric Company		
7.5–15	Indoor	120
15	Outdoor	230
Westinghouse Electric and Manufacturing Company		
115	Oil-immersed Self-cooled	433

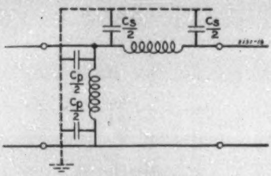


Figure 18. Capacitance distribution for induction regulators

Thus the effective capacitance at the source end is equal to $(C_P+C_S)/2$ while the effective capacitance at the load end is $C_S/2$.

Effective values determined from test readings obtained from regulators on the Philadelphia Electric Company system are shown in Table X.

Current-Limiting Reactors

An average value for the effective capacitance at each line terminal is approximately 100 micromicrofarads in the usual cell structure regardless of rating.

Generator Windings

General Electric Company. The following equation for the total capacitance per phase for generator windings has been taken from Park and Skeats' paper:[3]

$$C=K_c\frac{\text{mva}}{\sqrt{\text{kv}(1+0.08\,\text{kv})}}\ \text{microfarads}$$

where the constant K_c has the following values:

Solid round rotors	0.0187
Salient-pole generators without amortisseurs	0.0347
Salient-pole generators with amortisseurs	0.0317

and

mva = rated capacity of generator megavolt-amperes

kv = rated line-to-line voltage of generator in kilovolts

One half of the value obtained from the equation should be considered concentrated at each end of the generator winding.

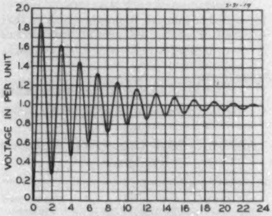
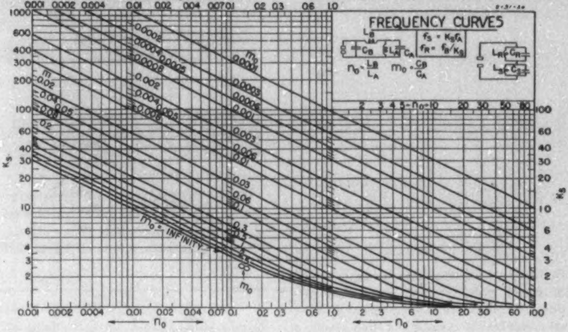


Figure 19. Curve for determining $1-\epsilon^{-\alpha t}$ cos ωt for decrement to 20 per cent in five cycles

$1-\epsilon^{-0.081\omega t}\cos\omega t$

π in microseconds for any frequency $=\dfrac{10^6}{2f}$



Westinghouse Electric and Manufacturing Company. The effective capacitance per phase (one half the total capacitance per phase) is given in Figure 15 for salient pole generators, and in Figure 16 for turbine generators.

Oil Current Breakers and Bushings

The capacitance of an oil circuit breaker consists principally of the bushing capacitances. The values given in Table XI are for one bushing or for one side of an open breaker and should be multiplied by two where the circuit passes through the breaker.

The capacitance of General Electric Pyranol-filled bushings is approximately the same as that of similar oil-filled bushings.

General Electric type *H* breakers—15-kv (including bushings)

One tank to ground	125 micromicrofarads
Both tanks to ground	150 micromicrofarads

Insulators

Suspension-Type

Standard duty (Westinghouse)	23 micromicrofarads
Higher strength (Westinghouse)	25–28 micromicrofarads

Locke Insulators

Rating and capacitance of Locke insulators are shown in Table XII.

Figure 20. Frequency curves

Bus Supports and Wall Bushings

Indoor bus supports	12–25 micromicrofarads
Wall bushings	100–200 micromicrofarads

Lightning Arresters

General Electric Company. Approximately 20 micromicrofarads. However, this is subject to considerable variation depending upon design and voltage rating.

Determination of Capacitance by Flux Plots

In order to determine the capacitance of a bus or of a conductor by this method,[8] a cross section of the conductor in the cell should be drawn to scale. If there are any grounded parts running through the cell, the flux path will be from the conductor to these parts. However, if the conductor is surrounded by a concrete cell, the walls of the cell can be assumed as ground potential, and the capacitance calculated on this basis.

In the space between the conductor and the grounded parts or cell wall, draw equipotential surfaces and draw lines of flux

Figure 21. Magnitude curves

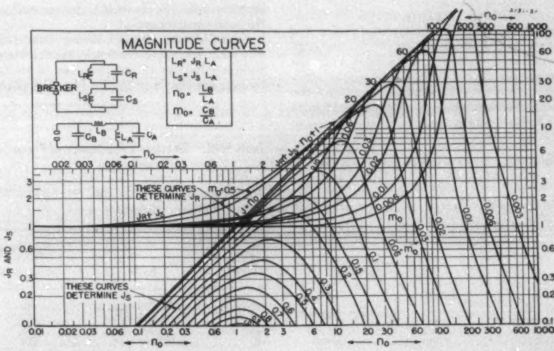


Table X. Effective Capacitance of Induction Regulators

Regulator Rating and Make	Effective Capacitance	
	Source End Micro-micro-farads	Load End Micro-micro-farads
75-kva, 300/600-ampere, 2,500-volt, single-phase General Electric	6,250	2,250
72-kva, 300/600-ampere, 2,400-volt, single-phase Westinghouse	8,750	5,500
48-kva, 200/400-ampere, 2,400-volt, single-phase Westinghouse	4,750	2,500
34.5-kva, 150/300-ampere, 2,300-volt, single-phase General Electric	3,600	1,600
36-kva, 150-ampere, 2,400-volt, single-phase Westinghouse	3,300	1,500

from the conductor to ground. These should be so drawn that the lines of force and equipotential surfaces cross at right angles, and so that the cells formed are approximately squares. The lines of force should leave the conductor and enter the grounded surface at right angles.

The capacitance of the section can then be determined by counting the number of tubes of flux between the conductor and ground and the number of cells in series in each tube and by calculating the capacitance from the following formula:

$C = 2.7\frac{w}{l}$ micromicrofarads per foot of length of conductor at right angles to the paper where

w = the number of tubes of flux
l = the number of cells in series per tube

FAULT CAPACITANCE

The capacitance for a three-phase ungrounded fault consists of the capacitance to ground of the three conductors and of any equipment adjacent to the fault. Usually the capacitance of the cable or line will predominate and that of the apparatus will be negligible in comparison.

Any line or cable may be assumed, with little error, to be open at its far end. If such a line is short, it will behave substantially as if all its capacitance were lumped at the fault. If the line is long, on the other hand, it will behave for a considerable time like a resistance rather than a capacitance. Nevertheless, in the interest of simplicity, it is very desirable to represent the line as a capacitance; but if the entire capacitance of a long line is used, the resulting calculation gives an initial rate of rise which is much too low, and accuracy in this component is more important in the early part than later on. Consequently, an approximation has been adopted which consists in placing a limit on the length of line taken into consideration. This limit is set at a value such that the first reflection from the far end of the line, as limited, returns to the fault point after one-fourth cycle of the oscillation resulting from the corresponding value of capacitance in combination with the inductance of the return circuit.

Table XI. Effective Capacitance of Oil Circuit Breakers

Breaker or Bushing Rating		General Electric Company*		Westinghouse Electric and Manufacturing Company†
Kv	Amperes	Without Capacitance Tap Micro-micro-farads	With Capacitance Tap Micro-micro-farads	Micro-micro-farads
15	600	196		100–300 150–425 250–500 300–700
	1,200	196		
	2,000	315		
	3,000	380		
25	600	160		
	1,200	160		
	2,000	270		
	3,000	335		
34.5	600	150		
	1,200	150		
	2,000			
	3,000			
46	600	145		
	1,200	145		
69	600	126	230	150–275
	1,200	126	230	
92	600	163	280	
	1,200	163	280	
115	600	163	294	250–315
	1,200	163	294	
138	600	160	306	300–375
	1,200	160	306	
161	1,200	155	310	340–435
196	1,200	235	390	
230	1,200	240	400	425–525
287	1,200			400–500

* Values apply to present standard bushings but will serve as a reasonably accurate approximation for bushings which are no longer standard. The former standard 400- and 800-ampere bushings have the same capacitance as the 1,200-ampere bushings listed here.

† Capacitance increases with rated continuous current and rated interrupting capacity.

This limits the error in the first part of the transient to a maximum of about seven per cent of the total circuit voltage. In general, this maximum error will not occur at the time of an important peak in the recovery transient, and hence the effect on the recovery rate from this source will usually be quite small. This limit in miles of line is given by the equation:

$$M = \left(\frac{\pi}{4}\right)^2 \frac{L}{l}$$

where

M = limiting line length in miles
L = inductance of the return circuit in henrys
$l = {}^1/_3$ zero phase sequence inductance in henrys per mile

If M as calculated is equal to or greater than the actual length of the line, the actual length of the line should be used in determining the fault capacitance. If M is less than the actual length of the line, this value should be used for the length of the line in determining the fault capacitance.

TableXII. Locke Insulators

Catalog No.	Rating Kv	Capacitance of 1 Unit Micro-microfarads
29,150	15	15
10,455	34.5	25
9,153	46	11
7,785	115 (3)*	56
9,473	7.5	61
23,070	15–23	60

* Three units of 7,785 are used for 115 kv. Above value is for one unit. Three units will have approximately one-half the capacitance of one unit

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Electric Facilities and Operating Plan for the First Chicago Subway

CHARLES E. De LEUW
NONMEMBER AIEE

Synopsis: The $64,000,000 subway project now being completed will provide long-needed rapid-transit terminal facilities in the Chicago downtown district. The State Street subway will be completely equipped and in operation early in 1943 and will constitute a major contribution to the problem of handling additional traffic resulting from a decrease in the use of passenger automobiles.

This first subway route is five miles long and with fixed equipment will cost almost $34,000,000. The subways were built with track sections in tunnel—at low level. Platforms 500 feet long are provided at stations, except in the congested area where there is a continuous island platform 3,300 feet in length with access provided by mezzanine stations in each block.

All fixed equipment is being installed by the city. Despite the delay in securing equipment because of present scarcity of critical materials, the entire project will be built and equipped in a period of four years. This high-speed construction program required expeditious planning of locations for all electric facilities.

Power will be 600 volts direct-current. The 144-pound contact rail will be energized by existing substations and energy delivered by conventional positive and negative feeders, with contact rails reinforced with parallel feeders.

Adequate sectionalization of the entire system is provided largely by automatic operation. Centralized supervisory control of the all-relay type includes most modern accessories, such as an illuminated diagram board and remote-control dispatching equipment.

Train movements will be controlled throughout by a modern signal and electropneumatic interlocking system with automatic stops. Signals are spaced and timed for operation of 40 trains per hour on each track. Signals and other electric facilities, including lighting, pumps, fans, and escalators will have a-c power supply.

Fluorescent lamps will be utilized at all subway stations. Lighting intensities on mezzanine floors and station platforms will vary from six to eight foot-candles. Escalators are provided at all stations. At the downtown and heavier outside stations two four-foot escalators are provided operating at a speed of 90 lineal feet per minute.

The State Street subway will be operated initially by 455 steel cars now owned by the Chicago Rapid Transit Company. However, new equipment will be modern streamlined articulated units. The ultimate capacity of the State Street subway as thus equipped will be 80,000 passengers per hour.

The Initial Subway Project

THE $64,000,000 subway now being completed, is the first step in a comprehensive plan for modernization and extension of all local transit in Chicago. The second step, now well under way, will be the consolidation of three separate and competing transit companies into a single unified corporation. This will result in economies in operation and the elimination of unnecessary and costly duplication of service.

The unification ordinance, granting an indeterminate franchise to the new unified company, has been passed, and virtually all of the preliminary steps taken, so that there now remains only the formal approval by the present security holders of the reorganization plan, the approval of the issuance of new securities by the Illinois Commerce Commission, and the final approval by the voters at a referendum to bring to its final culmination this objective which the city has sought for more than 25 years.

The third step will follow the granting of an indeterminate franchise to the new company and will consist of a thoroughgoing modernization of transit equipment, the substitution of trolley and motor busses for inefficient trolley-car service on about one third of the track mileage of the present surface-lines system, the extension of and improvement of service in intermediate and outlying areas of the city, not now adequately served with local transit, and the unification of all facilities through universal transfer between rapid transit and surface routes.

FUNCTION

An understanding of the function of the new Chicago subways requires a brief outline of the present local transit situation.

More than three quarters of the city's traffic is carried by the Chicago Surface Lines, operating a comprehensive network of street-car and, in more recent years, trolley-bus and motor-bus routes. During the last two decades, this surface transportation system has been supplemented by the operation of motor-bus routes, largely over the boulevards, by the Chicago Motor Coach Company, now carrying about seven per cent of the total traffic. The only form of urban rapid transit, other than that afforded by steam-railroad suburban roads, is that provided by the elevated railroad system operated by the Chicago Rapid Transit Company. However, unification of these companies will bring universal transfer and thus making rapid-transit facilities indirectly available to all.

For years engineers have realized the inadequacy of the downtown terminal facilities of this system. Thirteen elevated tracks, four on the north side, six on the west side, and three on the south side, deliver their loads to a two-track loop in the center of Chicago (except for a limited number of trains handled in stub terminals).

One of the principal objectives of the State Street subway is to provide terminal facilities for the rapid-transit lines to relieve the elevated loop. The loop, about two miles around, consists of two tracks both operating in the same direction. The cars using the inner loop must cross the outer-loop tracks at grade on both entering and leaving.

In spite of interference between all trains at two points, the outer loop now carries at the peak of service 208 cars in 35 trains during the maximum 30-minute period—see Figure 1. The inner loop now carries 167 cars in 36 trains. Commonly accepted practice to move trains expeditiously is a maximum of 40 trains per hour, and the heavily traveled New York subways operate at a lesser frequency than this. As a result of this congestion, speeds on the Chicago elevated loop lag down to six or seven miles per hour, and overcrowding of trains results—even under present traffic conditions.

The importance of additional terminal tracks for the Chicago rapid-transit system became vastly more important with the order rationing passenger automobile tires which became effective about the first of the year 1942. It has become evident that public transportation agencies will be required to carry, not only the additional traffic resulting from the accelerated pace of commerce and industry due to war activities, but also an ever increasing group of passengers who now use auto-

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CHARLES E. DE LEUW is acting chief engineer, Chicago Department of Subways and Superhighways.

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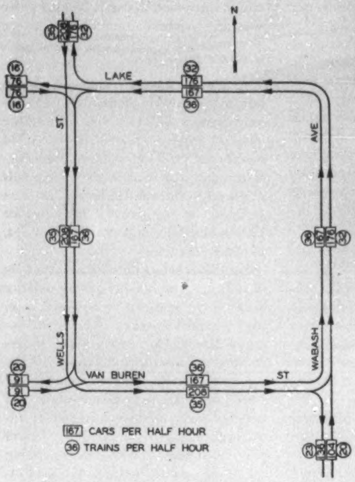
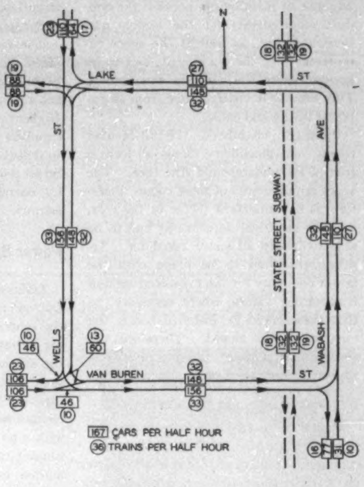


Figure 1 (left). Chicago rapid-transit car flow on the Union Loop during maximum half hour, 8:15–8:45 a.m., December 1941

Figure 2 (right). Car flow on Union Loop and State Street subway during maximum half hour, 8:15–8:45 a.m., expected in December 1942



mobiles exclusively for their transportation.

Because most automobile traffic to and from Chicago's downtown district is long haul traffic, it is estimated that most of the present automobile passenger load will be transferred to rapid-transit trains. Our estimates indicate that unless relief is afforded by the operation of the State Street subway during the year 1943, it will be virtually impossible to operate enough cars over the existing union loop terminal to provide transportation with any reasonable loading standard. Figure 2 shows the estimated redistribution of present traffic after the start of operation of the State Street subway.

Description of Subways

The initial project consists of two terminal subways which will be operated with and become an integral part of the rapid-transit system. Route 1, the State Street subway, connects with the four-track north-side elevated route near Armitage Avenue, $2^1/_2$ miles to the north, and extends along Clybourn Avenue, Division Street, and State Street to a connection with the three-track south-side elevated route near 16th Street, $1^1/_2$ miles to the south. This subway has a total length of 4.9 miles and is estimated to cost $33,750,000 including fixed equipment.

Route 2, the Dearborn Street subway, is the first unit of an underground terminal system for west-side rapid-transit routes. The initial section extends from a downtown terminal loop to a connection with the Logan Square elevated route, $3^1/_2$ miles to the northwest of the downtown district. The total length of route is 3.9 miles, and the total cost, including fixed equipment, will be approximately $30,400,000. Except for short sections near the terminals, the entire project was built with track sections in tunnel at low level. Reinforced concrete tunnel sections were utilized throughout.

At each station platforms 500 feet in length are provided. At the three side-platform stations these structures consist of multiple arches—largely of reinforced concrete construction. Stations with continuous island platforms 22 feet in width were built in the loop district proper. This construction consists of a multiple three-arch section—two large bores on each side accommodating the trains and a portion of the platforms, with a smaller arch sprung from longitudinal steel girders supported by heavy steel columns. In general, stations are of the mezzanine type, constructed by cut-and-cover methods and located as close as possible to the street surface. Figure 3 shows an artist's perspective of the subway structure at a station.

All architectural features were designed with a view to providing attractive, light, and well-ventilated stations. A type of finish is planned which, in addition to being attractive, will stand wear and tear as well as the rigorous atmospheric conditions of downtown Chicago.

As finally developed the plans for the mezzanine stations provide for reddish-colored cement floors, scored in a tile pattern, light-colored structural glass walls, painted concrete ceilings, with dark (radio-black) marble column encasement, and metal doors and trim. Stairways have abrasive tile treads and glazed tile walls. The platforms are also to have colored cement floors, scored, with various colored paints on metal columns and all exposed metal—also on the arched ceilings.

Construction

Electrical Problems

Since the subways were tunneled through the soft clay underlying most of the city, air pressure was used during mining operations, and one of the first electrical problems confronting the subway department was the regulation of the contractors' electric facilities, including the service transformers, motor-driven air compressors, temporary electric lighting and hoists. The specifications were written to provide safety to the men working under air pressure in the tunnels. To insure reliability the power company was required to provide two entirely separate feeders from separate substations to each plant, each supplying a separate bank of transformers. Transfer switches were required as well as high-class equipment and wire throughout. Tunnel lighting and air compressors were especially safeguarded against failure.

The design of the permanent electrical work in the structure had to be made far in advance of installation, because the conduit requirements for the feeders and branch wiring, as well as the space requirements of the equipment, had to be known before the concrete was poured. This subway is virtually free from all exposed wiring and cable.

Splicing chambers, circuit-breaker rooms, and machinery rooms all form a part of the underground structure. The space requirements of these rooms, therefore, is an important factor in the cost, and the electrical department had to be able to justify all space asked for. Arrangements had to be made with the power company for duct connections and transformer vaults, where necessary, so that these could be installed before the excavation was closed. Therefore, the electrical studies were intensive, even while the mining operations were just beginning.

Equipment Installed by City

Under the provisions of the unification ordinance passed in 1940, the new transit company is obligated to install all equipment in city-built subways. However, by the spring of 1941 it had become apparent that the seemingly interminable delays incident to completing all preliminary legal and financial negotiations might result in the first subway being completed before the reorganization was effected.

Therefore, the City Council, in May 1941, by ordinance directed the Commissioner of Subways and Superhighways to purchase and install the fixed subway equipment and to make arrangements with the trustees of the Chicago Rapid Transit Company for subway operation. Subsequently, the department proceeded vigorously with this work with the understanding that the city will be subsequently reimbursed for all equipment expenditures by the new transit company. In this task our engineers have had the full co-operation of the engineering staff of the Chicago Rapid Transit Company.

Progress

Basic subway construction on route 1 is complete. The elevated-to-subway connections at termini are now under construction. Station finish, much of which was redesigned to reduce the quantity of critical war materials, is now 50 per cent complete.

Anticipating some difficulty in securing the large quantities of steel, copper, and rubber required for fixed subway equipment, orders were placed and contracts let for furnishing most of the necessary equipment late in the year 1941. The last such contract, for signals and interlocking, was placed during April 1942; subsequently, contracts have been awarded for the installation of all traction power distribution equipment—work which is now under way.

The War Production Board has recognized the importance of completing the State Street subway by assigning a project rating of A-10 in February 1942. Higher ratings have been assigned various suppliers and manufacturers as required, so that the completion of the entire State Street project, including equipment, ready for operation early in 1943, now seems assured.

Power System

Trains operating in the State Street subway will be routed through the subway in the central area from the present outlying elevated terminals. The power for the subway trains will therefore be the same as that used on the elevated system, that is, 600-volt direct-current on the contact rails with paralleling feeders and with a return to the substations through running rails, negative feeders, and, to a limited extent, through steel elevated structures.

Substations and Feeders

Because the State Street subway is generally parallel to the present north and south elevated route, and because the downtown terminal traffic will be transferred from the elevated system to the subway, the subway power requirements will not be entirely in addition to the present system power requirements. Therefore, it is practicable to use existing substations now supplying the transit system. In most cases, these substations are actually adjacent to the subway structure, although one, the Sedgwick Street substation, is several blocks distant. New feeders will connect substations to the subway.

The substations are owned and operated by the Commonwealth Edison Company and under present plans this company will supply traction power to the subway. In general, the only new substation equipment required for the subway will be feeder circuit breakers and their automatic and control features.

The underground positive and negative feeders from the substations to the subway cable shafts will be paper-insulated, lead-covered cable. They will be spliced to water-resisting compound insulated feeders leading from cable shafts to circuit-breaker rooms in the subway structures. All power feeders in the subway will be insulated with water-resisting compound and finished with heavy asbestos braid. The subway circuit-breaker rooms at feed points will contain circuit breakers which will act as remote-controlled disconnect switches between these substation feeders and contact-rail feeders.

Contact-Rail System

A new contact-rail section has been adopted, similar in design but considerably heavier than that now used on the elevated system. This rail weighs 144 pounds per yard and will have a conductivity equivalent to 2,250,000 circular mils of copper. The rail will have a hardness somewhat greater than has been used for high-conductivity rails on other subways, to obtain longer life.

Since the contact rails are not generally of sufficient conductivity for the subway loads, it is necessary to reinforce them with paralleling feeders. These reinforcing feeders will be insulated with water-resisting compound and tapped to the contact rails at intervals of approximately 1,000 feet through disconnecting tap switches. The negative reinforcing feeders, in parallel with the running rails, will also be insulated with water-resisting compound and will be tapped to the running rails at intervals of approximately 400 feet. The positive and negative reinforcing feeders will be carried in a duct bench containing 12 asbestos-cement conduits along one side of each track in the train sections. The duct bench also serves as an emergency walkway—see Figure 4. Splicing chambers are located at 250- to 400-foot intervals.

The taps to the contact rails and running rails will be cables similar to the reinforcing feeders and are carried in asbestos-cement conduits in the concrete track ballast to pothead terminals, from which flexible bare bonds will connect to the contact or running rails. The location and gauge of the contact rail will be the same as that of the contact rail on the elevated system, so that the same collecting shoes can be used. This type of contact shoe is of the suspended overrunning contact type. The contact rails will be mounted on porcelain insulators on the track ties in the usual manner. Expansion and contraction will be taken care of by gaps at intervals of approximately 1,000 feet on level tangent track in the subways and at shorter intervals on curves and grades.

Sectionalizing

The contact rails with their reinforcing feeders are sectionalized at the substation feed points, interlocking plants, the emergency track crossovers, and at the tie points which are approximately midway between feed points. Short-circuit and overload protection on any section between substations is obtained by automatic opening of substation circuit

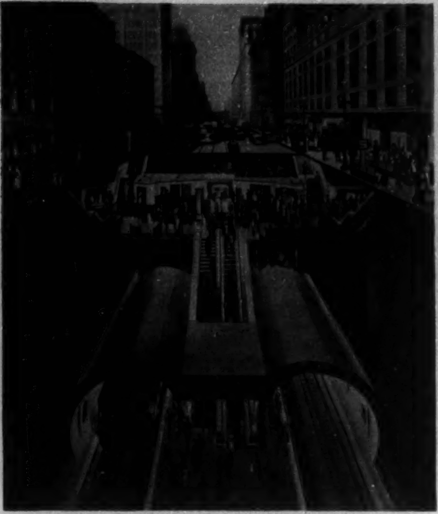


Figure 3. Artist's perspective of the subway structure at a downtown station

breakers and tie circuit breakers without affecting other sections. The other sectionalizing circuit breakers do not have automatic opening features but serve as remote-controlled disconnect switches with high interrupting capacities. The function of the automatic circuit breakers at tie points is to connect the contact rails and feeders of the two tracks together in order to utilize the conductivity of both for feeding distant loads.

The tie station circuit breakers, the substation circuit breakers and all sectionalizing and disconnect circuit breakers will be of the latched-in, air-break type and will be remotely controlled by the supervisory control system.

Return Circuit and Bonding

Since the block signal system of the subway tracks will be of the single-rail track circuit type, one rail of each track will be used exclusively for signals, leaving only one rail of each track available for the traction current to return to substations. Each running rail will have conductivity equivalent to approximately 1,000,000 circular mils of copper. This conductivity must be supplemented by the negative reinforcing feeders. Running rail joints will be welded, except at special track work where bonds will be required across joints. Bonds will be bare flexible copper cable with terminals suitable for welding to the head of the rail by the oxyacetylene welding process.

The contact-rail joints will not be welded, so they must be bonded. The contact-rail bonds will be short U-shaped cable bonds located underneath the rails with terminals welded to the flanges. There will be two 750,000-circular-mil copper bonds per joint.

Supervisory Control

Due to the physical extent of this project and the diversity of operations associated with power services for a subway, including ventilating and pumping systems, centralized control and supervision is essential. This is to be provided by supervisory control of the all-relay type.

The dispatching equipment of the supervisory-control system will be located in the power supervisor's office, two blocks from the State Street subway. Remote station supervisory-control equipment will be located at eight control centers.

A power supervisor's desk, an illuminated diagram board, and relay cabinets of the swinging panel type will comprise the principal elements of the dispatching equipment. Although these elements will incorporate dispatching equipment for all eight outlying centers, the equipment will function independently for each. Thus operations of any given center will not interfere with simultaneous functions of another center.

The functions to be performed include remote control and supervision of all feeder, sectionalizing, and tie-station d-c circuit breakers, as well as the operation of the two-speed station and tunnel ventilating fans. Additional functions will include supervision of the continuity of the normal and emergency a-c services, high water level and pump-motor overload, low supervisory-battery voltage, battery ground or ground on control wires to apparatus remote from the control center.

The supervisory-control system is an "all-relay" system using relays of the standard telephone type, having uniform mechanical construction, extreme simplicity, and established dependability. Operation of this system is based on codes of impulses suggestive of a simple telegraph system, but different from the telegraph in that the impulses are all of the same time duration. Several impulses in uniform sequence form a code, and the number of impulses in a code is varied to obtain various results. All codes are produced automatically, the dispatcher's action being limited to the simple twisting of a control key and the pushing of a button.

Figure 4. Duct bench and walkway during construction

As an example of operation, assume that it is desired to close a circuit breaker. The dispatcher first operates the selection key on the escutcheon of the desired breaker. This sets the proper relays into operation to select the breaker. As soon as this selection is made, the fact is indicated by the lighting of the white selection lamp on the associated escutcheon, as well as the lighting of a similar selection lamp on the diagram board. The supervisory equipment now comes to rest, awaiting the will of the dispatcher. Then, to close the breaker, the dispatcher first places the twist-type control key in the closed position and then operates the master control key. This results in the closing of the breaker. When the breaker closes, the auxiliary signal contact closes and energizes supervision relays which operate to change the indications of the red and green supervision lamps on the diagram board. The supervisory control equipment then returns to the normal rest position, ready for the next operation or change in indication.

The system is operated entirely from 48-volt storage batteries to insure against loss of control from loss of a-c power. A check-back feature is employed as a guard against the possibility of obtaining false indications or performing false operations. Multiconductor control cable will provide the necessary channels for operation of the supervisory-control equipment. Each of the eight remote supervisory centers will be controlled and supervised over a single pair of wires. In addition to the pair per station, there will be two standby pairs. One standby pair will be available to the five stations to the north of the dispatcher's office, and the second pair will be available to the three stations to the south.

Throw-over switches at all points will facilitate transfer to the emergency lines in case of trouble. An audible and a visual alarm will be given in the power supervisor's office in case any of the control lines are opened, grounded, or short-circuited. In addition, each line will be equipped with supervisory-control protectors which will function to clear any heavy surges which may appear on the line wires.

EMERGENCY ALARM SYSTEM

Adjacent to splicing chambers located at intervals of about 400 feet along each subway track, emergency alarm boxes will be located on the walls above the walkways. These alarm boxes will be similar to city fire-alarm boxes in that when the lever on one of them is pulled, an alarm is given at the power supervisor's office. The alarm will be audible and visible, and the number of the box on which the lever is pulled will be recorded on a tape together with the time and date. Also, the pulling of an alarm-box lever will immediately open all circuit breakers through which power is normally supplied to the contact-rail section where the alarm box is located. Thus, in case of an emergency, such as a train wreck or fire, the motorman of an affected train, or other employee, can quickly de-energize the contact rail and at the same time give the alarm at the power supervisor's office. A telephone will be located by the side of each alarm box to enable the person actuating the alarm box to give further information and ask for and receive instructions concerning the emergency.

The alarm circuit will be a single-wire loop connecting all boxes in series. Each alarm box, when its lever is pulled, will cause a spring-driven contactor to open and close this loop circuit automatically at intervals in a code corresponding to the box number. If levers on two to four different boxes are pulled at the same or nearly the same time, each will transmit its code in turn, and all signals will be received at the supervisor's office without interference. The loop circuit will have automatic line supervision which will cause a distinctive alarm signal to be transmitted and recorded, in case of a ground or open circuit on the loop. The source of current for the alarm circuit and recorder will be a 48-volt storage battery at the power supervisor's office.

The circuit breaker tripping circuits will be an individual series loop for each contact-rail section. The opening of one of these loops will effect the de-energizing of the contact-rail section by means of relays, causing the supervisory-control tripping relays to trip the breakers. The alarm-system tripping relays are so designed that the power supervisor can reclose the circuit breakers by supervisory control after a short interval. The source of current for the alarm system's tripping loops will be the storage batteries at the outlying supervisory-control centers.

Signal System

The signal system consists of wayside colored-lights, approach interlocking signals, home signals, and interlocking dwarf signals. All of these except the dwarf signals have automatic controls; the approach and home signals are also controlled manually to govern train movements to and through interlocking plants. Dwarf signals (sometimes called backup signals) are manually controlled.

AUTOMATIC TRAIN STOPS

Associated with each automatic block, approach, and home signal there will be a wayside electropneumatic train-stop mechanism which in the tripping position would engage trip arms on the train thereby applying brakes and enforcing obedience to the STOP indication of the signal. A stop-release contactor mounted on the signal when operated by the motorman will clear the stop arm and permit him to pass a red signal when it is necessary to close up to the train ahead, or when signal or car-equipment failure occurs—according to the "stop, then proceed" rule.

TIMING CONTROL

The tracks are divided into blocks with a signal and train stop at the entrance to each block. The minimum length of a block is equal to the emergency braking distance for the expected maximum speed of a train as it enters the block, plus an additional length as a factor of safety. Minimum-length blocks are used in station timing where a train is allowed to close in at restricted speed when the station section is occupied. An illuminated T sign informs the motorman of the train where to begin this restricted speed. Signals governing entry into a station have extended controls so that the train standing in the station is protected by three or four red signals or by a distance sufficient to protect it from a following train traveling at maximum speed. The approaching train, on entering a track section or block where the T sign is located, energizes a time-element relay which picks up at a predetermined time and cuts off the extended control of the next signal, allowing it to clear for the train now traveling at a restricted speed. If this train continues at restricted speed, the next signal will have its extended control cut off by a second timing relay and so on, to bring the following train up closer to the station. This station timing is necessary to provide for the 90-second headway for which the signal system is designed. With no train standing in a station, all closing-in signals will be green, and a train can then proceed at normal speed.

On descending grades the braking distance is greater, and therefore the blocks at high speed would be so long as to reduce the track capacity. Restricting the speed of trains on grades results in increased safety and increased capacity. To accomplish this, "grade timing" is used with one or more signals normally showing the stop indication. If the trains run at excessive speed, a caution signal will be displayed, together with an additional lunar white indication, which indicates that the next signal is at "stop", only because the allowable speed is being exceeded, and that it will clear if the train runs at the permissible speed as measured by a timing relay.

These timing relays will receive coded energy for the measurement of the time intervals controlled by contacts on the track relays. The code transmitter, consisting of a coil, a mechanically tuned oscillator, and contacts, will generate the desired code for a number of time relays as determined by the location and distribution of these timing relays. Timing relays are also used for the emergency release of approach and route locking at interlocking plants. The time intervals will vary from four seconds for station timing to a maximum of 120 seconds for emergency switch operation.

INTERLOCKING

At interlocking plants, switches and signals are controlled by means of buttons on a control board, and electric relay interlocking, in lieu of the mechanical locking which formerly was standard with interlocking machines. The control board is mounted on a desk within easy reach of the operator and has engraved on its face a miniature diagram of the tracks and switches of the interlocking plant, backed with lights repeating the track sections, thus showing the presence of a train as it approaches and moves through the plant. The interlocking control circuits are designed so that to initiate a route the operator need only manipulate a knob or button on the control board at the beginning of a route and then a second button at the end of said route. This operation causes the various switches and signals of the route to be correctly positioned and indicates on the control board that these switches have responded. This operation further electrically locks other switches and signals so that no conflicting move can be set up. Circuits are also provided for approach and sectional-release route locking which is standard in interlocking practice. Each switch and crossover has a separate operating lever on the control board for independent manipulation. An indication is also provided for each switch or crossover to show when they are not free to move, either because of the electric locking, or because a conflicting position of the switch has been called for.

The entrance control button has a red and a green lamp within it. The red lamp will flash when a route is initiated, will become a steady red light when the route has been completed, and will change to a steady green light when the signal has cleared.

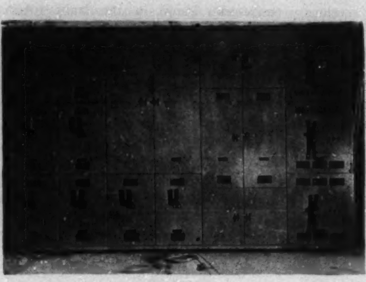


Figure 5 (left). Front of typical a-c switchboard

Figure 6 (right). Typical pump room

RELAYS AND TRACK CIRCUITS

All relays are of the plug-in type with terminal prongs at the back. These prongs fit into sockets set into bases that are permanently mounted on racks. The control wires are soldered to these sockets. A maintainer desiring to replace a relay pulls one out of a socket and plugs in a duplicate without disturbing the wiring.

All interlocking and signal-control relays operate on ten volts direct current. Track relays are of the a-c vane type and operate over single-rail track circuits, where one rail is given over to signal track circuits, and the other rail is used both for power return and for track circuits. A track relay is connected across the signal and negative rails at the entrance to a block, while the approximately four-volt secondary winding of an air-cooled track transformer is connected across these rails at the far end of the block of track section. The relay and transformers are protected from unbalanced propulsion current by means of a resistance unit inserted in the leads to the signal rail and also by 600-volt fuses. The resistance unit in the transformer lead further limits the flow of current with a train in its track section. The track transformer has another secondary winding which furnishes ten-volt energy for lighting all signal lamps.

SIGNAL POWER SUPPLY

The signal power-supply equipment is located in eight signal rooms in the State Street subway and at the north and south portal interlocking plants. Two liquid-cooled transformers at each location, one furnishing the normal supply and the other acting as a reserve source of power, receive energy at 60 cycles 208 volts in the subway and 240 volts at the two portals. The secondary circuits are interconnected by means of an automatic transfer switch and feed power to the a-c signal mains at 110 volts.

Two rectifiers of the copper-oxide type at each power-supply location are connected in parallel and feed power to the d-c signal mains at approximately 16 volts. These signal mains extend in each direction and in each tube from the power-supply source approximately half way to each adjacent supply location. The a-c signal mains feed the interlocking control-board lights, all signal lights, and track circuits. The d-c mains feed the relays at interlocking plants, all signal-line control relays, timing relays, and the electro-pneumatic valves.

COMPRESSED AIR

Air-compressor plants are located at the north-portal interlocking tower and at the 13th Street junction. Each compressor plant is equipped with two compressors connected to individual 600-volt d-c motors with automatic controls and fed from the propulsion current supply. The automatic controls are so arranged that either or both of the compressors may be cut in, depending on the drop in pressure. The compressed air is for operation of the automatic train stops and the track switches at interlocking plants.

The air distribution system consists of $1^{1}/_{2}$-inch air pipe extending the entire length of each subway tube and two-inch air mains on the elevated structure. Branch air lines feed compressed air at a normal pressure of approximately 65 pounds per square inch to the cylinders of the automatic stop and switch operating mechanisms.

Other Electrical Features

A-C POWER SUPPLY

The power for operating the a-c electric equipment will be supplied from frequent distribution centers along the subway by the Commonwealth Edison Company through 208/120-volt three-phase four-wire grounded neutral feeders. The total connected a-c load of the State Street subway and approaches (approximately ten track miles) is divided roughly as shown in Table I.

On this 4.9-mile two-track State Street route there are 21 a-c switchboard rooms in the subway, each supplied by duplicate service feeders from the Commonwealth Edison Company's transformer vaults. In the section outside of the central business district, the duplicate feeders are not merely parallel feeders; they come from normal and emergency sources, fed from different transformers. In the downtown area, the feeders come from transformer vault busses supplying the 208-volt three-phase network of the power company. These busses are interconnected with other vaults, and trouble in a vault supplying the subway would not interrupt services because of the network isolators. The reliability of the primary networks feeding transformers is well known.

Each a-c switchboard in the subway—see Figure 5—consists of a manually operated normal and an emergency service feeder main circuit breaker, on the load side of which is an electrically operated latched-in circuit-breaker type of automatic transfer switch to transfer the load from the normal to the emergency source in the event of failure of the former, and automatically transfer it back again on restoration of the normal source. In order to facilitate maintenance of the transfer switch there are by-pass and disconnect switches so arranged that the load can be by-passed around the transfer switch and the latter isolated, without interrupting the load. The remainder of the switchboard consists of the meter panel and branch feeder circuit breakers. The switchboards are all of dead-front steel construction with draw-out type of main service breakers. All circuit breakers are of the air-break type, main breakers having 50,000 amperes interrupting capacity, and the branch breakers in the switchboard 25,000 amperes interrupting capacity.

PUMPS

The drainage pumps are located in pump rooms over sumps constructed below track level—see Figure 6. Each installation includes two pumps, one acting as a standby normally, and each consisting of a submerged centrifugal pump driven through a long shaft by a 208-volt three-phase squirrel-cage splash-proof motor automatically controlled by float switches. At locations where the subway crosses below the Chicago River the motors are located at the ground surface, and they drive the pumps below by 80-foot shafts. The motors—ranging from 5 to 50 horsepower—are equipped with line-voltage starters. In the State Street subway, there is a total of ten pump rooms.

Table I

	Kva
Lighting	270
Pumps	360
Fans	350
Escalators	370
Miscellaneous	280
Total	1,630

VENTILATION

In general, it is proposed to depend upon the piston action of the moving trains for the normal ventilation of the tunnels, excepting in those portions of the tunnels which are at so great a depth as to make the cost of vent shafts to the surface prohibitive. These deeper sections of tunnel under the river will be ventilated by fans which can be operated continuously.

It is also proposed to depend upon the piston action of the moving trains for the normal ventilation of the stations, excepting the loop stations where the piston action will be somewhat ineffective.

Emergency fans will be installed midway between each pair of stations outside of the central district. In addition, small fans for the ventilation of toilet rooms and other service rooms will be provided in all of the stations. Vent shafts from train sections to the surface have been built on about 450-foot centers. The vent shafts near the stations have been located to serve a double purpose, namely, ventilation and blast relief for the station platforms. Without blast-relieving shafts at the tunnel entrances and exits of the stations, the high air velocities produced on the station platforms by the piston action of the trains would be intolerable. All of the openings in the tunnel walls to vent shafts will be equipped with power-operated louvers. These louvers will be interlocked with the emergency fans, so that when these fans are in operation, the louvers will be closed.

Two-speed reversible station fans will provide normal ventilation at the loop stations.

The horsepower of the fan motors ranges from $7^{1}/_{2}$ to 60. All of the fans except one will be of the propeller type, six to ten feet in diameter, and belt driven. The motors will be two-speed three-phase squirrel-cage motors with splashproof frames. One fan will be of the centrifugal exhaust type driven by a 60-horsepower motor and will be used to ventilate a deep tunnel section that does not have ventilating shafts. There will be a total of 26 fan rooms in the State Street subway including 13 in the downtown stations. The fans will be controlled by the supervisory control system of the railway.

LIGHTING

The normal illumination of the subway will be by fluorescent lamps in the stations and over station platforms, and by incandescent lamps in the train tubes. Forty-eight-inch, 40-watt white fluorescent tubes will be used in the stations. The tubes will be mounted in single-tube fixtures with single-tube power-factor correcting auxiliaries mounted in the base. Each tube will be entirely enclosed by a frosted glass globe covering the lower half of the tube, and a reflector and the fixture base covering the upper half. The lighting fixtures and lamps are planned to produce lighting intensities varying from six to more than eight foot-candles on mezzanine floors and station platforms.

Sixty-watt incandescent bare lamps will illuminate the tunnels between station platforms. They will be provided with opaque shields on one side to protect the eyes of the motormen. The lamps will be spaced at approximately 30-foot intervals on each of two lines of lamps per tunnel. Every fourth light in the tunnels will be on a 600-volt d-c circuit, with 56-watt railway-type lamps connected five in series in a group. Emergency 600-volt lights in the stations will be incandescent lamps.

The a-c lights will be remote-controlled from the cashiers' booths in the stations,

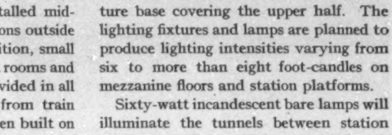


Figure 7. Streamlined and articulated subway-elevated cars of the type proposed for Chicago

single-phase latched-in contactors closing the lighting circuits. In general, there will be two degrees of a-c illumination in the tubes and the stations, with space sectionalization. The 600-volt emergency lighting circuits will always be energized from one of two sources, with an automatic transfer switch being provided at each station for both tubes and station. A special 600-volt lighting cable running the full length of the tubes will be one of these sources, and the nearest traction positive feeder will be the other source.

All lighting loads will have duplicate feeders, one of which will be exclusively for the lighting load, and the other may serve other loads or act as a standby emergency feeder for one lighting load, while serving as a normal feeder for a motor load. Manual transfer switches will be used for some lighting loads when near a switchboard, but the tunnel-lighting feeders will have automatic transfer switches. This duplication of lighting feeders will be in addition to the triple reliability of three-phase, four-wire lighting circuits with single-pole subbranch breakers serving the same area. Thus, the importance of reliable illumination in a metropolitan subway is recognized.

Provision will be made for more intense illumination at the portals during daylight hours, to permit adjustment of the motorman's eyes to the comparatively dark tunnels, by flood lights located near subway portals. Control of these additional lights will be from outdoor photoelectric cells.

ESCALATORS

For the reason that the grade of the subway platforms is generally 40 feet or more below the sidewalks, the escalator installation in the State Street subway is exceptionally liberal. There will be two escalators connecting the mezzanine level with the platform level at almost all stations. Escalators are attractively finished and will operate at a speed of 90 lineal feet per minute. The downtown station escalators are four feet in width, while those in lighter outlying stations will be three feet. All the usual safety devices will be provided. The escalators will be reversible and will be controlled by push buttons located near the top and bottom of each escalator. The horsepower required to operate the various escalators will vary from 16 to 25 each when fully loaded. This equipment was purchased late in the year 1940 at the total cost of $1,174,000 for both subway routes.

Transportation Features

TRAIN EQUIPMENT

It appears that the subway will be ready for operation prior to the time new rolling stock is available. The war is likely to cause an extensive delay in securing modern equipment for the proposed subway service. Therefore, operation will be initiated with trains of existing steel cars of the Chicago Rapid Transit Company.

There is a total of 455 of these units available—389 of which are equipped with two 170-horsepower motors each—and 66 trailers. The seating capacity is 52 passengers per car, and the total capacity 100. These rapid-transit cars were originally designed for use in a subway and are well adapted for the purpose. Trains will be composed chiefly of motor cars, insuring high free-running speeds and rapid acceleration.

The problem of new rolling stock was thoroughly studied by a committee of engineers, organized at the suggestion of the city, composed of representatives of the Illinois Commerce Commission, Chicago Department of Subways and Superhighways, the Chicago Rapid Transit Company, and the Chicago Surface Lines. The committee, after investigating the needs of the subway and the type of car best-suited to meet requirements of modern rapid-transit operation, drafted specifications for a modern lightweight high-speed rapid-transit unit.

A streamlined articulated unit, consisting of three compartment bodies on four trucks, and weighing approximately 82,000 pounds complete without load, was proposed. The general type of car is illustrated in Figure 7. The seating capacity is 106 passengers, and the total capacity 200. Propulsion would be with eight motors totaling 440 horsepower, and the free running speed 45 miles per hour on level tangent track. Such a car would start and stop at the rate of three miles per hour per second under average operating conditions.

In planning the new unit, the committee sought to utilize as many as possible of the favorable characteristics and equipment of the PCC street car. The latter vehicle, which obtains its alphabetical name from the Presidents' Conference Committee sponsoring its development, has been successfully used during the past six years for surface city transit. It embodies many "passenger-comfort" features, such as wide clear-view windows, plentiful illumination, adequate ventilation, uniform distribution of heat, smooth rapid starts and stops, noiseless operation, and the easy riding qualities resulting from rubber springs and resilient wheel construction. The operation of these new units over the welded running rails of the subway tracks promises an innovation in smooth-riding quiet subway-train operation.

The initiation of service on the State Street subway with the 455 steel cars now being specially equipped for subway use will provide three-minute service in each direction. When additional rolling stock is obtained, the number of trains will be increased with the demands of traffic up to a maximum of 40 trains per hour in each direction, the limit of the track capacity.

OPERATION

The maximum capacity of two-track rapid-transit subways is calculated by assuming the operation of 40 trains of five articulated units per track per hour. Units with maximum dimensions of 9 feet 6 inches in width and 90 feet in length, and weighing about 41 tons, are contemplated. With an average loading of 200 passengers per unit such an operation would develop a capacity of 40,000 passengers per hour per track. Thus the total capacity of the State Street subway would be 80,000 passengers, or 40,000 during the maximum half hour. This total capacity may be compared with the present total combined load for rapid transit, street cars, and motor busses of less than 90,000 in the maximum 30-minute period. Considering the fact that convenience requires the retention of some local bus and trolley-car routes at street grade, it is apparent that this terminal subway layout will provide adequately not only for present but for future traffic for years to come.

The 3,300-foot long platform planned for the State Street subway in the loop district will provide space for three double stops—each about 1,100 feet in length. Separate berths will be provided at each station stop for trains operating in different directions, thus providing the broadest possible distribution of incoming and outgoing passengers along the platform and through escalators, turnstiles, and stairways.

A New Multiple High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem

J. W. SEAMAN
ASSOCIATE AIEE

L. W. MORTON
ASSOCIATE AIEE

Synopsis: Mercury-arc power rectifiers have been used increasingly to supply power to d-c utilization systems in the United States since the early twenties. War requirements, because they have multiplied the demand for aluminum, magnesium, chlorine, zinc, and copper, all of which utilize d-c current in their reduction, have tremendously increased d-c power consumption. It is estimated that $12^1/_2$ per cent of the kilowatt-hours generated in 1941 were consumed in such electrochemical processes. Since the mercury-arc rectifier is now the almost universally accepted form of conversion apparatus for this purpose, the attention of many more engineers has recently been focused on it.

Many advantages of the modern rectifier have led to its wide acceptance, but it is not within the scope of this paper to deal with these. One persistent factor in connection with the application of rectifiers which has claimed much attention is the phenomenon of arc-back. Various methods of attack have been employed successfully. High-speed anode switching appears to be the most satisfactory way to handle this problem. Such a solution is widely used in the aluminum industry.[1]

Part I of this paper includes an analysis of the arc-back problem and various means of protection from its effects. Reasons why high-speed anode switching *is* an improved type of arc-back protection are set forth.

Part II of this paper describes this multipole high-speed air circuit breaker. The requirements which the breaker must meet are discussed, and the electrical and mechanical features described. Performance of the breaker was checked in field tests. Oscillographic data are presented and prove that the performance is acceptable.

Part I. Analysis of Arc-Back Problem

Definition of Arc-Back

ARC-back may be defined as a failure of the insulating properties in an arc rectifier during the inverse half wave which results in the rapid reversal of current through the rectifier element. When such an event occurs, there are usually manifestations of this failure, such as high short-circuit a-c current and reversal of d-c current normally flowing from the rectifier.

Various theories have been developed to explain the reasons for this occasional failure of rectifying property or arc-back.[2–6] One commonly accepted explanation is the "particle theory" developed by Doctor Kingdon.[3–5]

In this theory an assumption is made that a minute particle of insulating material, perhaps as small as 10^{-6} centimeter in diameter, becomes accidentally attached to the surface of the anode. Under certain conditions this particle may become charged by positive ions from the surrounding ionized plasma, and a positive voltage built up. Since the distance between the particle and anode is infinitesimal, a high enough gradient may be established between particle and anode to draw electrons from the anode surface (one to ten million volts per centimeter). When the anode emits electrons, it becomes a cathode, and the rectifier is in arc-back. The chances of such particles or patches becoming charged sufficiently to create arc-back is a statistical study.

Engineering Approach to the Arc-Back Problem

Ever since the first commercial mercury-arc power rectifiers were installed, engineers have struggled with the arc-back problem. Constant research and developmental work have been and will continue to be conducted to minimize the occurrence. It has even been the subject discussed at several informal conferences under the sponsorship of the AIEE electronics committee at winter conventions.

Rating and Arc-Back Frequency

Much progress has been made. During the past 15 years, reliable rectifier ratings have been increased from 750 kw per vacuum system at the outset[4] until today there are many units in very successful operation supplying as high as 3,750 kw per vacuum system.

For d-c output voltages below 300 the arc-back problem has not been difficult. Absence of arc-backs has permitted the engineer to base the ratings on thermal limits of the rectifier element and associated transformer equipment.

However, at higher voltages, increases in rating have required progress in the art of reducing arc-backs. Usually, the rating is based on the level of load which the rectifier will carry with acceptable arc-back frequency rather than on the thermal limits of the apparatus.

Anode Shielding

One of the most powerful means available to engineers of reducing arc-back tendency is to increase the deionizing shielding which is placed near the anodes. The shielding may be increased by placing more deionizing surface in the arc stream below the anode, by reducing the area of the holes in the grid through which the arc passes, by changing spacing and configuration, by increasing arc length, or by multiplying the number of grids. It must be realized though that most of these measures also result in increasing the arc losses and lowering the efficiency.

In the modern rectifier a satisfactory compromise has been reached. Sufficient shielding has been provided to insure acceptable performance as regards arc-back frequency. On the other hand, it has not been increased to the point where it has endangered the attractive higher efficiency that the rectifier usually possesses, as compared to other types of conversion apparatus. Sufficient shielding to reduce arc-back near the vanishing point would likely sacrifice that margin of efficiency.

Reactance

Another factor, which bears on arc-back tendency, is the value of reactance in the transformer supplying the rectifier, and in the a-c system supplying the a-c power. Engineers have learned that a given rectifier may arc back frequently when connected to a transformer and a-c system having certain reactance characteristics, whereas, the same rectifier may be completely free from arc-back for other circuits and reactance characteristics, even though the d-c output voltages and loads are identical. Advantage is usually taken of this factor in the struggle to minimize arc back.

Paper 42-139, recommended by the AIEE committees on protective devices and industrial power application for presentation at the AIEE summer convention, Chicago, Ill., June 22–26, 1942. Manuscript submitted April 20, 1942; made available for printing June 1, 1942.

J. W. Seaman and L. W. Morton are both with General Electric Company, Mr. Seaman as engineer in the air-circuit-breaker division in Philadelphia, Pa., and Mr. Morton as electrical engineer in Schenectady, N. Y.

PROTECTIVE EQUIPMENT

Designing engineers, after having arrived at an economic balance between efficiency and shielding, have produced rectifiers which are relatively low in arc-back frequency. However, they know that they are unable to predict zero arc-backs. Therefore, they must depend upon protective equipment associated with the rectifier apparatus to eliminate the effect of arc-back, both on the electric equipment and on the continuity of service required.

Paths and Magnitudes of the Arc-Back Currents

In order to select protective equipment it is necessary to study the direction of current flow, its path through the various electric circuits, and the probable magnitudes involved. Figure 1 shows typical power circuits for two six-anode rectifiers operating in parallel on the same d-c bus, supplying a counter electromotive force load. Arrows indicate the direction of direct current in the various parts of the circuit when an arc-back occurs in one of the rectifying elements. Figure 2 indicates the behavior of current flowing in the important parts of the circuit shown in Figure 1 as they would be seen in an oscillograph. Steady-state values are typical of what would occur in many conventional rectifier installations if protective equipment were not provided. Current values shown are typical, being values measured oscillographically during representative tests on rectifiers rated 5,000 amperes 600 volts direct-current.

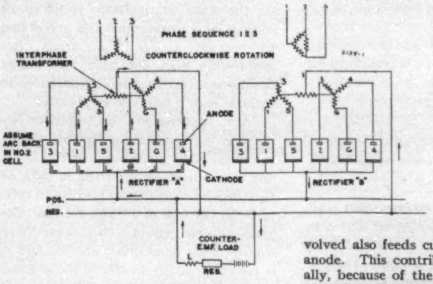


Figure 1. Direction of current in various parts of circuit when rectifier *A* arcs back

Accurate mathematical prediction of the current magnitude which would flow in a given circuit would be extremely difficult. The most important reasons for this are that reactance values in any transformer involved in an arc-back do not remain constant because of d-c saturation of the iron; and voltage drops in the arcs are not well known under the high current conditions obtaining. Usually such attempts give results in magnitude somewhat higher than test measurements. Therefore, studies of this sort are usually based on actual oscillographic tests. Certain general rules however, may be used to estimate the behavior of arc-back currents.

RULES FOR BEHAVIOR OF ARC-BACK CURRENTS

1. The reverse current flowing in the anode circuit of the rectifier element in arc-back consists of several components:

Contribution From Same Wye. The two other phases of the same transformer wye feed currents into the affected anode in turn as they commutate. Their magnitudes are limited by the commutating impedance of the rectifier circuit. This component is alternating or pulsating in character being displaced from the zero axis by action of the rectifier.

Contribution From Other Wye. The other wye of the rectifier transformer involved also feeds current into the affected anode. This contribution increases gradually, because of the initial high impedance of the interphase transformer. As the interphase transformer saturates due to undirectional current, its impedance decreases, and this contribution becomes greater. Its magnitude is determined by commutating impedance. This component is displaced from the zero axis by the action of the rectifying elements.

Contribution From Parallel Conversion Apparatus. Another component is fed from any parallel conversion apparatus such as other rectifiers connected in parallel to the same bus. This component is not pulsating being direct current in character. Its rate of rise is determined by the reactance due to air leakage fluxes throughout the circuit, both in the leakage spaces of the transformer windings conducting the reverse current and in the connections between the rectifiers. Final or steady-state magnitude of this component is limited only by the metal resistances of the station conductors in series, the resistance of one-half transformer secondary winding, the resistance of one-half the interphase winding, and the lumped or equivalent resistance of all contributing conversion apparatus in parallel.

Contribution From the D-C Load. If the nature of the d-c load is counter electromotive force, it too will feed current through the anode in arc-back. This component is d-c in character. Its rate of rise is determined by the air flux reactance of the circuits involved and its steady-state value by the total series metal resistance of the circuit.

2. All of these components add arithmetically to give the total reverse current at any instant carried by the anode in arc-back.

3. The effective voltage driving the short-circuit currents is reduced in proportion to the increase in arc drops caused by the higher currents passing through the arcs. Values for this increase in arc drop have been difficult to obtain, partly because of the transient and unpredictable nature of arc-backs and partly because of the instability of high-current arcs. Estimates of 50 to 100 volts are often used.

4. The effective driving voltage is reduced by a-c system regulation.

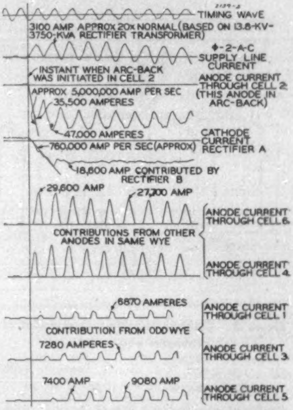


Figure 2. Behavior of current in various parts of circuit during arc-back

5. In practice it is found that the major contributions to the total reverse current are from the same wye and from parallel conversion equipment. The latter contribution is increased as the number and rating of paralleled conversion apparatus on the same d-c bus is raised. Such contribution theoretically reaches alarming proportions for many large electrochemical installations. It has been estimated that theoretical steady-state values of reverse current in an anode circuit might reach 600,000 amperes for certain large installations. (Anode reverse-current rates of rise for large installations have been measured and range as high as 10,000,000 amperes per second.)

6. Other anodes in the same rectifier unit or in other rectifiers on the same d-c bus are caused to arc back as a result of their high contributions to the first one in arc-back. It is doubtful if such theoretically possible high steady-state values as 600,000 amperes would be reached, and certainly they would not be maintained. Sympathetic arc-backs would always spread among the

associated rectifiers. End result would be that all rectifiers would become involved, and all contributions from parallel apparatus vanish.

7. Contributions from d-c loads usually are transitory. Rotating-type d-c loads come to a standstill quickly. Electrolytic loads are usually inductive, so that the current reversal is relatively slow, and rate of rise limited. Electrolytic-cell polarization voltages are usually only a fraction of the d-c applied voltage, and resistance drops are a large factor. Under short circuit such cells lose their polarity rapidly.

8. Current increases in the primary windings of the rectifier transformer corresponding (by transformer action) to the secondary currents. The values would be similar to those reached when all the secondary windings of such a transformer are short-circuited, except for the fact of the rectifying action and the higher-exciting currents due to d-c saturation in the iron. These factors increase the primary currents over the values that would obtain if all the transformer secondary windings were merely short-circuited.

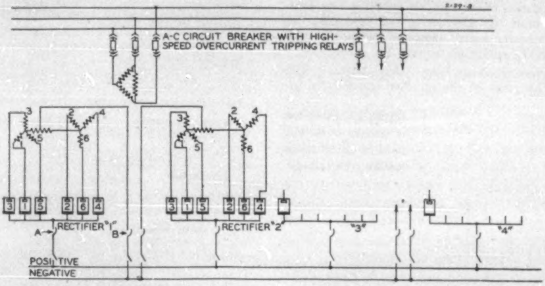

A-C CIRCUIT BREAKER WITH HIGH-SPEED OVERCURRENT TRIPPING RELAYS
3
5
1
2
6
4
A
B
RECTIFIER "1"
RECTIFIER "2"
"3"
"4"
POSITIVE
NEGATIVE

Figure 3. Cathode-switching arrangement of protective equipment

A—High-speed d-c circuit breaker with built-in reverse-current tripping action

B—Medium-speed d-c circuit breaker with time-delay overcurrent trip

EFFECT OF ARC-BACK WITHOUT PROTECTION

The preceding analysis indicates that the high currents impose stresses on nearly all parts of the circuit. Various injurious results which could be expected if protective equipment were not provided may be listed as follows:

1. The a-c supply system may be overloaded. Increase in current supplied during arc-back often reaches 20 times the full-load current rating of the transformer.

2. The rectifier transformer would be damaged. Rectifier transformers are usually specially braced to withstand mechanical shocks due to arc-back. The duty caused by the high current in the windings connected to the anodes in arc-back would be particularly severe.

3. The rectifier element itself would be affected in several ways:

A. The higher currents would heat parts not previously degassed for such high currents and drive off gases which would impair the vacuum.

B. The high-current arcs would burn the metal walls of the vacuum chamber.

C. While the graphite anodes will withstand severe heating, continued presence of a cathode spot at the anode would remove material and roughen the surface.

4. Continuity of service would be interrupted, because other conversion apparatus on the same bus would be overloaded to the failure point.

TRANSIENTS WHICH SIGNAL ARC-BACK

It is obvious that the sudden reverse and overcurrent conditions which appear in the circuits during arc-back offer ready means for signaling the event and actuating the protective equipment. A study of Figures 1 and 2 in the light of these requirements is worth-while. Current suddenly increases in the a-c supply lines and in all the anodes operating in normal forward-current fashion. However, these are symptoms of overload as well as arc-back. It is difficult by this means to distinguish arc-back from plain overload. If there is parallel conversion apparatus, a reliable indication of arc-back is the presence of reverse current in the cathode and negative connections. However, where a single rectifier feeds a purely resistance-type d-c load, no reversal occurs in these connections. The invariably present unmistakable evidence of arc-back is the presence of reverse current in the connection between the transformer secondary winding and the anode of the rectifier.

Four General Methods of Arc-Back Protection

Broadly there are four methods of protecting from arc-back. Three of these have been used in conventional large power installations.

REACTANCE

First is the use of large reactance values to limit the arc-back currents. Often if the resulting flow of current is limited to relatively low values by reactance, the condition causing the arc-back at the anode disappears, and normal rectifier action is automatically re-established. This can be true only where no other counter-electromotive-force apparatus is connected to the same d-c system. This method is sometimes used for such rectifier installations as those furnishing high-voltage d-c power to dust precipitators, oil-cracking processes, and laboratory apparatus.

ARC SUPPRESSION

Second is the so-called "arc-suppression" method. Arc suppression—in the case of multianode rectifiers—consists of grid action to prevent anodes from firing; in the case of ignitron rectifiers—it consists of ignitron action and grid action to prevent anodes from firing. Arc suppression does not have the ability to interrupt current passing from anode to cathode once it has been established.

"Arc suppression" accomplishes its purpose by preventing anodes—which would normally contribute current to the one in arc-back—from firing in turn as they become successively positive in voltage. In the case of a single rectifier, the anode reverse current consists entirely of contributions from other anodes, and it is possible quickly to starve the affected anode and eliminate the arc-back. It should be added that, if other conversion apparatus is connected to the same d-c bus, the current which they contribute must also be interrupted. One method is to interrupt the reverse current in the cathode line by means of a high-speed d-c circuit breaker. Another method, assuming all other conversion apparatus consists of rectifiers, is to apply "arc suppression" simultaneously to all other rectifier elements connected to the same bus during arc back.

"Arc suppression," by means of grids, is accomplished by suddenly energizing the grid with negative potential. With a negatively charged grid between the anode and cathode spot, electrons are repelled, and the anode is often effectively blanketed so that it cannot fire. In the ignitron, the anode is prevented from firing by suddenly cutting off the electric excitation of the igniter point which creates a new cathode spot each cycle. Without the existence or creation of the cath-

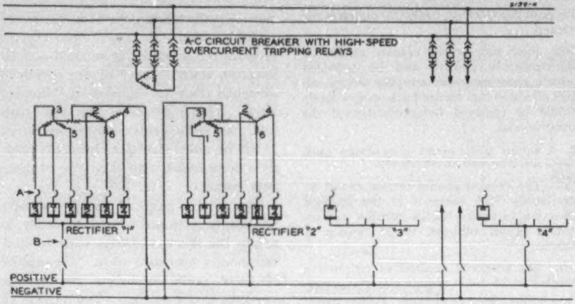

2/39-4
A-C CIRCUIT BREAKER WITH HIGH-SPEED OVERCURRENT TRIPPING RELAYS
3
2
4
5
6
1
A
B
RECTIFIER "1"
RECTIFIER "2"
"3"
"4"
POSITIVE
NEGATIVE

Figure 4. Anode-switching arrangement of protective equipment

A—High-speed multipole-anode circuit breakers with built-in reverse-current tripping action for each pole

B—Medium-speed d-c circuit breakers with time-delay overcurrent trips

ode spot in the mercury cathode pool, the anode should not fire.

Experience with "arc suppression," however, indicates that it is not always effective. Therefore, other backup protection in the form of protective switchgear is always provided to interrupt the overcurrents when "arc suppression" fails.

When it is successful, "arc suppression" is so rapid in action (one-half to two cycles) that it is not necessary for the a-c circuit breaker to open. The yardstick often used to measure the effectiveness of "arc suppression" is to record and compare the number of arc-backs accompanied by a-c breaker operations with those unaccompanied by a-c breaker operations. It is obvious that the relay or device which detects the presence of arc-back and actuates the "arc suppression" must be designed to operate in a small fraction of a cycle.

Experience indicates that "arc suppression" appears to be more successful in installations where reactance values are relatively high.

Cathode Switching

Third is the method most commonly employed until recently—that of disconnecting the rectifier and transformer from the system by means of circuit breakers. A typical arrangement is shown in Figure 3. In its best form it consists of an a-c circuit breaker with special high-speed induction-type a-c overcurrent relay protection, for each rectifier transformer, and a special high-speed reverse-current d-c air circuit breaker in the d-c side for each rectifier.

To illustrate the operation of the cathode-switching arrangement, assume an arc-back in cell 2 of rectifier 1—Figure 3. The following action takes place:

1. The d-c current reverses in the cathode line of rectifier 1. This action trips the reverse-current high-speed d-c breaker, which starts reducing the current in one-half to three-fourths cycle and interrupts the d-c circuit. Reverse current in the cathode line had been contributed by rectifiers 2, 3, and 4. This duty imposed overloads on these units and had started to operate the overcurrent tripping relays associated with the a-c circuit breaker supplying rectifiers 3 and 4 and all the negative d-c-line breakers. However, the high-speed action of the above reverse-current breaker usually should relieve the short circuit in time to prevent outages on 3 and 4, unless they too arc back sympathetically.

2. Even though the reverse-current contribution to cell 2 from the other parallel rectifiers is removed quickly by the high-speed cathode breaker, contributions from the other anodes in the same rectifier continue. Overcurrent relays associated with the a-c circuit breaker protecting the main transformer for rectifiers 1 and 2 therefore complete their operation, disconnecting the transformer and both rectifiers 1 and 2 from the a-c system.

3. Conventional a-c power-circuit breakers require a minimum of seven to ten cycles including relay time to interrupt an a-c short circuit. This means that the heavy duty imposed by the high arc-back current in both the rectifier element and the transformer winding continues throughout this time.

4. Two rectifiers connected to one transformer have been disconnected from service because of an arc-back in only one unit, as it was necessary to open the a-c power circuit breaker. Only rectifiers 3 and 4 remain in service.

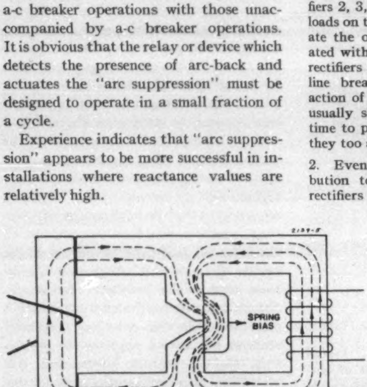
Anode Switching

Fourth is the latest method of dealing with the problem. A high-speed air circuit breaker in the anode line interrupts the fault current resulting from the arc-back.

Such an arrangement is shown in Figure 4. In its best form it consists of a power circuit breaker with standard inverse time a-c overcurrent relay protection for each transformer primary winding, a special high-speed multipole circuit breaker in the anode circuits with reverse-current tripping action, and a standard medium-speed d-c air circuit breaker in the cathode line for each rectifier. To illustrate the operation of anode switching and compare it with operation of cathode switching, assume an arc-back in cell 2 of rectifier 1, Figure 4. The following action takes place:

1. The d-c current reverses in anode line 2. This action trips pole 2 of the high-speed multipole-anode air circuit breaker, which starts to reduce the overcurrent in one-half cycle or less and interrupts the arc-back current in less than one cycle. Current contribution from rectifiers 2, 3, and 4, as well as the other anodes in the same rectifier unit, are all cut off. The common circuit path where they all add up and flow has been opened.

2. Since the short-circuit condition in the



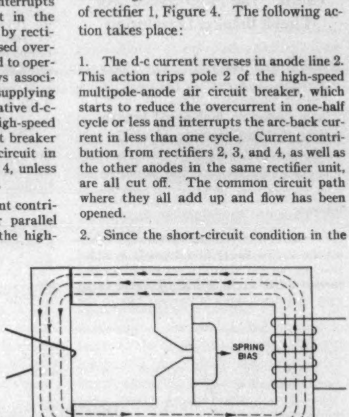


Figure 5. Magnetic circuit of high-speed trip mechanism, forward and reverse directions

transformer supplying rectifier 1 has disappeared in one cycle, its a-c overcurrent relay does not trip the main breaker. Therefore, the transformer remains energized, and rectifier 2 may remain in service.

3. High-speed action of pole 2 in interrupting the reverse current removed the overcurrent condition imposed on rectifiers 3 and 4 soon enough to prevent their outage.

4. Because the duration of short circuit does not last more than one cycle, only a small fraction of the same duty is imposed on both rectifier element and transformer winding of rectifier 1 as compared to the longer duty imposed with cathode switching when the overcurrent lasts 7–10 cycles.

5. The medium-speed cathode breaker can be interlocked with all poles of the anode breaker, so that the cathode breaker serving rectifier 1 would open, but without severe duty. Interlocking would be employed where it is inadvisable to allow a rectifier to remain in service without all anodes in operation.

Cathode switching for arc-back protection was adopted early in the history of rectifier application. It had wide acceptance, but "arc suppression" was later developed as recognition of some of the cathode-switching limitations, grew. Then when experience proved that "arc suppression" was not dependable under all conditions, and because wide use of rectifiers justified the effort, the high-speed anode switching was introduced. It now appears that the use of adequate high-speed anode breakers fulfills the requirements for arc-back protective equipment.

Part II. The High-Speed Air Circuit Breaker Developed

Basic Requirements

Before embarking upon the design of a new multipole high-speed breaker to satisfy the requirements of anode protection for mercury-arc rectifiers, a study was made to determine the prime requisites of this new breaker. These are as follows:

(*a*). The breaker should have a high opening speed. To clarify this it was determined that the breaker should start to reduce line current a half cycle or less (60-cycle basis) with maximum rates of current rise in the range of 2,000,000 to 6,000,000 amperes per second.

(*b*). Each pole of the breaker should be independently tripfree and be provided with a reverse-current tripping device, direct acting so that an arc-back on any anode would be removed independently of the other anodes.

1. A breaker should consist of six of such poles, closed simultaneously or individually.

(*c*). The breaker should remain closed on excessively high currents in the forward direction to insure that a rectifier not involved in an arc-back would remain in service.

(*d*). The proposed application required a breaker which could remain closed carrying load for long periods of time. It was therefore evident that existing switchgear standards should not be sacrificed in the interest of breaker opening speed.

There were other miscellaneous minor requirements attendant to the design of this new breaker, such as electrical closing, mounting, and so forth, which, while of interest to the designer, are not so pertinent to the discussion of this new high-speed breaker as the four preceding main requirements.

Design Problems

An analysis of the above requirements indicated that four major design problems were involved. These were as follows:

1. A high-speed current directional tripping mechanism.
2. A suitable breaker structure.
3. A current-interrupting device, compact in size, yet capable of coping with voltage in the order of 1,700 between phases.
4. A simple drive for operating a number of poles mechanically in parallel at a reasonably constant speed, regardless of the number of poles to be closed.

Trip Mechanism

The magnetic circuit of the tripping device selected is shown in Figure 5. The energy necessary for tripping the breaker on arc-back is stored in a compression spring restrained normally by the small armature. Flux provided by a potential coil normally holds the armature in the restrained position. A reversal of current caused by an arc-back quickly shifts the flux path of that portion normally holding the armature in the sealed position; therefore, upon current reversal the armature is released and allows the stored energy of the spring to dissipate itself in tripping the breaker latch. Figure 5 shows the flux path under normal operation and on arc-back. It will be noted that flux shifting is readily accomplished with this type of magnetic circuit.

The moving parts of the device were necessarily extremely light in order to reduce the effect of inertia and obtain a satisfactory operating time. The weight of these parts, excluding the spring, amounts to 2.4 ounces. The portion of the magnetic circuit in which the flux shifts, due to current reversal, was made of laminated silicon steel in order to facilitate the flux change. The magnetic circuit associated with the potential coil proper was made of solid iron and the iron surrounded with copper, so as to obtain a structure resistant to any sudden changes in applied voltage.

Since this device would be subjected to widely varying rates of current rise (in the order of 1,000,000 to 10,000,000 amperes per second), it was necessary to determine the effect of an extremely high rate of rise on its performance. It was conceivable that with an extremely high rate of rise of current in the tripping direction, the flux in armature would be reduced to zero and then built up in the opposite direction before the armature had moved sufficiently to be free of the influence of this flux. Such a condition would make the device inoperative and would prevent its functioning to trip the breaker. The level at which this would occur could be controlled by varying the inertia of the moving parts and by reducing the air gap of the poles bridged by the armature, the final air gap arrived at by test being in the order of $^{3}/_{32}$ inch.

A rather novel and interesting method was adopted to determine the effect of widely varying rates of current rise on the performance of the device. This consisted of testing the device in a low-voltage high-current a-c circuit. For a half cycle of current in the normal direction the tripping armature would not release, but on the following half cycle of current in the reverse direction this armature would release to trip the breaker. By simply changing the current magnitude, rates of current rise in the order of 23,000,000 amperes per second were readily obtainable, and the release mechanism was tested and operated satisfactorily at this rate of rise in a time of 0.002 second from current zero to point of release. A typical oscillogram of this performance is

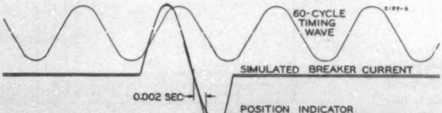


Figure 6. Typical oscillogram of low-voltage high-current a-c tests on high-speed trip mechanism

shown in Figure 6. Inasmuch as satisfactory performance was obtained at this rate of current rise, which is considerably above the rate of current rise obtainable in practice in d-c circuits, the design of the tripping mechanism was considered acceptable.

Breaker Structure

The tripping device just described was mounted on a breaker of conventional structure during the tests in the high-current circuit, both as a convenient method for testing the device itself, and also to observe the effects of high-speed operation on the mechanisms usually associated with more or less conventional circuit breakers.

As a result of these observations a novel breaker structure was evolved. Figure 7A illustrates the mechanism and contact arrangement designed for the application, with the mechanism reset ready to close. Positioned before the armature is a rotative latch normally supporting a cam-shaped member. The cam is so shaped that drawing a roller over its surface, by means of lever-actuated links, causes the movable contact to move to the fully closed position. Opening is equally simple to accomplish. Releasing the armature (Figure 7A) causes the latch to rotate from under the cam. The cam, closing rollers, and movable contact then collapse simultaneously. The opening speed is thereby dependent only on the applied opening force. Toggles with their hard-to-accelerate characteristics, under normal usage, are entirely eliminated. This mechanism represents a considerable departure from the usual toggle-activated circuit-breaker mechanism which does not lend itself so well to high-speed operation and is responsible to a considerable degree for the high-speed opening performance obtained with this new circuit breaker.

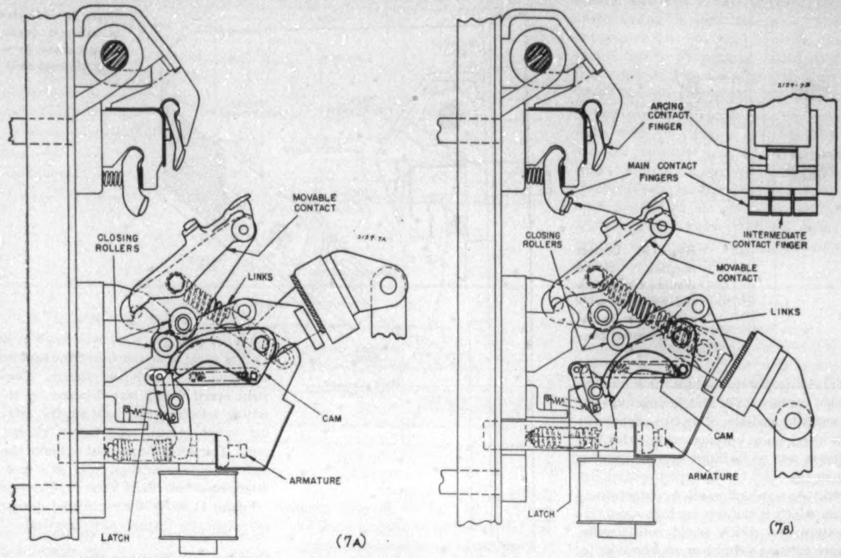


Figure 7A and B. Side views of breaker mechanism and contacts in open and collapsed positions

The contact arrangement designed for this high-speed breaker represents a considerable advance in the art of circuit-breaker design. Bearing in mind that it was considered desirable to design a breaker which would successfully withstand any tendency to blow open on currents of high magnitude in the forward or normal direction, it was decided to advantageously use the magnetic forces rather than to resort to brute force which would lead to a massive hard-to-accelerate structure. To this end the contacts were arranged so that the magnetic forces would add to the contact pressure rather than subtract from it.

Figure 8 shows that all points of contact are beneficially affected by the magnetic forces. The path of the current at the arcing contacts, main contact fingers, and at the lower end of the movable bridging contact is reversed sufficiently to "Magne-seal" then closed against currents of high magnitude and to gain equally from the accelerating effect of the magnetic forces when the restraint of the operating mechanism is removed.

Several other advantages accrued from this contact construction other than those listed above. A one-piece relatively light movable contact member was obtained which permitted the accomplishing of the high-speed operation objective. The pivoting of the lower end of this contact member on a fixed point on the lower stud permitted the eliminating of a heavy flexible connection which would otherwise have been mandatory. The difficulties experienced in designing adequate life into heavy braids capable of carrying currents in the order of 1,600 amperes with reasonable temperature limits and in overcoming their inherent resistance to angular movement are well known. The mounting of the arcing and main contacts on the upper stud resulted in light low-inertia parts which solved the problem of obtaining proper contact sequence between the primary and secondary circuits. Proper contact sequence is especially difficult to obtain with high-speed operation if the parts involved in the sequence are of relatively high inertia.

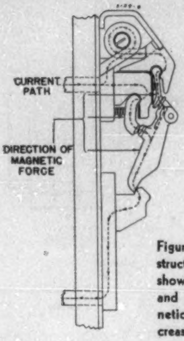


Figure 8. Contact structure closed showing current path and effect of magnetic forces to increase contact pressure

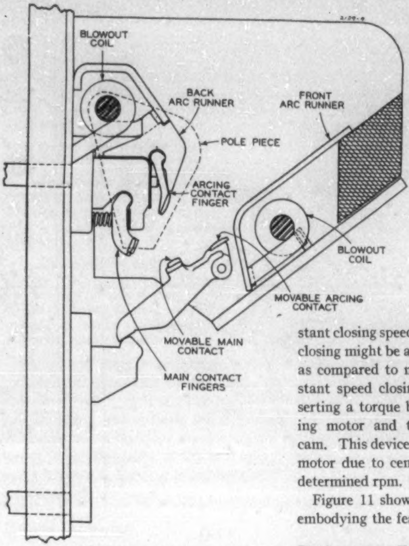


Figure 9. Side view of contacts (open) and arc chute, showing blowout coils

Three fingers were used for the stationary main contact. The middle finger was arranged to lead the other two slightly, so as to minimize burning on the other two fingers and to facilitate current transfer from the main to the arcing contact circuit. As a general result, a contact structure which is suitable for high-speed operation and which would satisfactorily meet existing switchgear standards as to temperature rise and reliability was obtained. Since electrochemical processes generally signify continuous full-current operation, this new contact structure for high-speed breakers provides a practical answer to continuity of service, by virtue of having both main and arcing contacts, generally considered necessary for continuous low-temperature-rise operation.

Current-Interrupting Device

The current-interrupting device designed for this breaker is extremely simple in construction and in operation. Figure 9 is a side view showing the component parts. The interrupting unit consists of a simple open-type arc chute and two blowout coils. One coil is in series with the arcing contacts on the upper stud of the breaker. Associated with this coil is an iron core and two pole pieces of conventional construction which act upon the arc terminus on the arc runner and the arc proper. The other coil is in series with arc runner located over the movable contact. This coil has only an iron core (no pole piece) which acts only on the arc terminus when it strikes the arc runner.

Successful operation is based entirely on rapidly increasing the length of the arc, therefore, the resistance, to the point where extinguishment takes place. Evidence of the effectiveness of this simple chute is shown in Figure 10. From current peak to zero is a smooth unbroken line descending at a rapid rate, an excellent illustration of efficient current interruption. The advantage of this type of chute is that, having been designed for a specific voltage level, it has no current limitation. Any increase in current merely increases the magnetic forces which in turn speed interruption.

CLOSING DRIVE

Since rectifier anodes are operated in groups of six or multiples of six, it was decided that six individual breaker poles, each with its independent current directional trip, would be mounted on a common panel. A motor-driven closing mechanism was selected as the most suitable means for obtaining reliable closing performance. This mechanism consisted of a series motor suitable for either a-c or d-c operation, a torque brake, and a gear-driven closing cam. This complete unit was connected to a closing crossbar affixed to the closing lever of each individual pole. Operating the closing crossbar through 360 degrees of cam travel would cause any number of poles in the open position to be closed. This was particularly desirable since on arc-back with high-speed breaker operation, the arc-back is removed before all anodes feeding into the rectifier are caused to arc back. Since under this condition the closing load on the motor drive can vary widely, it was desirable to obtain a relatively constant closing speed. Otherwise single-pole closing might be accomplished too rapidly as compared to multipole closing. Constant speed closing was obtained by inserting a torque brake between the driving motor and the gear-driven closing cam. This device functioned to brake the motor due to centrifugal force at a predetermined rpm.

Figure 11 shows the completed design embodying the features just described.

Performance Tests on the High-Speed Anode Breaker

FACTORY TESTS

During the progress of development of this new high-speed anode air circuit breaker, several representative short-circuit tests were made in the factory testing department. After the final design had been reached a complete six-pole model was built and tested in a rectifier circuit in the factory. Figure 12 indicates the test arrangements made and power circuits used.

Results of these tests were gratifying, but it was realized that the test conditions imposed might not be so severe as would be encountered in the field, especially for large electrochemical pot lines. The rectifier transformer used during these tests and the a-c system capacity were such that current contributions from other anodes in the same rectifier which fed into the anode in arc-back were as high as would be usually encountered in the field. However, it was impossible to duplicate d-c contributions from parallel rectifiers such as would be encountered in large electrochemical installations. Instead, an 1,800-kw 600-volt motor-generator set was connected to the same bus, but this unit lacked capacity for such large d-c contributions as were anticipated in the field. It also required its own high-speed breaker protection to prevent commutator damage, so that the duration of

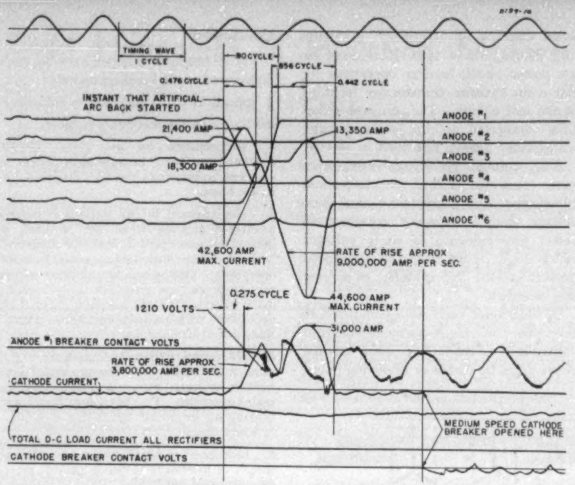


Figure 10. Typical oscillogram taken during field tests

its current contribution to the arc-back was limited.

FIELD TESTS

Therefore, it was felt advisable to confirm final factory tests by making operating-speed and interrupting-capacity tests in a large rectifier installation. Arrangements were made for such tests. This rectifier installation consisted of eight 650-volt 2,600-kw rectifiers, supplied from four rectifier transformers, all connected to the same d-c bus. Short-circuit capacity at the 13.8-kv a-c supply bus was approximately 400,000 kva. D-c load during the test period averaged 20,800 kw on the eight rectifiers.

Figure 13 shows the one-line power-circuit diagram for the eight rectifiers and test arrangements for the rectifier and six-pole high-speed anode circuit breaker. Two six-element oscillographs were used to record current and voltage behavior in the various circuits simultaneously.

Since arc-backs cannot be produced at a predictable instant, it is necessary to simulate an arc-back by short-circuiting one rectifier element with a switch at the desired instant. Such a method imposes short-circuit currents which exceed the values reached under natural arc-back conditions, because they eliminate the effect of arc drop in the rectifier element in arc-back. Thus such test conditions are in error in the conservative direction. A large electrically operated power circuit breaker was used as the short-circuiting switch during the tests. No overcurrent tripping action was provided with this switch and so it remained closed until it was opened manually after each short-circuit test was conducted.

This particular installation had been made with the cathode switching-type arc-back protective equipment on all rectifiers. During the tests the regular high-speed cathode breaker remained in the open position at all times. A suitable 4,000-ampere medium-speed air circuit breaker with overcurrent tripping device was substituted instead.

OSCILLOGRAPHIC RESULTS

Figure 10 is a reproduction of an actual oscillogram obtained during the field tests. Several such oscillograms were taken, and this particular one is typical of the maximum fault conditions recorded.

Trace for anode 1 shows the current through the short-circuiting switch. It is apparent that the instant of switch closure occurred during the time of normal forward-current conduction for anode 1. Performance of pole 1 of the high-speed anode circuit breaker is clearly indicated on the oscillogram. Maximum current contributions come from anodes 1 and 5 and from the other paralleled rectifiers through the cathode line.

An interesting piece of incidental data obtained in this test is the record of the natural arc-back in anode 5. After its high forward-current contribution, it immediately went into arc-back as proved by the reversal of current. Performance

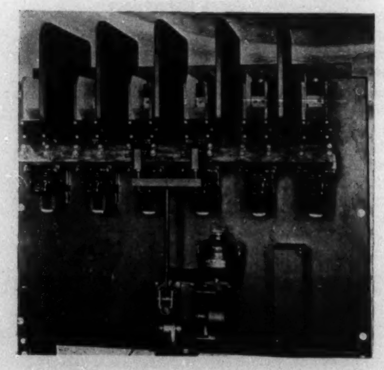


Figure 11 (left). The new high-speed breaker

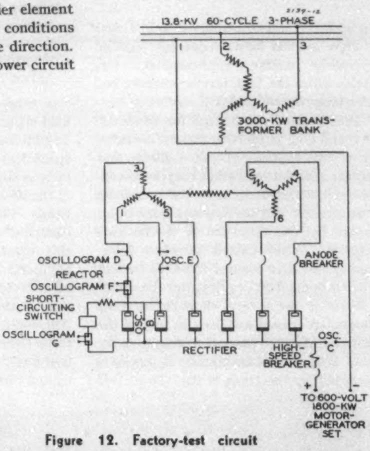


Figure 12. Factory-test circuit

of pole 5 is clearly indicated by the current trace for that anode.

Time required for contacts of pole 1 to part after current reversal was 0.275 cycle as measured by the trace of "anode 1 breaker contact volts." Current reduction as a result of arc quenching in the arc chute always started appreciably earlier than one-half cycle, according to the oscillograms taken during each test. The values of 0.476 cycle and 0.442 cycle illustrated in Figure 10 were typical.

Study of the cathode-current behavior shows that the individual poles each contributed interrupting effort, not only to that of the currents from the other anodes, but also to those from other rectifiers as well. In fact, pole 5 successfully interrupted all contributions. This is proved, because the traces for anodes 2, 3, 4, and 6 show normal forward-current rectifier operation lasting approximately $1^1/_4$ cycles after the last reverse current has been interrupted by pole 5.

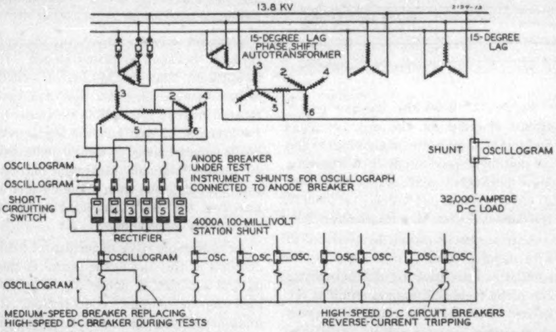

13.8 KV
15-DEGREE LAG PHASE SHIFT AUTOTRANSFORMER
15-DEGREE LAG
3
2
4
1
5
6
SHUNT
OSCILLOGRAM
OSCILLOGRAM
OSCILLOGRAM
ANODE BREAKER UNDER TEST
INSTRUMENT SHUNTS FOR OSCILLOGRAPH CONNECTED TO ANODE BREAKER
SHORT-CIRCUITING SWITCH
1 4 3 6 5 2
RECTIFIER
4000A 100-MILLIVOLT STATION SHUNT
32,000-AMPERE D-C LOAD
OSCILLOGRAM
OSC.
OSCILLOGRAM
MEDIUM-SPEED BREAKER REPLACING HIGH-SPEED D-C BREAKER DURING TESTS
HIGH-SPEED D-C CIRCUIT BREAKERS REVERSE-CURRENT TRIPPING

Figure 13. Field-test circuit

Traces of anode current for anodes 2, 3, 4, and 6 cathode current, and medium-speed cathode-breaker volts indicate that regular rectifier operation may have continued without distress on the remaining four anodes, had service not been interrupted by the operation of the cathode breaker. This circuit breaker interrupted less than normal full-load current when it opened $2^1/_2$ cycles after the event.

None of the several short-circuit tests conducted caused arc-back in any of the other rectifiers connected to the same d-c bus. Evidence of continuity of service is indicated by the trace of the "Total D-C Load Current All Rectifiers" in Figure 10. During these tests there were no a-c power circuit-breaker operations for the main rectifier transformer included in the test circuit. The companion rectifier connected to the same rectifier transformer always remained in service.

Surge-voltage recording instruments were connected across the interphase transformer and anode to neutral low-voltage main-transformer windings to detect any evidence of surge voltages which might result from the high-speed breaker action. No evidence of surging was recorded.

Oscillographic tests were conducted to determine the time required to interrupt normal load on the rectifier when tripping all six poles by means of removing polarizing coil voltage. Approximately $1^1/_8$ cycles were required after the switch in the polarizing circuit was opened.

CONDITION OF ANODE CIRCUIT BREAKER AFTER TESTS

A total of six tests were made in the field using the circuit shown in Figure 13. In addition to obtaining satisfactory high-speed operation, the results were particularly gratifying, in view of the condition of the breaker after the completion of the tests. The contacts were in excellent condition and justified the adoption of this arrangement for commercial use. No parts would have required replacement to continue in service. The condition of the breaker after tests and the high-speed performance obtained during tests clearly indicated that a new and useful tool had been provided to cope with mercury-arc rectifier arc-backs.

Conclusion

The advantages obtained by using such high-speed anode breakers include:

1. Shock to the transformer is reduced.

2. Shock to the rectifier is reduced.

3. Maintenance on a-c power circuit breaker is reduced, because it no longer is required to interrupt the high currents due to arc-back.

4. The vacuum in the rectifier is not so adversely affected when the arc-back is limited to one cycle or less, as compared with the longer time for a-c power-breaker operation. This means any rectifier which arcs back can be returned to service at once, whereas time out for pumping is often necessary with cathode-switching protection.

5. When two rectifiers are connected to the same transformer, only the one which arcs back is subjected to outage. The other remains in service. This has a most important bearing on improved continuity of service in many cases.

6. Likelihood of simultaneous arc-backs or spread of arc-backs to other parallel rectifiers is minimized. The reason is that the short circuit on the d-c bus is removed in one cycle or less. Again continuity of service is improved.

7. The anode breakers are useful disconnecting devices when two rectifiers are fed from a single transformer. They lend themselves to the isolating of one rectifier for maintenance or repair work without interrupting operation on the other rectifier.

8. High overcurrent duty is confined to one device for the anode-switching arrangement, whereas with cathode switching both cathode switch and a-c power circuit breaker open under high duty requirements. Maintenance is thus reduced.

9. Since each pole of the six-pole anode breaker opens independently of the other poles, and each pole is equipped with an operation counter, a record is provided of the anode arc-backs. This often is advantageous in locating and correcting arc-back trouble.

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Field Harmonics in Induction Motors

M. M. LIWSCHITZ
MEMBER AIEE

I. Introduction

ABOUT 40 papers have been written on the asynchronous torques, synchronous cusps at running, synchronous cusps at standstill (dead points), and noise of the induction motor. The subject has been also treated in some textbooks.* My aim is to give a coherent representation of the harmonic problem in induction motors. It is based on the work of my predecessors, on my own work, and on my experience.

It is intended to give not only a physical explanation of the phenomena but also to determine them quantitatively, that is, to determine the magnitudes of the different forces which produce the noise as well as of the different torques which influence the torque-speed curve. An exact knowledge of the currents produced by the field harmonics in the rotor is necessary for solving this problem. Since the magnitude of the harmonic currents in the rotor depends on the rotor leakage, a special study of the leakage of the single harmonics will be necessary. Of great influence is here the differential leakage; its amount increases with the order of the harmonic and therefore becomes important for the slot harmonics.

Thus a complete representation of the harmonic problem in induction motors involves the following topics:

1. The harmonics of the stator, their amplitudes, and their speed with regard to the stator and rotor.
2. The harmonics of the rotor, their amplitudes, and their speed with regard to the stator and rotor.
3. The force waves produced by the harmonics and their frequencies.
4. The resistance and reactance of the rotor with regard to the different harmonics.
5. The rotor currents produced by the different harmonics.
6. The forces produced by the harmonics.
7. The asynchronous torques.
8. The synchronous torques during running and at standstill.
9. Means to avoid the noise and the parasitic torques.

It is intended to divide the representation in three parts. This paper contains the topics 1, 2, and 3, that is, it treats the noise problem qualitatively. The magnitude of the forces that produce the noise will be determined in the third part, when the resistance and reactance of the rotor with regard to the different harmonics have been discussed in the second part.

II. The Stator and Rotor Harmonics

Since not only the windings with an integral number of slots per pole per phase are considered, but the windings with a fractional number of slots as well, it becomes expedient to take as the "fundamental" harmonic the wave, the length of which is equal to the circumference of the stator bore. The synchronous wave will be then the harmonic of the order $n' = p$; this is the "main" or "synchronous" harmonic. If in the case when the number of slots per pole per phase is an integer, it is desirable to use the main harmonic as the fundamental, the ratio $n = n'/p$ is valid; n is then the order of the harmonic with regard to the synchronous wave.

As shown in appendix 1, the order as well as the direction of rotation of the stator harmonics is defined by the two equations

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k \tag{1}$$

$$\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=k_1 \tag{2}$$

where k is an integer excluding 0, k_1 an integer including 0. When the minus sign in equation 2 is used, n' travels with the synchronous harmonic; when the plus sign in equation 2 is used, n' travels opposite to the synchronous harmonic.

When the number of slots per pole per phase q_1 is an integer, β has to be set in equations 1 and 2 equal to 2, whence

$$n'=(k_1m_1+1)P \tag{3}$$

or

$$n=k_1m_1+1 \tag{4}$$

where k_1 is any positive or negative integer including 0. When q_1 is an integer, and the winding is a symmetrical three-phase 60-degree phase belt winding, k_1 is a positive or negative even integer including 0. When n' is positive, the harmonic travels with the synchronous harmonic; when n' is negative, the harmonic travels opposite to the synchronous harmonic.

In appendix 1, it is further shown that the n' harmonic of the stator produces the following rotor harmonics

$$m'=k_2Q_2+n' \text{ for the squirrel-cage rotor} \tag{5}$$

$$m'=k_2pm_2+n' \text{ for the wound rotor with } q_2 \text{ an integer} \tag{6}$$

where k_2 is a positive as well as a negative integer including 0. For positive values of the slip s_n' the harmonic m' travels with the rotor when m' is positive, opposite to the rotor when m' is negative.

The stator slot harmonics are given by the equation

$$n_{sl}'=\pm Q_1+p \tag{7}$$

and the rotor slot harmonics by the equation

$$m_{sl}'=\pm Q_2+p \tag{8}$$

It follows from equations 5, 6, and 8 that the rotor slot harmonics are produced by the main (synchronous) wave of the stator, and that $k_2 = \pm 1$ corresponds to the slot harmonics of the squirrel-cage rotor, while a large k_2 corresponds to the slot harmonics of the wound rotor.

Equation 6 assumes that the number of slots per pole per phase q_2 of the rotor is an integer. If q_2 is a fractional number, $k_2\dfrac{2}{\beta_2}pm_2$ has to be inserted in equation 6 instead of k_2pm_2 (corresponding to equation 34, appendix 1). This is valid also for the formulas derived from equation 6 in the following.

In appendix 2 formulas are given for the harmonics of the magnetomotive forces of the stator and rotor which take into account their speed relative to the stator as well as to the rotor. Since the noise problem only is considered here, the magnetomotive forces with regard to the stator are of importance.

The magnetomotive force of the n'th stator harmonic is

$$b_{n'}=B_{n'}\sin\left(\omega t-n'\frac{\pi}{p\tau}x_1\right) \tag{9}$$

and the magnetomotive force of the m'th rotor harmonic

$$b_{m'}=B_{m'}\sin\left[\left(1+\frac{m'-n'}{p}(1-s)\right)\times \omega t-m'\frac{\pi}{p\tau}x_1\right] \tag{10}$$

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M. M. Liwschitz is with The Polytechnic Institute of Brooklyn, Brooklyn, N. Y., and is also consulting engineer for Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

* The fundamental paper on the influence of harmonics in the squirrel cage rotor has been published by L. Dreyfus.[16] The most detailed representation is given in the textbook of R. Richter, ELEKTRISCHE MASCHINEN.[10]

The rotor harmonic m' is produced by the stator harmonic n'; therefore, corresponding to the equations 5 and 6

$$b_{m'}=B_{m'}\ \sin\left[\left(1+\frac{k_2Q_2}{p}(1-s)\right)\times \omega t-m'\frac{\pi}{p\tau}x_1\right] \text{for the squirrel-cage rotor} \tag{10a}$$

$$b_{m'}=B_{m'}\sin\left[(1+k_2m_2(1-s))\omega t-m'\frac{\pi}{p\tau}x_1\right] \text{for the wound rotor} \tag{10b}$$

The values of $B_{n'}$ and $B_{m'}$ are given in appendix 2.

III. The Force Waves and Their Frequencies

The driving force of the machine is a tangential force produced by the main flux wave and the main magnetomotive force wave. The flux harmonics and the harmonics of the magnetomotive force produce parasitic tangential forces which contribute to the noise. However, the main source of noise is the group of radial forces produced by the flux harmonics. The radial force that corresponds to the induction b in the gap is

$f_r=1.385b^2\times10^{-8}$ pounds per square inch

b consists of all stator and rotor harmonics. Hence b^2 is the sum of the squares of all stator and rotor harmonics plus twice the product of each stator harmonic with each other stator harmonic, of each rotor harmonic with each other rotor harmonic and of each stator harmonic with each rotor harmonic. We write in general form

$$f_r\equiv b_{\gamma'}b_{\mu'}$$

We can use this formula for all five components of b^2 making γ' and μ' equal to the values of n' and m' in consideration. It is in general

$$b_{\gamma'}=B_{\gamma'}\sin\left(\omega_{\gamma'}t-\gamma'\frac{\pi}{p\tau}x_1\right)$$

$$b_{\mu'}=B_{\mu'}\sin\left(\omega_{\mu'}t-\mu'\frac{\pi}{p\tau}x_1\right)$$

so that

$$f_r\equiv b_{\gamma'}\,b_{\mu'}\equiv\frac{1}{2}B'B_{\mu'}\times \cos\left[(\omega_{\gamma'}-\omega_{\mu'})t-(\gamma'-\mu')\frac{\pi}{p\tau}x_1\right]- \frac{1}{2}B_{\gamma'}B_{\mu'}\cos\left[(\omega_{\gamma'}+\omega_{\mu}')t-(\gamma'+\mu')\frac{\pi}{p\tau}x_1\right] \tag{11}$$

Thus the radial force f_r consists of two force waves, one having $\gamma'-\mu'$ force pole pairs and the frequency $\omega_{\gamma'}-\omega_\mu$ the other having $\gamma'+\mu'$ force pole pairs and the frequency $\omega_{\gamma'}+\omega_{\mu'}$. We designate the number of force pole pairs by k. $k=0$ is a stationary pulsating force, $k=1$ is a force wave with two poles, $k=2$ is a force wave with four poles, and so on. The larger the value of k, the shorter is the wave length. The stator is, in general, stiffer to short wave distortion (k large) than to large wave distortion (k small). Yet, depending on the construction of the machine, the small wave distortion may be dangerous as well.

We consider now the different components of b^2.

(*a*). *Stator harmonics only.* In equation 11 we have to insert $\gamma'=n_a'$
$\mu'=n_b'$

It follows further from equation 9

$$\omega_{\gamma'}=\omega_{\mu'}=\omega=2\pi f_1$$

Thus

$$\begin{aligned} k&=n_a'+n_b' \qquad f_+=2f_1\\ k&=n_a'-n_b' \qquad f_-=0\end{aligned} \tag{12}$$

f_+ is the frequency of the force wave, when the sum of n_a' and n_b' is taken; f_- is the frequency of the force wave, when the difference of n_a' and n_b' is taken. n_a' and n_b' can be positive as well as negative.

When a single stator wave is considered, then $n_b'=n_a'$ and

$$k=2n_a' \qquad f_+=2f_1 \tag{12a}$$

Thus the force waves produced by the stator harmonics have double the line frequency. Also the main harmonic produces a force wave of double the line frequency.

The consideration of the case $k=1$, which must be avoided, we limit for the sake of clearness to integral values of q_1, since fractional numbers of q_1 are not often used in induction motors. $k=1$ means

$$n_a'+n_b'=\pm1 \text{ or } n_a'-n_b'=\pm1$$

With equation 3 this gives

$$\begin{aligned} n_a'+n_b'&=[(k_{1a}+k_{1b})m_1+2]p=\pm1\\ n_a'-n_b'&=(k_{1a}-k_{1b})m_1p=\pm1\end{aligned}$$

Since k is a positive or a negative integer including 0, the case $n_a'+n_b'=\pm1$ cannot occur. $n_a'-n_b'=\pm1$ is possible only when $p=1$, $m_1=1$ and k_{1a} or k_{1b} is an odd number, that is, when the magnetomotive force contains even harmonics.

(*b*). *Rotor harmonics only.* In this case $\gamma=m_a'$, $\mu=m_b'$. It follows further from equation 10

$$b_{m_a'}=B_{mba'}\sin\left[\left(1+\frac{m_a'-n_a'}{p}(1-s)\right)\times \omega t-m_a'\frac{\pi}{p\tau}x_1\right]$$

$$b_{m_b'}=B_{m_b'}\sin\left[\left(1+\frac{m_b'-n_b'}{p}(1-s)\right)\times \omega t-m_b'\frac{\pi}{p\tau}x_1\right]$$

Inserting here equation 6, we find for the wound rotor with q_2 integral

$$\begin{aligned} k&=m_a'+m_b' \qquad f_+=[2+(k_{2a}+k_{2b})\times m_2(1-s)]f_1\\ k&=m_a'-m_b' \qquad f_-=[(k_{2a}-k_{2b})m_2(1-s)]f_1\end{aligned} \tag{13}$$

and inserting equation 5, we find for the squirrel-cage rotor

$$\begin{aligned} &k=m_a'+m_b'\\ &\qquad f_+=\left[2+(k_{2a}+k_{2b})\frac{Q_2}{p}(1-s)\right]f_1\\ &k=m_a'-m_b'\\ &\qquad f_-=\left[(k_{2a}-k_{2b})\frac{Q_2}{p}(1-s)\right]f_1\end{aligned} \tag{14}$$

m_a', m_b', k_{2a}, and k_{2b} in equations 13 and 14 can be positive as well as negative.

For the case of the squirrel-cage motor as shown in appendix 2, the value of $B_{m'}$ becomes smaller, the larger the value of k_2. $k_2=\pm1$ gives the largest amplitude for the rotor harmonic. Considering this case, that is,

$$k_{2a}=\pm1 \qquad k_{2b}=\pm1$$

we find for the squirrel-cage rotor

$$\begin{aligned} f_+&=\left[1\pm\frac{Q_2}{p}(1-s)\right]2f_1\\ f_-&=0\end{aligned} \tag{13a}$$

when k_{2a} and k_{2b} have the same sign and

$$\begin{aligned} f_+&=2f_1\\ f_-&=\frac{Q_2}{p}(1-s)2f_1\end{aligned} \tag{13b}$$

when k_{2a} and k_{2b} have different signs.

When a single harmonic is considered, then $m_a'=m_b'$; $k_{2a}=k_{2b}=k_2$

$$k=2\mu_a' \qquad f=\left[1+k_2\frac{Q_2}{p}(1-s)\right]2f_1 \tag{13c}$$

m_2 instead of Q_2/p is to be inserted in this equation for the wound rotor.

If we limit again the consideration of the case $k=1$ to q_1 integral there will be

$$m_a'+m_b'=\pm1$$

or

$$m_a'-m_b'=\pm1$$

With equations 3 and 5, this gives for the squirrel-cage rotor

$$m_a'+m_b'=\pm1=(k_{2a}+k_{2b})Q_2+[(k_{1a}+k_{1b})m_1+2]p$$

k_{2a} and k_{2b} are positive or negative integers including 0. In the usual case of a 60-degree-phase-belt winding, k_{1a} and k_{1b} are even integers including 0. Only the 120-degree-phase-belt winding, (two speed motors) may have even values as well as odd values of k_1. In order that $m_a'+m_b'=\pm 1$, the equation must be satisfied.

$$(k_{2a}+k_{2b})Q_2=-(k_{1a}+k_{1b})m_1p-2p\mp 1$$

With k_{1a} and k_{1b} even integers, this is possible only when Q_2 is an odd number. For $m_a'-m_b'=\pm 1$ it follows that

$$m_a'-m_b'=\pm 1=(k_{2a}-k_{2b})Q_2+(k_{1a}-k_{1b})m_1p$$

or

$$(k_{2a}-k_{2b})Q_2=(k_{1a}-k_{1b})m_1p\mp 1$$

With k_{1a} and k_{1b} even integers this equation can be satisfied when Q_2 is an odd number. Thus the rotor harmonics produce $k=1$ (two-pole force waves) when Q_2 is an odd number. When the stator winding produces even harmonics (120-degree-phase-belt winding) also an even Q_2 may produce $k=1$. The wound rotor has usually $q_2=$integer and Q_2 an even number. In the case $q_2=$fractional number, the equation 3 and equation 34, appendix 1, have to be used in order to find whether or not $k=1$ is possible.

The value $\dfrac{Qn}{60}=\dfrac{Qf}{p}$ is the slot frequency. It follows from equations 13*a* and 13*b* that the frequency of the noise which the rotor harmonics produce is approximately equal to double the slot frequency or to double the line frequency. Seldom another frequency corresponding to equation 13 may occur.

(*c*). *Products of stator and rotor harmonics.* Here

$$\gamma'=n_b' \qquad \mu'=m_a'$$

m_a' is produced by the stator harmonic n_a'. From equations 9 and 10 we get for the squirrel-cage rotor

$$\left.\begin{aligned} k&=n_b'+m_a' \quad f_+=\left[2+K_{2a}\frac{Q_2}{p}(1-s)\right]f_1\\ k&=n_b'-m_a' \quad f_-=\left[K_{2a}\frac{Q_2}{p}(1-s)\right]f_1 \end{aligned}\right\} \quad (15)$$

In this equation m_2 is to be substituted for $\dfrac{Q_2}{p}$ for the wound rotor with q_2 equal to an integer.

It follows from equation 15 that the frequency of the force waves produced by stator harmonics in co-operation with rotor harmonics does not depend on the order of the stator harmonic; it depends on the factor k_2 and also on the number of rotor phases, if the rotor is a wound rotor, or on the number of rotor slots, if the rotor is of squirrel-cage type.

When $k_{2a}=0$, m_a' is equal to n_a' and there will be

$$\left.\begin{aligned} k&=n_b'+m_a' \quad f_+=2f_1\\ k&=n_b'-m_a' \quad f_-=0 \end{aligned}\right\} \quad (15a)$$

For the rotor slot harmonics of the squirrel cage, $k_{2a}=\pm 1$. Thus, when the rotor slot harmonics are involved, the frequencies of the force waves of the squirrel-cage rotor are

$$\left.\begin{aligned} k&=n_b'+m_a' \quad f_+=\left[2\pm\frac{Q_2}{p}(1-s)\right]f_1\\ k&=n_b'-m_a' \quad f_-=\frac{Q_2}{p}(1-s)f_1 \end{aligned}\right\} \quad (15b)$$

It follows from equations 7 and 8 for the co-operation of the stator slot harmonics with the rotor slot harmonics

$$k_{s1}=n_{s1}'+m_{s1}'=\pm Q_1+p\pm Q_2+p$$

and

$$k_{s1}=n_{s1}'-m_{s1}'=\pm Q_1\mp Q_2$$

The smallest absolute values of k_{s1} are

$$\begin{aligned} k_{s1}&=Q_1-Q_2\pm 2p\\ k_{s1}&=Q_1-Q_2 \end{aligned} \quad (16)$$

Disturbing noise can be expected, when k_{s1} as given by equation 16 is a small number. $k_{s1}=1$ is dangerous, but under certain conditions even $k_{s1}=20$ may not be permissible. The noise produced by the machine depends not only on the harmonics and the radial forces produced by them, it depends also on the mechanical construction of the machine and on the number of poles which determines the depth of the stator core.

When the stator harmonics co-operate with rotor harmonics, the case $k=1$ results in

$$n_b'+m_a'=\pm 1$$

or

$$n_b'-m_a'=\pm 1$$

With equations 3 and 5, this gives for the squirrel-cage rotor (q_1 is an integer)

$$n_b'+m_a'=\pm 1=[(k_{1a}+k_{1b})m_1+2]p+k_{2a}Q_2$$

$$n_b'-m_a'=\pm 1=(k_{1b}-k_{1a})m_1p-k_{2a}Q_2$$

Applying here the same considerations as in the case of two rotor harmonics, treated under (*b*), we find the same results as there, namely that $k=1$ occurs when Q_2 is an odd number.

It follows from equations 15*a* and 15*b* that the frequency of the force waves of a squirrel-cage motor which are produced through the co-operation of the stator and rotor harmonics have approximately the slot frequency or double the line frequency. Occasionally higher frequencies corresponding to equation 15 may occur.

Since equation 8 is valid also for the wound rotor, the results derived for the squirrel-cage motor are valid also for the wound-rotor motor.

(*d*). *Zero-pole forces* ($k=0$). Under special circumstances the case $k=0$ may cause considerable trouble. $k=0$ means that there are no force poles, that the force is constant around the stator bore at any instant, but that it changes its value with the frequency $\omega_{\gamma'}-\omega_{\mu'}$ or $\omega_{\gamma'}+\omega_{\mu'}$ (equation 11). It can be seen from this equation and from the considerations under (*a*) and (*b*) that $k=0$ is possible only when stator harmonics co-operate with rotor harmonics; then we have $\omega_{\gamma'}-\omega_{\mu'}\neq 0$ and also $\omega_{\gamma'}+\omega_{\mu'}\neq 0$

Referring to (*c*) we have

$$\gamma'=n_b' \qquad \mu'=m_a'$$

and from equations 3 and 6 for the wound rotor

$$k=n_b'+m_a'=0=(k_{1b}+k_{1a})m_1+2+k_{2a}m_2$$

$$k=n_b'-m_a'=0=(k_{1b}-k_{1a})m_1-k_{2a}m_2$$

The number of phases of the rotor m_2 is usually equal to three. Thus $k=n_b'+m_a'$ will be equal to 0, when

$$(k_{1b}+k_{1a})\frac{m_1}{3}+\frac{2}{3}=-k_{2a}$$

that is, the sum $n_b'+m_a'$ cannot produce $k=0$, if the stator has $m_1=3$. $k=n_b'-m_a'$ will be equal to 0, when

$$(k_{1b}-k_{1a})\frac{m_1}{3}=k_{2a} \quad (17a)$$

If the stator has a three-phase winding there will be in this case, many combinations which will give $k=0$. The frequency of the force is given by equation 15 when m_2 is substituted for Q_2/p. The equation 15 is independent of the value of k.

We consider for example, the combination (q_1 integral)

$$m_1=m_2=3 \quad k_{1a}=0 \quad k_{1b}=-2=k_{2a}$$

To $k_{1a}=0$ corresponds $n_a'=p$; to $k_{1b}=-2$ corresponds $n_b'=-5p$; to $k_{2a}=-2$ corresponds $m_a'=-6p+p=-5p$ and $n_b'-m_a'=0$. The frequency of the stationary force produced by $n_b'=-5p$ and $m_a'=-5p$ is (equation 15)

$$f_-=-2\times 3(1-s)f_1=360(1-s) \text{ cycles per second}$$

If we would consider the combination $k_{1a}=0$, $k_{1b}=-4=k_{2a}$, that is, $n_a'=p$, $n_b'=-11p$, $m_a'=-11p$, we would find that the stationary force oscillates with 720 (1−s) cycles per second.

For the squirrel-cage rotor we find from equations 3 and 5

$$k=n_b'+m_a'=0=[(k_{1b}+k_{1a})m_1+2]p+k_{2a}Q$$

$$k=n_b'-m_a'=0=(k_{1b}-k_{1a})m_1p-k_{2a}Q_2$$

Since k_{1a} and k_{1b} are integers, $k=0$ will occur here when

$$\left.\begin{array}{l}\dfrac{k_{2a}Q_2}{p} \text{ is an integer or}\\[2ex] \dfrac{k_{2a}Q_2}{m_1p} \text{ is an integer}\end{array}\right\} \quad (17b)$$

The frequency of the forces is given by equation 15 which is independent of the value of k.

The equation 17a for the wound rotor can be satisfied by small values of k_{1a}, k_{1b}, and k_{2a}. The corresponding field amplitudes as well as the $k=0$ force will therefore be considerable. To the equation 17b corresponds usually a relatively large value of k_{2a} or k_{1b} or k_{1a}, that is, a harmonic with small amplitude. This is the reason why $k=0$ may more often make trouble in a machine with a wound rotor than in a machine with a squirrel-cage rotor.

The natural frequency of a stator core for radial vibrations is approximately

$$f_{\text{rad. vib.}}=\frac{32.4\times10^3}{r}\sqrt{\frac{1}{1+h_t/h_c}} \text{ cycles per second} \quad (18)$$

where r is the radius in the middle of the core, h_t the depth of the tooth and h_c the depth of the core. It depends mainly on the radius of the core.

INFLUENCE OF SATURATION

For the stator harmonics (equation 37, appendix 2) is valid.

$$b_{n'}=B_{n'} \sin\left(\omega t-n'\frac{\pi}{p\tau}x_1\right)$$

Since all harmonics n' are produced by the same current, ω is common for all harmonics, and each harmonic travels exactly its wave length during one cycle of the current.

If the magnetic circuit is saturated, the main harmonic of the magnetomotive force produces, besides the main field harmonic, also saturation field harmonics which travel 3, 5, ... wave lengths when the main harmonic travels only one wave length. The saturation harmonics are bound to the main harmonic and travel with it without changing position with regard to each other. For these harmonics we have to write

$$b_\gamma=B_\gamma \sin\left(\gamma\omega t-\gamma\frac{\pi}{\tau}x_1\right) \quad \gamma=3, 5, \ldots. \quad (19)$$

The saturation harmonics produce in cooperation with the rotor harmonics, force waves, the frequencies of which can be found from equations 11, 10, and 18 as follows

$$\left.\begin{array}{ll} & \gamma_b'=3 \quad f_+=\left[4+\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1\\ k=\gamma_b'+m_a' & \\ & \gamma_b'=5 \quad f_+=\left[6+\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1\\[2ex] & \gamma_b'=3 \quad f_-=\left[2-\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1\\ k=\gamma_b'-m_a' & \\ & \gamma_b'=5 \quad f_-=\left[4-\dfrac{m_a'-n_a'}{p}(1-s)\right]f_1\end{array}\right\} \quad (20)$$

The difference $m_a'-n_a'$ is given for the wound rotor as well as for the squirrel-cage rotor, by equations 5, 6, and equation 34, appendix 1.

Appendix I. The Order of the Stator and Rotor Harmonics

(a) The Stator Harmonics

We consider a stator winding with a fractional number of slots per pole per phase. Since subharmonics are possible, we take as fundamental the wave, the length of which is equal to the complete developed armature, that is, to 2π. The harmonic $n'=p$ is then the main harmonic (the "synchronous" harmonic).

The stator may have m_1 phases. The n'th harmonic of the magnetomotive force of one of the phases which we designate by 0 is

$$f_{1n'}^{0}=F_{1n'} \sin\omega t \cos n'\frac{x_1}{p\tau}\pi \quad (21)$$

When the number of slots per pole per phase is

$$q_1=a+\frac{b}{\beta} \quad (22)$$

where a is an integer, b/β a fractional number, and b and β have not a common divisor, then the winding repeats itself after each β poles. The winding with q_1=integer repeats itself after two poles. The time and space angle between two adjacent phases are for q_1 an integer $\frac{2\pi}{m_1}$ and $n'\frac{2\pi}{pm_1}$; correspondingly these angles are for q_1 a fractional number

$$\frac{\pi\beta}{m_1} \text{ and } n'\frac{\pi\beta}{pm_1}$$

The time and space angle between the phase designated by 0 and the cth phase are therefore for q_1=fractional number.

$$\frac{\pi\beta}{m_1}c \text{ and } n'\frac{\pi\beta}{pm_1}c$$

Thus the magnetomotive force of the cth phase is

$$f_{1n'}^{c}=F_{1n'} \sin\left(\omega t-\frac{\pi}{m_1}\beta c\right)\times \cos\left(n'\frac{x_1}{p\tau}\pi-n'\frac{\pi}{pm_1}\beta c\right) \quad (23)$$

or

$$f_{1n'}^{c}=\frac{1}{2}F_{1n'}\left\{\sin\left[\left(\omega t-n'\frac{x_1}{p\tau}\pi\right)+\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c\right]+\sin\left[\left(\omega t+n'\frac{x_1}{p\tau}\pi\right)-\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c\right]\right\} \quad (23a)$$

In order to find the resultant magnetomotive force we have to take the sum of all m_1 phases, that is, from $c=0$ to $c=m_1-1$. Thus

$$f_{1n'}=\frac{1}{2}F_{n'}\left\{\left[\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c+\cos\left(\omega t-n'\frac{x_1}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c\right]+\left[\sin\left(\omega t+n'\frac{x_1}{p\tau}\pi\right)\times\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c-\cos\left(\omega t+n'\frac{x_1}{p\tau}\pi\right)\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c\right]\right\} \quad (24)$$

The four sums we designate in turn by a, b, a_1, and b_1, and the angles in the round parenthesis by α and δ. We can write

$$f_{1n'}=\frac{1}{2}F_{n'}[(a\sin\alpha+b\cos\alpha)+(a_1\sin\delta-b_1\cos\delta)] \quad (25)$$

or

$$f_{1n'}=\frac{1}{2}F_{n'}\left[\sqrt{a^2+b^2}\sin(\alpha+\gamma_1)+\sqrt{a_1^2+b_1^2}\sin(\delta+\gamma_2)\right]$$
$$=\frac{1}{2}F_{n'}\sqrt{a^2+b^2}\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi+\gamma_1\right)+\frac{1}{2}F_{n'}\sqrt{a_1^2+b_1^2}\sin\left(\omega t+n'\frac{x_1}{p\tau}\pi+\gamma_2\right) \quad (25a)$$

These are two waves traveling in different directions, and both of the same order n'. Since the stator currents produce from each harmonic only one wave, traveling with the synchronous harmonic or opposite to it, it follows that for some harmonics the sums a and b, for other harmonics the sums a_1 and b_1, must be equal to zero.

We assume that a given harmonic, for example the n'th harmonic, travels with the synchronous harmonic. Then for this harmonic the sums a_1 and b_1 must be equal to zero, while $\sqrt{a^2+b^2}$ must have a definite value. It can be seen that this will occur when

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k \quad k \text{ integer excluding } 0$$

and

$$\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta=2\pi k_1 \quad k_1 \text{ integer including } 0$$

There will be

$$a=\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\cos k_1 2\pi c=m_1$$

$$b=\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}-1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\sin k_1 2\pi c=0$$

$$a_1=\sum_{c=0}^{c=m_1-1}\cos\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\cos k\frac{2\pi}{m_1}c=0$$

$$b_1=\sum_{c=0}^{c=m_1-1}\sin\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta c$$

$$=\sum_{c=0}^{c=m_1-1}\sin k\frac{2\pi}{m_1}c=0$$

On the other hand, if we assume that a given harmonic travels opposite to the rotation, we find that when

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k \quad k \text{ integer excluding } 0$$

$$\left(\frac{n'}{p}+1\right)\frac{\pi}{m_1}\beta=k_1 2\pi \quad k_1 \text{ integer including } 0$$

the sums a and b will be zero while $\sqrt{a_1^2+b_1^2}$ has a definite value. Thus

$$\left.\begin{aligned}&\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=k &&k \text{ integer excluding } 0\\&\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=k_1 &&k_1 \text{ integer including } 0\end{aligned}\right\} \quad (26)$$

are the criterions for the existence of the n'th harmonic in the magnetomotive force. When the minus sign is used, the harmonic travels with the synchronous harmonic; when the plus sign is used, the harmonic travels opposite to the synchronous harmonic.

The first criterion is independent of m_1, it indicates the existence of the n'th harmonic in the magnetomotive force of a single phase; the second criterion indicates the existence of the nth harmonic in the magnetomotive force of the total winding.[18]

It follows from the preceding that with $\beta=2$, the equations 26 are the criterions for the existence of the nth harmonic in a winding with q_1=integer. Thus, when q_1=integer,

$$\left.\begin{aligned}&\frac{n'}{p}+1=k &&k=\text{integer excluding } 0\\&\left(\frac{n'}{p}\mp 1\right)\frac{1}{m_1}=k_1 &&k_1=\text{integer including } 0\end{aligned}\right\} \quad (27)$$

are the criterions for the existence of the n'th harmonic. Since n'/p is also an integer when q_1=integer, the first criterion says that the magnetomotive force of a single phase contains in general all odd as well as all even harmonics. The usually used 60-degree phase-belt winding does not contain even harmonics; for this winding k is limited to even integers.

The second criterion can be more conveniently used in the form

$$n'=(k_1 m_1+1)p \quad (28)$$

where k_1 is a positive as well as a negative integer including 0. For the normal 60-degree phase-belt winding, k_1 is again limited to positive and negative even integers including 0.

(b) The Rotor Harmonics

The n'th stator harmonic induces in the rotor phase designated by 0 the current

$$i_{2n'}=\sqrt{2}\, I_{2n'} \sin s_{n'}\omega t$$

s_n' is the slip of the rotor relative to the n'th stator harmonic. To the current $i_{2n'}$ corresponds a rotor magnetomotive force, which contains different harmonics. We consider the m'th of these harmonics. Its magnetomotive force is

$$f_{2m'}=F_{2m'}\sin s_{n'}\omega t \cos m'\frac{x_2}{p\tau}\pi \quad (29)$$

We assume at first that the number of slots per pole per phase of the rotor is an integral number. Then the space angle between two adjacent phases is, as for the stator, $m'\frac{2\pi}{pm_2}$. The time angle between the electromotive forces of two adjacent rotor phases is determined by the n'th harmonic of the stator which produces the m' harmonic of the rotor, and since the space angle between two adjacent rotor phases is equal to $\frac{2\pi}{pm_2}$ this time angle is $n'\frac{2\pi}{pm_2}$. The time and space angle between the phase 0 and the cth phase are $n'\frac{2\pi}{pm_2}c$ and $m'\frac{2\pi}{pm_2}c$, and thus the resultant magnetomotive force

$$\begin{aligned}f_{2m'}=\frac{1}{2}F_{2m'}\Bigg\{\Bigg[&\sin\left(s_{n'}\omega t-m'\frac{x_2}{p\tau}\pi\right)\times\\&\sum_{c=0}^{c=m_2-1}\cos(m'-n')\frac{2\pi}{pm_2}c+\cos\Bigg(s_{n'}\omega t-\\&m'\frac{x_2}{p\tau}\pi\Bigg)\sum_{c=0}^{c=m_2-1}\sin(m'-n')\frac{2\pi}{pm_2}c\Bigg]+\\\Bigg[&\sin\left(s_{n'}\omega t+m'\frac{x_2}{p\tau}\pi\right)\sum_{c=0}^{c=m_2-1}\cos(m'+n')\times\\&\frac{2\pi}{pm_2}c-\cos\left(s_{n'}\omega t+m'\frac{x_2}{p\tau}\pi\right)\times\\&\sum_{c=0}^{c=m_2-1}\sin(m'+n')\frac{2\pi}{pm_2}c\Bigg]\Bigg\}\end{aligned} \quad (30)$$

Since m' and n' are integers, the sums are equal to 0 except when

$$(m'-n')\frac{2\pi}{pm_2}=k_2 2\pi \quad k_2 \text{ integer including } 0$$

and

$$(m'+n')\frac{2\pi}{pm_2}=k_2 2\pi \quad k_2 \text{ integer including } 0$$

or except when

$$m'=k_2 pm_2 \pm n' \quad (31)$$

For a given value of k_2 to any value of n' correspond two different values of m', that is, two traveling waves. Giving k_2 positive as well as negative values including 0, we can write

$$m'=k_2 pm_2+n' \quad k_2 \text{ positive or negative integer including } 0 \quad (32)$$

n' can be here, as before, positive or negative.

For the squirrel-cage rotor $m_2=\frac{Q_2}{p}$, and therefore

$$m'=k_2 Q_2+n' \quad (33)$$

While considering the wound rotor we have made the assumption q_2=integer. When q_2=fractional number it will be

$$m'=\frac{2}{\beta_2}k_2 pm_2+n' \quad (34)$$

This equation can be found by a similar consideration as for the stator.

Appendix 2. The Amplitudes of the Stator and Rotor Harmonics and the Speed of the Harmonics Relative to the Stator and to the Rotor

(a) The Stator Harmonics

For the n'th harmonic of the stator magnetomotive force

$${}_{1}, t)=F_{n'}\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi\right) \quad (35)$$

is valid, where

$$F_{n'}=\frac{\sqrt{2}}{\pi}m_1 N_1\frac{K_{dn'}K_{pn'}}{n'}I_1 \quad (36)$$

For the n'th harmonic of the field

$$b_{n'}(x_1, t)=B_{n'}\sin\left(\omega t-n'\frac{x_1}{p\tau}\pi\right) \quad (37)$$

where

$$B_{n'}=\frac{3.19}{gk_c k_s}\frac{\sqrt{2}}{\pi}m_1 N_1\frac{K_{dn'}K_{pn'}}{n'}I_1 \quad (38)$$

k_c is the Carter factor. The factor k_s takes into account the saturation. It is

$$k_s=\frac{AT_g+AT_{t1}+AT_{t2}}{AT_g} \quad (39)$$

For the higher values of n', k_s is to be set equal to 1, since the paths of these harmonics are through the tooth tops without penetrating further into the teeth and the core.

The velocity of the n'th harmonic of the field with regard to the stator will be found by differentiation with respect to t of the equation

$$\omega t - n' \frac{x_1}{p\tau} \pi = \text{constant}$$

as

$$v_{1n'} = \frac{dx_1}{dt} = \frac{p}{n'} \frac{\tau}{\pi} \omega = \frac{P}{n'} v_p \tag{40}$$

where

$$v_p = \frac{\tau}{\pi} \omega = 2\tau f_1 \tag{41}$$

is the velocity of the main (synchronous) harmonic ($n' = p$).

In order to determine the electromotive force induced by the n'th stator harmonic in the rotor winding, it is necessary to know the velocity of this harmonic with regard to the rotor.

If s is the slip of the rotor relative to the main harmonic, the velocity of the rotor is

$$v_r = (1-s)v_p = (1-s) \frac{\tau}{\pi} \omega$$

and therefore

$$x_2 = x_1 - v_r t = x_1 - (1-s) \frac{\tau}{\pi} \omega t$$

or

$$x_1 = x_2 + (1-s) \frac{\tau}{\pi} \omega t \tag{42}$$

Inserting this in equation 37 there will be found

$$b_{n'}(x_2, t) = B_{n'} \sin \left(s_{n'} \omega t - n' \frac{x_2}{p\tau} \pi \right) \tag{43}$$

where

$$s_{n'} = 1 - \frac{n'}{p} (1-s) \tag{44}$$

is the slip of the rotor with regard to the n'th stator harmonic. It is also

$$s_{n'} = \frac{f_{2n'}}{f_1} \tag{45}$$

$f_{2n'}$ is the frequency of the electromotive force induced by the n'th stator harmonic in the rotor winding. It follows from equation 43 for the velocity of the n'th stator harmonic with regard to the rotor

$$v_{2n'} = \frac{dx_2}{dt} = s_{n'} \frac{p}{n'} v_p = s_{n'} v_{1n'} \tag{46}$$

It follows further from equation 40

$$(\text{Rpm})_{n'} = \frac{P}{n'} n_s = \frac{60 f_1}{n'} \tag{46a}$$

that is, the speed of a harmonic is inversely to its order. If there exists a fundamental ($n' = 1$), its speed relative to the stator will be equal to $60 f_1$, that is, 3,600 rpm for $f_1 = 60$ cycles per second. This is the highest speed that a stator harmonic can reach.

(b) The Rotor Harmonics

Corresponding to equation 37 we can write for the m'th harmonic of the rotor field

$$b_m (x_2, t) = B_{m'} \sin \left(s_{n'} \omega t - m' \frac{\pi}{p\tau} x_2 \right) \tag{47}$$

This m'th rotor harmonic is produced by the n'th stator harmonic. The frequency of the rotor currents depends on the slip of the rotor relative to the n'th stator harmonic. For the amplitude $B_{m'}$ is valid (equation 4)

$$B_{m'} = \frac{3.19}{g k_c k_s} \frac{\sqrt{2}}{\pi} m_2 N_2 \frac{K_{dm'} K_{pm'}}{m'} I_{2n'} \tag{48}$$

$I_{2n'}$ is the current induced in the rotor by the stator harmonic n'.

For the squirrel-cage rotor we have to insert in equation 48

$$m_2 = \frac{Q_2}{p}, \; N_2 = {}^1/_2, \text{ and } K_{dm'} = K_{pm'} = 1$$

Further, we have to multiply equation 48 by p, if we understand $I_{2n'}$ to be the current per slot. Thus the field amplitude $B_{m'}$ of the squirrel-cage rotor is

$$B_{m'} = \frac{3.19}{g k_c k_s} \frac{\sqrt{2}}{2\pi} Q_2 \frac{1}{m'} I_{2n'} \tag{48a}$$

It follows from equation 33 (appendix 1) for the squirrel-cage rotor that its value of $B_{m'}$ is usually the larger the smaller is k_2. $k_2 = \pm 1$ gives usually the largest value for $B_{m'}$.

From equations 47 and 44, it follows for the velocity of the m'th rotor harmonic with regard to the rotor

$$v_{2m'} = \frac{dx_2}{dt} = s_{n'} \frac{p}{m'} \frac{\tau}{\pi} \omega = \frac{p}{m'} \left[1 - \frac{n'}{p} \times (1-s) \right] v_p \tag{49}$$

In order to study the noise problem it is necessary to know the velocity of the harmonics of the rotor fields with regard to the stator. Inserting in equation 47 the value of x_2 from equation 42

$$b_{m'}(x_1, t) = B_{m'} \sin \left[\left(1 + \frac{m' - n'}{p} (1-s) \right) \times \omega t - m' \frac{x'}{p\tau} \pi \right] \tag{50}$$

The velocity of the m' rotor field harmonic with regard to the stator is thus

$$v_{1m'} = \frac{dx_1}{dt} = \frac{p}{m'} \left[1 + \frac{m' - n'}{p} (1-s) \right] v_p \tag{51}$$

In the last two equations we have to substitute equations 32, 33, and 34 (appendix I)

$$\frac{m' - n'}{p} = k_2 m_2 \quad \text{for the wound rotor with } q_2 = \text{integer}$$

$$\frac{m' - n'}{p} = \frac{2}{\beta_2} k_2 m_2 \quad \text{for the wound rotor with } q_2 = \text{fractional number} \tag{52}$$

$$\frac{m' - n'}{p} = k_2 \frac{Q_2}{p} \quad \text{for the squirrel cage}$$

where k_2 is a positive or negative integer including 0.

We have seen that with $f_1 = 60$ cycles per second, the speed of the stator harmonics cannot exceed 3,600 rpm. The rotor harmonics are produced by currents of low as well as of high frequency. The speed that corresponds to the latter harmonics may be very high. It follows from equations 49 and 51

$$(\text{Rpm})_{2m'} = \frac{p}{m'} \left[1 - \frac{n'}{p} (1-s) \right] n_s \tag{53}$$

and

$$(\text{Rpm})_{1m'} = \frac{p}{m'} \left[1 + \frac{m' - n'}{p} (1-s) \right] n_s \tag{54}$$

We consider for example a squirrel-cage motor of 30 horsepower, four poles, 60 cycles, three-phase, with $Q_1 = 72$, $Q_2 = 58$ and examine the combination $n' = +62$, $k_2 = -1$, $m' = +4$. It follows from equations 53 and 54 with $s = 1.5$ per cent

$$(\text{Rpm})_{2m'} = \frac{1}{2} \left(1 - \frac{62}{2} \times 0.985 \right) 1{,}800 = 26{,}500$$

$$(\text{Rpm})_{1m'} = \frac{1}{2} \left(1 - \frac{58}{2} \times 0.985 \right) 1{,}800 = 24{,}800$$

The frequency of the rotor currents that correspond to the combination in consideration is (equation 44 or 45)

$$f_{2n'} = \left(1 - \frac{62}{2} \times 0.985 \right) 60 = 1{,}768 \text{ cycles per second}$$

Nomenclature

b—Instantaneous value of the field intensity
B—Amplitude of the field
f—Instantaneous value of the magnetomotive force
f—Frequency
f_1—Line frequency
F—Amplitude of the magnetomotive force
k—Integer
k—Number of force pole pairs
k_d—Distribution factor
k_p—Pitch factor
k_c—Carter factor
k_s—Saturation factor
m_1—Number of stator phases
m_2—Number of rotor phases
m'—Order of the rotor harmonics
m_{s1}'—Order of the slot harmonics of the rotor
n—Speed (rpm)
n'—Order of the stator harmonic
n_{s1}'—Order of the slot harmonics of the stator
N—Number of turns per phase
p—Number of pole pairs
q—Number of slots per pole per phase
Q—Total number of slots
s—Slip
v—Velocity
β—Number of poles per group, when q_1 = fractional number
τ—Pole pitch
ω—$2\pi f_1$

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Transient Recovery Voltages and Circuit-Breaker Performance

R. C. VAN SICKLE
MEMBER AIEE

EXPERIENCE with power circuit breakers has frequently shown that two breakers of identical design may give radically different performances, even though the operating voltages and short-circuit currents to which they are subjected are the same. Studies of the conditions surrounding these circuit breakers have traced the cause to the differences in the manner in which the recovery voltage has appeared across the contacts of the circuit breakers subsequent to the final interruption. This voltage which occurs between the final arc voltage and the 60-cycle recovery voltage is an equalizing phenomenon defined as the transient recovery voltage. To take it into account in breaker applications, studies have been made of the transient voltages which can occur on systems, and other studies have been made of the response of the circuit breakers to transient recovery voltages.

In the higher-voltage classes of circuit breakers, 34.5 kv and above, the voltage-recovery rates encountered in service are most severe when the circuit breaker is located adjacent to the power transformers. This is exactly the condition which is encountered in high-power laboratories, and so the recovery rates produced on the laboratory circuits for a given voltage and current closely approximate the maximum natural frequency and voltage-recovery rate which can be encountered in the service. In fact, service conditions will frequently involve the use of transmission lines supplying power to the circuit breaker. These lines may be supplying only a fraction of the total power which the breaker is called upon to interrupt, but the presence of the line on the bus greatly increases the capacitance of the circuit and reduces the rate at which the voltage appears across the breaker contacts. Consequently, when the voltages and currents can be duplicated on the laboratory high-voltage circuits, the transient recovery voltages obtained will probably be as severe or more severe than the transient recovery voltages encountered in service. A laboratory demonstration of the interrupting ability of a high-voltage breaker is, therefore, an excellent assurance of equally good service performance.

Studies of power-system circuits at generator voltage have revealed that the location of a reactor close to a circuit breaker produces a very high natural frequency in the transient recovery voltage. Some of these values have been found to be even higher than those encountered in normal high-power laboratory circuits. Very little was known about the performance of circuit breakers when subjected to these extremely high frequencies, and the need for additional data was apparent.

To eliminate this deficiency by studying transient recovery voltages having very high natural frequencies and to determine the effect of two-frequency transients, the tests described in this paper were made. These tests, made on four different types of circuit breakers, demonstrate the effects of various types of transient recovery voltages and show that the extremely high natural frequencies obtained with reactors close to breakers are not appreciably more severe on the circuit breakers than the natural frequencies normally obtained in the laboratory. They also reveal that tests made on the regular laboratory circuits may often be used to demonstrate the ability of the breaker to open under the most severe circuit conditions.

The Problem

Interrupting a circuit by a breaker consists of changing the current path through it from a good conductor to a good insulator. This is done preferably in as short a time as possible, with a minimum depreciation of breaker parts and as little external demonstration as possible. To accomplish this, the breaker separates the metallic current-carrying parts, thereby drawing an arc which temporarily completes the circuit. This current goes to zero periodically twice each cycle, and it is at one of these current zeros that the breaker completes the transition from a conductor to an insulator by deionizing the space between the contacts. An opportunity to do this occurs at each zero, but the arc will restrike if the voltage applied across the breaker exceeds the dielectric strength of the arc space. The dielectric strength of an ionized space has been defined by J. Slepian as the voltage required for sustaining or increasing the conductivity of the arc space, and it depends on the amount of ionization remaining and the rate at which the breaker is reducing it. The removal of the ionization requires time, and the breaker dielectric strength increases at a finite rate. This strength as defined above is finite at all times but increases toward the end of a half-cycle and just before current zero may attain high values. Unless at some time the applied voltage exceeds it, the dielectric strength will continue to increase after current zero, and the transition to an insulating state will be completed.

At the time of current zero in the

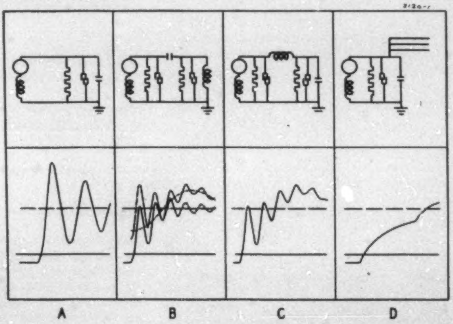

Figure 1. Typical simplified circuits and their corresponding transient recovery voltages

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R. C. VAN SICKLE is in the circuit breaker and protective devices engineering department of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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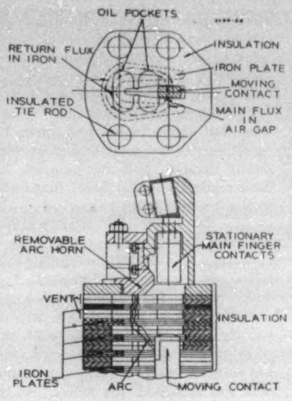


De-ion grid

breaker, the amount of ionization will sometimes be negligible, and at other times a sufficient amount may remain so that a current of a few amperes will flow through the arc space when the transient recovery voltage is applied across it.

The transient recovery voltage rises at a rate determined by the ability of the circuit to charge the capacitance adjacent to the breaker. The voltage does not increase at a constant rate nor always in the same general manner. The characteristics of the transient are determined primarily by the circuit constants. A simple circuit will have a transient recovery voltage which is essentially a sinusoidal wave as illustrated in Figure 1A. If the circuit breaker separates two parts of a circuit as shown in Figure 1B, each part will oscillate independently, and recovery voltage on the circuit breaker will be a two-frequency transient, the relative frequencies and amplitudes depending upon the inductances and capacitances of the circuit. A similar transient is produced by the more complicated circuit shown in Figure 1C. The addition of feeders to the bus, as shown in Figure 1D, may produce transients that are not sinusoidal.

These variations in the character of the transients are, of course, accompanied by variations in the speed with which they take place, as the inductances, capacitances, and number of lines are variable. The natural frequencies of the circuits used in these tests varied from 250 to 210,000 cycles per second.

The higher natural frequencies are caused by a low value of capacitance adjacent to the breaker, and only small currents flow into these capacitances during the transient. Consequently, if the ionization remaining in the arc space permits a comparable current to flow through the breaker in parallel with the current through the capacitance, a considerable modification of the transient recovery voltage is to be expected and actually takes place, as will be shown by cathode-ray oscillograms.

The circuits of a high-power laboratory for testing the interrupting capacity of breakers have one or more generators, current-limiting reactors, closing switches, backup breakers, and the circuit breaker on test. For convenience, the reactors are permanently installed as a part of the laboratory equipment and are provided with suitable switching arrangements for varying the effective value of the reactance in the lines over a relatively wide range. Consequently, the capacitance of the bus and switches between the reactor and the circuit breaker may be greater than it would be in a power station having the current-limiting reactors located within a few feet of the circuit breaker. To determine how much effect this additional capacitance might have and to extend the range of tests up to as high values as could be obtained, reactors were placed in the test cells with the breakers and were connected to them by short leads.

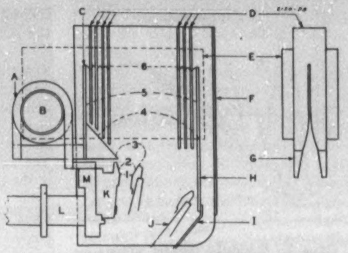
Four types of breakers were studied. The first was an oil circuit breaker using plain-break-type contacts with no means of controlling or directing the gases generated in the oil. The second was also an oil breaker but was equipped with modern arc-rupturing devices ("De-ion grids"), Figure 2. It belonged to the class of breakers using self-generated gas blasts. The third was an air circuit breaker which produced a magnetic blast to drive the arc into a confining slot. The fourth type was the compressed-air circuit breaker which subjected the arc to a transverse blast of compressed air and forced it against arc splitters.

Because of the various transients which may be encountered, three different types of tests were used.

In one type of test, the single-frequency transients, illustrated in Figure 1A, were used with the voltage and current constant and the capacitance varied. These tests demonstrated the effect of varying the natural frequency of the transient over a very wide range.

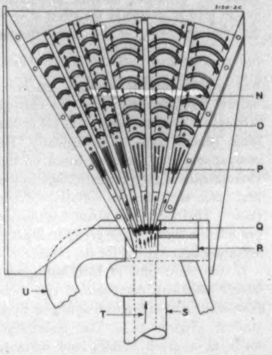
Another type of test covered a wide range of currents at a given voltage. Two series of tests were made, differing only in the natural frequency of the transient recovery voltage. Due to the inductance varying with current and the limitations within which the capacitances of the laboratory could be controlled, it

Figure 2. Diagrammatic views of equipment used in these tests



Magnetic blowout air-breaker stack (right)

A—Magnet coil
B—Iron yoke
C—Panel-end horn
D—Insulation plates
E—Laminated iron shoes
F—Outline of arc chamber
G—Arc shield
H—Arcing horn
I—Shunt strap
J—Moving contact arm and arcing contact
K—Arcing and secondary contact platform
L—Micarta bushing
M—Upper stud



Compressed-air-breaker stack

N—Metal coolers
O—Exhaust gases
P—Insulation splitters
Q—Arc
R—Stationary contact
S—Tubular support
T—Air blast
U—Moving contact

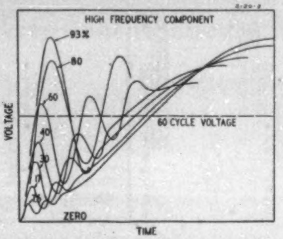


Figure 3. Calculated transient voltages for circuits used to determine the effect of varying the amplitude of the high-frequency component

was not possible to maintain the same natural frequency for all of the current settings or even to keep them all of the same general type. Consequently, for comparison, they were all based on the average rate of increase in voltage to the first peak, the transient voltage-recovery rate. This value was about 2,000 volts per microsecond for one group and 3,000 volts per microsecond for the other.

In the third type of test, voltage-recovery transients composed of two components having different frequencies were studied (Figure 1C). The tests were made at a given voltage and current, and, by varying the distribution of the impedance in the circuit, the amplitude of the high frequency component was varied from 7.5 to 93 per cent of the total amplitude of the transient (Figure 3). This was done to determine how high the first peak of voltage had to be before it exerted an influence on the performance of the circuit breaker. If the amplitude of the higher frequency component is relatively small, the voltages reached during the part of the transient controlled by it may be so low that they exert little or no influence on the breaker performance. However, if the amplitude of the higher-frequency component is relatively high, the transient approximates a single-frequency transient and naturally produces practically the same effect.

All tests were made on single-phase 60-cycle circuits. The 7,620-volt circuits had one side of the breaker grounded. The 13,200-volt circuits had the neutral of the generator grounded through a resistor. Records were made on a magnetic oscillograph and on a cathode-ray oscillograph having a rotating film drum. To expedite the testing, records of two similar tests frequently were made on one cathode-ray-oscillograph film using two different time scales.

Plain-Break Oil Circuit Breaker

The tests on the oil circuit breaker not equipped with arc-extinguishing devices demonstrated its performance over a wide range of circuit voltage-recovery rates. The breaker used had a tank diameter of 30 inches, a contact stroke of $11^1/_8$ inches and an average contact opening speed of 8.2 feet per second. The two breaks were each ten inches long. All tests were single-phase with one side grounded at the breaker. The tests were close to the voltage-interrupting ability but were well below the current-interrupting ability of the breaker.

A series of tests was made to determine the effect of varying the natural frequency of the circuit. This was done on a 7,620-volt 5,200-ampere circuit but at a potential of about 3,810 volts and a current of 2,600 amperes. A wide range of natural frequencies was obtained. The highest frequency, 208,000 cycles per second, resulted from placing a 1.2-ohm reactor in the test cell immediately adjacent to the circuit breaker. The lower values, down to 380 cycles per second, were obtained by various laboratory circuit connections and by adding capacitance to the normal laboratory circuit. The arcing time was a function of the natural frequency as shown by Figure 4.

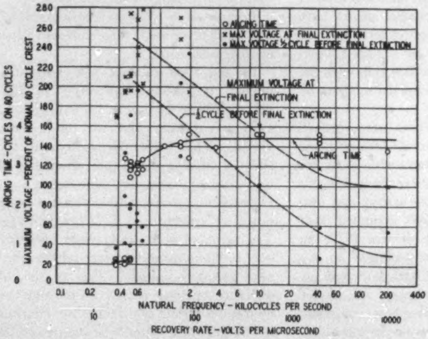


Figure 4. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Effect of natural frequency of transient recovery voltage

The cathode-ray oscillograms, Figure 5, show that at the higher frequencies the oscillations of the transients were completely damped, and the damping became relatively less at lower frequencies so that the maximum voltage which was reached during the extinction transients varied as shown in Figure 4. The voltage reached by the transient a half-cycle before interruption was also measured and plotted in Figure 4. At the higher frequencies, these voltages were well below the voltages reached on the final transient. However, as the frequency was reduced, and the energy of the transient was increased, the voltage on the transients increased and approached double the normal crest of the sixty-cycle wave. As this approximates the normal maximum voltage of a recovery transient, further reduction in frequency permitted the arc to extinguish on this transient which then became the final transient. The group of crosses representing maximum voltages on final transient recovery voltages for 500 to 600 cycles per second can be considered an indication of the end of this curve.

An interesting discontinuity exists in the arcing time curve at about this frequency. The breaker seemed to make a good effort to extinguish the arc within the first cycle and, if it failed to do so, would be unable to accomplish it until about the end of the third cycle of arcing. Below 400 cycles per second the breaker interrupted with about one-half cycle of arcing.

At six of the steps the maximum voltage which could be consistently interrupted was determined and was found to be about 3,810 volts 2,600 amperes. At higher voltages occasional failures occurred, and this limit appeared to be independent of the natural frequency of the circuit.

Two other series of tests were made to determine the effect of the amplitude of the higher-frequency component of two-frequency transients. They were planned and made prior to the series described above and before it was known that this breaker had a constant arcing time over such a wide range of natural frequencies. The lower frequencies chosen were as low as could be easily obtained but varied from 2,700 to 8,000 cycles per second on the low-current series and from 7,000 to 17,000 cycles per second on the higher-current series. These series gave data

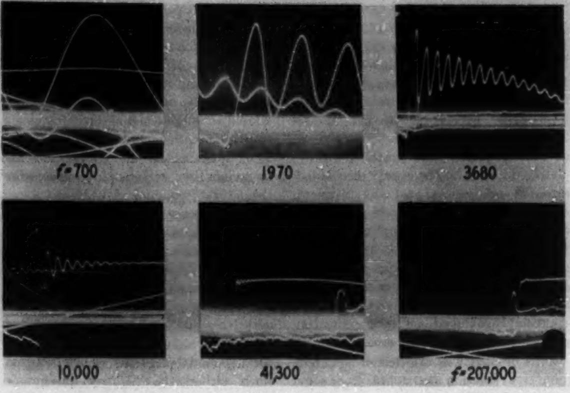


Figure 5. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Cathode-ray oscillograms showing effect of conductivity in the arc space on the transient recovery voltage

which indicated that at both currents the arcing time was substantially independent of the amplitude of the higher-frequency component, and this result was difficult to explain until the effect of varying the single-frequency transients over much wider ranges was disclosed by the later test. Consequently, the data on these tests are of interest in supplementing the previous data and in showing the effect of the conductivity of the arc space on the damping of the double-frequency transients.

In one series of tests, made with a current of 1,400 amperes at 3,180 volts, the amplitude of high-frequency component was varied from 7.5 to 93 per cent of the total amplitude in about ten per cent steps. The transients calculated during the planning of the test are shown in Figure 3, and the transients actually obtained are shown in Figure 6. The actual transients were modified considerably by the current being passed through the arc space of the circuit breaker.

The transients having calculated high-frequency components of 80 and 90 per cent were so heavily damped by the current through the arc space that they appeared aperiodic. Data from these tests are plotted in Figure 7 as a function of the amplitude of the calculated high-frequency component. The arcing time remained substantially constant. The maximum peak voltage decreased almost linearly, indicating that the higher-frequency component had negligible amplitude at the time of the maximum crest. The first peak increased almost linearly with the amplitude but at a rate only about half as great as would have been reached without conductivity in the arc space.

A similar series of tests were made at higher current, 6,000 amperes at 3,050 volts with high-frequency components having from 7 to 75 per cent of the total amplitude. For these values also the arcing time was independent of the amplitude of the high-frequency component, Figure 8. The conductivity was sufficient in all cases to damp completely the high-frequency component, and in some cases, the low-frequency component also. The low-frequency components increased in frequency as the amplitude decreased, but the range was only from 7,000 to 17,000 cycles per second. The constant arcing time and the damping of the transients indicates that the breaker easily dissipated all of the energy of the high-frequency component and that arcing times were the same for either the high or the low frequency.

The maximum voltage-interrupting ability was found at each setting by making tests at increasing voltages until failure occurred. The limit was found to be approximately the same for all of these circuits. The breaker failed on about half of the tests at 3,430 volts 6,750 amperes.

The tests on the plain-break oil circuit breaker operating near its maximum voltage-interrupting ability indicates that conductivity of the arc space following the normal current zero plays a big part in modifying the transients. The very slow high-energy transients are not appreciably affected as the energy loss is relatively low. As the frequency increases, and the energy decreases, the losses through the breaker result in increasing damping of the transients. At high natural frequencies, the admittance of the parallel capacitance path is so much smaller than the conductivity of the arc space that the capacitance current becomes negligible. Consequently, although reducing the capacitance further can greatly increase the natural frequency of the circuit and the calculated voltage-recovery rate, the actual transients obtained on test or in service will not be changed. When the conductivity damps out the higher-frequency components, the lower-frequency greater-energy components become more important. No increase in arcing time occurs with increase in natural frequency above the value where conductivity modifies the transients.

Only at natural frequencies below 2,000 cycles per second, corresponding to less than 45 volts per microsecond, did the arcing time decrease with decrease in frequency. Above 2,000 cycles per second, the arcing time was independent of the natural frequency or of the combination of components in double-frequency transients.

"De-ion Grid" Oil Circuit Breakers

The breaker used for this part of the test had a tank diameter of 30 inches, a stroke of $11^{1}/_{8}$ inches, a contact separation of 10 inches and an average opening speed of about 8.2 feet per second. It was equipped with a heavy-duty type of "De-ion grid" of the type shown in Figure 2. The breaker was tested at normal operating voltages but well below its interrupting ability.

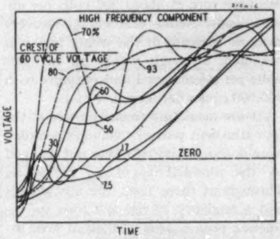


Figure 6. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Tracings of cathode-ray oscillograms for comparison with calculated transient voltages of Figure 3

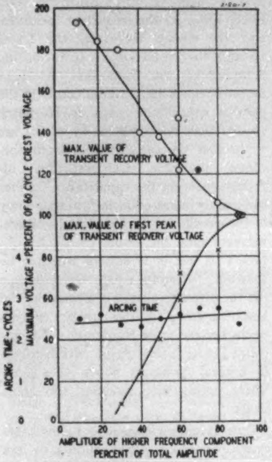


Figure 7. Plain-break circuit breaker, 3,800 volts, 2,600 amperes

Effect of the amplitude of the higher-frequency component

The tests made on this breaker were similar to those made on the plain-break circuit breaker. In many cases the circuits were the same, but the voltages and currents were higher.

One series of tests was made to determine the effect of varying the frequency of a single-frequency transient. This test was made at 7,620 volts 5,350 amperes. Frequencies from 375 cycles per second to 209,000 cycles per second were used.

The arcing time obtained on these tests varied with the natural frequency and voltage-recovery rate as shown in Figure 9. At the lower values the arcing times were less with a gradual rise up to a recovery rate of about 200 volts per microsecond. Above this voltage-recovery rate, no increase in the arcing time was obtained, although values up to 8,500 volts per microsecond were obtained with 209,000 cycles per second.

At the maximum frequency two of the four transient recovery voltages recorded were damped, and two were not damped by the conductivity of the arc space. Throughout these tests, the arc voltage had a tendency to rise and form an extinction peak. This resulted in some increase in the amplitude of the transient. The extinction peak indicates rapid deionization of the arc space and a high effective resistance at current zero. Consequently, damping of the transient was observed only at the highest natural frequency where the total energy of the oscillation was very small.

The preceding tests were supplemented by another series which showed that at 13,200 volts 8,000 amperes, the arcing time was constant for natural frequencies from 3,900 cycles per second up to 200,000 cycles per second. The highest natural frequency was practically completely damped by the energy discharged into the arc spaces, and the transient showed only the 32,000-cycle per second component having an amplitude about 15 per cent of the total. At the next step which had frequencies of 26,000 cycles and 42,000 cycles per second, the highest-frequency component was not appreciably damped, and the extinction peaks were definitely increased.

This breaker was also tested to determine its reaction to double-frequency transients. When the higher-frequency component is either very large or very small, the performance can be expected to be the same as the performance obtained with single-frequency transients corresponding to the predominant frequency. Accordingly, a series of tests was made at 7,620 volts 5,200 amperes with transients having components of two frequencies, one normally causing a shorter arcing time than the other. As the amplitude of the higher-frequency component increased, the arcing time increased as shown in Figure 10. The change in arcing time is gradual, and even small high-frequency components exert a noticeable influence.

These same relations were demonstrated at 15,000 amperes 7,620 volts, as shown on the same curve. The lower frequency varied from 594 cycles per second at the lower amplitude of the high-frequency component to 1,280 cycles per second at the higher amplitudes. These frequencies may be a little higher than those producing minimum arcing and may explain why the arcing time does not increase for high-frequency components greater than about 65 per cent of the total amplitude.

An attempt to get a similar curve at 7,620 volts 2,800 amperes failed, because at the lowest frequency available, 250 cycles per second, the arcing time was as long as at the higher frequencies. The first peak of the transient recovery voltage increased approximately linearly with the increase in amplitude of the higher-frequency component and indicated a negligible damping of the high-frequency component by the conductivity of the arc space. The maximum voltage reached during the transient was highest when one of the two components predominated and was lower when the two components approached each other in magnitude. The arcing time was practically independent of both the first peak and the maximum voltage of the transient.

The tests on this breaker demonstrated that the effect of single-frequency transients varied with the current. At 2,800 amperes no appreciable increase in arcing time resulted from increasing the natural frequency of the circuit above 250 cycles per second. At 5,200 amperes the corresponding point was 3,000 to 4,000 cycles, and at 15,000 amperes it was about 10,000 cycles.

Double-frequency transients demonstrated that the breaker performance varied gradually with the amplitudes of the two components. The limits were the two end conditions which occurred when either the higher-frequency or lower-frequency component was negligible.

Magnetic Blowout Air Circuit Breaker

During the development of a magnetic blowout air circuit breaker, tests were made in the high-power laboratory on single-phase circuits giving about 15,000 amperes at four kilovolts to determine the effect of recovery voltages on the arcing time and voltage-interrupting ability. These values were near the maximum interrupting capacity of the breaker. By

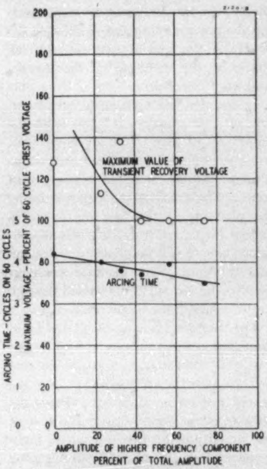


Figure 8. Plain-break circuit breaker, 3,050 volts, 6,000 amperes

Effect of the amplitude of the higher-frequency component

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varying the capacitance of the circuit, natural frequencies from 3,880 cycles per second to 12,000 cycles per second were obtained. For still lower transient recovery voltages a resistor was connected across the breaker to simulate a parallel resistive load. Some of the transient recovery voltages obtained are reproduced in Figure 12.

The arcing time was less than one cycle and independent of the transient recovery voltage. This indicated that the arc was interrupted at the first current zero after it had reached a suitable location in the arc chute. The arc voltage prior to interruption was relatively high and smooth. The lack of an extinction peak indicated that there would be some conductivity in the arc space subsequent to the current zero, and this is further indicated by the subsequent damping of the recovery transients. The damping is very noticeable on the circuits having high frequencies at which the current through the arc space is comparable to the current into the capacitance. At the lower frequencies the damping is less pronounced, as the current into the capacitance is large with respect to the current through the arc space.

How large this current into the parallel capacitance can become is illustrated in a striking manner in Figure 13. This is a section of an oscillogram made on one of the tests at 4.4 kv, a little above the interrupting ability of the breaker, and on a circuit having a large parallel capacitance obtained by connecting to it three large power transformers. The natural frequency of the transient recovery voltage was 4,000 cycles per second. Due to the high test voltage, the arc continued to restrike, and traces of the natural frequency of the transient recovery voltage appear in the arc voltage. The transient at the time of restriking excited the resonant circuit and produced a sinusoidal current in it which modified the current through the circuit breaker sufficiently to produce a sinusoidal ripple on the arc voltage.

With the transient recovery voltages of an oscillatory character, the breaker could interrupt about 4 kv regardless of frequency of the oscillation. By placing a resistor across the breaker to simulate a parallel resistive load, the breaker was able to interrupt 4.95 kv. Possibly the increase in the voltage was due to elimination of the tendency of the transient recovery voltage to reach crest voltages in excess of the normal crest of the 60-cycle recovery voltage.

This type of breaker, having a relatively high arc voltage, appears to be sensitive to the crest value which may be reached by the transient recovery voltage, but the arcing time remained constant from 3,880 to 12,000 cycles per second.

Compressed-Air Circuit Breakers

The compressed-air circuit breaker was the fourth type to be included in this investigation. One series of tests, made on a 15-kv 1,500,000-kva breaker with currents up to 36,000 amperes, showed that for circuit voltage-recovery rates of 1,500 to 2,250 volts per microsecond, the arcing time was substantially constant and lasted only to the first current zero after the contacts had been separating for one-tenth cycle. This resulted from the ability of the breaker to interrupt currents in this range at the first current zero, after contact separation adequate to hold this voltage was reached. Another series of tests over the same range, but with higher recovery rates varying from 2,600 to 3,800 volts per microsecond, showed that the minimum contact separation at which arc extinction could be accomplished varied with current. It corresponded to an arcing time of 0.35 cycle at 6,000 amperes (the same as for the lower recovery rate), and to an arcing time of about one cycle at 36,000 amperes, as shown by Figure 14.

This increase in arcing time with current was not due to a corresponding increase in voltage-recovery rate. The increased arcing time was apparently associated with the increased difficulty of interrupting the heavier currents which have greater quantities of ionized gases and which approach current zero at higher rates.

This is an important relation, since it indicates the nature of the mechanism by which arc extinction is brought about. It indicates that the arc column is being deionized, and that this process requires more time as the diameter of the arc (and its current) increases. If the arc extinction were brought about by a breaking of the arc column and the separation of the two ionized sections of the arc, higher recovery-voltage rate, rather than higher current would require additional arcing time for the introduction of additional arc splitters.

These tests also show the sensitivity of the compressed-air circuit breaker to relatively low high-frequency components in the transient recovery voltage. At the maximum current setting and the lower recovery rate, 2,200 volts per microsecond, the transient was composed of a single component having a frequency of 28,500 cycles per second. For the higher recovery rate, about 2,600 volts per microsecond, the transient had two compo-

Figure 9. "De-ion grid" oil circuit breaker, 7,620 volts, 5,350 amperes

Effect of natural frequency of transient recovery voltage

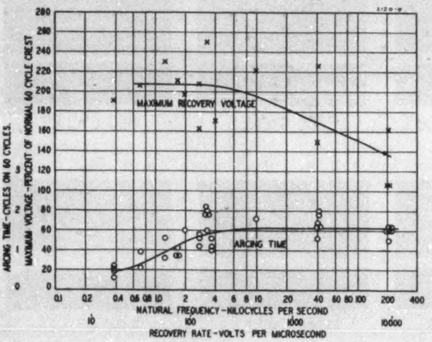
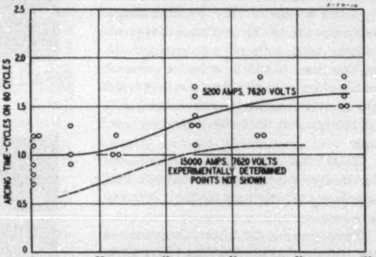


Figure 10. "De-ion grid" oil circuit breaker

Effect of the high-frequency component is roughly proportional to amplitude



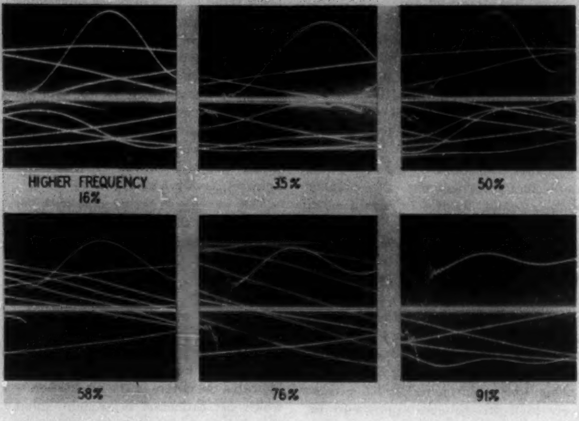


Figure 11. "De-ion grid" oil circuit breaker, 7,620 volts, 15,000 amperes

Cathode-ray oscillograms showing variation in high-frequency component

nents with frequencies of 33,600 and about 190,000 cycles per second. The amplitude of the 190,000-cycle component was only about 25 per cent of the total amplitude of the transient, but as shown by Figure 14, it caused at least a half-cycle increase in arcing time, and the last breakdown occurred at approximately the crest of this first peak of voltage, about 45 per cent of the crest of the 60-cycle recovery voltage.

The circuit transient recovery voltage on this setting, which would be based on zero arc voltage, would have a first crest of voltage about 57 per cent of the 60-cycle crest. This crest is below the 80 per cent which has been used as an arbitrary minimum voltage to be used in calculating circuit transient recovery voltage rates and emphasizes that lower amplitudes must be considered for some types of breakers. The circuit voltage-recovery rate to this first peak is approximately 4,600 volts per microsecond and, in this case, would be a better criterion of the circuit severity than the 2,600 volts per microsecond based on the second peak at 120 per cent of the 60-cycle crest voltage.

Tests made on another breaker of similar design, rated 15 kv 2,500,000 kva, supplemented the tests on the 1,500,000-kva breaker.

Tests varying the natural frequency of the transient recovery voltage from 3,900 to 200,000 cycles per second, and the circuit voltage-recovery rate from 300 to 13,600 volts per microsecond were made at 8,000 amperes 13,200 volts. The arcing time was practically constant over the entire range, varying in minimum and maximum value by only one or two tenths of a cycle.

The series of recovery voltage transients obtained on these tests and shown in Figure 15 is particularly significant and interesting. They show that at the lower frequencies, the transient is almost unaffected by the conductivity of the arc space. With smaller parallel capacitances, the charging current is less, and therefore the current through the breaker is relatively greater. The breaker current at moderate frequencies resulted in partial damping of the transient and finally at high frequencies became the controlling factor when the capacitance current became negligible with respect to it.

Other tests were carried up to 100,000 amperes, and the oscillograms indicated that the larger the current interrupted, the lower the effective resistance of the arc space at the final current zero. The approximate relations are shown on Figure 16. These resistances permitted sufficient current to flow subsequent to the normal current zero to partially damp the recovery transients at the higher currents.

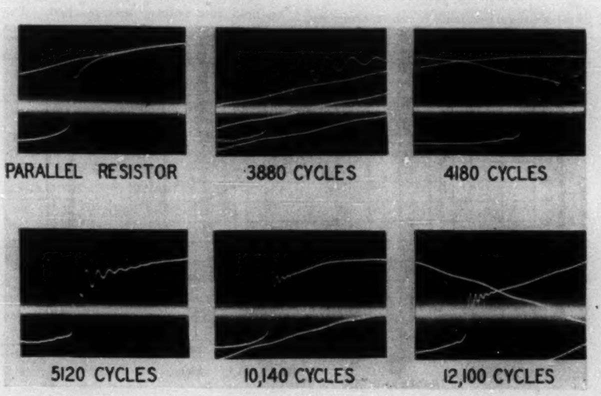
Discussion

These studies of four types of circuit breakers indicate certain points of similarity in their performance, even though the manner in which arc extinction is produced within the interrupter differs considerably in detail between types. All of these types of breakers modify the transient recovery voltages. The means by which this is accomplished is the same, but the range in which the modification begins to appear, and the range in which it overcomes completely the oscillating tendency of the circuit vary with the types of breakers and not necessarily in relation to their rated voltages and rated interrupting currents.

All of the circuit breakers were tested over a sufficiently wide range to include both modified and unmodified transient recovery voltages. The modification was produced by the breaker when it passed a small current through the arc space subsequent to the final normal current zero. This was evidenced by the transient recovery voltage departing from the values

Figure 12. Magnetic blowout air circuit breaker, 4 kv, 15,000 amperes

Effect of natural frequency of transient recovery voltage



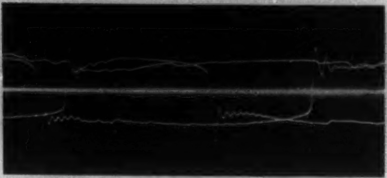


Figure 13. Magnetic blowout air circuit breaker, 4.4 kv, 16,500 amperes

Large power transformers connected to the same generator produced these oscillations in the arc voltage during an unsuccessful attempt to interrupt the circuit

which could be calculated for the circuit if all conductivity of the arc space was neglected. Also, the transient varied from test to test on the same circuit depending upon the conditions existing within the circuit breaker. The arc conductivity and rate of deionization are indicated roughly by the arc voltage prior to current zero.

The conductivity of the arc space at the time of current zero varies with the current being interrupted. This conductivity is a function of the ionization during the preceding half-cycle and the rate at which the space is being deionized. The ionization is produced by the current which will approach zero at a rate approximately a direct function of the rms current being interrupted. The residual ionization will depend on how rapidly these ions are removed.

In circuit breakers which use the energy of the arc to produce the deionizing effect, the rate of deionization might vary directly with the rms current, with the result that the right amount of deionizing activity would be present for each current. The larger the rms current being interrupted, the greater the rate at which the current approaches zero and the stronger the deionizing effect. The production and effectiveness of the deionizing action is probably never directly proportional to the current. Published data have frequently shown that breakers with self-generated deionizing action have shorter arcing times at high currents than at low currents. These shorter arcing times indicate that the deionizing activity produced by the arc is relatively stronger at the high currents.

Other breakers may have a deionizing activity produced by some source which is entirely independent of the current to be interrupted. For example, a compressed-air breaker delivers a deionizing blast of air, determined by the design of the circuit breaker and independent of the current being interrupted.

When very low values of current are interrupted, the deionizing activity may keep up with the current, and the conductivity at the current zero may be practically zero. Oscillograms made on various types of breakers show that even a short time before the normal zero in these low-current circuits, the arc may become unstable and extinguish suddenly, the current to the arc space transferring at this time to the path through the parallel capacitance of the circuit and resulting in a rise in the voltage across the breaker at the beginning of the transient recovery voltage. The oscillograms also show that this phenomenon, which mathematically can reach enormous voltages, is still under the control of the breaker, because, as the voltage rises, it cannot exceed the dielectric strength of the space between the contacts without the arc restriking. This dielectric strength, determined by ionization and contact separation, limits the voltages if an attempt is made to interrupt the current so early that the voltage rise is rapid and high. Oscillograms of a large number of tests on several oil and air breakers have indicated that the maximum voltage obtained in this manner probably is not over three times the normal crest of the line to ground voltage.

In other cases, the conductivity of the arc space may become zero as a current zero is reached, and the transient recovery voltage takes place in a manner which can be explained entirely on the basis of the inductance, capacitance, and losses of the power circuit. This type of phenomenon which is generally assumed in calculations may occur only at low currents, or it may extend even up to the rated interrupting current. In general, test data show that as the current is increased, the tendency for some of the ionization to remain at the time of current zero increases. From oscillograms made on the compressed-air circuit breaker, a curve was plotted to show qualitatively the relation between the equivalent resistance of the arc space at the time of current zero and the current which was being interrupted. Other types of breakers, including those having self-generated arc-extinguishing actions, have similar curves.

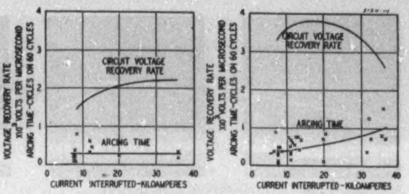


Figure 14. Compressed-air circuit breaker

Arcing time as a function of current interrupted for two series of tests made with different transient recovery voltages

The residual ionization, lasting for an appreciable time interval after the normal current zero, acts as a variable resistor connected across the terminals of the circuit breaker. This residual ionization or conducting path between the breaker contacts permits a current to flow through the breaker during the time of the transient recovery voltage. This current is a function of the equivalent resistance of the arc space and the voltage impressed across it. It probably has only a very low current density in this space, and the deionizing activity can remove more ions than it produces. Calculations based on the voltage records and knowledge of the circuit characteristics have indicated that currents up to approximately 30 amperes have been carried through the arc space of breakers without the actual formation of an arc. The maximum value of current which can flow during this period without producing an arc probably depends upon the previous current and upon the design of the interrupter.

The actual value of current which flows through the arc space during the transient is, of course, very small with respect to the short-circuit current. In fact, it is too small to be indicated on a normal magnetic oscillogram of the interruption of a short circuit. It flows for only a part of the time consumed by the transient recovery voltage and is detected by its influence on it.

The significance of this small current lies in its magnitude with respect to the charging current which flows into the capacitance in parallel with the breaker. This capacitance can vary within wide limits. Its minimum value corresponds to the capacitance to ground of one terminal of a circuit breaker, a few feet of conductor, and one end of a reactor. Consequently, this current too can be very small. With about 200 micro-

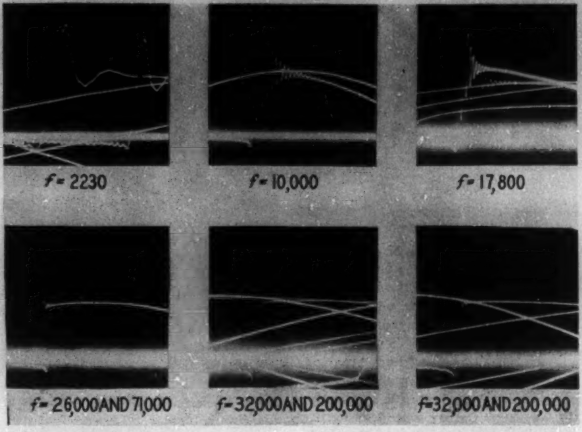


Figure 15. Compressed-air circuit breaker, 13,200 volts, 8,000 amperes

Cathode-ray oscillograms showing the effect of the natural frequency of the transient recovery voltage

microfarads, 200,000 cycles per second, and 7,600 volts rms to ground, it would be about two amperes. When the current through the breaker becomes comparable with the capacitance current, either by the increase of the one or the decrease of the other, it begins to exert a damping influence on the transient recovery voltage and, when large enough, critically damps the transient. For still larger currents through the arc path, the small current flowing into the capacitance becomes negligible, and the transient recovery voltage rises as though it were the voltage appearing across the resistance in a simple circuit containing resistance and inductance in series. As these values of resistance current and capacitive current are relative, the same results can be obtained by reducing the capacitance and thereby the capacitive current until such a point is reached that the current through the arc space is greater than the current through the parallel capacitance. These relations were demonstrated by these tests.

The testing of circuit breakers carried on in high-power laboratories at generator voltage can approximate closely the conditions under which the circuit breaker is to be used in service. The effects, if any, of differences in frequencies of the transients can sometimes be evaluated by a study of the test data to give a definite indication of the manner in which the breaker will perform in service.

The tests on the plain-break circuit showed that at very low values of natural frequency the recovery voltages were undamped. At higher natural frequencies damping of the transient recovery voltage appeared, and after a point of critical damping had been reached, further increase in the natural frequency of the transient recovery voltage resulted in no appreciable change in the actual voltage appearing across the circuit-breaker contacts. This was produced by the effect of the currents through the circuit breaker subsequent to the current zero. This current passing through the breaker was so large with respect to the current flowing into the parallel capacitance of the circuit that it controlled the transient recovery voltage, making it approach an exponential curve. After the point of critical damping was reached, further reduction in the capacitance across the terminals of the circuit breaker did not result in further increase in the severity of the duty on the circuit breaker. This was carried over a wide range above the critical damping without producing any appreciable change in these transients (Figure 5).

A similar set of cathode-ray oscillograms was obtained on the compressed-air circuit breaker, operating at a different current and voltage (Figure 15).

These data demonstrate that if a circuit breaker can successfully interrupt a given voltage and current and critically damp the transient recovery voltage, it can interrupt any other circuit having the same voltage and current and less parallel capacitance without any increase in the severity of the duty on the breaker.

Conclusions

These tests supplement each other to give a picture of the phenomena of arc interruption by high-power circuit breakers. They show that the interruptions brought about by air or oil breakers are similar in characteristics. In circuits having a low natural frequency, the recovery of the dielectric strength of the arc space proceeds with negligible influence exerted upon it by the slowly rising transient recovery voltage. The maximum value of the dielectric strength depends upon the distance between the contacts and the condition of the fluid or fluids filling the space. Above certain values of current and natural frequency, depending on the design of the breaker, the transient recovery voltage is impressed on the arc space while considerable ionization still remains. Consequently, a small current flows through this space, thereby hindering further deionization and modifying the transient recovery voltage. For very high natural frequencies this current may exceed the parallel current through the capacitance across the breaker and may control the transient recovery voltage. If the discharge current becomes too high, it develops into an arc which conducts current till the next normal current zero. The values of voltage, current, and natural frequency at which these phenomena occur vary with the designs of the breakers and not in proportion to their rated interrupting capacities.

The difficulty of circuit interruption

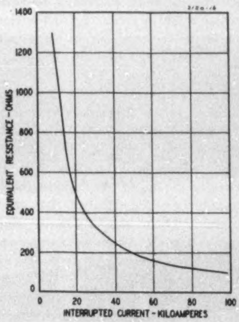


Figure 16. Compressed-air circuit breaker

Approximate resistance of the arc space at the time of the final current zero as a function of the current interrupted does not increase indefinitely with increase in the natural frequency of the circuit. The speed with which the circuit tends to apply the transient recovery voltage to the circuit breaker varies directly with the natural frequency. However, if the current passed through the arc space is greater than the current through the capacitance across the breaker, further decrease in the capacitance does not materially increase the actual rate at which the voltage appears across the contacts and, consequently, does not make the circuit more difficult to interrupt.

The maximum arcing times on these tests were reached with transient recovery voltages having natural frequencies which were not heavily damped by the conductivity.

The tests indicate that the effect of the high-frequency component of a two-frequency transient voltage varies almost in proportion to the amplitude of the component. The effect is negligible when both frequencies tend to produce the same arcing time.

The transient recovery voltages obtained on high-voltage circuits energized through transformers can be equalled in high-power laboratories within the range of voltages and currents available. The laboratory circuits closely approximate the severe service conditions as the leads between the transformers and breakers are short. The lead capacitance is small with respect to the transformer capacitance, and, consequently, small variations in it are not significant.

At generator voltages, the reactors in the laboratory circuits have natural frequencies which are sufficiently high to produce the maximum arcing times in circuit breakers.

The plain-break oil circuit breaker at 3,800 volts 2,600 amperes did not increase in arcing time for natural frequencies above about 2,000 cycles per second. Similar points were found for the "De-ion grid" oil breaker, 250 cycles per second for 2,800 amperes, 7,620 volts, 3,000 to 4,000 cycles per second for 5,200 amperes, and 10,000 cycles per second for 15,000 amperes. The compressed-air breaker operating at 8,000 amperes 13,200 volts had the same arcing time for natural frequencies from 3,900 to 200,000 cycles per second, but at higher currents arcing times increased when the natural frequency was over 30,000 cycles per second. The lowest frequency at which the maximum arcing could be reached was not determined, but on a laboratory circuit a natural frequency of about 190,000 cycles was heavily damped, indicating that the maximum arcing time had been obtained.

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Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits

G. F. LINCKS
ASSOCIATE AIEE

C. R. CRAIG
NONMEMBER AIEE

THE combination of line sectionalizing in the main feeder, individual protection at branch-line junctions, and reclosing relays and breakers at the substation has been shown to provide a cumulative reduction in consumer minutes outage (minutes of outage per consumer per year) that no one of the methods can provide alone.[1] Branch protection proved to be of greater value than line sectionalizing.

However, in order to be of any real assistance in efficient system planning, such knowledge of the ways and means to improve service continuity must be combined with an understanding of the expense involved in restoring service for all combinations and types of protective equipment that are available. Such "restoration expense" involves the man-hours required to locate the fault, to make any necessary repairs on the line, and to restore service, and involves also the automotive miles which must be traveled in doing it. Naturally, initial costs must be included for an over-all consideration of the economics of overcurrent protection.

Calculations and Presentation of Data

Separate studies were made on two different setups of the distribution lines, namely, one for branch protection, as in Figure 1, and another for line sectionalizing with protective devices connected in series as in Figure 2. The assumptions in the appendix are basically identical with those used in the previous study for the calculation of consumer minutes outage.[1] The minor changes involve interpretations as to what portion of this outage time is for location, repair, and restoration of service, and where automotive mileage is required. Thus, it is possible to correlate directly the service outage value of different combinations and types of protective equipment from the previous study[1] with the values now being presented for the "restoration time" and the automotive miles traveled.

The following special terms are used throughout the paper:

"*Restoration time*" is the minutes per year spent by the trouble crew for all faults from the instant of notification of an interruption in service until all necessary repairs are made and service is restored.

"*Automotive mileage*" is the miles traveled by the trouble crew in performing the duties listed under "restoration time."

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G. F. Lincks, electrical designing engineer, and C. R. Craig are both with the distribution fuse cutout section of General Electric Company in Pittsfield, Mass.

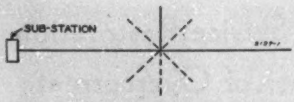

Figure 1. Individual branch-line protection added to 30-mile feeder

Branches located at center of feeder for average of even spacing along feeder

No. Branches	Total Length of Line		
	1-Mile Branches	5-Mile Branches	10-Mile Branches
0	.30	.30	.30
1	.31	.35	.40
2	.32	.40	.50
3	.33	.45	.60
4	.34	.50	.70
5	.35	.55	.80
6	.36	.60	.90

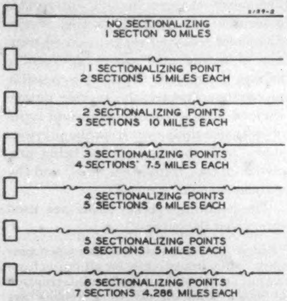

Figure 2. Line sectionalizing with protective devices connected in series on 30-mile line

"*Restoration expense*" is the total expense for "restoration time" and automotive mileage.

The "yardstick" is either the "restoration time" in minutes per year or the automotive mileage per year caused by "permanent faults alone" with no automatic or manual line sectionalizing and no branch protection.

Using the assumptions in appendix A, calculations were made to determine the "restoration time" and the automotive mileage traveled for each system setup, and the values are given as a percentage of the "yardstick" on the curves, Figures 5 to 12 inclusive, that is:

$$\text{Per cent} = 100 \times \frac{\text{Actual calculated values for specific system setup*}}{\text{"the yardstick"*}}$$

By making comparisons in terms of this percentage, we eliminate to some extent the effect of departures of actual practice from the assumptions employed in the study. Wherever such departures might affect the calculations, an attempt was made to be conservative in showing benefits for line sectionalizing or branch protection with the less costly line protective devices.

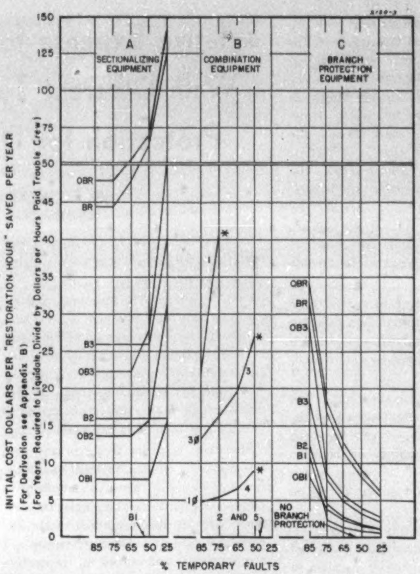

Figure 3. Curves showing cost per hour of "restoration time" saved per year to liquidate relative initial costs of protective equipment

Automotive mileage not included

Symbols defined in Figure 6

Column *A*. Curves for sectionalizing equipment show relative cost of making savings in "restoration time" over that required with single-element fuse cutouts

Column *B*. Curves for different combinations of equipment affording approximately equal service continuity show cost of saving in "restoration time" where made by higher cost equipment. *Such savings not made where curves not extended

Compare curve 1 with 2

Curve 1 for three resetting reclosers at sectionalizing points with three unprotected branches. Curves 2–8 for two-element reclosing cutouts, three at branches, five at sectionalizing points

Compare curves 3 and 4 with 5 (line sectionalizing only)

Curves 3 and 4 for three three-element reclosing cutouts three and one-phase circuits respectively

Curve 5 for five two-element reclosing cutouts one- or three-phase circuits

Column C. Curves for branch protection show the relative cost of savings in "restoration time" secured with different types and combinations of equipment

Observe that the cost of all reclosing equipment increases at a higher rate than the decrease in "restoration time" they provide

Known first costs are brought into the picture to determine the length of time required to liquidate any increased initial costs for equipment which provides savings in "restoration expense," as in Figure 3. (See also appendix B.)

The Curves

The data are given in curve form showing the percentage of the "yardstick" for varying arrangements of line protection. Figures 5 to 10 inclusive are for *branch protection*, and Figures 11 and 12 are for *line sectionalizing* with a number of protective devices connected in series. While these data may not fit a specific circuit exactly, they should be sufficiently close to be usable in planning the overcurrent protection on practically all distribution systems.

Actual Values for "Yardstick" Affect Comparisons

In addition to comparing the percentages given on the curves, it is important to consider also the value of the "yardstick" and the calculated values for specific cases in terms of the actual "restoration time" in minutes per year or the automotive mileage per year. Since we have assumed a constant number of total faults, the "yardstick," which is based on "permanent faults alone," is obviously lowest with 85 per cent temporary faults. It increases to a maximum at the 25 per cent temporary faults as shown in Table I. These values would increase or decrease respectively if extended beyond the 25 to 85 per cent range that was studied.

Much greater "restoration time," automotive mileage, and consumer minutes outage are required to locate, repair, and restore service with a permanent fault than just to restore service with a temporary fault. Thus, as the percentage of temporary faults becomes greater, the "restoration time," automotive mileage, and consumer minutes outage caused by equipment opening on temporary faults increases less than the decrease in these values as caused by permanent faults. Because of this relationship, the calculated actual "restoration time" and automotive mileage for 25 per cent temporary faults are greater than that for 85 per cent temporary faults. This relationship is reversed when the calculated values are given as a percentage of the "yardstick" as on the curves, Figures 5 to 12 inclusive. This shift in relationship is important when evaluating the benefits secured by the use of the more effective device which usually costs more (see Figure 3).

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* The calculated values and the "yardstick" may be in terms of either "restoration time" in minutes per year or automotive mileage traveled per year.

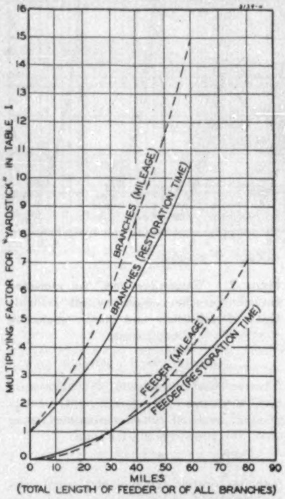


Figure 4. Conversion curves for determining "yardstick" with different lengths of feeder and total length of branches

How to use data:

1. Determine the multiplying factor

(1). For the proper length of the feeder

(2). For the total length of all the branches

2. Multiply the "yardstick" in Table I by the multiplying factor for the feeder

3. Multiply the corrected "yardstick" determined in item 2 by the multiplying factor for the branches

Observe that the "yardstick" is increased more by adding mileage to branches than by increasing the length of the feeder

Some Protective Devices Can Be Connected in Series in Greater Numbers Than Others

More two-element than three-element reclosing fuse cutouts can be connected in series and co-ordinated to provide about equal improvement in service continuity. Figures 11 and 12 indicate that the use of a greater number of two-element than three-element cutouts may increase the normal differences in "restoration expense" for these two devices at the higher percentages of temporary faults. However, when the difference in the initial costs of these two types of cutout is divided by the savings in "restoration expense," it shows that the cost per hour of "restoration time" saved per year by the use of the three-element design would be quite high, especially for three-phase circuits. For example, the use of two-element cutouts at five sectionalizing points and three-element cutouts at three sectionalizing points provides approximately the same service continuity. A comparison of these, as in Figure 3 (column *B*, curves 3, 4, and 5) indicates that from 6 to 13 years might be required to liquidate the extra cost of three three-element cutouts per phase on a three-phase 30-mile circuit, with a wage rate of one dollar per hour per man for a two-man crew.

A similar comparison is made in Figure 3 (column *B*, curves 1 and 2) to show the effect of the limitations in co-ordinating automatic resetting reclosers with fuses at branch junctions or at transformers. The use of three two-element cutouts to protect three branches added to a line sectionalized with the five two-element cutouts provides at least equal service continuity to that provided by three reclosers at sectionalizing points on the same line with the three branches unprotected. With 85 and 75 per cent temporary faults Figure 3 indicates that from 11 to 20 years respectively will be required to liquidate the additional cost of the reclosers with a wage rate of one dollar per hour per man for a two-man crew. With 65 and lower percentages of temporary faults the use of the two-element reclosing cutouts in the branches makes possible lower "restoration time" as well as a lower initial cost and a slight improvement in service continuity.

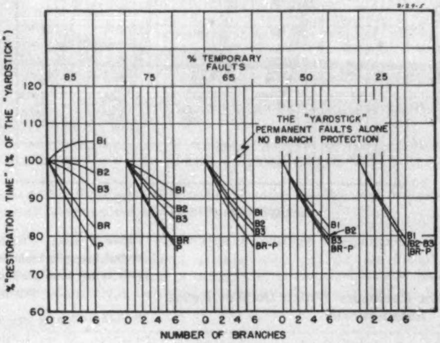


Figure 5. "Restoration time" for protected one mile branches employing nonreclosing or reclosing substation breakers and all available types of branch protective equipment

Symbols defined in Figure 6

Observe that a saving is made for all but single-element fuse cutouts when used with nonreclosing substation breakers at 85 per cent temporary faults

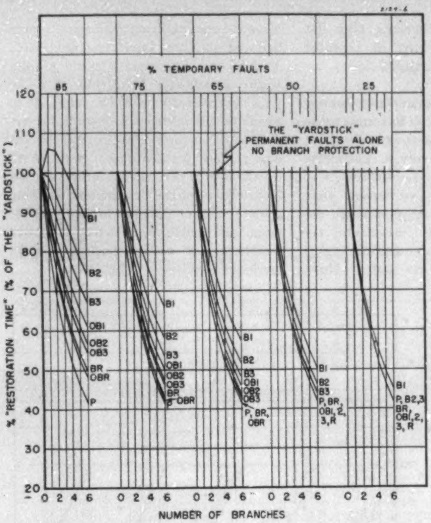
Effect of Nonreclosing and Reclosing Substation Equipment

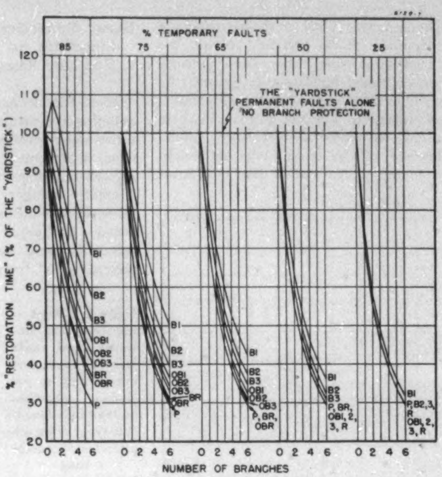
With no line sectionalizing or branch protection, "permanent faults alone" are the only cause for sending out the trouble crew. The restoration of service after a temporary fault by manual or automatic reclosing of the substation breaker involves only the station attendant and thus the type of equipment employed has no effect on the "restoration time" or automotive mileage.

Combining Branch Protection and Line Sectionalizing

The cumulative effect on "restoration time" and automotive mileage of combining the protection of a number of short branches with line sectionalizing can be approximated from the data on the curves, Figures 5 to 12 inclusive. See appendix C for description of how to do this.

Where the circuit consists of two or more long branches, each branch should be studied separately from the standpoint of line sectionalizing and branch protection. Then the cumulative effect should be determined by adding or subtracting





the percentages as described in appendix C.

For Reclosers Which Do Not Reset Automatically

The conclusions drawn and the data in the curves for reclosing fuse cutouts apply to any type of apparatus with an equal number of reclosers and without the feature of automatically resetting after clearing a temporary fault.

Conclusions

Some general conclusions can be drawn from the study, although it is anticipated that the data will prove even more helpful in system planning when applied to the problems of specific circuits.

Substation Equipment

1. The substitution of reclosing for nonreclosing breakers at the substation, while improving service continuity, has no effect on the "restoration time" or automotive mileage (unless the substation attendant's time to manually reclose the breaker is classed as "restoration time").

2. Overlapping the substation reclosing protection with all line protective devices provides a major reduction in "restoration time" and automotive mileage. It is quite effective when combined with branch protective devices. (With such overlapping the breaker trips and recloses once without the branch or line protective device opening for all faults out to the ends of the line, and then the relay provides time delay so that the branch or line-sectionalizing protective device disconnects the faulted portion of the circuit ahead of the second tripping of the relay.)

Figure 6 (left). "Restoration time" for protected five-mile branches employing all available types of substation and branch protective equipment

B = Nonreclosing or reclosing breaker at substation

OB = Overlapping reclosing breaker at substation, set for instantaneous tripping on first opening with all types of line protective equipment delayed to open only ahead of second opening of breaker

1 = Single-element fuse

2 = Two-element reclosing fuse

3 = Three-element reclosing fuse

R = Automatic resetting recloser

P = Permanent faults alone—all temporary faults cleared with only a momentary outage

(These are combined to show type of substation and line equipment employed)

Observe:

1. The major reduction in "restoration time" for "permanent faults alone" with the branches protected

2. The comparatively small increase in "restoration time" for outages caused by equipment opening on temporary faults

3. Single-element fuses provide the major portion of the reduction obtainable except at 85 per cent temporary faults

3. Such overlapping protection of the entire line is the most economical of all system setups providing automatic reclosing, when both "restoration expense" and initial costs are considered.

Figure 7. "Restoration time" for protected ten-mile branches employing all available types of substation and branch protective equipments

Symbols defined in Figure 6

Observe that the reduction in "restoration time" is greater with ten- than with five-mile branches with all other relationships being proportionally the same

4. Where such overlapping protection as in paragraph 2 reaches only part way out on the line, the decrease in "restoration time" and automotive mileage over a nonreclosing or reclosing breaker is only approximately one quarter of the decrease provided by overlapping the whole line.

Branch Protection Reduces Maintenance

1. Overcurrent protection of individual branches generally reduces "restoration time" and automotive mileage, even with short branches. The exceptions are with only a few or very short branches at high percentages of temporary faults where the actual number of minutes and miles are small (see Figures 5 to 10 inclusive).

2. The length of the branch has a major effect on the "restoration time" and automotive mileage.

3. Overcurrent protection of individual branches provides less reduction in terms of actual "restoration time" and automotive mileage at higher percentages of temporary faults than at the lower percentages.

4. The savings in "restoration expense" with branch protection are less likely to liquidate the initial cost of all types of overcurrent protective equipment at higher percentages of temporary faults than at

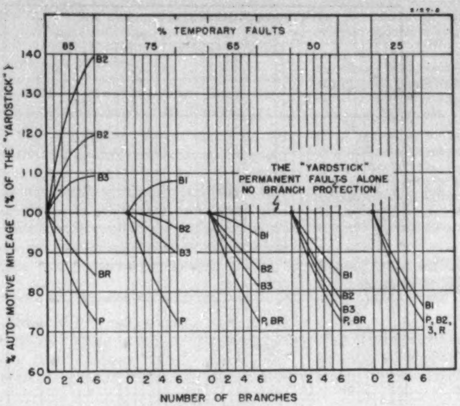


Figure 8. Automotive mileage for protection of one-mile branches employing nonreclosing or reclosing substation breaker and all available types of branch protective equipment

Symbols defined in Figure 6

Observe that the saving in automotive mileage for "permanent faults alone" is greater than the saving in "restoration time" (Figure 5). Also that the effect of outages caused by temporary faults is greater. However, the actual values for the "yardstick" in terms of automotive mileage is much less than for "restoration time"

the lower percentages, where the protective orbs of the substation relay and breaker includes the whole line.

5. Single-element fusing of branches, five miles and longer, is the most effective in saving sufficient "restoration time" and automotive mileage to liquidate the initial investment. Only a few years are required because of the comparative low first cost to the savings effected. For example, with 30 miles of single-phase branches on a 30-mile feeder and a wage rate of one dollar per hour per man for a two-man trouble crew, the liquidation would be effected in about 1 to 2.5 years. The exception is with 85 per cent temporary faults where the actual minutes and miles saved are too small to offset the initial cost of even single-element cutouts within a reasonable time.

6. Two and three-element reclosing fuse cutouts and automatic resetting reclosers provide reductions in "restoration time" and automotive mileage which are worthy of consideration. However, this saving is not proportionate with the increase in initial costs over that for single-element fuse cutouts.

7. Reclosing breakers at the substation which overlap single-element fuses at a num-

Figure 9 (below). Automotive mileage for protected five-mile branches employing all available types of substation and branch protective equipment

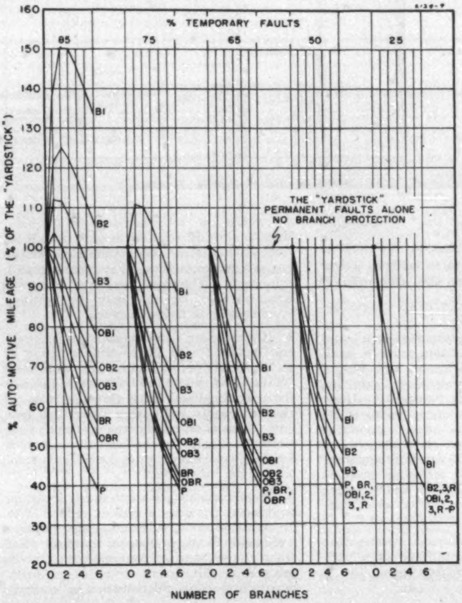
Symbols defined in Figure 6

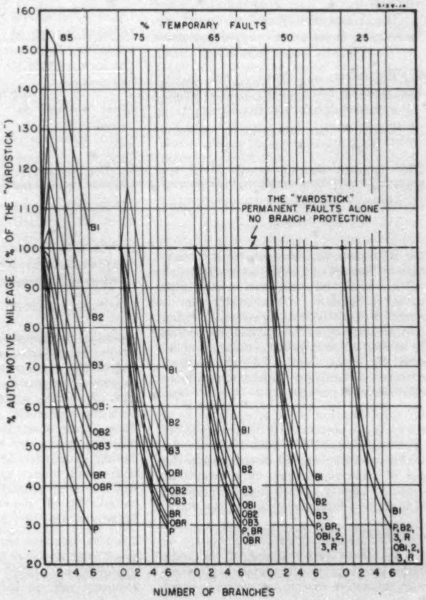
Observe the same greater reduction in automotive mileage for "permanent faults alone" and the effect of outages caused by temporary faults as observed in Figure 8

Figure 10. Automotive mileage for protected ten-mile branches employing all available types of substation and branch protective equipment

Symbols defined in Figure 6

Observe that the reduction in automotive mileage is greater than with one- and five-mile branches (Figures 8 and 9), and that the effect of outages due to temporary faults is proportionately greater, especially at the higher percentages of temporary faults





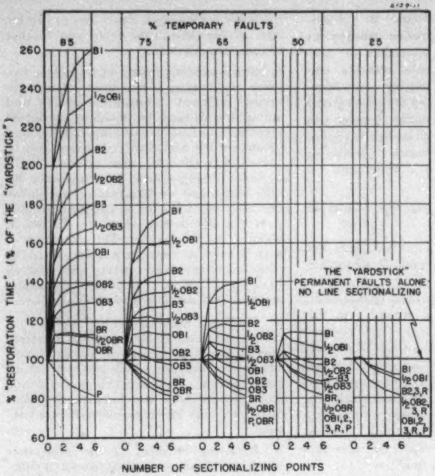


Figure 11. "Restoration time" with line sectionalizing employing all available types of substation and line overcurrent protective equipment

½OB = overlapping reclosing breaker at substation, protection for only half of line

Other symbols defined in Figure 6

Observe:

1. The "restoration time" as caused by "permanent faults alone" is reduced only slightly by line sectionalizing
2. The effect of outages caused by devices opening on temporary faults is greater than with branch sectionalizing
3. Therefore, line sectionalizing generally increases "restoration time"

(This increase amounts to only about ten hours per year with single-element fuses at 85 per cent temporary faults on a 30-mile feeder)

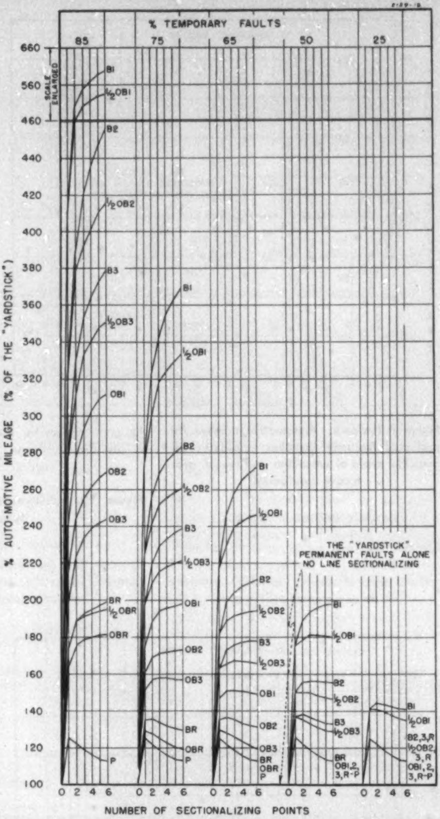


Figure 12. Automotive mileage with line sectionalizing employing all available types of substation and line overcurrent protective equipment

½OB = overlapping reclosing breaker at substation, protection for only half of line

Other symbols defined in Figure 6

Observe:

1. The automotive mileage even for "permanent faults alone" is increased by line sectionalizing
2. As with branch protection, the effect of outages caused by temporary faults is greater than with "restoration time" (Figure 8)

ber of branch junctions provide more savings in "restoration time" and automotive mileage than three-element cutouts with reclosing breakers. This combination is almost as effective as single-element fusing of branches without overlapping protection in having the savings in "restoration time" offset the initial costs, at 75 and lower percentages of temporary faults, and is more effective at 85 per cent.

8. Consideration of the economic value of reducing the percentage of temporary faults appears to be justified. Such a reduction for the line as a whole might be obtained by the use of reclosing devices on a branch or branches having a much higher percentage of temporary faults than the line as a whole.

Automatic Line Sectionalizing

1. Automatic sectionalizing of the main feeder (and long branches treated as feeders) with protective devices connected in series generally increases "restoration time" and automotive mileage and thus must be justified by the savings in revenue and the improvement in customer good will resulting from the better service continuity. However, the automatic resetting reclosers reduce "restoration time" and automotive mileage, as do two and three-element reclosing fuse cutouts at 50 per cent temporary faults and lower, and reclosing breakers which overlap single-element and reclosing cutouts at 65 per cent and lower (see Figures 11 and 12).

2. The increase in actual "restoration time" and automotive mileage even with single-element fuse cutouts generally is comparatively small.

3. Automatic sectionalizing with single-element cutouts requires the most additional "restoration time" and automotive mileage but at a minimum over-all cost, see Figure 3, column 1. Therefore, sectionalizing with single-element cutouts may be justified economically in many instances.

4. Overlapping reclosing substation breakers combined with single-element fusing at sectionalizing points requires less additional "restoration time" and automotive mileage than sectionalizing with three-element reclosing cutouts and with a lower total first cost. This is especially true on three-phase circuits. (Only the additional equipment for the special relaying was considered necessary at the substation.)

5. Sectionalizing with automatic reclosing equipment provides a greater reduction in "restoration time" and automotive mileage compared to single-element cutouts than when the comparison is made with branch protection. However, as in the case with branch protection, the reduction in "restora-

tion time" and automotive mileage is not proportional with the increase in initial cost except possibly on lines appreciably longer than 30 miles.

BRANCH PROTECTION AND LINE SECTIONALIZING SHOULD BE COMBINED

The cumulative benefit obtained by combining branch protection and line sectionalizing should not be overlooked. Sectionalizing with equipment, the characteristics of which are such as to preclude branch protection, prevents the securing of major reductions in "restoration time" and automotive mileage.

MANUAL LINE SECTIONALIZING

Manual line sectionalizing generally is employed where automatic protection has not been adopted. With manual sectionalizing "restoration time," automotive mileage, and consumer minutes outage are caused by "permanent faults alone" and thus are the same percentages of the "yardstick" at all percentages of temporary faults. Therefore, automatic sectionalizing and branch protection, which may cause outages on all or some temporary faults, are less likely to be justified over manual sectionalizing at the higher percentage of temporary faults (above 75 per cent) than is indicated when compared with the "yardstick." This is especially true on a feeder with relatively long branches where manual sectionalizing eliminates traveling out and back on a number of the branches in search of the fault.

Table I. Actual Calculated Values⊖ for "Yardsticks" for Feeder Without any Branches† or Manual Line Sectionalizing

Values Decrease as the Percentage of Temporary Faults Increases

Units in Which Calculated Actual Values Are Expressed	Per Cent Temporary Faults 25	50	65	75	85
	Actual Values for Permanent Faults Alone*				
Consumer minutes outage per year	2,137.5	1,425	996.25	712.5	427.5
"Restoration time" in minutes per year	2,137.5	1,425	996.25	712.5	427.5
Automotive mileage per year	337.5	225	157.5	112.5	67.5

⊖ The values in this tabulation are based on a 30-mile feeder. To convert the values for longer or shorter feeders, use the multiplying factors as determined in Figure 4.

* These actual values will not apply exactly to any system, but the relationship shown should apply about proportionally. To determine actual "restoration time" and automotive mileage for any system setup, multiply the "yardstick" by the percentage for that setup as given on curves 5 to 12 inclusive.†

† The values in this tabulation do not apply directly to feeders with branches. Corrections can be made by use of the multiplying factors for the total length of all branches as determined from Figure 4.

LOWERING OF THE PERCENTAGE OF TEMPORARY FAULTS SHOULD BE CONSIDERED

1. Since outages caused by temporary faults make all types of equipment less efficient in attaining the minimum values for "permanent faults alone," consideration should be given to the economics of reducing the percentage of temporary faults, especially in the range above 75 per cent.

2. If the percentage of temporary faults is decreased, less costly equipment generally will provide equal or better service continuity with lower "restoration time" and automotive mileage.

3. A reduction in the percentage and thus the number of temporary faults on a specific system lowers the total number of faults and provides greater reductions in "restoration time" and automotive mileage than is shown in this study, where the total number of faults remain constant.

Use of Data for Continuing Expansion of Knowledge

Although over-all values of combining branch protection, line sectionalizing, and reclosing relaying at the substation as presented are based on a set of rigid assumptions, these data should facilitate more efficient planning of overcurrent protection on specific systems. It is hoped that these data will be extended, or if necessary, modified as the result of operating records secured in actual practice.

Appendix A. Assumptions Employed in Mathematical Study

The same general assumptions employed in the study to determine the relative value of different types of overcurrent protection for distribution circuits in terms of service continuity[1] were used in continuing the mathematical study to check the effect on "restoration expense." All of the assumptions pertinent to the present study are tabulated below:

1. *The Distribution Lines Studied (Length of Lines)*

(*a*). In one part of the study one to six unprotected and protected branches, each one, five, or ten miles in length, were added at the mid-point of an unsectionalized 30-mile main feeder, increasing the total length of line, as in Figure 1. (The greater total length will increase the actual "restoration time" and automotive miles, but when these are expressed as a percentage of the "yardstick" (for "permanent faults alone" without branch protection), the effect of the length is cancelled. Thus the percentage shown on curves of Figures 5 to 10 inclusive for any system setup can be applied to any length of line or branches, if the "yardstick" is modified as directed in Figure 4. Connecting the branches at the mid-point provided an average for an even spacing of the branches along the feeder.)

(*b*). In the other part of the study, a 30-mile main feeder was broken up into from one to seven sections by zero to six overcurrent protective sectionalizing devices connected in series as in Figure 2.

2. *Equipment Employed.* Both studies included checking comparative results with overcurrent protection provided by:

(*a*). Substation breakers actuated by nonreclosing or automatic reclosing relays, and automatic reclosing relays which overlap all branch and line protective equipment so that the breaker trips and recloses once without the branch or line devices opening, and then provides time delay before the second tripping of the relay on more persistent faults; each combined with

(*b*). Sectionalizing or branch-line protection with single-element or two and three-element reclosing fuse cutouts or reclosers which reset automatically after clearing a temporary fault.

3. *Number and Type of Faults.* One fault per mile per year uniformly distributed with temporary faults equaling 25, 50, 65, 75, and 85 per cent of the total. (One fault per mile is probably somewhat high. Any lower value might have been used without changing the percentages used for comparison but would decrease the values for the "yardstick" in Table I and would change the values in Figure 3—decreasing these values for cost of "restoration" with line sectionalizing in column *A* and increasing the values for the cost to secure the savings with branch protection in column *C*.)

4. *Attended Substation.* No "restoration time" was included for the substation attendant to close the station breaker or to notify the trouble crew. ("Restoration time" for temporary faults was zero, regardless of whether service was restored by manual or automatic reclosing of the station breaker. Thus, there is no difference in man-hours of "restoration time" or automotive mileage for nonreclosing or reclosing relays and breakers.)

5. *Trouble Crew.* Consisted of one man who was always available at the substation to start instantly with no time allowed for notification or preparation. (More often the crew consists of two men to meet safety requirements, but any multiple of one will have no effect on the percentages used in the presentation of the data. The actual "restoration time" in Table I can be multiplied by the exact number in the crew if over one, to make the values comparable with the utility's actual practice, see also the directions on Figure 3. Also, the crew is more likely to be out on the system, sometimes closer to and sometimes farther from the fault location, so that this assumption provided an average.)

6. *Time to Locate Fault, Make Necessary Repairs, and Restore Service.* The trouble crew:

A. With no branch protection or line sectionalizing (All temporary faults cleared when breaker locks open. Service restored by station attendant. Trouble crew required for "permanent faults only.")

(*a*). Traveled 15 miles per hour to the mid-point of the feeder as the average, and out to the mid-point of each branch and back as the average for each branch.

B. With automatic line sectionalizing and individual branch protection

(*a*). Traveled 30 miles per hour to the sectionalizing point at which the protective device had opened. No time was allowed for examining sectionalizing or branch protective devices en route, as it was assumed that these would have indicating features visible from the car.

(*b*). Spent five minutes to climb the pole and to restore service if a temporary fault had caused the outage at the sectionalizing or branch protection point.

(*c*). Traveled 15 miles per hour to the mid-point of the branch or section on which the fault persisted. The mid-point provided the average for the uniform spacing of faults.

(*d*). Spent 30 minutes repairing a permanent fault. In a majority of cases the trouble crew will repair faults without calling for the assistance of the line crew, generally in less than 30 minutes. Where the services of the larger line crew are required, the time would generally exceed the 30 minutes average. This makes the values presented more conservative than actual practice.

(*e*). After repairing the fault:

1. No time was allowed to notify the substation attendant when he was required to close the breaker (although some time probably would be required for notification which would have increased the values of the "yardstick" Table I and the "restoration time" or automotive mileage for outages caused by temporary faults. This would have shown greater benefits for line sectionalizing and branch protection for reclosing cutouts and especially for automatic resetting reclosures).

2. The crew traveled 30 miles per hour to return to the sectionalizing or branch protective device to restore service.

3. No "restoration time" or automotive mileage was included after service was restored, since the crew might then be headed for another job to which the additional mileage and time would be charged.

7. *Service Restoration by First, Second, Third, and Fourth Reclosures* was assumed to be 50 per cent of the total number of faults for devices which reclose once, 65 per cent for those which reclose twice, 70 per cent for those which reclose three times, and 73 per cent for combinations which reclose four times (see Table IV in appendix of previous study[1]). This gives maximum advantage to three-element reclosing fuses and other multireclosing devices, as compared with two-element reclosing fuse cutouts, since these values for perfect operation are low for the first reclosing and high for the second as compared with operating experience. Some data show as high as 75 to 90 per cent restoration of service following the first reclosure.

8. *No Inspection With Reclosing Fuse Cutouts Was Assumed.* Because such inspection would have permitted refusing before an outage occurred, the data indicate only the minimum benefit for these devices.

(This benefit would be increased to a degree approaching that of the device that resets automatically, as such fuse renewal before the occurrence of an outage approaches 100 per cent. The percentage of such discovery and renewal is fairly high, because linemen and trouble crews are on the lookout for the indicating devices as they pursue their regular duties along the lines.)

Fuse renewal is accomplished in a few minutes while they are at the installation, without any traveling time or mileage as was included in the calculated values presented. Thus all reclosing fuse data are ultraconservative.

9. *The Protective Orbit of the Substation Breaker* (which is determined by minimum pickup current of the relay) included the whole feeder and all of the branches. In many instances distribution lines have outgrown this orbit. Consequently, the lower current individual protection of the branches and line sectionalizing on the otherwise unprotected portion may prevent burning down lines or annealing of the conductors. Such faults would require a long time to repair and might necessitate the assistance of the line crew. Conversely, this lower current protection may cause outages which might otherwise have burned clear. Depending on the type of equipment, after such outages service will be restored automatically or will require the trouble crew to go out to the sectionalizing or branch protection point in order to restore service. There would have to be a large number of such trips to equal the "restoration time" required for one case of putting back up lines burned down because of lack of protection.

Table II

Assumed Initial Costs Used for Figure 3	Cost Each†
Single-element fuse cutouts	$ 7.00
Two-element fuse cutouts	17.00
Three-element fuse cutouts	32.00
Automatic resetting reclosers	85.00
Additional relay equipment*	58.00

† These initial costs are approximately correct. Additional material other than the crossarm will be negligible as mounting hardware is furnished with the devices. Installation costs are not included as they will vary on different systems.

* It was assumed that induction-type reclosing relays and breakers are available.

No actual data are available on this relationship. However, it would appear that the "restoration time" in the area beyond the protective orbit of the station breaker would be reduced perceptibly by line sectionalizing and branch protection. This would decrease the higher "restoration time" and automotive mileage shown in this study for line sectionalizing, where the protective orbit encompassed the whole line, and would increase the savings shown for branch protection.

Appendix B. How to Determine the Cost Per Hour of "Restoration Time" Saved Per Year to Liquidate Relative Initial Costs of Protective Equipment

This is the method employed in figuring the values in Figure 3. It is applicable to the studying of automotive mileage costs as well (substitute mileage values for "restoration time" and proceed as described). Since with line sectionalizing the "restoration time" is increased over that for no sectionalizing, the comparison is made with the single-element cutout which requires the greatest "restoration time." Branch protection generally provides a reduction in "restoration time" from that with no protection and thus the comparison is made with the "yardstick" to determine the cost of securing this reduction.

Procedure

1. Determine the percentage for specific system setup from curves 5 to 12 inclusive, making additions for the cumulative effect of combining branch protection and line sectionalizing as described in the text.

2. Determine the actual "restoration time" for each specific system setup by multiplying the "yardstick" from Table I (modified if necessary for variations in length of the total line as described in Figure 4) by the percentage determined in paragraph 1.

3. Determine the difference in actual "restoration time" for the equipment being compared.

4. Determine the difference in initial costs of the equipment required for each system setup (see Table II).

5. Divide the difference in costs by the difference in "restoration time" to determine the cost per hour of "restoration time" per year.

6. Divide the values determined in paragraph 5 by the total wages per hour paid the trouble crew to determine the number of years required to liquidate the difference in initial cost (as determined in paragraph 4). Formulating this:

$$\text{Cost per unit}^{\dagger}\text{ saved/year} = \frac{\begin{pmatrix}\text{unit}^{\dagger}\text{ for less}\\ \text{effective device}\\ \text{(from item 2)}\end{pmatrix} - \begin{pmatrix}\text{unit}^{\dagger}\text{ for more}\\ \text{effective device}\\ \text{(from item 2)}\end{pmatrix}}{\begin{matrix}\text{Cost of more effec-}\\ \text{tive device}^{*}\end{matrix} - \begin{matrix}\text{Cost of less effec-}\\ \text{tive device}^{*}\end{matrix}}$$

$$\text{Number of years to liquidate} = \frac{\text{cost/unit saved}^{\dagger}\text{/year}}{\text{dollars per hour paid trouble crew}}$$

Appendix C. How to Combine "Restoration Time" or Automotive Mileage Values for Branch Protection, Figures 5 to 10 inclusive, With Those for Line Sectionalizing, Figures 11 and 12

1. Where both cause "restoration time" or automotive mileage greater than 100 per cent (the "yardstick"), add to the percentage on one curve the percentage above 100 per cent on the outer curve, that is:

Resultant per cent = (per cent on one curve) + (per cent − 100 on other)

2. Where one system causes "restoration time" or automotive mileage greater than 100% and the other system less than 100 per cent, subtract from the first percentage the value of 100 − the second percentage, that is,

Resultant per cent = (per cent which is greater than 100 per cent) − (100 − per cent which is less than 100 per cent)

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[*] The cost value should be either the initial cost or the installed cost.

[†] The unit used may be in terms of restoration hours per year or automotive mileage per year.

Tests and Analysis of Circuit-Breaker Performance When Switching Large Capacitor Banks

T. W. SCHROEDER
ASSOCIATE AIEE

E. W. BOEHNE
MEMBER AIEE

J. W. BUTLER
MEMBER AIEE

THE increased kilowatt burden on generation and transmission equipments—occasioned by the existing national emergency—calls for a reduction in the use of these equipments for generating and transmitting reactive kilovolt-amperes. When operating conditions permit the use of either capacitors or synchronous condensers, the relatively shorter production time of capacitors is making them more and more justifiable as a reactive source in the range of kilovolt-ampere ratings where economics might normally dictate a synchronous condenser.[1] At the present time the use of capacitors in banks up to 10,000 kva is being given increased consideration, and several installations having steps in this range have already been made.

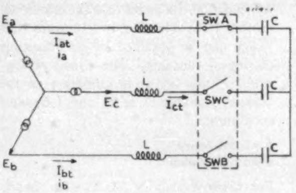
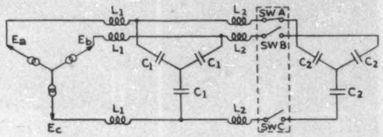


Figure 1. Transient-analyzer circuit for determining effects of sequential pole closing

(a). Total capacitor bank energized

(b). Capacitor bank energized against already energized bank. L_2 is inductance of breaker and bus work between banks



With this growing use of capacitors, it is becoming increasingly necessary to apply adequate power circuit breakers to the task of switching these large banks, either totally or in steps, on and off the system. It has long been recognized that the job of switching capacitive circuits, such as long unloaded transmission lines, can become difficult, and it therefore logically follows that both the duty on the breaker and its effect on the system during switching of large capacitor banks should be investigated.

When a capacitor bank is energized, a large component of natural frequency current initially flows in addition to the normal-frequency circuit current. If the bank is energized against an already energized bank, even larger natural-frequency equalizing currents may flow through the breaker. The effect of these currents must be evaluated.

When a capacitor bank is de-energized, larger than normal system voltages may exist across the breaker contacts because of the trapped charge on the capacitor. This may impose a greater than usual task on the breaker dielectric to interrupt with a minimum of restriking, and if restriking occurs, there may be voltage stresses above normal on the capacitor units as well as on the other connected equipment.

This paper offers an analysis of the problem together with test results on full-scale and miniature capacitor banks. Certain conclusions are drawn relative to the duty on and selection of breakers for this service.

Summary of Conclusions

A total of 338 tests on oil blast and Magne-blast types of power circuit breakers in the voltage class up to 15 kv and up to 250,000 kva short-circuit capacity, have been made. The results of this investigation, coupled with the analytical treatment contained in this paper, have allowed the following conclusions to be reached:

1. Based upon transient-analyzer analysis, it is shown that under the most pessimistic conditions of controlled restrike, abnormal circuit overvoltages can occur in de-energizing capacitors. This agrees with the results of other investigations[2] covering fault clearing on circuits containing capacitance.

2. Full-scale tests demonstrate that *standard* oil-blast breakers in the above classification recover their dielectric strength sufficiently fast to switch capacitor banks up to at least 10,000 kva without creating abnormal overvoltages.

   As the capacitor kilovolt-amperes is further increased, a point is reached where restriking becomes more probable. Whether abnormal overvoltages appear or not is then determined by the randomness of the restriking phenomena and the inherent damping characteristics of the associated circuits.

3. Full-scale tests show that the high series resistance in the arc chute of the Magne-blast type of breaker, when restriking occurs, prevents the abnormal overvoltages usually associated with restriking phenomena. This is evidenced by the successful interruption of 31,400 kva—3,400 amperes at 5,350 volts.

4. Full-scale tests demonstrated that the peak values of transient equalizing currents upon energizing capacitor banks are within the values given by the equations presented in this paper and in reference 2.

5. The short-circuit interrupting rating of a breaker need not be increased by the

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T. W. Schroeder and J. W. Butler are both of the central station engineering department, General Electric Company, Schenectady, N. Y., where Mr. Butler is sponsor engineer. E. W. Boehne is research engineer in the power circuit breaker engineering department, General Electric Company, Philadelphia, Pa.

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3. Where both systems cause "restoration time" and automotive mileage less than 100 per cent (the "yardstick"), subtract from the percentage on one curve the value of 100 − percent on the other curve, that is,

Resultant per cent = (per cent on one curve) − (100 − per cent on other curve)

To determine the actual "restoration time" and automotive mileage multiply the "yardstick" in Table I, modified as directed in Figure 4, by the resultant percentage.

Reference

1. Relative Value of Different Types of Overcurrent Protection for Distribution Circuits, G. F. Lincks. AIEE Transactions, volume 61, 1942, January section, pages 19–26.

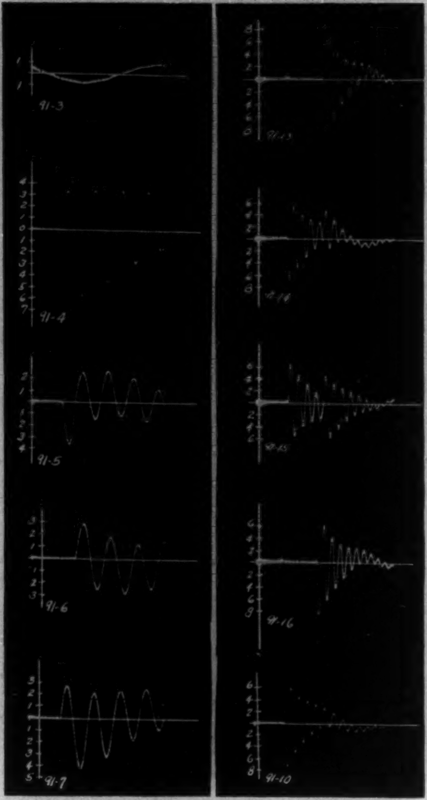

Figure 2. Transient-analyzer oscillograms showing currents obtainable when energizing capacitors

Current calibration in times normal steady-state capacitor current

91-3 One cycle of normal magnitude, fundamental frequency capacitor current

91-4 Inrush current, phase *b*, on energizing capacitor bank of Figure 1a. Switch *b* closed at E_{ab} maximum; switch *c* closed 36 degrees later for maximum I_b

91-5 I_a for condition of 91-4

91-6 I_c for condition of 91-4

91-7 Inrush current, phase *b*, on simultaneous energizing of capacitors of all three phases of Figure 1a at the instant that makes I_b maximum

91-10 Inrush current, phase *b*, on simultaneous energizing of capacitors of all three phases of Figure 1b at the instant that makes I_b maximum

91-13 Inrush current, phase *c*, on energizing capacitor bank of Figure 1b. Switch *b* closed at E_{ab} max; switch *c* subsequently closed to give I_c a maximum

91-14 I_b for condition of 91-13

91-15 I_a for condition of 91-13

91-16 Same as 91-13, except switch *b* closed 10 degrees ahead of E_{ab} max

Switch *c* subsequently closed to give I_c a maximum

proximity of a capacitor bank on the bus side.

6. All tests made under various energizing and discharging conditions—many of which are presented in this paper—showed that the peak values of transient current obtained were not harmful to the breakers tested. The maximum value reached was 52,000 amperes.

The Energizing Problem

Energizing a Single Bank

It has been shown[3] that the maximum inrush current, neglecting system capacitance and damping, when energizing a single capacitor bank at the peak of the voltage wave is given by

$$I_{max}=I_0(1+\sqrt{X_c/X_L})$$

where

I_0 = peak value of steady-state capacitor current
X_c = reactance of capacitor bank
X_L = system reactance viewed from capacitor location

Since a switching problem is being considered, it may be appropriate to express inrush currents to capacitors in terms of the short-circuit current available at the capacitor location. Doing this, the above expression may be written as

$$I_{max}=I_c(1+\sqrt{I_{sc}/I_c})$$

where the subscripts stand for the capacitor rated current and the short-circuit currents—as initialed. All currents are expressed in peak values. This amounts to approximately 2,500 amperes for a 10,000-kva bank of capacitors closing on to a 13.8-kv system capable of delivering 250,000-kva short circuit.

These expressions hold for each phase of a wye-grounded bank on a grounded system. In wye floating neutral or delta-connected banks, however, the initial individual phase currents are a function of the order of pole closing. And since breakers probably do not close their contacts simultaneously, this case was investigated to determine if larger currents than those indicated by the above expression, could be obtained.

The analysis of this case*—as given in appendix B—indicates that the maximum current from this effect is 1.15 times that which can be obtained by simultaneous closing of contacts. The circuit of Figure 1(a) was set up in miniature on the transient analyzer[4] with the following constants:

L = 0.234 henry
C = 1 microfarad

For these values $\sqrt{X_c/X_L}=5.5$ hence I_{max} by above equation should be no greater than $1+5.5=6.5$ times normal. Oscillographic results are presented in Figure 2. For the equivalent single-phase or simultaneous closing case, oscillogram 91-7 shows a peak inrush current in phase *b* of 4.5 times normal. Oscillogram 91-4 shows I_b for switch *b* closed at E_{ab} a maximum and switch *c* closed 36 degrees later to give I_b a maximum. This maximum of approximately 6.0 times normal, and the 4.5 above indicates that even the small damping in the miniature setup greatly reduces the maximum obtainable.

Energizing a Bank Against an Already Energized Bank

In this case the energized bank discharges into the oncoming bank through the relatively small impedance between them. The resulting large equalizing current of short duration oscillates at the high natural frequency determined by the value of series capacitance of the two banks, and the inductance of the connections between them.

Equations for determining these currents have already been given[3] for simul-

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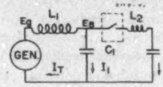
* A time of at least 1/4 cycle of natural frequency constitutes a severe nonsimultaneous pole-closing condition from this standpoint.

Table I. Single-Phase Closing Tests When Energizing a Bank Against an Already Energized Bank

No	X_L Ohms	C_1 Micro-farads	C_2 Micro-farads	E_B RMS Volts	I_1 RMS Amperes	I_T RMS Amperes	I_2 Peak Test	I_2 Peak Calculated	Closing Angle Degrees		Natural Frequency (Cycles Per Second)	L_2 Millihenry Calculated from Test
Oil-Blast Breaker—250,000 Interrupting Kva												
1	6.72	17.7	17.7	15,150	101	210	*	8,940	68		8,100	0.0436
2	3.72	35.4	35.4	16,500	220	400	*	15,800	72		6,400	0.0349
3	2.12	70.2	66.7	15,130	400	827	13,000	18,200	88	(Figure 3)	3,900	0.0487
4	0.42	440	417	5,850	960	2,030	15,400	17,700	80		1,750	0.051
5	2.12	70.2	66.7	14,380	380	786	640	1,250	4		4,100	0.0440
6	0.92	440	417	7,800	1,280	3,200	1,380	2,000	6		1,300	0.0700
Magne-Blast Breaker—150,000 Interrupting Kva												
7	0.42	440	417	5,850	960	2,400	12,400	14,400	65		1,440	0.051
8	0.42	440	417	5,850	960	2,400	14,800	18,220	90	(Figure 4)	1,650	0.051
9	0.42	440	417	5,850	960	2,500	5,700	9,250	34		1,500	0.0525
10	0.42	440	417	5,850	960	2,400	10,600	11,400	51		1,330	0.067



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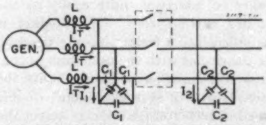
* Natural frequency beyond vibrator response.

Table II. Closing Tests Energizing a Three-Phase Delta Bank Against an Already Energized Delta Bank

Tests Were Made at 5,400 Volts

No	X_L Ohm L-N	C_1 Microfarads L-L	C_2 Microfarads L-L	I_1 RMS Before Closing	I_2 Peak During Closing Pole$_1$	Pole$_2$	Pole$_3$	Natural Frequency (Cycles Per Second)	Per Cent Rise Before Closing	Per Cent Rise After Closing
1	0.805	133	126	540	8,800	9,500	3,800	1,800	16	31.0
2	0.805	133	126	540	8,000	9,100	2,700	2,000	16	31.0
3	0.805	133	126	540	8,400	9,300	2,700	1,800	16	31.0
4	0.805	133	126	540	8,000	8,500	5,100	2,000	16	31.0



taneous pole closing, so will not be repeated. The case of sequential pole closing, however, was set up in a miniature circuit of Figure 2, using the following constants:

$L_1 = 0.234$ henry $C_1 = 3$ microfarads

$L_2 = 0.048$ henry $C_2 = 1$ microfarad

(L_1 and L_2 in per unit of the total capacitive kilovolt-amperes are 0.133 and 0.027, respectively)

The equalizing current on energizing C_2 was measured by means of the cathode-ray oscillograph. A comparison of the effect of simultaneous closure of the three poles of the switch with that of sequential closure (with the interval between pole closures controlled to give maximum current) is given in Figure 2 by oscillograms 91-10 and 91-13. It will be noted that the first natural-frequency current peak is nearly the same (eight times normal) in the two cases, indicating that sequential closing for the adjacent bank case has less effect on current maximum than in the single bank case.

Oscillogram 91-16 shows the effect of closing the second switch *b* ten degrees ahead of the time when the voltage across it would be a maximum. The same maximum current (in phase *c*) was obtained

Figure 3a. Oscillograms showing the equalizing currents associated with the energizing of an adjacent bank with an energized bank on the bus

See Figure and test 3 in Table I

B—Bus capacitor current, I_1 *C*—Cathode ray oscillograph relay trip
D—Oncoming capacitor current, I_2 *E*—Line current, I_T *F*—Breaker travel

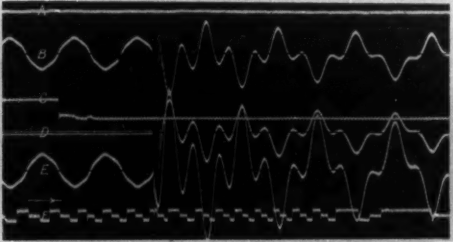
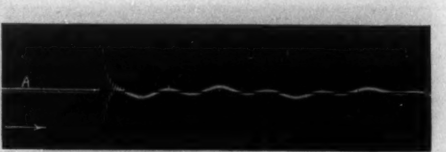


Figure 3b. Oncoming capacitor current of Figure 3a

See test 3 of Table I



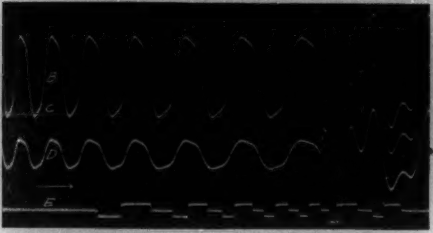


Figure 4a. Oscillogram showing the currents associated with the energizing of a 417-microfarad bank with a 440-microfarad bank on the bus

See test 8 in Table I

B—Bus capacitor current, I_1
C—Oncoming capacitor current, I_2
D—Line current, I_T
E—Breaker travel

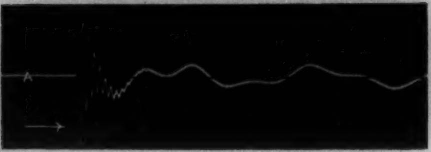


Figure 4b. Oncoming capacitor current of Figure 4a

See test 8 in Table I

by varying the angle of closing the last switch.

Full-Scale Tests

Tables I and II list a few of the typical test data recorded during the energizing tests. The test arrangement was designed to have small inductances in order to obtain maximum currents. In spite of this, the maximum current measured was below 20,000 amperes. In none of these tests were current-limiting impeders used. Figures 3, 4, and 5 are typical oscillographic records of the energizing phenomena with cross reference to Tables I and II giving the numerical values.

Discussion

The data in Tables I and II show the equalizing current values of adjacent single-phase and three-phase bank energizing. The largest adjacent bank energizing currents are well below the nominal values of current which these breakers are normally called upon to handle. The successful performance of the breakers under these conditions, together with the results presented in the section on "Capacitor Discharge During Short Circuit," shows that the "closing-in" duties of standard breakers constitute a very minor problem. The equalizing currents are high-frequency (1,000 to 5,000 cycles) highly damped discharges, producing negligible distress either as contact burning or as a delaying mechanical impulse to the closing breaker contact. Figure 6 shows an enlarged portion of the breaker contacts after 34 closing tests.

The De-energizing Problem

The initial voltage *across* a circuit breaker following the interruption of a capacitor circuit is zero, since the capacitor on one side holds the same crest voltage that existed on the bus side of the breaker at and immediately following current zero. This permits any circuit breaker to interrupt quite easily at the first arc current zero. The breaker is thus lulled into a feeling of easy triumph over the circuit with the minimum of contact separation when suddenly—with the cyclic reversal of system voltage—double instead of zero voltage appears across the open breaker just one-half cycle following the interruption. If the breaker withstands this double voltage without restriking, then and not until then can the circuit be considered as cleared. If, on the other hand, the circuit is restruck as the voltage approaches this cyclic reversal, then a series of events is possible which must receive special consideration.

In counterdistinction to capacitive interruptions, the current in inductive circuits is interrupted at each current zero, whereupon the double-voltage-recovery transient is immediate, and circuit interruption is either achieved, or the arc is restruck for another half cycle to repeat the attempt under similar conditions at the next current zero. Thus the insulation is permitted to build up between the separating contacts until such a point is reached that the circuit is cleared without accumulative voltage distress. (There

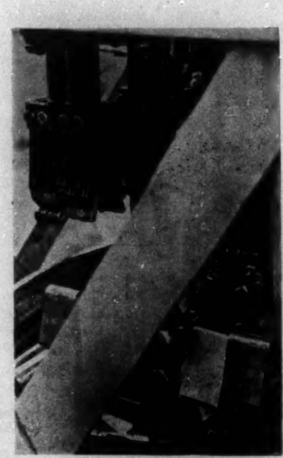


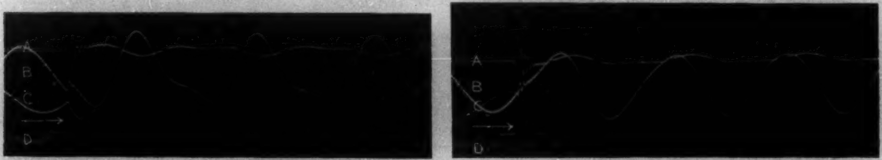
Figure 6. Circuit breaker contacts following a total of 34 closing tests in which an adjacent bank was energized with an energized bank on the bus

Typical tests are shown in Table I

Figure 5 (below, right and left). Oscillograms showing the phenomena in two phases associated with the closing on a 4,150-kva delta bank with a 4,400-kva bank on the bus

See test 4 of Table II

A—Capacitor current, I_1
B—Line-to-line voltage
C—Bus side capacitor current, I_2
D—Breaker travel



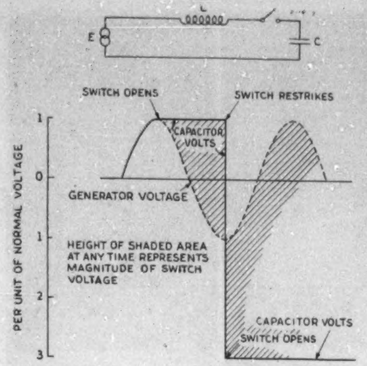
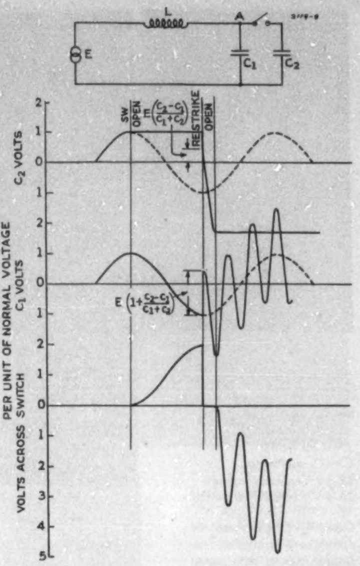

L
E
C
SWITCH OPENS
SWITCH RESTRIKES
CAPACITOR VOLTS
GENERATOR VOLTAGE
HEIGHT OF SHADED AREA AT ANY TIME REPRESENTS MAGNITUDE OF SWITCH VOLTAGE
CAPACITOR VOLTS
SWITCH OPENS
PER UNIT OF NORMAL VOLTAGE
1
0
1
2
3

Figure 7 (left). Case I—De-energizing grounded neutral capacitor bank

Total capacitance switched—shows basis of analysis

Figure 8 (right). Case II—De-energizing grounded neutral capacitor bank

Part of capacitance unswitched C_1/C_2= 0.2/0.5


L
A
E
C_1
C_2
SW OPEN
$E\left(\frac{C_2-C_1}{C_1+C_2}\right)$
RESTRIKE
OPEN
C_2 VOLTS
C_1 VOLTS
$E\left(1+\frac{C_2-C_1}{C_1+C_2}\right)$
PER UNIT OF NORMAL VOLTAGE
VOLTS ACROSS SWITCH
2
1
0
1
2
1
0
1
2
1
0
1
2
3
4
5

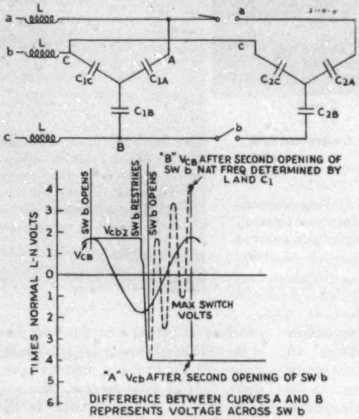

a
b
c
L
C
A
B
C_{1C}
C_{1A}
C_{1B}
C_{2C}
C_{2A}
C_{2B}
"B" V_{CB} AFTER SECOND OPENING OF SW b NAT FREQ DETERMINED BY L AND C_1
SW b OPENS
SW b RESTRIKES
SW b OPENS
V_{cb2}
V_{CB}
MAX SWITCH VOLTS
"A" V_{Cb} AFTER SECOND OPENING OF SW b
DIFFERENCE BETWEEN CURVES A AND B REPRESENTS VOLTAGE ACROSS SW b
TIMES NORMAL L-N VOLTS
4
3
2
1
0
1
2
3
4
5
6

Figure 9. Case VIII—De-energizing ungrounded neutral three-phase capacitor bank with part of capacitance unswitched

Second switch to open

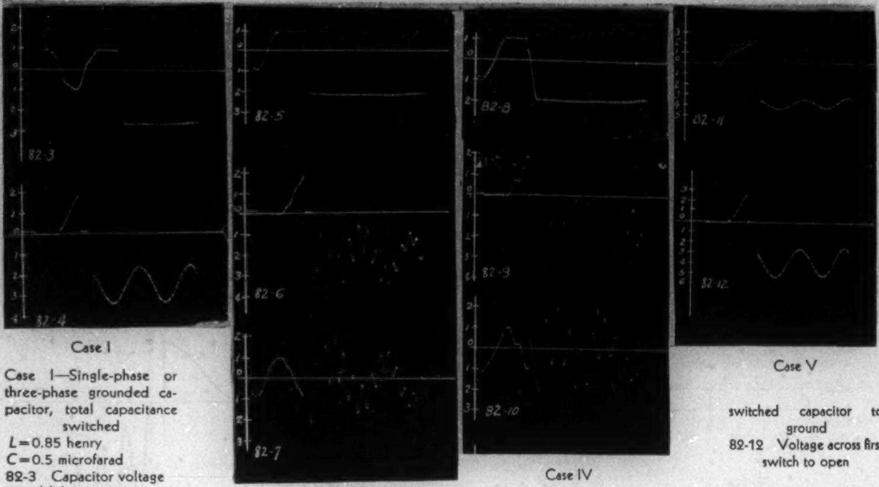
are however special cases of inductive circuits where abnormal restriking overvoltages can occur.[2] The effects of the restriking phenomenon on the capacitor switching problem are discussed under the section "Capacitor Discharge During Short Circuit.")

A comparison of these phenomena indicates that the interrupting capacity of standard breakers on capacitive circuits may be limited as described in this paper. The limitations are not so much in the ability of the breakers to eventually clear the circuit as they are in the desirability of limiting overvoltages on the system to safe values.

At this point, it is desirable to point out that the considerations contained in this paper deal with capacitor banks of high capacitive kilovolt-amperes, and in no way should the discussion presented here be construed to apply to circuits which contain relatively small capacitors—as for example, capacitors for the protection of electric equipment from lightning or switching surges and the lower-voltage equipments in industrial plants. In fact, the phenomena presented here are more akin to the phenomena of the interruption of line-charging current on high-voltage transmission systems. The two problems are identical in every major respect and differ only in degree. The capacitive kilovolt-amperes to be interrupted on high-voltage lines is usually large. As with the capacitor bank, interruption takes place easily at the first current zero in the arc, thus leaving the entire line charged to the crest value of generated voltage. As the generated voltage reverses on the bus side, double voltage appears across the switch. The breakers required to interrupt this line-charging current are inherently large, and although the contact velocity on parting is considerably larger than in the smaller breakers, it is not larger in proportion to the voltage. It follows that the voltage to appear across the open pole one-half cycle after contact parting is therefore much greater on the high-voltage systems. This fact, together with the large kilovolt-amperes to be interrupted explains why these interesting phenomena were first found on the high-voltage transmission systems. The trend toward large capacitor banks tends to project the above phenomena into the lower-voltage systems, not by reason of voltage, but because the heavier currents to be interrupted act to weaken the arc gap one-half cycle after parting, thus permitting the gap voltage on the reversal of bus voltage more easily to produce a restrike. The studies presented in this paper reveal the degree to which these phenomena are important to the lower-voltage switching equipments.

Analysis

Tests have been made on the transient analyzer[4] to determine the maximum magnitude of transient overvoltage that would be obtained when switching off various connections of capacitors, with the assumption that the circuit-opening mechanism would restrike once. The capacitor switch was opened at a funda-



Case I

Case II

Case IV

Case V

Case I—Single-phase or three-phase grounded capacitor, total capacitance switched
$L=0.85$ henry
$C=0.5$ microfarad
82-3 Capacitor voltage
82-4 Voltage across switch

Case II—Single-phase or three-phase grounded capacitor, part of capacitance switched
$L=0.85$ henry
C_1 (unswitched)$=0.2$ microfarad
C_2 switched$=0.5$ microfarad
82-5 Capacitor voltage
82-6 Voltage across switch
82-7 Unswitched capacitor voltage

Case IV—Same as case II, except $L=1.67$
$C_1=0.2$ microfarad
$C_2=0.5$ microfarad 82-8
82-9
82-10

Case V—Three-phase ungrounded neutral capacitor, total capacitance switched—first switch to open, phase *a*
82-11 Voltage of switched capacitor to ground
82-12 Voltage across first switch to open

Figure 10. Transient-analyzer oscillograms showing voltage obtainable when de-energizing capacitors, for various connections and assuming one restrike at peak of voltage wave

All voltage calibrations in times normal of normal *L-N* crest voltage

mental frequency current zero and reclosed by means of a synchronous switch at the subsequent recovery voltage maximum to represent a restrike. Although this switching procedure does not involve the randomness present in the circuit-breaker performance, it gives the highest possible magnitudes of both switch and capacitor voltage.

Table III presents the results of these tests, giving the peak switch and capacitor volts for the condition of no restrike and one restrike. Figures 7 through 9 show some of the connections tested and a "free-hand" graphical analysis of the voltages. These supplement the oscillograms in Figure 10.

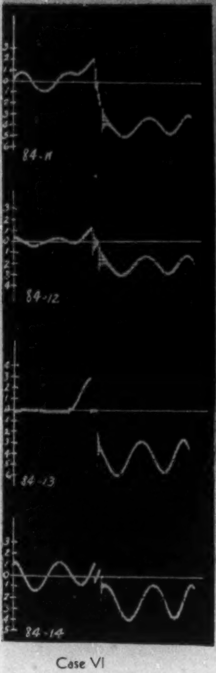
Figure 7 shows the fundamentals by which these transient overvoltages are obtained. At a current zero (peak of the voltage wave) the switch is opened, whereupon a full charge remains on the capacitor. The fundamental frequency system voltage moves onward until it is a negative maximum resulting in a switch voltage of twice normal. A restrike is permitted at this point, and the capacitor voltage changes to system voltage on the circuit natural frequency and overshoots (neglecting resistance damping) an amount equal to its attempted change, or to -3 times normal, as shown. At the first subsequent current (largely natural frequency) zero, the switch is reopened, and the charge of -3 times normal remains on the capacitor.

Figure 8 shows a similar analysis for switching off part of a bank (65 per cent of the total in all these tests) with one restrike. At the time just previous to restrike, C_1 is at -1.0 per unit volts and C_2 is at $+1.0$ per unit volts. In the transient-analyzer tests there was no intentional current limiter between C_1 and C_2; hence immediately upon restrike the charge on C_1 and C_2 equalized to a value

Table III. Transient Analyzer De-energizing Tests

Case No*	Condition	Peak Capacitor Volts to Ground: No Restrike	Peak Capacitor Volts to Ground: One Restrike	Peak Switch Volts: No Restrike	Peak Switch Volts: One Restrike
Grounded Neutral					
I	Switching 0.5-microfarad bank	1.0	3.0	2.0	4.0
II	Switching 0.5-microfarad bank, 0.2 microfarad unswitched	1.0	2.4	2.0	4.8
III	Same as I, L_1 changed from 0.85 to 1.67 henries	Similar to case 1			
IV	Same as II, L_1 changed from 0.85 to 1.67 henries	Similar to case 2			
Ungrounded Neutral					
V	Switching 0.5-microfarad bank, 1st phase	2.0	5.0	3.0	6.0
VI	Switching 0.5-microfarad bank, 2nd phase	2.6	6.0	3.46	6.6
VII	Switching 0.5-microfarad bank, 0.2-microfarad unswitched 1st phase	2.0	3.7	3.0	6.1
VIII	Switching 0.5-microfarad bank, 0.2-microfarad unswitched 2nd phase	2.5	6.0	3.46	8.2
IX	Same as VII, neutrals of capacitors connected	1.6	3.3	2.6	4.3

* See oscillograms, Figure 10.



Case VI

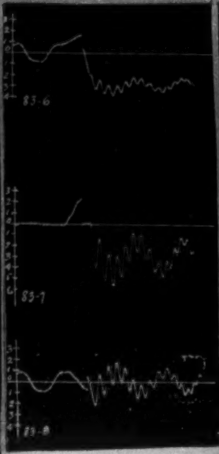
Case VI—Same as case V. Second switch to open (*b* phase)

84-11 Voltage of capacitor being switched (*b*) to ground

84-12 Voltage of capacitor already switched (*a*) to ground

84-13 Voltage across switch *b*

84-14 Voltage across switch *a*



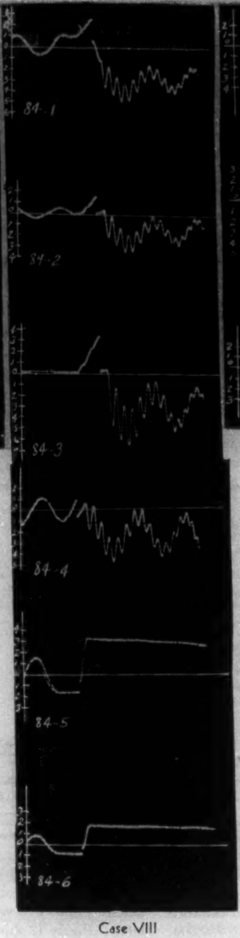
Case VII

Case VII—Three-phase ungrounded neutral capacitor, part of capacitance switched—first switch to open

83-6 Voltage of switched capacitor to ground

83-7 Voltage across first switch to open

83-8 Voltage of unswitched capacitor on phase *a* to ground



Case VIII

Case VIII—Same as case VII. Second switch to open (*b* phase)

84-1 Voltage of capacitor being switched (phase *b*) to ground

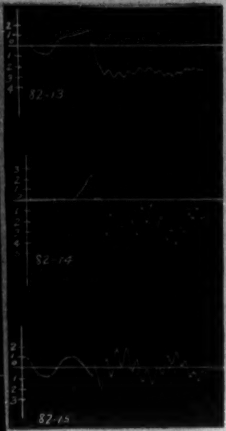
84-2 Voltage of capacitor already switched (*a*) to ground

84-3 Voltage across switch *b*

84-4 Voltage across switch *a*

84-5 Voltage V_{bc} on opened capacitors

84-6 Voltage V_{BC} on unswitched capacitors



Case IX

Case IX—Same as case VII. First switch to open, capacitor neutrals connected. (Comparison with case VII shows effect of unswitched capacitors in holding down neutral displacement of switched capacitors)

82-13 Voltage of switched capacitor to ground (phase *a*)

82-14 Voltage across first switch to open

82-15 Voltage of unswitched capacitor on phase *a* to ground

Figure 10 (continued.) Transient-analyzer oscillograms showing voltages obtainable when de-energizing capacitors, for various connections and assuming one restrike at peak of voltage wave

All voltage calibration in times normal of normal *L-N* crest voltage

given by $E(C_2-C_1)/(C_1+C_2)$ and then oscillated to a value given by $-E(C_1+3C_2)/(C_1+C_2)$. An analytical determination of this expression is given in appendix B.

The constants used for these transient-analyzer tests—in *L-N* values—were:

System inductance $L=0.85$ henry except cases 3 and 4, $L+1.67$ henries

Capacitance switched $C_2=0.5$ microfarad

Capacitance unswitched $C_1=0.2$ microfarad

The most severe overvoltage across the switch on de-energizing occurs when switching off part of an ungrounded wye (or delta bank) and when the second phase clears. This is shown by Figure 9 (case VIII). The capacitor overvoltage is equal to that obtained when switching all of the bank.

FULL-SCALE TESTS

Analysis shows—Table III—abnormal voltages are possible with one restrike under the most pessimistic conditions. Full-scale tests were made to determine practical conditions obtaining.

Table IV. De-energizing Tests—Three-Phase Ungrounded Capacitor

No	E_G Volts L-L	X_L Ohms L-N	C Micro-farads L-L	Capacitive Kva Switched	E_B Peak Opening L-G		E_B/E_G L-G Peak Opening	I_{RMS} Opening Amperes	Remarks
Oil-Blast Breaker—150,000 Interrupting Kva									
1	4,000	0.23	300	6,330	4,000		1.23	845	No disturbance—cleared
2	4,000	0.23	600	15,100	4,100		1.26	1,850	
3	4,000	0.23	600	15,100	4,000		1.23	1,850	
Oil-Blast Breaker—250,000 Interrupting Kva									
4	14,500	3.38	17.7	4,850	16,600		1.40	180	No disturbance—cleared
5	14,500	3.38	17.7	4,850	16,600		1.40	180	
6	14,500	3.38	34.4	10,800	16,000	Figure	1.35	375	
7	14,500	3.38	34.4	10,800	19,400	12	1.64	375	
8	14,500	3.38	34.4	10,800	19,000		1.61	375	
Magne-Blast Breaker—150,000 Interrupting Kva									
9	4,000	0.23	600	15,200	5,000		1.54	1,850	Moderate report—cleared
10	4,000	0.23	300	6,300	6,000		1.85	845	
11	4,000	0.23	975	31,400	4,900		1.51	3,400	
12	4,000	0.23	975	31,400	5,000		1.54	3,400	
Magne-Blast Breaker—250,000 Interrupting Kva									
13	5,400	0.47	259	11,400	6,000		1.37	1,050	Moderate report—cleared
14	5,400	0.47	259	11,400	6,500	Figure	1.49	1,050	
15	5,400	0.47	259	11,400	5,100	11a	1.17	1,050	
16	5,400	0.47	259	11,400	5,400		1.23	1,050	

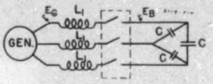
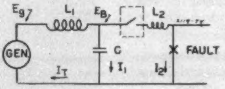


Table V. Closing–Opening Tests in Which Capacitor Discharged Through Breaker on Initiation of Fault

For These Tests Capacitance = 857 Microfarads

No	X_L Ohm	E_g Volts	E_B Volts	I_1 RMS Before Closing	I_1* Peak on Closing (Measured)	I_1 Peak on Closing (Calculated)	I_1 Peak on Opening	I_2 Short-Circuit Peak on Closing (60 Cycles)		I_2 RMS Symmetrical Fault (60 Cycles)	Switching Angle Degrees	Natural Frequency (Cycles Per Second)	L_2 Millihenry Calculated From Test
Magne-Blast Breaker													
1	0.421	5,400	6,300	2,050	10,000	11,100	8,800	36,000		11,800	12	1,100	0.0244
2	0.421	5,400	6,300	2,050	42,000	47,000	9,700	25,000		11,700	62	1,100	0.0244
3	0.421	5,400	6,300	2,050	7,800	7,400	9,400	36,000	Figure 13b	11,800	8	1,100	0.0244
4	0.421	5,400	6,300	2,050	52,000	53,000	9,500	20,000	Figure 13a	11,800	86	1,100	0.0244
Oil-Blast Breaker													
5	0.421	5,400	6,300	2,050	38,000	44,000	8,450	27,000		10,900	48	1,220	0.020
6	0.329	5,400	6,040	1,950	8,000	8,350	8,500	45,000		14,400	9	1,160	0.022
7	0.329	5,400	6,040	1,950	37,000	44,200	8,750	32,000		14,500	56	1,160	0.022



* This peak current flows through breaker in addition to short-circuit current, but has disappeared with the arrival of peak short-circuit current.

The typical test results summarized in Table IV indicate the scope of these tests. Oil and Magne-blast breakers of the voltage class up to 15 kv and up to 250,000-kva interrupting capacity were tested. It is interesting to note that the Magne-blast breaker successfully interrupted 31,400 kva at 4,000 volts without disturbing overvoltage in spite of some restriking. This result is caused by the high series resistance present[5] when restriking occurs within the arc chute, see Figure 11a. This desirable characteristic of the Magne-blast type of breaker places it in a very favorable position when switching capacitive circuits. Figure 11 exhibits typical cathode-ray oscillograms of the voltages attending these interruptions.

The 15-kv, 250,000-interrupting-kva oil breaker was tested up to approximately 11,000 kva with results as tabulated. Figure 12 shows a typical recording of this series of tests when de-energizing the larger bank ratings.

Discussion of Results

The transient-analyzer approach allowed the circuit constants to be readily varied for determining generalized qualitative results, thus materially reducing the full-scale test requirements. Also, with complete control of the random phenomenon called "restriking," certain conclusions could be drawn with respect to the maximum possible overvoltages obtainable under the most pessimistic conditions, which again helped to plan and analyze the full-scale program. The miniature setup allowed the following conclusions to be drawn:

1. Effect of circuit constants (L and C). If the entire bank is switched, the magnitude of the switch or capacitor voltage is not appreciably affected by the system constants provided that the circuit natural frequency is not less than approximately 180 cycles. (Refer to cases I and II of Figure 10.)

2. Presence of an unswitched bank. If part of the bank remains unswitched, the capacitor and switch volts are the same as the condition of total bank switched, if no restrike occurs. If restrike occurs, the smaller the bank unswitched, the larger the switch voltage, the limit being a very small unswitched capacitance for which a somewhat greater voltage may be obtained than for the case of absolutely no unswitched capacitance. Table I summarizes quantitative results.

3. Effect of grounding. Grounding the capacitor bank on a grounded system results in the lowest switching overvoltages, as may be seen by comparing the values of the grounded and ungrounded cases of Table III.

4. Effect of interconnecting neutrals of switched and unswitched banks. The unswitched bank under this condition has the tendency of holding the neutral of the switched bank at ground potential, and as the ratio of unswitched to switched capacity increases, the grounded case is more nearly approached. (Refer to cases VII and IX of Figure 10.)

Full-scale tests demonstrated that delta-connected or wye floating neutral capacitors up to 10,000 kva can be handled with standard breakers without creating the abnormal overvoltages indicated by the analysis. This appears to substantially satisfy present-day system requirements. Above these values a

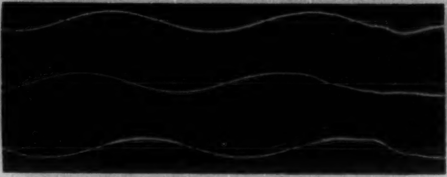


Figure 11a. Cathode-ray oscillogram showing the capacitor voltages to ground attending the interruption of a three-phase 6,500-kva bank with the Magne-blast air circuit breaker

See test 14 on Table IV

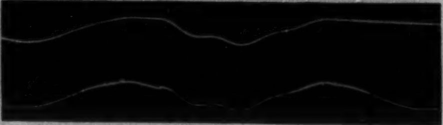


Figure 11b. Cathode-ray oscillogram of voltages to ground associated with the Magne-blast breaker opening a single-phase 3,300-kva 5,400-volt bank of capacitors

B—Capacitor volts
C—Bus volts

point is reached where restriking together with the attendant overvoltages becomes more probable, thus presenting a field for further development should the requirement greatly expand.

Capacitor Discharge During Short Circuit

When a short circuit occurs on the load side of a large capacitor bank, the capacitor discharges its energy into the fault—in some applications—along with the flow of short-circuit current. (See figure in Table V.) The question naturally arises regarding the extra duty, if any, required of the circuit breaker through which both of these currents flow. A study of the problem reveals that the most severe case of the above consideration concerns the closing of the breaker upon such a fault with immediate opening. The concern regarding this case is dispelled when it is realized that the maximum capacitor energy is "dumped" when the fault is initiated at the crest value of the system voltage. Under these conditions, the short-circuit current which flows is at its lowest value, namely the symmetrical value. In this case the highly damped capacitor discharge is practically over with the arrival of the first crest of normal-frequency short-circuit current. See Figure 13a.

If, on the other hand, the maximum short-circuit current is developed by initiating the fault at the zero point of the voltage wave, the capacitor charge is zero and hence contributes nothing. See Figure 13b.

Analysis shows that this latter case develops the maximum thermal and magnetic effects. Since this is a normal function of the breaker, it follows that the presence of the capacitor cannot add to the maximum closing duty of the breaker. It should be pointed out, in

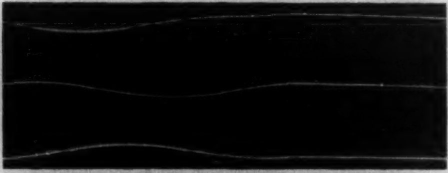


Figure 12. Cathode-ray oscillogram showing the capacitor voltages to ground associated with the interruption of a three-phase 10,800-kva bank with an oil-blast circuit breaker

See test 6 on Table IV

Figure 13 (left, below). Typical oscillograms showing the currents resulting when a circuit breaker closes upon and interrupts short circuit when a large capacitor bank is present on the bus

B—Trip-coil current
C—Capacitor current, I_1
E—Fault current, I_2
F—Line current, I_T
G—Breaker travel

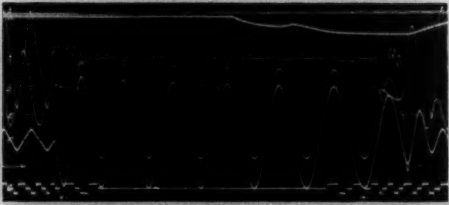


Figure 13a. Fault initiated near the peak of generated voltage

Maximum capacitor charge. Note the high capacitor discharge in the presence of approximately symmetrical fault conditions. See test 4, Table V

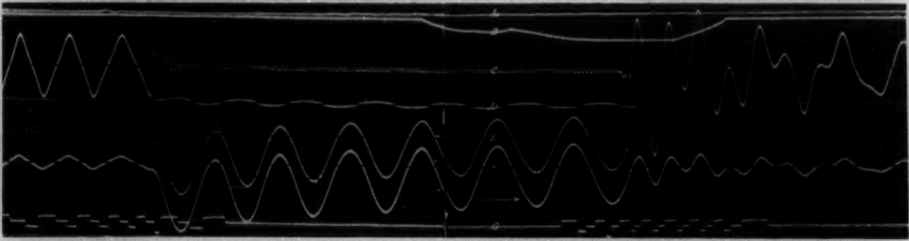


Figure 13b. Fault initiated close to the minimum point of generated voltage

Minimum capacitor charge. Note the low capacitor contribution in the presence of the displaced short-circuit current. See test 3, Table V addition to the above, that the presence of the shunt capacitor on the bus materially aids the circuit breaker in its duty by lowering the rate of rise of recovery voltage. This last deduction does not hold if the breaker is attempting to clear a *L-G* fault on a system that is susceptible to high arcing-fault voltages. Other[2] investigations indicate that on these systems the reduction of the ratio of capacitive zero-sequence reactance to the positive-sequence inductive reactance tends to give the more severe overvoltages if restrike occurs. These conditions however, are rare and can be eliminated by properly grounding the system. In order to determine breaker operation when performing this function, as well as an attempt to get higher inrush currents in the interests of completeness, tests were conducted in which short circuits were produced by closing breakers upon the fault and tripping with no time delay. Two oscillograms are shown demonstrating the above relationships. Figure 13a shows a fault initiated near the peak of generated voltage. Note that the high-frequency capacitor discharge is so short that, with the arrival of the first peak of the symmetrical short-circuit current, little of the capacitor discharge remains. Figure 13b shows a fault initiated near the zero of generated voltage, giving rise to a displaced fault. Note the insignificant contribution made by the capacitor. Table V shows a partial list of the tests made under these conditions. The above considerations show that the interrupting duty on a circuit breaker is not increased by the presence of a large shunt bank on the bus side. In no case did the breakers in this series of tests exhibit abnormal stress.

Appendix A. Sequential Energizing of a Three-Phase Ungrounded Wye Capacitor Bank—Inrush Current

Considering the circuit given by Figure 1a, switch *a* may be closed with no electrical effect. The maximum current in phase *ab* may be obtained by closing switch *b* at a time when the voltage across it, E_{ab}, is a maximum. Thus

$$i_a=-i_b=\frac{\sqrt{3}E\cos\omega t}{2\left(pL+\frac{1}{pc}\right)}=\frac{(1-a)E_a}{2Z(p)} \tag{1}$$

where E = line-to-neutral generated voltage in phase *a*.

The voltage across the open switch *c* is

$$a^2E_a-\{E_a-i_aZ(p)\} \tag{2}$$

which with equation 1 substituted is

$$E_a\left(a^2-1+\frac{1}{2}-\frac{a}{2}\right)=\frac{3a^2}{2}E_a=-1.5E\sin\omega t \tag{3}$$

It is interesting to note that the voltage across the last switch to close has no natural-frequency component, even though the current flowing in phases *b* and *c* at this time does have. This may be written

$$E_{sw_c}=-1.5E\sin(\omega t'+\theta) \tag{4}$$

where

$\theta=\omega\Delta t$, and Δt is the time elapsed between the closing of switches *b* and *c*. Considering the effect of this voltage by itself,

$$i_c=\frac{-1.5E\sin(\omega t'+\theta)}{1.5Z(p)} \tag{5}$$

At any time after Δt the total current in phases *a* and *b* is

$$I_{aT}=i_a-\frac{i_c}{2}$$

$$I_{bT}=-i_a-\frac{i_c}{2} \tag{6}$$

Since I_{bT}, the total current in phase *b*, will be the largest, the operational expression for I_{bT} given by equation 6 will be solved.

$$-i_{bT}=\frac{\sqrt{3}E\cos\omega t}{2Z(p)}-\frac{E\sin(\omega t'+\theta)}{2Z(p)} \tag{7}$$

The solution of this is

$$I_{bT}=I_0\left[\frac{\sqrt{3}}{2}\sin\omega t-\frac{\sqrt{3}}{2}\sqrt{X_C/X_L}\sin\omega_0 t+\frac{1}{2}\cos(\omega t'+\theta)-\frac{1}{2}\cos\theta\cos\omega_0 t'+\frac{1}{2}\sqrt{X_C/X_L}\sin\theta\sin\omega_0 t'\right] \tag{8}$$

where

$I_0=\frac{E}{Z}$ = normal capacitor current per phase

$X_C=\frac{1}{\omega C}$ $\quad X_L=\omega L$

$\omega_0=\omega\sqrt{X_C/X_L}$ = natural frequency of the circuit

t terms are components of i_b, the current before switch *c* is closed

t' terms are components of $\frac{i_c}{2}$ the additional current after switch *c* is closed

Since the natural-frequency components of equation 8 have large coefficients by comparison with the forced frequency components, the latter may be neglected to simplify the task of determining the value of θ which will give the largest total component of current in phase *b*. The natural-frequency components are

$$I_{NF}=\sqrt{X_C/X_L}\left[-\frac{\sqrt{3}}{2}\sin\omega_0 t+\frac{1}{2}\sin\theta\left(\sin\omega_0 t\cos\frac{\omega_0}{\omega}\theta-\cos\omega_0 t\sin\frac{\omega_0}{\omega}\theta\right)\right] \tag{9}$$

Putting $dI_{NF}/d\theta$ equal to zero permits the value of θ to be found which will give maximum I_{NF}. This will not be far from that which will put the two components of equation 9 inphase which, for a natural frequency of 5 is $\theta=180/5=36$ degrees.

If $\theta=36$, $I_{NF}\max=\sqrt{X_C/X_L}\,1.12$

If $\theta=40$, $I_{NF}\max=\sqrt{X_C/X_L}\,1.18$

Adding 1.0 to $\sqrt{X_C/X_L}\,1.18$ to allow for fundamental frequency components of equation 9 gives

$$I_{bT}=I_0(5.9+1.0)=6.9I_0$$

which is 15 per cent higher than $I_{\max}$ determined for simultaneous switching.

Appendix B. Capacitor De-Energizing Analysis; Capacitor Voltage upon Restrike

Case 2. Part of Bank Unswitched

Referring to Figure 8, just before restrike, at $t=0$, the voltage existing across the switch $=-E(1+\cos\omega t)$ neglecting the rise in voltage of point *a* due to *i*, flowing through *L*. The effect of closing the switch (restriking) may be obtained by applying this voltage to the operational impedance of the circuit, and the switch current is

$$i_2=EC_2\frac{\omega_0^2}{\omega_1^2}\left[\frac{p(\omega_1^2+p^2)}{(\omega_0^2+p^2)}+\frac{p^2(\omega_1^2+p^2)}{(\omega_0^2+p^2)(\omega_2^2+p^2)}\right] \tag{10}$$

where

E = crest value of generated voltage

$$\omega_1^2=\frac{1}{LC_1},\quad \omega_2^2=\frac{1}{LC_2},\quad \frac{1}{\omega_0^2}=\frac{1}{\omega_1^2}+\frac{1}{\omega_2^2}$$

The change of voltage of capacitor C_2 due to the restrike is $\Delta e_{c2}=i_2/pC_2$ for which the solution is

$$\Delta e_{c2}=E\frac{\omega_0^2}{\omega_1^2}\left[\frac{\omega_1^2}{\omega_0^2}+\quad-\frac{\omega_1^2}{\omega_0^2}+1+\frac{\omega_1^2-\omega_0^2}{\omega^2-\omega_0^2}\right)\cos\omega_0 t+\frac{\omega_1^2-\omega^2}{\omega_0^2-\omega^2}\cos\omega t\right] \tag{11}$$

If the natural frequency ω_0 is assumed very large compared with ω, then at $t=\pi/\omega_0$

$$\Delta e_{c2}=2E\left[\frac{C_1+2C_2}{C_1+C_2}\right] \tag{12}$$

and the capacitor voltage after restrike is

$$E_{c2}=E-\Delta e_{c2}=E\left[\frac{-C_1-3C_2}{C_1+C_2}\right] \tag{13}$$

For case II, where $C_1=0.2$ microfarad and $C_2=0.5$ microfarad, $E_{c2}=(-17/7)E$. This value may be obtained by conservation of charge—the sum of the charges on the capacitors after restrike is the same as before, hence immediately after restrike

E of both capacitors =

$$\frac{Q_T}{C_T}=\frac{(-C_1+C_2)E}{C_1+C_2}=-\frac{3}{7}E$$

The equalized capacitor voltage must still change through $2C_2/C_1+C_2\,E$ to arrive at the system voltage, and in changing through the inductance it will overshoot this amount and become a negative maximum of

$$\left(\frac{2C_2}{C_1+C_2}+1\right)E=\frac{C_1+3C_2}{C_1+C_2}E$$

which agrees with equation 13.

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Application of Vacuum-Tube Oscillators to Inductive and Dielectric Heating in Industry

J. P. JORDAN
ASSOCIATE AIEE

I. Introduction

THE knowledge that metals could be heated inductively and that nonconductors could be heated dielectrically dates from the earliest experiments with electricity. In the last few years inductive and dielectric losses, which have long been obstacles in the electrical industry, have been profitably employed in a new industrial tool powered by high-frequency motor-generator sets and vacuum-tube oscillators. Low-frequency inductive heating has been used in specialized melting applications for approximately 20 years and for some surface hardening work for the last ten years. However with the advent of the new equipment and knowledge of its use, it is no longer a highly restricted application but is ready to handle thousands of heating processes throughout the industry. Hence, it is felt that a short review of the theory of inductive and dielectric heating, together with a description of the vacuum-tube circuits used and some of their applications, would be of value at this time.

II. Classification of Equipment

Inductive heating can be roughly segregated by frequency into three classes. The first class covers applications to 1,000 cycles. It is almost entirely used for forging and melting steel and nonferrous metals either directly or indirectly (that is, by heating electrically conductive crucibles). Power requirements range from a few watts to thousands of kilowatts generated by motor-generator sets. It is being widely used to produce many of the specialized alloys demanded for war equipment.

The second class covers frequencies from 1,000 to 12,000 cycles almost entirely generated by rotating machines with power ratings of from 20 to 1,200 kw. The applications in this group cover practically all heating processes—surface hardening, forging, brazing, soldering, melting are all included. One to three thousand cycles are used for melting nonferrous alloys, for surface-hardening relatively large parts, and for some forging applications. The new 9,600-cycle generators are being used successfully for surface-hardening relatively small parts where the contours need not be too closely followed, and where the depth of case need not be too small. Widely used for brazing and soldering of other high-resistance metals, inductive heating is more difficult to apply to the heating of copper, brass, and other low-resistance alloys.

The third group for inductive heating covers all applications from 50,000 cycles to 1,000,000 cycles. Generation is almost entirely restricted to spark-gap and vacuum-tube oscillators. Spark-gap type of equipment can be built for frequencies from 50,000 cycles to 200,000 cycles and for power outputs up to 25 kw. Vacuum-tube equipment, while having no theoretical frequency or power limitation, is most economically applied at frequencies over 150,000 cycles and at power outputs up to 200 kw.

In the dielectric heating field the vacuum-tube oscillator is the only source capable of producing sufficient power at the frequencies necessary. From 1,000,000 to 30,000,000 cycles have been used at powers up to 200 kw.

III. Theories Involved

Before taking up some of the applications in greater detail, it might be well to review some of the fundamental theories involved. Any charge to be heated inductively can be thought of as a short-circuited secondary winding of a transformer, the primary being the heater coil. The currents produced in the charge heat it by resistive and hysteresis losses according to the well-established theory of eddy currents. The heat generated is confined to the surface of the parts by the skin-effect phenomena. The depth of penetration varies inversely with the square root of frequency, directly with the specific resistance of the material, and, in the case of magnetic materials, inversely with the permeability.

With nonmagnetic metals the heat is generated in this superficial volume throughout the heating cycle, varying only as the resistance of the metal changes

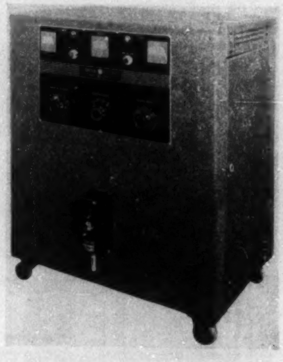

Figure 1. Five-kilowatt power oscillator for high-frequency induction heating

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J. P. JORDAN is in the radio transmitter engineering department, General Electric Company, Schenectady, N. Y.

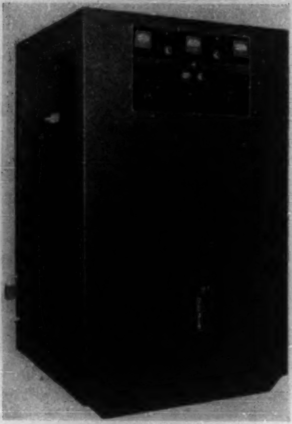


Figure 2. Fifteen-kilowatt power oscillator for high-frequency induction heating

with temperature—the core of the charge being heated largely by thermal conduction. Thus silver and copper are difficult to heat because of their low specific resistance and the resulting small depth of penetration. In magnetic materials the depth of the heated layer, which is very small due to the effect of the high permeability, increases rather abruptly at the temperature above which the material becomes nonmagnetic. Simultaneously, the power being absorbed in the original layer is reduced. Therefore steel with a high permeability and high specific resistance is readily heated in a thin surface layer while below the critical temperature, whereas above this temperature the power absorbed is less, and the depth of penetration considerably greater.

Because of these effects, the majority of steel parts can be readily surface-hardened at the intermediate frequencies by heating the surface inductively and quenching when the desired penetration has been achieved. Much higher frequencies are required for irregular parts where the contour must be followed closely, where coupling is difficult because of shape or small size, and where extreme localization of the heat is a necessity. To heat readily the low-resistance metals such as silver, copper, aluminum, and brass, the magnetic flux densities required at the intermediate frequencies are difficult to obtain, whereas the higher frequencies allow the use of more normal flux densities.

In dielectric heating the material to be heated is placed between two electrodes, and voltage applied at a frequency above 1,000,000 cycles. The whole forms a high-loss capacitor. The theory of dielectric losses is somewhat complex and is not yet thoroughly understood. However for most materials the heat developed is roughly proportional to the frequency and voltage applied and to the power factor of the dielectric. The entire mass is uniformly heated throughout, provided the electric field is uniform.

IV. Vacuum-Tube Circuits

The vacuum-tube oscillators used in the high-frequency field are of the simplest possible type. Radically departing from the usual radio construction practice, they are truly an industrial tool. Mechanically they are ruggedly built to withstand normal factory usage. Electrically they have been greatly simplified to provide the maximum reliability. The circuits most generally used are the Colpitts and the coupled-grid self-excited oscillator. In either of these circuits, the alternating supply voltage is stepped up by means of a power transformer to a voltage in the neighborhood of 7,500 volts to 15,000 volts and then rectified by a suitable bank of mercury vapor rectifier tubes. The direct current thus obtained supplies the oscillating circuit which consists primarily of a grid-controlled vacuum tube shunted across a parallel resonant circuit. The tube acts as a very rapidly operating switch. When this switch operates at the fundamental frequency of the parallel resonant circuit, it transmits power surges at this frequency by effectively short-circuiting the line once each cycle. Thus the voltage across the oscillation capacitor and inductance is alternately reduced and increased, setting up a circulating current in the resonant circuit.

To assure that the tubes operate at the desired frequency, their control grids are excited directly from the resonant circuit. This excitation voltage is obtained by several methods. In the Colpitts circuit (Figure 3) the excitation voltage is obtained by a direct tap on the oscillation capacitor, while in the coupled-grid circuit (Figure 4) it is developed in a coil inductively coupled to part of the oscillation inductance. All direct current is prevented from appearing in the resonant circuit by means of blocking capacitors. In both of these circuits the heater coil is usually in series with the parallel resonant circuit and forms part of the oscillation inductance. Since the frequency of operation is primarily determined by the constants of the resonant circuit, it will shift slightly as the inductance of the heater coil changes with load, thus always assuring operation at resonance and reducing fluctuations in the output current. For some applications requiring higher coil currents than are economically obtainable with a resonant circuit, an air-core transformer is used to step up the current.

V. Applications—Inductive Heating

This type of equipment can be applied to a great variety of heating problems. In the case of a small one-quarter-inch shaft the central portion was used as a bearing surface while the ends were riveted over to hold it in position. The overall length was approximately $1^{1}/_{4}$ inches. The problem was to harden the bearing surface while leaving the ends unaffected. Previously, this was achieved by copper-plating the ends and then casehardening the exposed parts by carburizing—a lengthy and expensive process. To apply induction hardening, a small coil was wound which was approximately the length of the desired hardened area. The part was centered in this coil, and the whole was immersed in water. Power at a frequency of 500,000 cycles was applied. As the steel heated up, a film of steam formed around that area, protecting it from the cooling action of the water.

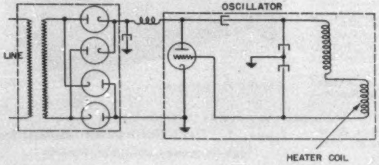
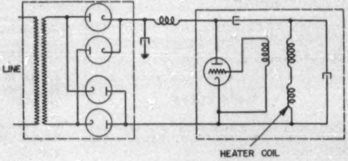


Figure 3 (left). Basic Colpitts oscillator circuit as used for induction heating

Figure 4 (right). Basic coupled-grid oscillator circuit as used for induction heating



But, when power was removed, this vapor envelope collapsed, allowing the water to rapidly quench the part. The entire cycle was complete in $3^1/_2$ seconds, and a satisfactorily hardened area obtained. Approximately four kilowatts output was used.

Curiously enough, in surface-hardening split pipe-threading dies an external coil can be used to harden the inside teeth. This is possible, because the induced currents must travel in a closed loop. Since there is no continuous external path around the circumference of a split die, the currents must travel completely around the inside of the die before returning to the outside surface. Thus such a die can be located inside a coil and, with proper heating and quenching cycles, can be hardened on both the inside and outside surfaces simultaneously, leaving a soft tough core.

In applying inductive heating to brazing, the usual procedure consists of assembling the fluxed parts using a preformed piece of brazing alloy between the parts. The advantages of the use of this method are speed, cleanliness, simplicity, and localization of heat. No experienced operators are necessary, and the method is admirably suited to automatic or semiautomatic operation.

In assembling the stator punchings of a small motor, the shell holding them had previously been spun over on either end. However, in using this method, the punchings had a tendency to work loose. To solve the problem, one end of the shell was spun over, the stator punchings assembled in position, and a thick washer placed over the end. A groove in the washer accommodated a ring of silver solder adjacent to the shell. The use of torch heating was impractical, because the heat conducted to the punchings tended to carbonize the insulating varnish. However, by using high-frequency induction, the heat could be so localized and the time interval used so short, that little heating of the punchings took place. Also, since the entire brazing operation could be performed while in a press, no loosening was encountered.

The manufacture of a small terminal

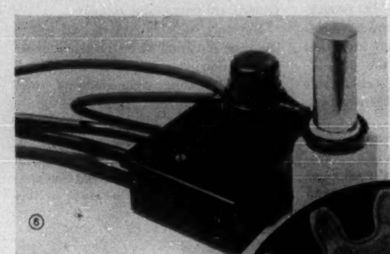


Figure 6. Brazing adapter on small assembly using air-core output transformer

Photo courtesy of Ajax Electrothermic Corporation

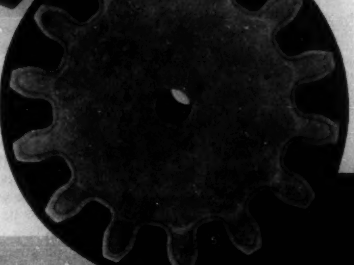


Figure 5 (right). Surface-hardened gear

Photo courtesy of The Ohio Crankshaft Company

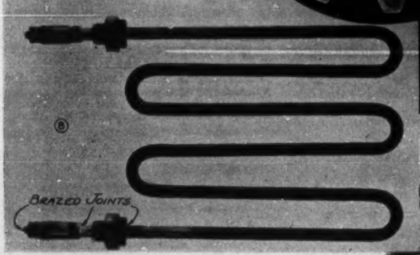


Figure 8. Nickel-silver-sheath Calrod unit resistor with Mycalex terminals and bushings inductively brazed

Figure 7. Terminal bushing assembly in which brazed joint and fused glass on rim were heated simultaneously

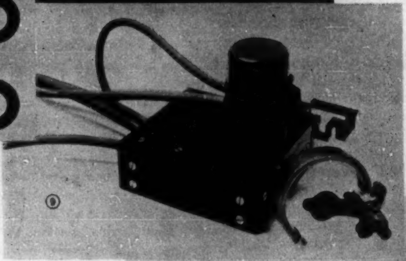


Figure 9. Focus inductor for specialized brazing showing output transformer with two-turn secondary winding

Photo courtesy of Ajax Electrothermic Corporation

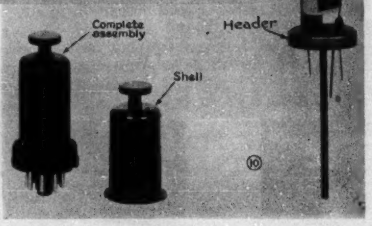


Figure 10. Metal-tube-type crystal unit

Shell soldered to header assembly by inductive heating to be sealed in a glass bushing required two heating operations. First, the copper stud had to be silver-brazed to a nickel-steel alloy cup using a gas torch. Then, after spraying the rim of the cup with a suspension of finely divided glass in alcohol, the whole was fired in a muffle furnace to fuse the glass preparatory to sealing it into the insulator. These two operations are performed simultaneously, and a much more uniform product achieved by the induction heating method. The coil used is so proportioned that, as the proper temperature is reached for brazing at the joint, the flange is at the fusing temperature of the glass. The entire operation is complete in five seconds.

In the assembly of Calrod heating units the bushings and terminals are brazed in place. A great variety of metals must be heated since nickel-silver, copper, brass, and steel are all used for various applications. Also, the Mycalex insulating pieces must not be overheated or damage will result. Although the steel parts could be successfully heated using intermediate frequencies generated by rotating machines, high-frequency vacuum-tube equipment is used to obtain flexibility of operation throughout the range of materials used. Preformed silver solder rings are used. All operations being automatically controlled, there is no possibility of overheating or burning of the pieces. A further application of this machine has been the reclamation of short pieces of tubing previously regarded as scrap, since efforts to braze them together had been expensive and in many cases resulted in a weak joint. However, because of the uniformity of the brazed joint and the automatic operation made possible by inductive heat, these pieces are now being used successfully.

Because soft soldering is a low-temperature operation amenable to the use of resistance heating, only high-production items of an unusual nature have previously been attempted by induction methods. However, with the present-day enforced use of high-temperature soft solders, this application has been expanding rapidly.

In the manufacture of crystals as used in radio apparatus, one form of the crystal assembly is mounted on a header and enclosed in a shell very similar to the familiar small metal radio tube. The shell is soft-soldered to the base or header. This operation was performed in the past by pretinning the flange on the shell and the rim of the base and making the final joint in a ring burner, adding such solder as was necessary. However, the products of combustion of the gas used sometimes condensed on the crystal surface and seriously affected its operation. Also, the heat transmitted through the supporting bracket occasionally cracked the crystal. By heating inductively, the heat is entirely confined to the rim, and the operation considerably speeded up.

VI. Applications—Dielectric Heating

Dielectric heating of nonconducting materials is the newest application in the field and shows great promise of rapid development. Already one large unit has been placed in operation curing the glue in plywood with promise of many other such installations.* Several unsuccessful trials have been made to cure rubber by this method, but the applications to plastics, ceramics, and insulating compounds are progressing rapidly.

Not only will this type of heat reduce the time of curing of many plastic parts (a reduction in the case of thick laminated plastic insulating compounds of from hours to minutes), but it will allow the use of some types of plastics in forms previously impossible because of the difficulty of properly curing the material. Within the next two years it is expected that considerable progress will be made in this direction.

VII. Closure

A word of caution regarding the application of this type of equipment. The rotating machine and the high-frequency oscillator supplement but cannot replace each other. It is neither economically nor theoretically sound to attempt to heat large regularly shaped steel parts by use of oscillators, nor is it wise to use rotating machines of high power to heat a small irregular part where a low power oscillator could be applied. There is little overlapping of their respective fields of application, but, because of the great number of factors which must be considered, no broad definitions of their fields can be made. Thus each application must be judged on its own merit.

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* Installed at M & M Woodworking Company, Plylock division, Albany, Oregon by the Thermex division of the Girdler Corporation.

Energy Flow in Electric Systems—the *Vi* Energy-Flow Postulate

JOSEPH SLEPIAN
FELLOW AIEE

Synopsis: The conditions which a valid postulated electric-energy flow must satisfy are given and are stated to be insufficient for its unique determination. The commonly used *Vi* energy-flow postulate is shown by examples to be not generally valid, but by adding a simple term it can be made equally valid with other valid energy-flow postulates. Various examples are given of the application of this corrected energy-flow postulate. On power systems the engineer commonly limits his use of the uncorrected *Vi* postulate to applications where the correcting term should have a negligible net effect. Various examples of such use are discussed.

I. Introduction

Among power electrical engineers the following postulate as to electric-energy flow is extensively used. Let V be the electric potential at any particular point, referred to some arbitrarily chosen point of zero potential. Let i be the current density at that point. V, of course, is a scalar quantity, having magnitude but no associated direction in space; i is a vector, having magnitude and also direction in space. The product Vi is a vector and is postulated to describe or represent the density of a flow of electric energy in watts per square centimeter, V being in volts and i in amperes per square centimeter. In particular, the point in question may be a point in the cross section of a linear conductor or cable. The postulated electric-energy-flow density may be integrated over the section of the conductor to give the total postulated energy flow along the conductor. If, as is usually the case, the potential V is constant over the section of the conductor, then the total postulated energy flow will be VI, where I, a vector, is the total current flowing over the section of the conductor. This will be the reading of an instantaneous wattmeter connected into and to the conductor at that point. The average in time of this quantity VI will be the reading of the usual wattmeter at that point.

What are the phenomena which can be actually observed which justify or make valid the energy-flow postulate, if it is valid? They are the following. At some points in space electric energy is being generated or created. By that is meant that energy of some other well-known, recognizable, and measurable form is disappearing, and at the same time electric manifestations are taking place there, such as the flow of currents, the appearance of electric fields, and so forth. Thus, in a generator chemical energy of a pile of coal is disappearing, or potential energy of water in a reservoir is lessening in amount. In a discharging battery chemical energy is disappearing. In a thermocouple heat energy at the hot junction is disappearing at a rate greater than the heat energy appearing at the cold junction.

At other points in space recognizable and measurable forms of energy are appearing or being created, with simultaneous electric manifestations. In an electric oven heat is appearing in measurable amount. In an electric motor, mechanical energy is appearing as, perhaps, in the increasing potential energy of a rising elevator. In an electrochemical plant the chemical energy stored in matter is being increased.

Also, at various points in space electric and magnetic fields make their appearance, and it is found necessary, if the law of conservation of energy is to remain true, to assign a stored electromagnetic energy to these fields. It is commonly said: "It takes work to produce these fields." Where, as in the core of a reactor, there is a magnetic field, it is customary to assign a stored energy per cubic centimeter of amount $(10^{-7}/8\pi)H\cdot B$ joules where H is the magnetic intensity, and B is the magnetic induction in gauss. Where, as in the dielectric of a capacitor, there is an electric field, it is customary to assign a stored energy per unit volume of $^{1}/_{2}\,E\cdot D$ where E is the electric intensity in volts per centimeter, and D is the electric induction in coulomb-centimeters. In empty space, these assigned stored energy densities are respectively $\frac{10^{-7}}{8\pi}H^2$, and $\frac{1.11\cdot 10^{-12}}{8\pi}E^2$ joules per cubic centimeter. If these energy densities are integrated over all space, they give correctly the total energy which must be regarded as stored in the electromagnetic field if the law of conservation of energy is to remain true.[*]

At the various points of space this stored electromagnetic energy will be appearing or disappearing accordingly as the electric and magnetic fields are increasing or decreasing in intensity.

The postulated energy flow may be said to be substantiated, made valid, justified, or established if it properly co-operates with or fits in with these observable phenomena which have just been described. Where electric energy is being generated, the postulated energy flow must show energy flowing away and in proper amount. In regions where electric energy is being consumed, the postulated energy flow must make energy approach in proper amounts. At places where electric and magnetic fields are changing, and with them the associated stored energy, the postulated energy flow must make approach or recede the proper amounts of energy.

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Joseph Slepian is associate director of research, research laboratories, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

[*] It turns out that these energy densities are not the only ones which may be assigned to the various points of space, which will integrate to the correct total electromagnetic energy. See, for example, reference 1. Therefore, these stored energy densities do not have a unique validity. However, in this paper, only these assumed energy densities will be considered in their relation to postulated energy flow.

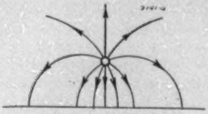

Figure 1 (left). Field E_1 in plane perpendicular to line

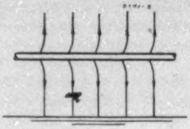

Figure 2 (right). Field E_1 in plane through line

The conditions establishing the validity of the postulated energy flow may be stated more exactly in this way. Construct a closed geometric surface anywhere in the electric system. Determine the postulated energy-flow density at every point of the surface. Integrate over the whole surface, obtaining the postulated total energy flow outward through the surface. This total energy flow out must correspond to the total changes taking place within the region enclosed by the surface. That is, the total energy flow out through the surface calculated according to the postulate must equal the excess of the rate of generation of electric energy over the sum of the rate of consumption and rate of increase of storage of electric energy within the surface.

It will be evident at once to the trained mathematician, and after a little study to the more lay engineer, that the conditions just given are not sufficient for determining uniquely an energy flow. For the postulate must merely direct the energy from the various generation points to the various consumption points, in their totality; which generation point is to feed which consumption point, and by what route, is completely undetermined so far as concerns any phenomenon which can be actually physically observed. Infinitely many postulates may be devised which will all be equally valid and equally well established by the conditions which have been described, and which are the only conditions available for defining an energy flow.

Actually, a postulated energy flow other than the *Vi* postulate, which is the subject of this paper, is very widely used, and not merely in abstract theory but for practical calculations, by radio engineers and physicists. This is the Poynting vector postulate about which there is an extensive literature.† We will not stop for a description of this Poynting vector postulate in detail at this stage. Suffice it to say that it is in its details completely different from the *Vi* postulate. In general, in an electric system it will lead energy from any particular generation point to a consumption point other than that to which it will be led by the *Vi* postulate, and where it leads energy from the same generation point to the same consumption point, it will in general do so by a different route.

Calling attention to the lack of uniqueness of the validity of any and all valid energy-flow postulates is not however the primary purpose of this paper. It is rather to examine the *Vi* postulate and to determine whether it in itself is valid or established in the only physically verifiable sense which has been just described. The completely general validity of the Poynting vector postulate has been established (with the formulas for stored electromagnetic energy given above) by mathematical derivation from Maxwell's equations. The validity of the *Vi* postulate can then be determined most conveniently by checking by well-known mathematical operations whether it is equivalent to the Poynting vector postulate in the sense already explained. This has been done in another paper.[1]

It turns out that the *Vi* postulate is not generally valid, that is, it is not generally equivalent to the Poynting vector postulate. By adding a simple term, however, a new or enlarged *Vi* postulate may be given which is generally valid. It so happens that as usually used by electric-power engineers the contribution of this added term is negligibly small. Hence, no error is committed by the use of wattmeter readings in the usual way for the usual and proper purposes by the engineers of electric-power systems.

Without going into the detailed mathematics, this paper will show how the simple *Vi* postulate fails in many simple cases. It will describe the term which must be added to make the enlarged *Vi* postulate universally valid and will show in simple cases how this term makes complete the *Vi* energy-flow picture. It will show also that this term makes a negligible contribution in the usual use of wattmeters on power systems for usual proper purposes. To do these things, however, it is necessary to examine somewhat carefully the meaning of the electric potential V, the current density i, and the nature of the electric field.

II. The Electric Potential, V

The potential difference between two points is frequently defined as the integral of the electric force from the one point to the other, or, in other words, as the work which must be done in moving a unit positive charge from the one point to the other. This is equivalent to saying that it is the reading of a suitable voltmeter connected to the two points in question.

For d-c systems, where electric and magnetic fields are steady and unchanging, this definition is satisfactory. In this case the electric-field intensity is the negative gradient of the potential so defined. In this case also the simple *Vi* postulate is generally valid.

In this d-c case, the electric field and the potential of which it is the negative gradient can be calculated from the distribution of charges. If there are no dielectrics, the potential V is given by

$$V=\iiint\frac{\zeta}{r}dv \qquad (1)$$

where ζ is the charge density at any variable point, r is the distance from this variable point to the point at which the potential is being determined, and the integration is carried out through all space. If dielectrics are present, then a term must be added under the integral of equation 1, giving the effect of the polarization density in the dielectrics, but for simplicity we shall leave this out of the formula, as it can be readily supplied by those readers sufficiently trained to feel the lack of it. The electric field is then given by

$$E=-\text{grad } V \qquad (2)$$

In the case of an a-c system, however, the definition of the potential given at the beginning of this section fails. The work done in moving a unit charge from one point to another is no longer uniquely determined. The work done is different according to the path chosen in going from the one point to the other. For example, the work done in moving a charge from a point near the core of a transformer to a point diametrically opposite, by a path half way round the core,

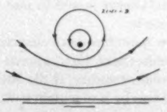

Figure 3 (left). Magnetic field around line

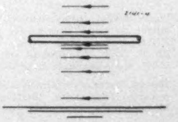

Figure 4 (right). Induced electric field E_2

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† For description of energy flow in usual electric-power machines, according to the Poynting vector postulate, see Slepian, *Electric Journal*, volume 16, July 1919, page 303.

will be different from the work done if the charge is moved from the one point to the other in the other direction round the core. If a suitable voltmeter is connected between the two points, the reading will be different according to whether the leads go round the core by the one path or the other.

We may however define a function calculated from the observable distribution of charges (and polarizations of dielectrics) by means of equation 1. We shall call this the Maxwell scalar potential,[‡] V_M, so that

$$V_M = \iiint \frac{\zeta}{r} dv \tag{3}$$

Of course, the electric field is not the negative gradient of this scalar potential

$$E \neq -\text{grad } V_M \tag{4}$$

In fact E is not the gradient of any potential.

We may regard the electric field E as made up of a superposition of two fields, E_1 and E_2,

$$E = E_1 + E_2 \tag{5}$$

where

$$E_1 = -\text{grad } V_M \tag{6}$$

E_1 may be said to be the field produced electrostatically by the charges defining V_M.

If a charge is moved around in a closed path, the work done against the component field E_1 is necessarily zero, since E_1 is the gradient of a potential. If there is a net work, not zero, done by the charge in going round the closed path in the total field E, it must be the work done against the component field E_2. Thus the integral of E_2 around a closed path is in general not zero. By Faraday's law, this integral, sometimes called the induced electromotive force, is equal to $1/10^8$ times the rate of change of the enclosed magnetic flux, the units of electric field and magnetic flux being the usual volts per centimeter and maxwells.

The lines of force of the field E_2 cannot terminate on any charge, since equations 3 and 6 make E_1 bring to or from all the charges all the corresponding lines of force, thus leaving no charges for the lines of E_2 to terminate upon. The lines of force of E_2 then must form closed loops, linking varying magnetic flux. E_2 may then be said to be the electric field induced by the varying magnetic field, and to have no electrostatic character.

Mathematically the defining properties of E_1 and E_2 are as follows:

$$\text{div } E_1 = -4\pi\zeta, \ \zeta \text{ in appropriate units} \tag{7}$$

$$\text{div } E_2 = 0 \tag{8}$$

$$\text{curl } E_1 = 0 \tag{9}$$

$$\text{curl } E_2 = -1/10^8 \frac{\partial B}{\partial t} \tag{10}$$

It appears that the potential used by electric-power engineers is precisely the Maxwell scalar potential which has just been described.

For example consider a single transmission line parallel to the surface of a good conducting earth, which is taken as having zero potential. The charges on the line by themselves, if they were unvarying, would produce the field E_1 with lines of force lying nearly completely in planes perpendicular to the line, as in Figures 1 and 2. The slight curvature in the lines of force of E_1, and their inclination to the line, shown in Figure 2 somewhat exaggerated, are due to the charge density on the line decreasing from left to right, due to the decreasing "potential" of the line.

The current in the line, produces the magnetic field shown in Figure 3, the lines of magnetic force lying in planes perpendicular to the line. If the current is increasing, the magnetic field also increases and induces an electric field E_2 whose lines of force are nearly parallel to the line as in Figure 4. This field is strongest at the line, where it is nearly equal, and opposite in direction to the component of the field E_1 which is parallel to the line. The parallel component of the net field, E, at the line is that required by the ohmic resistance of the line to the current it is carrying. The total field E is the sum of the electrostatic field E_1 and the induced field E_2.

To determine the potential at a point 1 in the line, the power engineer connects a voltmeter from that point to the ground immediately below. That is, he integrates the electric force along a path in the plane perpendicular to the line. Since such a path is perpendicular to E_2, he essentially integrates the field E_1 alone and, therefore, by equation 6, determines the Maxwell scalar potential, V_M, at the point 1 of the line.

To determine the potential at a second point 2 in the line, the engineer connects his voltmeter from the point 2 to the earth immediately below, and not to the earth point used for getting the potential at point 1. To find the potential difference between points 1 and 2, the engineer takes the difference between the potentials so determined. He does not connect a voltmeter from point 1 to point 2 with leads running parallel to and along the line. If he did, the field E_2 would contribute to the integral of E from 1 to 2 and he would get a result differing from that obtained by his usual procedure. He would say that this last procedure would give him only the resistance drop in the line, whereas his usual procedure gives him the total (resistance plus inductance) drop in the line. It is clear then that the power engineer uses the Maxwell scalar potential. Therefore, subsequently in this paper, the subscript M will be dropped from the V symbol for potential, it being understood that the Maxwell scalar potential is that which is meant.

III. The Corrected Vi Postulate

The appearance of the induced field E_2 is one of the features which distinguishes the variable-state or a-c system from the steady-state or d-c system. If E_2 is absent or negligibly small, then the usual energy-flow postulate,

$$P_1 = Vi \tag{11}$$

is generally valid, provided i includes displacement currents as well as conduction currents, as will be explained in the next section. If however, E_2 is not absent, equation 11 becomes invalid. Then the following energy-flow postulate,

$$P_2 = Vi + \frac{10}{4\pi}[E_2 \times H] \tag{12}$$

is generally valid. The bracket symbol in equation 12 indicates the vector product of E_2 and H, whose meaning will be explained later in examples. The two terms in P_2 may be called respectively the conductive and inductive components of the postulated energy flow.

IV. The Current Density i—Example of Variable-State System Where P_1 Is Valid

To see how the inclusion of displacement currents in i makes P_1 valid as long as E_2 is absent, consider the following example:

A sphere of radius R, far removed from other objects, is charged to potential V_o. It is then surrounded by a spherically symmetrical radial electric field, with $V = V_o R \frac{1}{r}$, and $E = V_o R \frac{1}{r^2}$ where r is the distance from the center. There will then be a stored energy density, $T_e = \frac{1.11 \cdot 10^{-12}}{8\pi} E^2 = \frac{1.11 \cdot 10^{-12}}{8\pi} V_o^2 R^2 \frac{1}{r^4}$ joules per cubic centimeter in the space around the sphere.

[‡] Physicists now more generally use the retarded scalar potential of Lorentz. Except for very long lines in power systems, or high frequencies, this potential will differ very slightly from the scalar potential of Maxwell, which is used here because of its simpler description.

Now let the sphere be joined by a fine high-resistance wire to the remote ground. The sphere slowly discharges by the conduction current through the wire. The small current flowing produces a magnetic field, but let us suppose it is so small that we may neglect the stored energy which is associated with it and also the small induced field E_2 which it produces by its slow change.

The stored energy in the electrostatic field slowly disappears, and a corresponding amount of heat appears in the wire. The valid energy-flow postulate, $P_1=Vi$ must show energy flowing from the space where it is initially stored to the wire where it appears as heat.

In the wire itself, let the total conduction current be I. At a point where the potential is V_a, the postulate P_1 asserts that there is an energy flow in the wire equal to V_aI watts away from the sphere. At a more remote point on the wire, where the potential is V_b, the energy flow in the wire is V_bI, watts away from the sphere. V_bI is less than V_aI, the difference being $(V_a-V_b)I$. But if ζ_{ab} is the resistance of the wire in ohms between points a and b, $V_a-V_b=\zeta_{ab}I$, and $(V_a-V_b)I=\zeta_{ab}I^2$ which is precisely the joulean heat developed in the wire between a and b. Thus the postulate $P_1=Vi$ shows energy flowing from the sphere down the wire where it appears as heat at the proper places and in the proper amounts.

But the valid postulate $P_1=Vi$ must also show how the energy stored in the electrostatic field arrives at the sphere so that it may be directed thence into the wire. The displacement current density comes to the rescue for this purpose.

Where an electric field E is varying, in free space, a displacement current density is defined as $i=\frac{1.11\cdot 10^{-12}}{4\pi}\frac{\partial E}{\partial t}$ amperes per square centimeter. In the present example E is decreasing so that there is a displacement current density flowing inwards towards the sphere given by $i=\frac{1.11\cdot 10^{-12}}{4\pi}R\frac{dV_o}{dt}\frac{1}{r^2}$. The total displacement current flowing inwards over the surface of a sphere of radius r_a will be $4\pi r_a^2i=1.11\cdot 10^{-12}R\frac{dV_o}{dt}$. The potential at r_a being $RV_o\frac{1}{r_a}$ volts, there is a total energy flow inwards across the surface, according to the Vi postulate, of $1.11\cdot 10^{-12}R^2V_o\frac{dV_o}{dt}\frac{dV_o}{dt}\frac{1}{r_a}$ watts. Across a more remote sphere, of radius r_b, the total energy flow inwards is $1.11\cdot 10^{-12}R^2V_o\times\frac{dV_o}{dt}\frac{1}{r_b}$. The flow inwards across the inner sphere exceeds the flow inwards across the outer sphere by $1.1\cdot 10^{-12}R^2V_o\frac{dV_o}{dt}\times\left(\frac{1}{r_a}-\frac{1}{r_b}\right)$ watts. This should be equal to the rate of decrease of the energy stored in the shell between the two spherical surfaces.

But integrating the stored energy density, $T_e=1.11\cdot 10^{-12}V_o^2R^2\frac{1}{r^4}$ over this spherical shell, we get $\frac{1.11\cdot 10^{-3}}{2}V_o^2R^2\times\left(\frac{1}{r_a}-\frac{1}{r_b}\right)$ joules. The rate of change of this energy, $1.11\cdot 10^{-12}V_o\frac{dV_o}{dt}R^2\left(\frac{1}{r_a}-\frac{1}{r_b}\right)$ checks exactly with the assertion of the Vi postulate in the preceding paragraph.

Figure 5 shows diagrammatically this flow of the stored energy to the sphere and thence to the wire according to the Vi postulate. For comparison, Figure 6 shows the energy flow according to the Poynting vector postulate.

V. Example of Failure of Simple Vi Postulate

Consider a long straight linear conductor of circular section and radius R. Let a current flow in it which is increasing at a constant rate, $I=I_ot$. Let the return circuit be the parallel-plane good conducting ground a considerable distance below the conductor.

There will be a magnetic field surrounding the conductor as in Figure 3, and this field will be increasing in strength at a constant rate. The stored energy associated with this field and distributed in the space will also be increasing. A valid energy-flow postulate must show a flow of energy into these regions where the stored energy density is increasing.

A potential drop (Maxwell scalar potential drop) will exist along the line, called by the engineer the inductive drop along the line. Neglecting the ohmic resistance of the line this potential drop will be constant in time. Associated with this distribution of potential on the line will be an electrostatic field E_1 as pictured in Figure 2. This field will be constant in time.

The steadily increasing magnetic field of Figure 3 will induce an electric field E_2 as pictured in Figure 4. This induced field E_2 is also constant in time.

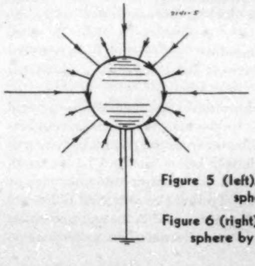
Let us apply the simple $P_1=Vi$ postulate to this case. It shows an energy flow in the line at each point of an amount VI watts, where V is the potential at the point. Everywhere else, other than in the line, the current density is zero. There is no displacement current in the space because the electric fields E_1 and E_2 are constant in time. The simple $P_1=Vi$ postulate then shows energy flowing in the line and nowhere else.

Consider a short length of the line. Because the potential at the entering end is larger, the energy flow entering the section is greater than that leaving the section. Hence, if the simple Vi postulate is valid, there should be appearing in the section an increasing amount of some form of energy, such as heat for example. But no such energy appears there! The simple Vi postulate fails!

Likewise, out in space the stored magnetic energy is increasing. The Vi postulate should show an energy flow bringing this stored energy to its proper position in space. It does not! It fails!

VI. Success of the Enlarged Postulate, $P_2=Vi+\frac{10}{4\pi}[E_2\times H]$, With Preceding Example

The bracket in the second or inductive term of the enlarged postulate, P_2, denotes the vector product of the induced electric field E_2 and the magnetic field H. This is a vector which stands perpendicular to both E_2 and H, with direction given by the right hand rule, that is, the direction of advance of a right-hand screw when



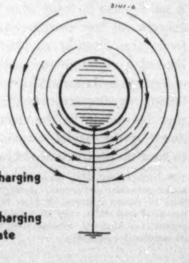


Figure 5 (left). Energy flow for discharging sphere by *Vi* postulate

Figure 6 (right). Energy flow for discharging sphere by Poynting vector postulate turned in the sense which brings E_2 most quickly into line with H. Its magnitude is equal to the product of the magnitudes of E_2 and H, respectively, and the sine of the angle between them.

Let the induced electric field E_2 have the value E_{20} at the conductor itself. The (Maxwell scalar) potential gradient along the conductor will have the same magnitude as E_{20}, since we are neglecting the ohmic resistance of the line, but will be oppositely directed.

Let the length of the portion of the line we are considering be l centimeters. The potential drop through this portion will then be $l|E_{20}|$, $|E_{20}|$ being the magnitude of the vector E_{20}. The first or conductive term of P_2 then will give an excess of energy entering the one end of the line portion over that leaving the other end by an amount equal to $l|E_{20}|I$ watts.

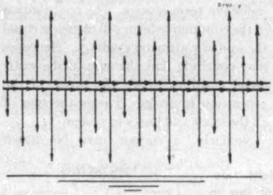


Figure 7. Energy flows about line carrying increasing current by P_2 postulate

At the surface of the line, the magnetic field H_o is perpendicular to the line and E_{20} and has the magnitude $|H|_o=\frac{2I}{10R}$. The second term of P_2 then shows an energy-flow density leaving the conductor surface of $\frac{10}{4\pi}|E_{20}|\frac{2I}{10R}=\frac{1}{2\pi}|E_{20}|\frac{I}{R}$ watts per square centimeter. Multiplying by the area of the surface of the line, $2\pi Rl$, we find the second term showing an energy flow out of the line of an amount $l|E_{20}|I$ watts. Comparing with the preceding paragraph we see that the enlarged postulate P_2 calls for the appearance in the line of heat or other energy of amount zero! In the line P_2 succeeds!

In the space outside the line, while the first term of P_2 shows zero energy flow, the second or inductive term shows energy flow radially outwards into space. It is not difficult to show that this radial flow is of just the proper amount to account for the increasing magnetic energy stored there. Figure 7 shows diagrammatically the energy flow about the line according to P_2.

P_2 succeeds everywhere! It must, since it has been shown in the paper of footnote 1 that it is equally valid with the Poynting vector postulate.

VII. Energy Flow in Transformer

It is instructive to trace the flow of energy in a transformer according to the simple and enlarged *Vi* postulates. The simple *Vi* postulate fails, of course. The enlarged postulate succeeds because of its inductive term.

In each turn of the primary of the transformer, there is a potential drop equal to the induced voltage per turn, V_t, if the resistance drop is neglected. According to the simple *Vi* postulate then, more energy flows into the turn than out by an amount V_tI_p watts, where I_p is the primary current. The simple *Vi* postulate then makes $V_tn_pI_p$ (n_p=number of primary turns) watts disappear in the primary, apparently causing energy to be annihilated.

Similarly, the simple *Vi* postulate calls for the apparent creation of energy in the secondary at the rate $V_tn_sI_s$ watts.

The second term of the enlarged *Vi* postulate avoids this apparent distressing destruction of energy in primary and creation of energy in secondary by providing an energy flow from primary to secondary.

Between the primary and secondary coils there will be a magnetic field perpendicular to the coil wires, Figure 8. This field will have magnitude approximately $|H|=\frac{4\pi}{10}\frac{n_pI_p}{l_1}$ where l_1 is the length of the coil. Between the coils there will also be an induced electric field E_2 with lines of force remaining parallel to the coil wires and perpendicular to H. The magnitude of E_2 will be approximately $|E_2|=\frac{V_t}{l_2}$ where l_2 is the perimeter of a turn.

According to the second term of P_2, then, there is an energy flow from primary to secondary of $\frac{10}{4\pi}[E_2\times H]=\frac{V_tn_pI_p}{l_1l_2}$ watts per square centimeter. Multiplying by l_1l_2, the total area carrying this energy-flow density, we get $V_tn_pI_p$ watts carried across from primary to secondary by the second term of P_2, which is just the energy brought into the primary by the first term *Vi*.

VIII. Relationship Between Poynting Vector Postulate and P_2

The Poynting vector postulate asserts the presence of an electric-energy-flow density given by

$$P=\frac{10}{4\pi}[E\times H] \text{ watts per cm}^2 \tag{13}$$

It differs from the enlarged *Vi* postulate, P_2 in making all the energy flow inductive, and none conductive.

This suggests that the resolution of the energy flow into a conductive and inductive component as in P_2 is largely arbitrary. This is true.

Let V' be any *arbitrary* scalar function of position in space. Let E_1' be a vector function of space defined by

$$E_1'=-\text{grad } V' \tag{14}$$

Let E_2' be defined by

$$E=E_1'+E_2' \tag{15}$$

Then it is readily shown that the energy flow postulate

$$P_2'=V_t'+\frac{10}{4\pi}[E_2'\times H] \tag{16}$$

is equally valid with P and P_2.

This bears out the statement made in the introduction that infinitely many valid energy-flow postulates may be devised.

The Poynting vector P, and the corrected *Vi* postulate P_2, are special cases of equation 16, obtained by respectively taking $V'=0$, and taking for V' the electric potential V.

IX. Validity of Simple Vi Postulate for Usual Proper Purposes on Power System

The power-system engineer postulates an energy flow in his lines given by connected wattmeter readings and with this postulate carries on his usual technical and commercial operations. This postulate is identical with the simple *Vi* postulate (except for the omission of displacement currents) which we have seen is not generally valid. The postulate can be made valid by the addition of the inductive term of P_2. Hence, since the engineer is successful in his usual operations, we conclude that he limits himself to those applications where the contribution of the inductive term of P_2 is negligible.

We have also seen that even the valid enlarged *Vi* postulate, P_2, is not uniquely valid. Hence, again since the engineer is successful in his usual operations, we conclude that he normally limits himself to those questions for which all valid postulated energy flows will give the same

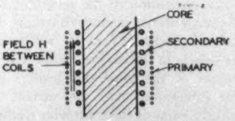


Figure 8. Magnetic field in transformer

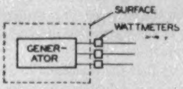


Figure 9 (left). Energy of generator by wattmeters

Correct connection

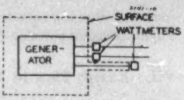


Figure 10. Energy of generators by wattmeters

Incorrect connection

answer. In referring back to the introduction, there is only one question to which all valid energy-flow postulates give the same answer, namely, the following: In a given region of space, in which there may be any and various kinds of electric apparatus, what is the excess of the total electric-energy generation over the sum of the total energy consumption and the increase in electric-energy storage?

Before giving examples, it is well to point out one use of wattmeter readings which is frequently made and which does not involve the energy-flow postulate at all. It is for determining the loading or heating of lines.

Now, the heating of a line certainly does not depend on any so intangible and intrinsically indeterminable a thing as a postulated energy flow. The heating is determined only and entirely by the current through it, and this is measured unequivocally by an ammeter. However, from the wattmeter reading the engineer, if he knows the usual voltage and power factor of the line, can make an estimate of what the current may be and thus estimate the loading or heating of his line. But in doing this the engineer is really using the wattmeter, together with his other knowledge of the line, as a kind of devious ammeter. Regardless of how he may colloquially refer to energy or power or the line, he knows that the heating of the line is determined by the current magnitude only.

As a first example, consider the determination of the energy generated by a generator. The engineer places wattmeters in all the leads coming from the generator, adds their readings algebraically, and asserts that number so obtained is the desired generation. He will be right if the inductive term of P_2 makes no contribution.

Surround the generator by a closed surface passing through the points in the leads where the engineer connected his wattmeters (Figure 9). The validity of P_2 ensures that, when integrated over this closed surface, it will give the net rate of energy generation in the generator since the energy stored remains constant. The conductive term, Vi, of P_2, will be zero everywhere in the surface except over the sections of the generator leads. This term integrated over the surface will equal the sums of the wattmeter readings. E_2 and H which enter into the inductive term, $\frac{10}{4\pi}[E_2\times H]$ of P_2, will each be negligibly small on the surface, except in the neighborhood of the leads. There, as explained in sections V and VI and shown in Figure 3, E_2 is parallel to the leads and therefore perpendicular to the surface. H however will lie parallel to the surface. Hence $\frac{10}{4\pi}[E_2\times H]$ lies entirely parallel to the surface and makes zero contribution to the total energy crossing the surface by the P_2 postulate. Hence P_2 integrated over the surface equals the sum of the wattmeter readings, and the engineer's usual use of wattmeters for determining the energy generation of a generator is justified.

It is clear from this discussion that the validity of the use of wattmeters alone in the preceding example depends on the points of attachment of the wattmeters to the leads lying in a surface which is perpendicular to the E_2 field. The engineer is aware of this. If one of the wattmeters is displaced along one of the leads from its proper position, as in Figure 10, the engineer knows that the sum of their readings will no longer equal the rate of energy generation in the generator. Ordinarily he will say that this is due to the inductive action of the leads, each on the other. This paper describes this situation by observing that the inductive term of P_2 in this case asserts a flow of energy not zero over the portion of the enclosing surface, which lies parallel to the leads.

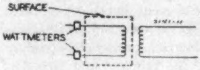


Figure 11 (left). Determination of loss in primary of a transformer

Another example: The engineer will not attempt to determine the losses in the primary coil of a transformer by means of wattmeters alone. In referring to Figure 11, showing a transformer diagrammatically, it is clear that for a surface which encloses the primary alone, there will necessarily be some portions across which the inductive term of P_2 will also assert there is an energy flow, as explained in section VII, and this energy flow must be added algebraically to the wattmeter readings to obtain the primary losses. However, the engineer will not hesitate to determine the total losses in a transformer alone, since in that case an enclosing surface may be drawn such that across it the inductive term of P_2 will assert zero energy flow.

A last example: Two power systems, A and B, operate with an interconnecting transmission line, in which wattmeters are properly placed. The engineer advises that the owners of these systems make payments to one another depending on the sum of the readings of these wattmeters. What is the physical basis on which the engineer makes his recommendation?

Referring to Figure 12, a surface may be drawn through the points of connection of the wattmeters and enclosing completely the system A, such that the contribution to the energy flow across the surface according to postulate P_2 of the inductive term is zero. The contribution of the conductive term will of course equal the sum of wattmeter readings. Hence we may conclude that the sum of the wattmeter readings equals the excess of the generation in system A over the sum of all the loads and losses in system A.

Similarly, a surface may be drawn through the wattmeter connections and enclosing the system B, which will justify the statement that this same sum of the wattmeter readings also equals the excess of the sum of the loads and losses over generation in system B.

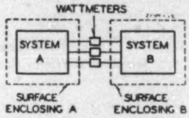


Figure 12. Power systems interconnected

Because of this numerical relationship between the wattmeter readings and the balance between generation, loads and losses on the two respective systems, the engineer makes his recommendation that the owners of system B compensate the owners of system A. The engineer may in addition make some irrelevant assertions concerning energy flow in the transmission line, while a physicist may equally irrelevantly speak of an energy flow from A to B through space.

The owners of the systems are indifferent to these reflections of engineer and physicist. Their esthetic tastes may perhaps be more like that of the engineer so that they may prefer an energy flow postulate P_2 which makes some of the energy (but not all) flow in wires, to one like P, where nearly all of the energy flows in space and quite remotely from the wires.

But what actually moves the owners of *A* to expect compensation is the physically observable fact that the generation in *A* exceeds the sum of the energies billed for at all the loads in *A* plus the losses in *A*. Likewise the owners of *B* are persuaded to make compensation by the physically observable fact that the total energy generated in *B* is less than the sum of the energies billed for at all the loads in *B* plus the losses in *B*. These same physically determinable facts as to location and magnitude of electric-energy generation and consumption, which basically are those alone in which owners of power systems are interested when engaged in "exchanges of energy," are also the only physically determinable facts on which the infinitely many valid energy-flow postulates will agree. The engineer is quite right in choosing one of them, namely $P_2 = Vi + \frac{10}{4\pi}[E_2 \times H]$, for determining the magnitude of the so-called "exchange of energy," and applying it under such a condition that the second term may be neglected, and determining the effect of the first term by wattmeter readings.

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Power-System Interconnection in Quebec

W. R. WAY
MEMBER AIEE

I. Introduction

THE main purpose of this paper is to describe the development of a major interconnected system located in the province of Quebec.

By means of a study, based on prewar data when stable conditions existed, and by suitable illustrations and explanations, it is aimed to show how it has been possible, through interconnection of three important hydroelectric systems, to increase the combined firm power capacity far beyond the sum of the capacities of the individual systems if operated independently. It will be brought out that the diversity in load characteristics and hydraulic conditions makes it feasible to accomplish this result and to effect other desirable economies on these systems, which are dependent solely on power supply from hydroelectric plants.

The possibilities of system consolidation had been under study for some years, but the war, with the urgent demand for more firm power, forced the issue to a conclusion, involving combined power resources of nearly 4,000,000 horsepower as indicated in Table I.

Reference will also be made to certain engineering features and operating practices of these systems, which may prove to be of interest.

II. Description of Geographical Areas

In leading up to a detailed analysis of the present interconnected system, it is thought desirable to describe briefly the areas under consideration.

The Montreal Area. This area is considered as including the territory in the immediate vicinity of Montreal Island on which is located the city of Montreal. Power is supplied by the Montreal Light, Heat, and Power Consolidated principally from two power developments on the St. Lawrence River. The plants of the Montreal Island Power Company and of the Canadian Light and Power Company also feed into this area.

Originally, in the year 1900, electric service was supplied by a number of small isolated power companies, not interconnected in any way and each serving its own independent district. In most cases standby steam plants were necessary to guard against the frequent interruptions to the supply from the hydroelectric plants because of ice trouble, breakdown, and other causes.

In 1902 the Montreal Light, Heat, and Power Consolidated was formed and acquired jurisdiction over most of the isolated systems in and near Montreal. It was soon realized that for reasons of economy, and to secure better service, some consolidation of systems or interconnection between plants was essential.

While some immediate constructive steps were taken in this direction, it was found that parallel operation was difficult or in some cases impossible because of the different frequencies, these being 66, 60, and 30 cycles. Furthermore, some systems were two-phase, while others were three-phase, and even direct-current. Eventually by compromising on 63 cycles, tying in through frequency changers, increasing the 30-cycle frequency to 31.5 cycles, and adopting three-phase alternating current, a common operating base was obtained, but this did not necessarily indicate parallel operation of the combined systems. Actually it was not until 1923 that all the subsystems of the Montreal area were operated completely in parallel.

The Shawinigan Area takes in the territory on both sides of the St. Lawrence River from Montreal to Murray Bay, some 80 miles below Quebec City, and is served mainly by the Shawinigan Water and Power Company, the Quebec Power Company, and the Southern Canada Power Company.

The Saguenay Area in the Lake St. John region is served mainly from plants on the Saguenay River by companies under the control of the Aluminum Company of Canada, Ltd.

III. Load, Output, and Hydraulic Characteristics of the Individual Systems

In the *Montreal Area* the St. Lawrence River is the chief source of power and has a flow which is unusually uniform, the average mean flow being 220,000 cubic feet per second, while the maximum and minimum recorded flows are 318,000 cubic feet per second and 173,000 cubic feet per second respectively. The heads involved are from 26 to 80 feet; comparatively long headrace canals are found, with the main difficulties in power output being caused by ice jams or other forms of ice trouble, occurring during the months of January and February. A typical hydrograph is shown in Figure 1.

The nature of the load in the Montreal area is that which might be expected in any large metropolitan area, with weekday load factor in the winter months of the order of 68 per cent and an annual load factor of 47.5 per cent during the prewar period. Maximum peak loads occur in the months of November or December, with a definite tapering off during the summer months. This is well

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W. R. Way is assistant general superintendent of the Shawinigan Water and Power Company, Montreal, Quebec, Canada.

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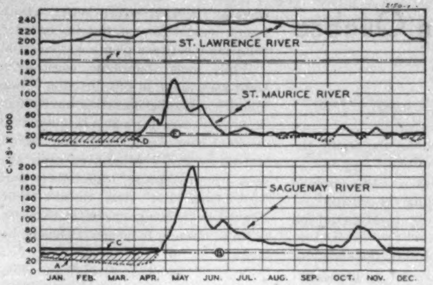

Figure 1. Hydrographs of St. Lawrence, St. Maurice, and Saguenay Rivers

A. Unregulated flow, Saguenay River

B. Regulated flow 35,000 cubic feet per second, 1942

C. Estimated regulated flow, 45,000 cubic feet per second 1943

D. Unregulated flow, St. Maurice River, 6,000 cubic feet per second, minimum

E. Regulated flow, 22,000 cubic feet per second, 1942

F. Utilization of St. Lawrence River flow, 1942—164,000 cubic feet per second

illustrated by comparing the typical daily load curves shown in Figure 2.

The chief need consequently is peak power for short periods during the winter months, while during the remainder of the year large and variable amounts of surplus energy and power are indicated, particularly during the months of April to November inclusive, with no immediate market available.

From the point of view of output, the gross primary kilowatt-hours sold in this entire area for 1941 were 3,341,902,000, while the corresponding maximum system peak load for primary power was 586,200 kw.

In March 1942, there was available 60-cycle capacity horsepower to the extent of 650,000 horsepower apart from that available from the Shawinigan Water and Power Company under the 160,000-horsepower contract.

In the Shawinigan Area the St. Maurice River is the most important source of power. It has a drainage area of 16,000 square miles and is suitably equipped for good regulation, having a number of storage reservoirs with a total storage of 13,000 square-mile-feet. This gives an indication of the extent to which the river is regulated and the possibilities of storing water for the combined system.

By using these storages, the regulated flow (Figure 1) can usually be maintained at 22,000 cubic feet per second, as compared with the unregulated minimum flow of 6,000 cubic feet per second. The maximum flow now experienced is of the order of 120,000 cubic feet per second which occurs early in May. As a result plants on this river have little difficulty in maintaining a high rate of output, excepting during the high tail-water period when in certain cases the plant output is markedly reduced. It so happens, however, that in the month of May the Montreal area is in a position to rectify this condition to a great extent, so that from the hydraulic standpoint, there is some diversity between the Montreal and Shawinigan areas.

From the load standpoint the Shawinigan system firm load is of an unusually high load factor, being of the order of 92 per cent on a week day, with an annual load factor under prewar conditions of 71 per cent. The type of firm load includes paper mills, aluminum production, carbide and carborundum furnaces, and asbestos mills, as well as a few steel plants.

Until recently, on account of the important paper-mill load which required the supply of quantities of process steam, large amounts (maximum peak over 500,000 horsepower) of surplus energy were supplied to electric boilers to replace coal-fired steam at the mills. This surplus was not only that obtainable from the Shawinigan area proper but also included secondary power purchased from neighboring companies.

Output characteristics of the Shawinigan system during the year 1941 included gross kilowatt-hour sales of 6,270,760,543 kilowatt-hours with a maximum 60-cycle firm peak of 868,000 kw. In March 1942 this area was served by 30-cycle and 60-cycle capacity to the amount of 1,154,500 horsepower aside from the 100,000-horsepower firm power contract with the Saguenay Power Company.

In the *Saguenay Area*, the Saguenay River is fed from a watershed area of 28,100 square miles. The hydrograph of this river (Figure 1) is somewhat similar to that of the St. Maurice River but is fortunately different as to the time of occurrence of the high water period, thus indicating advantages from interconnection with the St. Maurice River plants.

Prior to 1941 no storage reservoirs on this watershed had been constructed for regulating the flow of the Saguenay River, but Lake St. John itself (6,080 square-mile-feet usable storage) is utilized to the greatest practical extent from December to March inclusive.

Regarding load characteristics: before the war, power developed in the Saguenay area was utilized principally for the production of paper and aluminum and to supply the 100,000-horsepower firm power contract with the Shawinigan Water and Power Company. During periods of excess energy, the surplus was utilized for electric steam generation, the output for this purpose attaining at times the value of 375,000 horsepower. Since 1940, on account of the tremendous increase in demand for firm power, the electric boiler load has been discontinued.

The gross output of the plants in the Saguenay area during 1941 was 4,538,000,000 kilowatt-hours with a maximum firm peak of 639,000 kw. To serve this area, as in March 1942, there was capacity normally available to the extent of 1,000,000 horsepower.

IV. Possibilities of Maximum Utilization of Resources by Interconnection

In order to illustrate how the differences in the system characteristics of the three areas, could be utilized fully, a study has been made and a series of curves (Figure 3) prepared to show the peak (kilowatt) possibilities, while the kilowatt-hour problem is set out in Table II. The data have been based on *normal prewar conditions in 1939*, when interconnection was being considered.

The system in each area is analyzed graphically (Figure 3) to show its maximum available generator capacity (*G*),

Table I. Output Data of Plants Associated With Interconnection

	Total Rated Turbine Output (Horsepower)	Size of Largest Unit (Horsepower)	Number of Units
Montreal area supplied chiefly by Montreal Light, Heat, and Power Consolidated	968,000	53,000	45
Shawinigan area supplied chiefly by Shawinigan Water and Power Company	1,089,500	44,500	48
Saguenay or Lake St. John area supplied chiefly by Aluminum Company of Canada, Ltd	1,850,000	85,000	28
Miscellaneous Companies	80,000	6,000	20
Total	3,987,500 horsepower		

Maximum rated capacity operated in parallel (2,586,000 horsepower) 1,930,000 kw.

power available (P) as influenced by hydraulic conditions, and maximum firm load (L) which could be carried if operated as a completely independent system. To simplify the study, no allowance for reserve has been made to take care of load growth or frequency regulation.

From Figure 3A it may be noted that in the *Montreal area*, the comparatively steady power output from the St. Lawrence River plants having no storage facilities and the metropolitan load of low load factor are not an ideal combination from the point of view of matching available power facilities and load requirements. In the summer months, even at time of peak load, over 100,000 kw surplus output is indicated, while an additional 100,000 kw is available for about seven hours each day after midnight. This unfavorable situation results in surplus energy being available to an amount of over 800,000,000 kilowatt-hours per year (item 1, Table II; see also Figure 2).

In the *Shawinigan Area*, with its large storages and regulating possibilities on the St. Maurice River, it is possible to convert the entire available annual energy into firm load irrespective of daily or seasonal requirements (item 2, Table II). The curves (Figure 3B) also show that the maximum feasible firm load that may be carried at the normal 71 per cent yearly load factor is also just equal to the limit set by reduced system generator capacity during the high tailwater period.

Under these conditions, except for two months during the spring flood period, there is a minimum of about 60,000 kw reserve generator capacity. This reserve is admirably suited to assist in converting daily or seasonal surplus energy of adjacent areas into firm power by storing such surplus energy in the St. Maurice River storage reservoirs and returning it as firm power during the winter months to satisfy peak requirements. Additional generator capacity on the St. Maurice River would permit extension of this scheme.

In the *Saguenay Area*, using average runoff for the $4^1/_2$ winter months, the maximum possible firm load to be carried is dependent on the fixed storage capacity of Lake St. John. This results in a large amount of idle generator capacity during the winter months (Figure 3C) and large surplus summer energy (item 3, Table II).

The idle generator capacity during the winter months, combined with Lake St. John storage facilities, is useful in assisting in the conversion of surplus night energy in the Montreal area into firm power. Furthermore, part of the summer surplus energy may be stored in the reservoirs of the St. Maurice River and taken back during the winter months and thus be converted into firm power.

These curves indicate that if each of the systems in the three areas were to be operated independently:

(*a*). The firm loads which each system could carry successfully would be limited definitely to the values indicated by the load curves (see Figures 3A, 3B, 3C), on account of limiting local conditions.

(*b*). The equivalent energy (kilowatt-hours) consumed by such individual firm loads would be restricted to the values given in Table II.

Such isolated system operation would leave a fair portion of the combined total resources unused, or inefficiently used, as indicated by the following:

(*a*). 1.7 billion kilowatt-hours of idle energy (from a firm-power viewpoint) equal to over 18 per cent of the total energy available. This corresponds to 260,000 kw (74.6 per cent load factor) of unused potential firm power (Table II).

(*b*). Spare generator capacity and spare power could be made available to all areas at their critical periods during the year, because of the diversity in individual system power resources and load characteristics (Figure 3D).

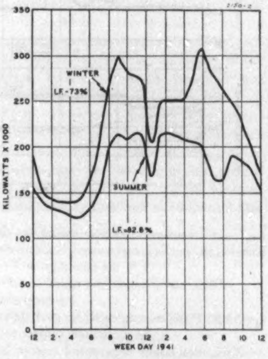

Figure 2. Typical summer and winter weekday (1941) firm load curves, Montreal area

Taken in combination with steady output of St. Lawrence River plants, the large amounts of surplus energy available for conversion into firm power are apparent

These unused resources illustrate what potential possibilities a flexible interconnection of the three systems would offer for complete utilization of combined resources.

While it is not represented that this study is complete, and without attempting to describe in detail any specific coordination scheme or outline its practical execution, which would be most difficult without knowledge of how and where the reclaimed firm power would be used, it is submitted that the study does give a measure of the benefits to be derived from interconnection and justifies the following conclusion:

> "By suitably interconnecting the three areas to permit free interchange of power, energy, and reserve generating facilities, by utilizing the Shawinigan facilities to store offpeak energy from neighboring areas, and by installing possibly 125,000 kw additional generating capacity on the St. Maurice River to permit returning the stored energy as firm power during the winter months, it is found possible to convert almost all of the surplus energy of the combined system into firm power.
>
> "Thus a total of 260,000 kw of additional firm power may be gained (Table II, item 5), based on the combined system firm load factor under normal conditions of 74.6 per cent. This is illustrated by the curves Figure 3E. The capital expenditure involved for additional generating capacity and suitable tie-line connections has been estimated to be approximately $38 per kilowatt of firm power reclaimed, as against $100 to $125 per kilowatt for new power developments."

Apart from the low cost and more efficient use of resources, it was realized that proper interconnection would be expected to result in better operating flexibility, increased service security, and a number of miscellaneous advantages, such as:

The actual situation in this respect in 1939 was:

(*a*). More complete utilization of surplus energy and less combined reserve generating capacity required.

(*b*). Better regulation of load, frequency, and voltage.

(*c*). Full advantage taken of diversity in load and hydraulic conditions.

(*d*). Better maintenance schedules of equipment possible.

(*e*). Deferment of individual system additions to generating plants.

V. Prewar Conditions

Although from many such studies and frequent discussions, the advantages of adequate system interconnection were well known, there was no economic justification for proceeding, during the prewar period, with the execution of any plan until the reclaimed power could be sold as firm power.

(*a*). Each system had large amounts of surplus power and energy available and was able to derive some revenue from the sale of such power to electric boilers, while firm load growth was proceeding normally.

(*b*). Since the Montreal and Shawinigan areas were interconnected in a limited manner, a portion of the surplus energy in Montreal area was utilized for electric-boiler load on the Shawinigan system.

(*c*). The Shawinigan and Saguenay areas were not interconnected, but some 30,000 kw to 50,000 kw were being interchanged, when desirable, by transferring blocks of load from one area to the other on the 60-kv system.

(*d*). Companies had to abide by commitments and contractual obligations in regard to new plant construction, and, therefore, there was no incentive to defer construction programs and to obtain prime power from neighboring companies by interconnection.

VI. Changed Conditions Caused by the War

With the advent of the war, there was an urgent and immediate demand for large amounts of additional firm power for defense industries. This abnormal demand was not confined to any one area but was most pronounced in the Saguenay and Shawinigan areas. As a result, some form of co-ordination involving proper system interconnection was immediately sought because of the attractive prospect of providing additional firm power in the shortest time at minimum cost.

Compared to the studies previously made on a prewar basis, war conditions modified the situation to some extent, and the actual trend for full development and utilization of total power resources, was along the following lines:

1. In the *Montreal area* there were immediately available some 825,000,000 kilowatt-hours of surplus energy annually which could be converted into prime power by utilizing Shawinigan storage. Such energy was available in the largest amounts during the summer months and during nights and week-

To utilize this energy necessitated increased back-feed from the 110-kv Montreal system and resulted in the installation (January 1942) of a 110-kv oil-filled underground cable seven miles long.[1] This eliminated the restriction or "bottleneck" in power flow through the 60-kv ring system and through the two 30,000-kva transformer banks in the city of Montreal and increased the transfer ability by over 100,000 kw.

2. In the *Shawinigan area* it was necessary to provide means for handling the increased feed-back from the Montreal area, as well as to store a portion of it, and transmit the remainder to the Saguenay area. This required certain changes on the system and included these items:

(*a*). Additional transformers and voltage regulating equipment were installed in certain districts to permit interchanging the full amount of power and to enable the reactive to be controlled.

(*b*). A number of additional storage projects were started on the St. Maurice River which, when completed, will provide additional capacity of 285 square-mile-feet equivalent to 16,000 horsepower years.

(*c*). To transmit the reclaimed firm power to the Saguenay area, a 220-kv transmission line was built and placed in operation December 1940. This line serves as a means of permanently interconnecting the Saguenay system with the Shawinigan and Montreal systems.

3. The *Saguenay Companies* decided on immediate industrial expansion and complete development and local utilization of power resources in the Saguenay area, as well as arranging for absorbing all available surplus power in the Shawinigan and Montreal areas. Physically, the program included:

(*a*). The construction of two storage reservoirs which, when completed, will provide the means of converting surplus summer energy in the Saguenay area into firm power. Storage capacities are estimated to be 2,460 and 5,360 square-mile-feet respectively and should permit an increase in the regulated flow of the Saguenay River from 32,000 to 45,000 cubic feet per second.

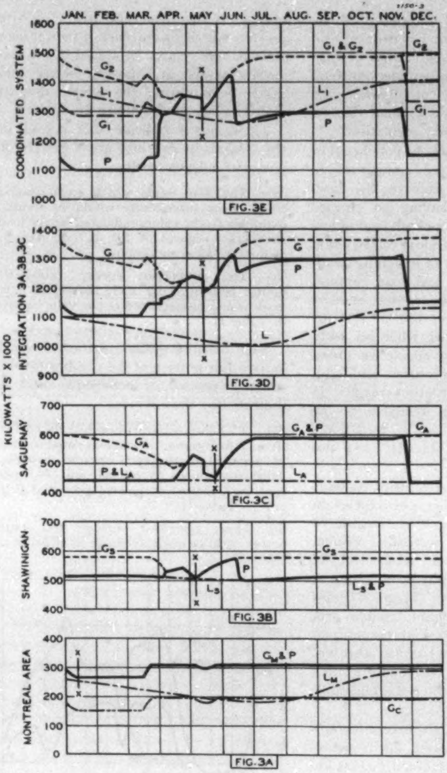

Figure 3. Curves to illustrate graphically the results of co-ordination study based on actual prewar system data

P = available firm power on a *daily* basis of at least normal system load factor. The variable shape of the curve is caused by water shortage or high tail-water conditions

G_M, G_S, G_A = maximum possible generator output at any instant on the individual Montreal, Shawinigan, and Saguenay systems respectively, as influenced by variable heads and flow conditions

G_C = actual Montreal area generator capacity, 1939, apart from Shawinigan contract

G = integration of G_M, G_S, and G_A, $G_2 = G + 125{,}000$-kw maximum system generator output with energy backing on a daily basis

$G_1 = G_2 - (G_A - P)$ = maximum system generator output with energy backing (normal-system firm load factor) from *seasonal* storage

L_M, L_S, L_A = maximum practical firm-power demand (at normal load factor) on the Montreal, Shawinigan, and Saguenay systems, respectively, and as dictated by maximum generator output at the critical point $X - X$ or shortage of water

These curves follow the typical or normal annual trend on the system concerned

L = integration of L_M, L_S, L_A

$L_1 = L + 260{,}000$ kw = estimated firm peak demand (at normal- system load factor) which could be successfully carried on the fully co-ordinated system

$X - X$ = Critical points of generator reserve [since the Montreal Light, Heat, and Power Consolidated contract supply from Shawinigan Water and Power Company is duplicated in the output figures of each company (Figures 3A and 3B) a correction in the form of a deduction of 120,000 kw, (160,000 horsepower) has been made in the integrated curves (Figures 3D and 3E) to obtain the true result]

Table II. Study of Kilowatt-Hour Resources and Load Requirements

Based on Normal Prewar Conditions 1939

	Maximum Feasible Firm Loading If Operated as Isolated Systems			Total Practical Energy Available Annually Kwhr×10⁶	Surplus Energy Available Annually Kwhr×10⁶
Item No.	Annual Peak Demand, Kw	Annual Load Factor (Per Cent)	Equivalent Firm Energy Kwhr×10⁶		
1. Montreal area	293,000	47.5	1,220	2,045	825
2. Shawinigan area	520,000	71.0	3,240	3,600	360
3. Saguenay area	440,000	93.0	3,580	4,100	520
Total	1,253,000	73.0	8,040	9,745	1,705
* Less Montreal Light, Heat, and Power Consolidated contract	120,000		620	620	
4. Integration of items 1, 2, and 3. Net =	1,133,000	74.6	7,420	9,125	1,705

5. Combined surplus energy, if properly utilized and if converted into firm power, would be equivalent to:

$$\frac{1,705,000,000}{8,760 \times 0.746} = 260,000 \text{ kw}$$

6. Percentage of total energy resources reclaimable as firm power $= \frac{1,705}{9,125} = 18.6$ per cent

* Since Montreal Light, Heat, and Power Consolidated contract supply from Shawinigan Water and Power Company is duplicated in the output of each company, its deduction is necessary to obtain a true result.

(*b*). Completion of a second double-circuit 154-kv line.

The result has been that with the provision of these facilities a rapid and fairly complete exploitation of all available power and energy resources in these areas has been accomplished with minimum outlay and in minimum time, providing as well additional generator reserve, improved operating flexibility, and service security.

As an illustration of the actual results obtained in combined system output, a typical daily firm-power peak load curve (Figure 4) for January 1942 has been plotted and shows how fully the resources are being utilized for firm-power purposes. It is to be noted that on the individual systems the margin of generator reserve is very limited, whereas on the combined system fairly large reserves are available excepting at time of maximum system peak.

VII. Analyzer Studies and Increased Fault Currents

Anticipating the tying up of the Saguenay and Shawinigan systems over the 220-kv tie line, a number of studies were made on an a-c network analyzer to determine among other matters:

(*a*). The magnitude of fault current at different locations.

(*b*). Possibilities of automatic reclosing and single-phase switching on the 154-kv and 220-kv lines.

(*c*). Degree of stability.

These studies indicated that the interconnection materially increased the magnitude of the fault currents at the interconnecting points.

In most cases, existing oil circuit breakers were capable of meeting the increased duty, but in certain localities it was found necessary to modify existing breaker designs in order to increase the rupture duty and to reduce operating times.

Where new circuit breakers were found to be necessary, there was a tendency to adopt the air-blast breaker for 69-kv, 132-kv, and 220-kv service. The type of air-blast breaker generally adopted was developed by engineers of the Montreal Light, Heat, and Power, Consolidated after several years of development work and field tests. However, also on the system may be found European designs of 11-kv, 154-kv, and 220-kv air-blast circuit breakers, as well as 69-kv and 138-kv "oil-poor" breakers. Circuit-breaker performance is considered satisfactory, although in some instances it has been necessary to modify designs slightly.

VIII. Stability and High-Voltage Automatic Reclosing

In the matter of automatic reclosing of high-voltage lines, actual experience gained over a period of eight years with an installation on two long 187-kv lines indicates that under many transient faults returning the faulted circuit to service promptly by automatic means has prevented the loss of the second line due to the systems falling out of synchronism.

Studies on the analyzer of the Shawinigan-Saguenay 220-kv tie line indicated that with a line-to-ground fault on the line cleared in five cycles, using conventional three-phase switching, and reclosing automatically in 20 cycles, the resulting swing curves showed every indication that stability would be maintained. Since the subsequent line loading is much higher than that used in the studies, attention is now being directed to complete a study of single-phase switching. In due course it is expected that some definite progress will be made in a practical application.

On the two 187-kv circuits, now operating at 154 kv, a few cases of instability have been experienced caused primarily by line short circuits in the Saguenay area and under unusually heavy load conditions. Such operations were not unexpected, and, considering the transmission constants and excessive loading, completely stable operation under all types of faults is not anticipated.

Continual efforts are being made to improve system protection so as to reduce time of fault clearance, with the object of improving stability.

IX. 220-Kv/165-Kv Transformation

In order to connect the 220-kv Shawinigan system to the Saguenay system (operating at 154 kv to 165 kv) and to permit a load of 150,000 kw to be carried it was necessary to consider carefully the transformation problem.

Originally, two obvious ideas were considered:

(*a*). The installation of three 50,000-kva conventional double-winding 220-kv/165-kv three-phase transformers with a limited-capacity tertiary winding.

(*b*). The installation of two 75,000-kva 220-kv/165-kv three-phase autotransformers.

In the case of the conventional-type transformers, it was found that 50,000

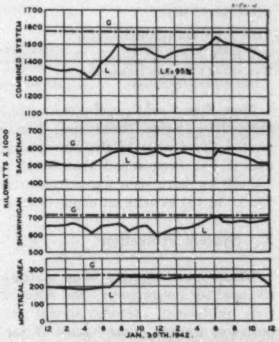

Figure 4. Actual daily load curve, interconnected systems, January 30, 1942, showing how available resources have been fully utilized for firm power purposes

kva was the largest size that could be handled from the point of view of transportation and servicing, and this would have required the purchase of three such units.

Regarding the use of autotransformers, because of their inherent lighter weight, it was possible to obtain from the manufacturer units of 75,000-kva rating so that only two transformers were required.

However, a third proposal was suggested, diagram of which is shown in Figure 5. This scheme was found to be practical by the manufacturer and was eventually used. The fundamental idea was to make use of the fact that there were already existing two 45,000-kva banks of 165-kv star to 66-kv delta. Since the problem was to transmit power from the 220-kv system to the 165-kv system, two three-phase "series" transformers were installed, each having a name-plate rating of 18,750 kva, 220 kv–55 kv/60 kv 196.8*A*, and supplied with a number of suitable voltage taps. The setup is really a scheme whereby use is made of the core structure of the existing 45,000-kva transformers in conjunction with the "series" transformers by electrically coupling at 60 kv, so as to simulate the effect of an autotransformer in the combined unit. By this means, the weight and rating of the additional transformer equipment may be considerably reduced without impairing the kilovolt-ampere transfer ability between 220 kv and 165 kv. Furthermore, the use of this idea results in a marked decrease in impedance and transformation losses between 220-kv, 165-kv, and 60-kv systems, as compared with the alternative schemes.

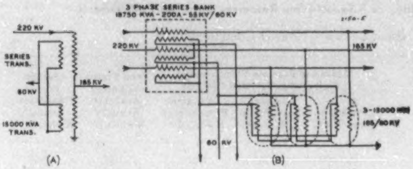

Figure 5. Diagram to illustrate method of interconnecting 220-kv system to 165-kv and 60-kv systems using series transformers

A. Development of one phase

B. 220-kv/165-kv/60-kv transformation

From the point of view of costs the percentages were:

Three 50,000-kva conventional-type three-phase transformers	100 per cent
Two 75,000-kva autotransformers	70 per cent
Two 18,750-kva series transformers (150,000 kva equivalent)	57 per cent

More than a year's operation on these three-phase series banks has demonstrated the success of the arrangement and it has been found that loads of 190,000 kw have been transmitted from 220 kv to 165 kv without undue heating.

X. System Protection

When interconnecting large systems, the need for instantaneous and selective zone protection assumes special importance, to reduce the hazard of permanent power arc damage and system instability, since in many instances troubles pyramid.

Fortunately, on this high-voltage network and particularly on the Shawinigan system, the matter of such relay protection had been given unusual attention. The double step impedance principle, with its features of instantaneous fault clearance to within 80 per cent to 90 per cent of the line length, was conceived, developed, and applied as early as 1920 for the clearance of phase-to-phase and ground faults. General application of the instantaneous differential-current zone protection, to generators, transformers, and station bus bars quickly followed.

With the adoption of high-voltage network distribution came the problem of instantaneous protection for the short tie lines. This led to the development and successful application of a number of differential-current pilot-wire schemes, using a separate control cable for the pilot wires. Subsequently, the directional impedance interlock principle was developed, with available communication circuits being used as a control channel.

Recently commercial carrier-current relaying, combined with a simplex communication, has been used in conjunction with some important high-voltage trunk lines to provide selective instantaneous clearance.

For transformer and regulator protection, in addition to differential schemes and supplemented by residual current features, gas-detector relays are being used. The pressure element is arranged to trip the associated circuit breakers, while the gas element operates an alarm.

For standby or backup protection, the impedance principle has been adopted almost exclusively since its initial development because of its effectiveness under all operating conditions and irrespective of the amount of system generator capacity connected. This is in contrast to the shortcomings, in these respects, of the inverse definite time overload protection previously used.

At controlling points, automatic oscillographs and high-speed recording voltmeters are utilized, as well as transmission-line fault locators, to ensure sufficient available data so that each case of trouble may be fully investigated and proper remedial measures applied.

To ensure proper over-all functioning of the protective schemes, the practice of making "primary" checks and tests on new protection installations is standard.

Concluding this section, it is apparent that without the progress and development just outlined in regard to protective schemes, interconnection of systems would have been a hazardous venture with added service difficulties, rather than the increased service reliability actually obtained.

XI. Basic System-Operating Principles

The general principles underlying the operation of the combined systems may be summed up as follows:

(*a*). To assure satisfactory parallel operation, each company undertakes as far as possible and with a practical spirit of co-operation to maintain load, voltage, and frequency conditions which are most suitable to the common interest, keeping in mind, of course, local problems and obligations to customers.

(*b*). Firm power contracts existing prior to interconnection remain in full force and effect.

(*c*). Operating agreements have been made to cover the supply or purchase of surplus energy.

(*d*). It is the objective of all companies concerned to ensure maximum utilization of all available power resources to assist in the war effort. Equitable energy rates have been agreed upon to make this result possible.

So far, any unusual operating matters which have arisen and which were not covered by agreement, have been promptly and satisfactorily rectified by the operating managements of the respective companies.

XII. Control of Voltage and Reactive

Constant voltage is required at the load in the Saguenay and Montreal areas and on the 60-kv busses. In addition, however, when the following items have been considered, it will be realized that a definite reactive control problem exists:

(*a*). Between the Montreal area and the Shawinigan area, involving the four 110-kv circuits, the load may have to vary between 120,000 kw *delivered to* Montreal and 130 kw *fed back from* Montreal.

(*b*). The load transmitted over the upper St. Maurice 220-kv line varies from 100,000 kw to 250,000 kw and is delivered at the receiving point at approximately 100 per cent power factor, so that most of the reactive kilovolt-amperes of the two generating plants feeding this line is used in transmission and is not available therefore for the load.

(*c*). The load transmitted from Quebec to Lake St. John in the Saguenay area may vary between 25,000 kw and 150,000 kw, and at Lake St. John it is frequently necessary to supply 50,000 kva; that is, the power is received at a leading power factor.

(*d*). System load reductions of the order of 45 per cent occur each Sunday when mills shut down.

(*e*). Only one synchronous condenser (30,000-kva capacity) is available on the system installed at Quebec.

This whole matter of voltage and reactive control was given attention in the a-c network-analyzer studies, and as a result, it was decided to install "on-load" tap-changing gear on all transformers in the Shawinigan area where a tie exists between the 220-kv system and the 110-kv Montreal system, as well as on a customer's 110-kv bank.

So far, with some extra supervision required, use of these facilities has enabled voltage levels to be maintained by proper generator field manipulation, as well as by occasional use of vented hydroelectric units as synchronous condensers, and by changing taps at controlling points when dictated by marked changes in loading conditions.

XIII. Frequency Regulation, Load Control, and Reserve Generation

Normally, generating stations in the Montreal area are operated on base load, with frequency controlled by either Shawinigan or Saguenay systems. Automatic frequency control is available and is used on two generating stations in the Shawinigan area. With manual control no difficulty has been experienced in holding frequency to plus or minus 0.1 cycle and synchronous time to a maximum error of ten seconds. This may not be as close regulation as many other systems obtain but is found to be quite satisfactory in this case. Manual control of frequency has been made more convenient to the operator by bringing the governor motor control to miniature keys on the operator's desk.

Automatic tie-line load control is not used and is not considered essential at the present time, although it would be a definite operating convenience. The long distances between exchange points and generating stations make the application of automatic tie-line control both difficult and expensive.

At the generating station on frequency control, it has been the custom in the past to try to maintain a minimum amount of spare equivalent to a 30,000-kw unit during the peak period, and to retain in service at other generating stations a sufficient number of units to carry the allotted load efficiently, consistent with flow and head conditions, so that when the need arose, an additional 30,000 kw–50,000 kw could be picked up by running wide open temporarily. However, present practice is to utilize, as far as possible, most of the normal generator reserve for supplying urgent defense loads. This results occasionally in the necessity of dropping blocks of electric furnace load or grinder motors as a temporary means of maintaining suitable frequency.

XIV. Summary and Conclusions

1. The interconnected system described and supplied solely from hydroelectric plants must be considered as one of the major power systems on the continent, having resources of nearly 4,000,000 horsepower (developed or under construction), of which 2,586,000 horsepower are now operating in parallel.

2. It is to be noted that while operating as individual systems or areas, the hydraulic power resources and load requirements of each group were not ideally matched, it was found that when viewed as a combined system, these characteristics were quite complementary, thus enabling much more complete firm-power utilization of total energy resources.

3. Under normal prewar conditions, undoubtedly this interconnection of systems would have been delayed in its execution, but because of the immediate demand for more firm power for defense industries, it became imperative and economically feasible to proceed, therefore in effect making available for firm power over 18 per cent of the total combined energy resources which otherwise would have been wasted or sold as secondary power to electric boilers. This estimated gain in firm power output is apart from that obtained from additional sources as given in section VI.

4. Because of the necessity of producing and utilizing every available kilowatt-hours for defense purposes, operating procedure has changed, resulting in larger ranges in frequency and voltage regulation and less generator reserve.

Control of reactive kilovolt-amperes is successfully accomplished mainly by generator field adjustment and use of transformer tap-changing equipment, with the assistance of a single 30,000-kva synchronous condenser.

Reference

1. Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada, **D. M. Farnham, O. W. Titus.** AIEE Transactions, volume 61, 1942, December section, pages 881–8.

Three-Winding Transformer Ring-Bus Characteristics

G. W. BILLS
ASSOCIATE AIEE

C. A. MacARTHUR
NONMEMBER AIEE

SUCH hydroelectric projects as the Boulder Dam, Grand Coulee, and that proposed for the St. Lawrence River present serious power limit and short-circuit problems. Each of these developments will be capable of producing over 1,000,000 kw, part of which for each case must be transmitted over 200 miles. Any method which will increase transient stability and lower fault currents should be carefully investigated; this paper presents the investigation of such a method.

Statement of Problem

To obtain optimum use of transmission lines between a generating station and a load center, it is desirable to parallel the circuits in such a manner that the loss of a line or section of a line will result in the least disturbance. In general, this is done by bussing the lines together and sectionalizing at one or more points.

However, to bus lines on the sending and receiving ends imposes very severe requirements on switching equipment. For example, the 287.5-kv Boulder Dam–Los Angeles lines require 287.5-kv breakers with interrupting capacities of 2,500,000 and 3,500,000 kva. Even on 230-kv systems circuit breakers with interrupting capacities of 2,500,000 kva are in use.

High-voltage bus reactors may be used to reduce short-circuit currents, but besides being costly, they tend materially to increase the reactance between the sending and receiving ends of the system after the loss of line or line section. It is probable, even though the fault currents on the circuits are reduced by the addition of reactors to the bus, that the dynamic power limit would be decreased because of the added impedance.

The use of reactors in the transformer neutrals to reduce ground-fault currents may interfere with relaying and imposes higher-voltage stresses on equipment. Also, for three-phase faults neutral reactors have no effect on fault currents and thus do not increase the power limit for such disturbances.

Description of Studies

Three-winding transformers connected so as to form a ring on the low-voltage side, as shown in Figure 1, lower short-circuit currents by making use of the reactance between low-voltage windings. To determine the transient power limits and fault currents of a system using this ring-bus arrangement, studies were made on an a-c network analyzer. For comparison similar studies were made on the same system with a standard high-voltage bus, as shown in Figure 2.

The studies prove that the transient stability limits are higher and the fault currents are lower for the three-winding transformer ring-bus system. A very interesting result was that the transient stability limits for the above system are almost independent of the reactance between low-voltage windings as it varies from zero to 40 per cent.

The basic system for the studies consisted of four 230-kv transmission lines 185 miles in length connecting a hydroelectric plant to a load center. The load center was connected through a short line to an infinite bus. The generating plant consisted of eight identical 60-cycle machines, each of 55,000-kva capacity. Each generator had a direct-axis transient reactance of 44 per cent and a WR^2 of 132,500,000 pound-feet squared.

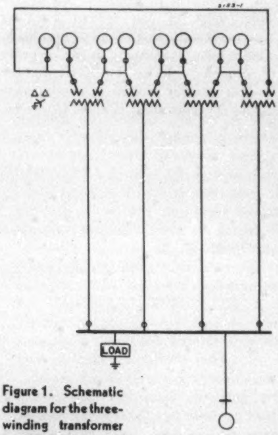

Figure 1. Schematic diagram for the three-winding transformer bus system

For the three-winding transformer ring bus the transformer banks were rated 110,000/110,000/150,000 kva at 13.8/13.8/230 kv and connected delta, delta, wye-grounded. With the low-voltage windings paralleled, the through reactance of the transformers was 11 per cent on a 150,000-kva base.

For the standard bussing arrangement the transformer banks were rated at 110,000 kva, 13.8/230 kv and connected delta, wye-grounded. The reactance was 11 per cent on a 110,000-kva base. Transformer banks rated at 110,000 kva but having a reactance of 11 per cent on a 150,000-kva base were also used.

The various sytems were placed on an a-c network analyzer and studies made of short-circuit and transient-stability characteristics. Faults were applied on a generator bus and also on the sending end of a transmission line. For the three-winding transformer ring bus the reactance between the low-voltage windings was varied from zero to 40 per cent.

The results of the short-circuit studies, shown in Figure 3, indicate that the severity of three-phase short circuits on the generator bus of the transformer ring-bus system can be made as low as that of the standard bus system. This is accomplished by using low-voltage windings having a reactance between them of at least 42 per cent.

By not bussing the transformers on the high-voltage side, the severity of faults on the transmission lines near the generating station are very greatly reduced. This materially reduces system disturbances resulting from the line faults.

Transient-stability limits for the two standard bus systems were found for both three-phase and two-phase faults on the lines near the sending end. For these and all stability studies a total clearing time of $4^1/_2$ cycles was assumed.

Similar studies were made on the three-winding transformer bus system as the reactance between the low-voltage windings was varied from zero to 40 per cent.

The power-limit values from Figure 4 show that the standard systems are much less stable in all cases. For example, the best standard system carries at least 36,000 kw less than can the transformer ring-bus system.

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G. W. BILLS and C. A. MACARTHUR are both with the Bonneville Power Administration, Portland, Oreg., as assistant engineers in the system engineering department.

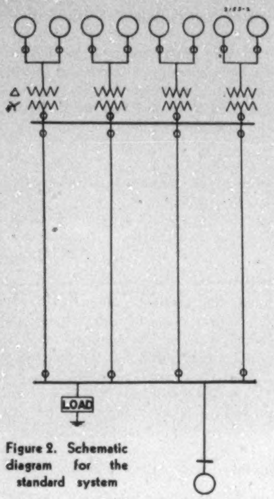

Figure 2. Schematic diagram for the standard system

It should be kept in mind that the assumed clearing time is almost the minimum possible for 230-kv systems. Longer clearing times would increase the difference in stability limits between the systems and so favor the ring-bus system.

The rise in power limits for the ring-bus system as the transformer reactance increases from zero to 15 per cent is explained by the decreasing severity of the faults. At 15 per cent reactance the effect of the bus reactance in decreasing synchronizing power between the generators becomes larger than that due to decreased fault currents, and the power limits tend to drop when the reactance is increased. However, the variation in power limit is small, being less than two per cent over the entire range; thus higher reactances between low-voltage windings may be used, resulting in greatly reduced currents for generator bus faults.

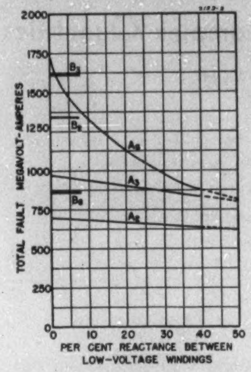

Figure 3. Short-circuit characteristics

Curves *A* for the three-winding transformer ring-bus system as follows:

A_G—Three-phase fault on a generator bus

A_3—Three-phase fault on transmission line near sending end

A_2—Double-line-to-ground fault on a transmission line near sending end

Curves *B* for the standard bus system with 11 per cent transformers on a 110,000-kva base as follows:

B_G—Three-phase fault on a generator bus

B_3—Three-phase fault on a transmission line near sending end

B_2—Double-line-to-ground fault on a transmission line near sending end

Conclusions

While a detailed economic study is beyond the scope of this paper, it may be mentioned that three-winding transformers with the ranges of reactance used in this study may be obtained for the same price as standard three-winding transformers. The increase in cost of the three-winding transformers over the smaller capacity two-winding transformers is partially offset by the elimination of high-voltage breakers.

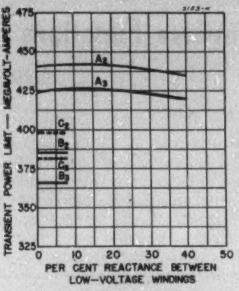

Figure 4. Transient-stability limits

Curves *A* for the three-winding transformer ring-bus system as follows:

A_3—Three-phase fault on transmission line near sending end

A_2—Double-line-to-ground fault on transmission line near sending end

Curves *B* for the standard system with 11 per cent transformers on a 110,000-kva base as follows:

B_3—Three-phase fault on transmission line near sending end

B_2—Double-line-to-ground fault on a transmission line near sending end

Curves C for the standard system with 11 per cent transformers on a 150,000-kva base

C_3—Three-phase fault on a transmission line near sending end

C_2—Double-line-to-ground fault on a transmission line near sending end

However, on transient stability and short-circuit considerations the three-winding transformer ring bus offers very definite advantages for systems of large magnitudes.

Inverse Functions of Complex Quantities

HERBERT B. DWIGHT
FELLOW AIEE

FORMULAS for inverse functions of complex quantities, such as $\sin^{-1}(x+iy)$, are of use in several branches of electrical engineering. Calculations for transmission circuits require them, particularly in connection with communication circuits. Integration of expressions involving complex quantities can involve inverse functions. They are encountered also in conformal transformations.

In presenting the formulas in this paper, it is necessary to specify angles with sufficient detail to avoid ambiguity and the liability of incorrect results. Also, all of the appropriate multiple values should be included. It is not always obvious by inspection that two different complex values are only values of different branches of the same function and are both correct.

In all cases, numerical examples are given as illustrations.

Inverse Sine $\sin^{-1}(x \pm iy)$

Let

$$\sin^{-1}(x+iy)=u+iv$$

where x, y, u, and v are real quantities and $i=\sqrt{-1}$.

$$\sin(u+iv)=(x+iy)$$

$$\sin u \cosh v = x \qquad (1)$$

$$\cos u \sinh v = y \qquad (2)$$

Squaring equations 1 and 2 and putting

$$\sin^2 u = 1-\cos^2 u$$

and

$$\cosh^2 v = 1+\sinh^2 v$$

there is obtained, by eliminating $\sinh^2 v$ $\left(\text{put } \sinh^2 v = \dfrac{y^2}{\cos^2 u}\right)$

$$\cos^4 u-(1-x^2-y^2)\cos^2 u-y^2=0 \qquad (3)$$

from which

$$\cos^2 u = {}^1/_2[1-x^2-y^2 \pm \sqrt{(1-x^2-y^2)^2+4y^2}] \qquad (4)$$

Since u is real and $\cos^2 u$ is positive, the positive value of the root is taken.

The quantity under the radical sign may be factored, and is

$$\{(1+x)^2+y^2\}\{(1-x)^2+y^2\} \qquad (5)$$

From equations 4 and 5

$$\sin^2 u = {}^1/_2[1+x^2+y^2- \sqrt{\{(1+x)^2+y^2\}\{(1-x)^2+y^2\}}]$$
$$= {}^1/_4[\sqrt{(1+x)^2+y^2}-\sqrt{(1-x)^2+y^2}]^2$$

$$\sin u = \pm\left(\frac{p-q}{2}\right)=\frac{\pm 2x}{p+q} \qquad (6)$$

where $p=\sqrt{(1+x)^2+y^2}$ (positive value) (7)

$q=\sqrt{(1-x)^2+y^2}$ (positive value) (8)

The second solution in equation 6 is obtained by rationalizing the numerator. It involves the sum of two quantities instead of the difference and so allows more convenient precise computation.

Since v is real, $\cosh v$ is positive, as may be seen from the series expansion. Therefore, by equation 1, $\sin u$ is the same sign as x. This allows $\pm$ in equation 6 to be changed to +. Therefore

$$u=\sin^{-1}\frac{2x}{p+q} \qquad (9)$$

Take the principal value of u, that is, the value between $-\pi/2$ and $\pi/2$. Since u lies in this range, $\cos u$ is positive.

The following expression corresponding to equation 3 can be obtained by eliminating $\cos^2 u$

$$\sinh^4 v+(1-x^2-y^2)\sinh^2 v-y^2=0$$

$$\sinh^2 v = {}^1/_2[x^2+y^2-1 \pm \sqrt{(1-x^2-y^2)^2+4y^2}]$$

The quantity under the radical sign is the same as equation 5 so that

$$\cosh v = \pm {}^1/_2(p+q)$$

Since v is real, the positive value is to be taken.

$$v=\cosh^{-1}\frac{p+q}{2} \qquad (10)$$

Since $\cos u$ is positive, then by equation 2, $\sinh v$ and v are the same sign as y. This may be secured by writing

$$\sin^{-1}(x \pm iy) = \sin^{-1}\frac{2x}{p+q} \pm i \cosh^{-1}\frac{p+q}{2}$$

where y is positive and the positive value of $\cosh^{-1}$ is taken (see reference 1, page 58, and reference 2, page 264).

For any angle θ, there are angles $2k\pi+\theta$ which are the same as θ in all respects, and there are angles $(2k+1)\pi-\theta$ which have the same sine, where k is a positive or negative integer or 0. These can be combined by stating that angles having the same sine as θ are

$$n\pi+(-1)^n\theta \qquad (11)$$

where n is an integer or zero. Therefore

$$\sin^{-1}(x \pm iy) = n\pi+(-1)^n \sin^{-1}\frac{2x}{p+q} \pm i(-1)^n \cosh^{-1}\frac{p+q}{2} \qquad (12)$$

taking the principal value of $\sin^{-1}$ and the positive values of $\cosh^{-1}$ and of the radicals p and q. The quantity x may be positive or negative, but the quantity y is positive.

Note that if $y=0$ and $x>1$, $q=x-1$ and $p+q=2x$. If $y=0$ and $x<1$, $q=1-x$ and $p+q=2$.

An alternative computation may be made by means of the well-known formula

$$\sinh^{-1} m = \operatorname{logn}(m+\sqrt{m^2+1}) \qquad (13)$$

where logn denotes natural logarithm. Let

$$\sin^{-1} A = u$$

where A and u are complex quantities

$$\sin u = A$$
$$iA = \sinh iu$$
$$iu = \sinh^{-1} iA$$
$$\sin^{-1} A = -i \sinh^{-1} iA+2k\pi$$
$$= -i \operatorname{logn}(\pm\sqrt{1-A^2}+iA)+2k\pi \qquad (14a)$$
$$\text{or } = i \operatorname{logn}(\pm\sqrt{1-A^2}-iA)+2k\pi \qquad (14b)$$

The two solutions of equation 14a indicated by $\pm$ correspond to the two angles θ and $\pi-\theta$ which have the same sine. The second or alternative form, equation 14b, is identical with equation 14a. The expression involving + in equation 14b is obtained from that involving + in equation 14a by rationalizing the numerator in equation 14a. In practice, the form should be used which involves the numerical sum of quantities instead of the difference, thus giving more convenient precise computation.

Square Root $\sqrt{(x \pm iy)}$

In the computation just described, the square root of a complex quantity is required. It may be expressed as follows:

$$\sqrt{(x+iy)} = \pm\left[\sqrt{\left(\frac{m+x}{2}\right)}+i\sqrt{\left(\frac{m-x}{2}\right)}\right] \qquad (15)$$

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HERBERT B. DWIGHT is professor of electrical machinery, Massachusetts Institute of Technology, Cambridge, Mass.

$$\sqrt{(x-iy)}=\pm\left[\sqrt{\left(\frac{m+x}{2}\right)}-i\sqrt{\left(\frac{m-x}{2}\right)}\right] \qquad (16)$$

where x may be positive or negative, y is positive
and

$$m=+\sqrt{(x^2+y^2)} \qquad (17)$$

The positive square roots of $(m+x)/2$ and $(m-x)/2$ are used (see reference 2, page 260).

An alternative method is to express the complex quantity in the polar form

$$r\angle\theta=re^{i\theta}=r(\cos\theta+i\sin\theta) \qquad (18)$$

where, if the complex quantity is $x+iy$

$$r=+\sqrt{(x^2+y^2)},\ \cos\theta=\frac{x}{r}\ \text{and}\ \sin\theta=\frac{y}{r} \qquad (19)$$

Then

$$\sqrt{(x+iy)}=\pm\sqrt{r}\,\underline{/\theta/2}$$
$$=\pm\sqrt{r}\ \cos\frac{\theta}{2}+i\sin\frac{\theta}{2}\Big) \qquad (20)$$

The angle θ may be in any one of the four quadrants, depending on whether x and y are positive or negative quantities. The angle is not specified according to the principal values of $\tan^{-1}$ and $\cos^{-1}$, and so forth, though the numerical value of θ may be conveniently found by using a table of tan or $\tan^{-1}$ and then determining the quadrant for θ by equation 19.

Logarithm logn (x+iy)

In using equation 14, the logarithm of a complex quantity is required. This is computed as an inverse function of e^z.

Let

$$\text{logn}\ (x+iy)=u+iv$$
$$e^{u+iv}=e^u\ (\cos v+i\sin v)$$
$$=x+iy$$

$$e^u\cos v=x \qquad (21)$$

$$e^u\sin v=y \qquad (22)$$

Squaring and adding

$$e^{2u}=(x^2+y^2)$$

Let

$$r=\sqrt{x^2+y^2}$$

The positive value of the root is to be taken since u is real and e^u is positive.

The angle v is to be specified with sufficient completeness so that the numerical values of cos v and sin v will have the correct signs.

$$\text{logn}\ (x+iy)={}^1/_2\ \text{logn}\ (x^2+y^2)+i(\theta+2\pi k) \qquad (23)$$

where $\cos\theta=x/r$, $\sin\theta=y/r$, $r=\sqrt{(x^2+y^2)}$ and k is an integer or 0, the positive value of r being taken. The quantities x and y may be positive or negative (see reference 7, page 3).

The angle θ, according to this specification, is not always a principal value of $\cos^{-1}$, $\sin^{-1}$, or $\tan^{-1}$. If both x and y are negative, tan θ is positive and the angle θ is in the third quadrant.

Another case where

$$\theta=\tan^{-1}\frac{m}{n}$$

is not a sufficient specification is in the equation

$$m\cos A+n\sin A=r\sin(A+\theta) \qquad (24)$$

where $r=\sqrt{m^2+n^2}$, $\sin\theta=m/r$ and $\cos\theta=n/r$
(See reference 5, number 401.2).

Example 1. $\sin^{-1}(2+i3)$

$$p=\sqrt{(9+9)}=4.243$$
$$q=\sqrt{(1+9)}=\underline{3.162}$$
$$p+q=7.405$$

$$\sin^{-1}\frac{4}{7.405}=\sin^{-1}0.540=0.570\ \text{radian}$$

$$\cosh^{-1}\frac{7.405}{2}=1.983$$

Putting $n=0$ in equation 12,

$$\sin^{-1}(2+i3)=0.570+i1.983$$

Putting $n=1$

$$\sin^{-1}(2+i3)=3.142-0.570-i\ 1.983$$
$$=2.572-i\ 1.983$$

In this computation, tables of $\sin^{-1}x$, $\cosh^{-1}x$, and so forth, as in reference 6, for real values of x, may be used. To check,

$$\sin(0.570+i\ 1.983)=0.540\times3.702+i\ 0.842\times3.563$$
$$=2.00+i\ 3.00$$

and

$$\sin(2.572-i1.983)=0.540\times3.702-i(-0.842\times3.563)$$
$$=2.00+i\ 3.00$$

If the only purpose of the real part of the value of $\sin^{-1}(2+i3)$ is to take the sin, cos, or tan, or to add it to other angles which are given in degrees, then a trigonometric table in degrees might be used, but care would be needed in choosing an appropriate notation.

In using equation 14 to obtain $\sin^{-1}(2+i3)=\sin^{-1}A$

$$1-A^2=6-i\ 12$$
$$=x-iy \qquad \text{as in equation 16}$$
$$m=\sqrt{(36+144)}=13.42\ \text{by equation 17}$$
$$m+x=19.42,\ m-x=7.42$$
$$\sqrt{(1-A^2)}=\sqrt{\left(\frac{19.42}{2}\right)}-i\sqrt{\left(\frac{7.42}{2}\right)}$$

by equation 16, using the + sign

$$=3.116-i\ 1.926$$
$$-iA=\underline{3\qquad -i\ 2}$$
$$\sqrt{(1-A^2)}-iA=6.116-i\ 3.926$$

Equation 14b is used instead of equation 14a so as to avoid the small difference of nearly equal quantities.

Let $6.116-i3.926=x+iy$ as in equation 23

$$6.116^2=37.4$$
$$3.926^2=\underline{15.4}$$
$$52.8$$

$${}^1/_2\ \text{logn}\ 52.8={}^1/_2(1.664+2.303)=1.983$$

The angle θ of equation 23 is in the fourth quadrant.

$$\tan(-\theta)=\frac{3.926}{6.116}=0.642$$

$$\theta=-0.571\ \text{radian}$$
$$\text{logn}\ (x+iy)=1.983-i0.571$$

by equation 23

$$\sin^{-1}(2+i3)=0.571+i1.983$$

by equation 14b

The second solution is given by the − sign in equation 14a or 14b, the former being preferable.

$$-\sqrt{(1-A^2)}+iA=-6.116+i3.926$$

The angle θ of equation 23 is in the second quadrant.

$$\sin^{-1}(2+i3)=2.571-i1.983$$

The quantity $2k\pi$ may be added to either of these solutions.

Inverse Cosine cos⁻¹(x ± iy)

Let $\cos^{-1}(x+iy)=u+iv$

$$\cos(u+iv)\ =x+iy$$
$$\cos u\cosh v\ =x \qquad (25)$$
$$\sin u\sinh v\ =-y \qquad (26)$$

Squaring and eliminating $\sinh^2 v$,

$$\sin^4 u-(1-x^2-y^2)\sin^2 u-y^2=0$$

$$\sin^2 u={}^1/_2[1-x^2-y^2\pm\sqrt{(1-x^2-y^2)^2+4y^2}]$$

Since u is real and $\sin^2 u$ is positive, use the positive value of the root.

$$\cos^2 u={}^1/_2(1+x^2+y^2-pq)$$
$$\cos\ u=\pm{}^1/_2(p-q)$$

Since v is real, cosh v is positive, and cos u is the same sign as x, by equation 25. Then

$$\cos u={}^1/_2(p-q)=\frac{2x}{p+q}$$

$$u=\cos^{-1}\frac{2x}{p+q}$$

Take the principal value, that is, the value between 0 and π. Then $\sin u$ is positive. Similarly

$$\sinh^4 v+(1-x^2-y^2)\sinh^2 v-y^2=0$$
$$\sinh^2 v={}^1/_2[x^2+y^2-1+\sqrt{(1-x^2-y^2)^2+4y^2}]$$

taking the positive value of the root since $\sinh^2 v$ is positive.

$$\cosh^2 v={}^1/_4(p+q)^2$$
$$\cosh v={}^1/_2(p+q)$$

taking the positive value since v is real.

$$v=\cosh^{-1}\frac{p+q}{2}$$

Since $\sin u$ is positive, $\sinh v$ and v are the same sign as $-y$, from equation 26.

$$\cos^{-1}(x+iy)=\pm\left[\cos^{-1}\frac{2x}{p+q}+2k\pi-i\cosh^{-1}\frac{p+q}{2}\right] \quad (27)$$

where y is positive, taking the principal value of $\cos^{-1}$ and the positive values of $\cosh^{-1}$ and of p and q. Also

$$\cos^{-1}(x-iy)=\pm\left[\cos^{-1}\frac{2x}{p+q}+2k\pi+i\cosh^{-1}\frac{p+q}{2}\right] \quad (28)$$

taking the same values of $\sin^{-1}$ and $\cosh^{-1}$ as with equation 27. For p and q see equations 7 and 8.

The quantity x may be positive or negative. The quantity y is positive.

An alternative method is by use of the equation

$$\cosh^{-1} p=\pm\operatorname{logn}(x+\sqrt{x^2-1})$$

Let $\cos^{-1} A=u$, a complex quantity.

$$A=\cos u=\cosh iu$$
$$iu=\cosh^{-1} A$$

$$\cos^{-1} A=-i\cosh^{-1} A=\mp i\operatorname{logn}(A+\sqrt{A^2-1})+2k\pi \quad (29a)$$

$$\text{or }=\pm i\operatorname{logn}(A-\sqrt{A^2-1})+2k\pi \quad (29b)$$

The second equation is obtained by rationalizing the numerator of the first and is to be used when it avoids the numerical difference of quantities.

Example 2. $\cos^{-1}(-2-i4)$

By equation 28, putting $k=0$

$$\cos^{-1}(-2-i4)=\pm[\cos^{-1}(-0.4385)+i\cosh^{-1}4.56]$$
$$=\pm[2.025+i2.198]$$

To check,

$$\cos(2.025+i2.198)=-0.4385\times4.56-i0.899\times4.45$$
$$=-2.00-i4.00$$

By the alternative method, let $A=-2-i4$

$$A^2-1=-13+i16$$
$$\sqrt{(A^2-1)}=1.952+i4.10$$
$$A=-2-i4$$

Using equation 29b to avoid the numerical difference of quantities

$$A-\sqrt{(A^2-1)}=-3.952-i8.10$$

In finding the logarithm of this, $\operatorname{logn} r=2.20$, and the angle θ is in the third quadrant and is 4.258 radians.

$$\cos^{-1}(-2-i4)=\pm(-4.258+i2.20)+2k\pi$$
$$=\pm(2.025+i2.20)$$

putting

$$k=1.$$

Inverse Tangent $\tan^{-1}(x+iy)$

Let $\tan^{-1}(x+iy)=u+iv$

$$\tan(u+iv)=x+iy=i\tanh\frac{u+iv}{i}$$

since

$$\tan z=i\tanh(z/i)$$

$$\tanh\frac{u+iv}{i}=\frac{x+iy}{i}$$

$$v-iu=\tanh^{-1}(y-ix)$$

$$={}^1/_2\operatorname{logn}\frac{1+y-ix}{1-y+ix} \quad (30)$$

where logn denotes natural logarithm.

$$e^{2v}(\cos 2u-i\sin 2u)=\frac{1+y-ix}{1-y+ix}$$

Rationalizing the denominator

$$e^{2v}\cos 2u=\frac{1-x^2-y^2}{(1-y)^2+x^2} \quad (31)$$

$$e^{2v}\sin 2u=\frac{2x}{(1-y)^2+x^2} \quad (32)$$

Squaring, adding, and factoring the numerator

$$e^{4v}=\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \qquad \text{by equation 5}$$

$$v=\frac{1}{4}\operatorname{logn}\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \quad (33)$$

Dividing equation 32 by equation 31

$$\tan 2u=\frac{2x}{1-x^2-y^2} \quad (34)$$

Let

$$2u=\pi-\tan^{-1}\frac{1+y}{x}-\tan^{-1}\frac{1-y}{x} \quad (35)$$

$$=\pi-\alpha-\beta$$

where the principal values of $\tan^{-1}$ are taken, that is, α and β are between $-\pi/2$ and $\pi/2$.

$$\sin(\pi-\alpha-\beta)=\sin(\alpha+\beta)$$
$$=(\tan\alpha+\tan\beta)\cos\alpha\cos\beta$$
$$=\frac{2}{x}\cos\alpha\cos\beta$$

which is the same sign as x, as it should be, from equation 32. Cos α and cos β are positive.

$$\cos(\pi-\alpha-\beta)=-\cos(\alpha+\beta)$$
$$=(-1+\tan\alpha\tan\beta)\cos\alpha\cos\beta$$
$$=(1-x^2-y^2)\frac{\cos\alpha\cos\beta}{x^2}$$

which is the same sign as $1-x^2-y^2$ and proportional to it (see equation 31).

The quantity $2k\pi$, where k is an integer, may be added to equation 35.

Therefore

$$\tan^{-1}(x+iy)=\frac{1}{2}\left\{(2k+1)\pi-\tan^{-1}\frac{1+y}{x}-\tan^{-1}\frac{1-y}{x}\right\}+\frac{i}{4}\operatorname{logn}\frac{(1+y)^2+x^2}{(1-y)^2+x^2} \quad (36)$$

where the principal values of $\tan^{-1}$ are taken and where x and y may be positive or negative.

An alternative method of computation is by means of equation 31

$$\tan^{-1}(x+iy)=\frac{i}{2}\operatorname{logn}\frac{1+y-ix}{1-y+ix}+2k\pi \quad (37)$$

Example 3

$$\tan^{-1}(-2-i4)=1.675-i0.2006$$

When computing this by equation 37, $k=-1$. Other values, given by adding $2k\pi$, are equally appropriate.

Inverse Hyperbolic Sine
$\sinh^{-1}(\pm x+iy)$

Let

$$\sinh^{-1}(x+iy)=u+iv$$
$$x+iy=\sinh(u+iv)$$
$$=i\sin\frac{u+iv}{i}$$

$$y-ix=\sin\frac{u+iv}{i}$$

$$u+iv=i\sin^{-1}(y-ix) \quad (38)$$

By equation 12

$$\sinh^{-1}(\pm x+iy)=\pm(-1)^n\cosh^{-1}\frac{s+t}{2}+i(-1)^n\sin^{-1}\frac{2y}{s+t}+in\pi \quad (39)$$

where n is an integer or 0,
x is positive,
y is positive or negative

$$s=\sqrt{(1+y)^2+x^2}\text{ (positive value)} \quad (40)$$

$$t=\sqrt{(1-y)^2+x^2}\text{ (positive value)} \quad (41)$$

The principal value of $\sin^{-1}$ (between $-\pi/2$ and $\pi/2$) and the positive value of $\cosh^{-1}$ are taken.

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Note that if $x=0$ and $y>1$, $s+t=2y$ and if $y<1$, $s+t=2$.
An alternative solution is

$$\sinh^{-1} A = \text{logn}\,(\pm\sqrt{1+A^2}+A)+i2k\pi \tag{42a}$$

$$\text{or}\quad = -\,\text{logn}\,(\pm\sqrt{1+A^2}-A)+i2k\pi \tag{42b}$$

The two solutions of equation 42a indicated by ± correspond to the two angles θ and $\pi-\theta$ which have the same sine. Logn $(A-\sqrt{A^2+1})$ is a value of $\sinh^{-1} A$ which differs from logn $(A+\sqrt{A^2+1})$ by logn (-1) or $i(2k+1)\pi$. Evidently one at least of the two values is complex, for any given value of A, whether A is real or complex.

The second or alternative form, equation 42b, gives the same results as equation 42a. It should be used when it enables one to avoid computing the numerical difference of two quantities.

Example 4

$$\sinh^{-1}(-2-i4) = -2.184-i1.097$$

putting $n=0$ in equation 39 or using the + sign in equation 42b.

Also $\sinh^{-1}\ (-2-i4)=2.184+i4.239$ putting $n=1$ in equation 39 or using the − sign in equation 42a.

The quantity $i\ 2k\pi$ may be added to both these solutions.

The results may be checked by computing sinh, which gives $-2-i4$.

Inverse Hyperbolic Cosine $\cosh^{-1}(x+iy)$

Let $\cosh^{-1}\ (x+iy)=u+iv$

$$x+iy= \cosh\,(u+iv)= \cos\frac{u+iv}{i}$$

$$u+iv=i\cos^{-1}(x+iy)$$

If y is positive, by equation 28,

$$\cosh^{-1}(x+iy)= \pm\left[\cosh^{-1}\frac{p+q}{2}+i\cos^{-1}\frac{2x}{p+q}+i2k\pi\right] \tag{43}$$

Also, if y in the following equation is positive, by equation 29,

$$\cosh^{-1}(x-iy)= \pm\left[\cosh^{-1}\frac{p+q}{2}-i\cos^{-1}\frac{2x}{p+q}+i2k\pi\right] \tag{44}$$

The quantities p and q are positive and are given by equations 7 and 8. The quantity x is positive or negative.

The positive value of $\cosh^{-1}$ and the principal value of $\cos^{-1}$ are taken.

An alternative solution is

$$\cosh^{-1} A = \pm\,\text{logn}\,(A+\sqrt{A^2-1})+i2k\pi \tag{45a}$$

or

$$= \pm\,\text{logn}\,(A-\sqrt{A^2-1})+i2k\pi \tag{45b}$$

Equations 45a and 45b give the same results and the one which involves the numerical sum of two quantities should be used in any given case.

Inverse Hyperbolic Tangent $\tanh^{-1}(x+iy)$

Let

$$\tanh^{-1}\ (x+iy)=u+iv$$

$$=\frac{1}{2}\ \text{logn}\ \frac{1+x+iy}{1-x-iy} \tag{46}$$

$$e^{2u}\cos 2v=\frac{1-x^2-y^2}{(1-x)^2+y^2} \tag{47}$$

$$e^{2u}\sin 2v=\frac{2y}{(1-x)^2+y^2} \tag{48}$$

Squaring, adding, and factoring the numerator

$$e^{4u}=\frac{(1+x)^2+y^2}{(1-x)^2+y^2}$$

$$u=\frac{1}{4}\ \text{logn}\ \frac{(1+x)^2+y^2}{(1-x)^2+y^2} \tag{49}$$

Dividing equation 33 by equation 32

$$\tan 2v=\frac{2y}{1-x^2-y^2} \tag{50}$$

Let

$$2v=\pi-\ \tan^{-1}\frac{1+x}{y}-\ \tan^{-1}\frac{1-x}{y} \tag{51}$$

$$=\pi-\alpha-\beta$$

where the principal values of $\tan^{-1}$ are taken, that is, α and β are between $-\pi/2$ and $\pi/2$.

$$\begin{aligned}\sin(\pi-\alpha-\beta)&=\sin(\alpha+\beta)\\&=(\tan\alpha+\tan\beta)\cos\alpha\cos\beta\\&=(2/y)\cos\alpha\cos\beta\end{aligned}$$

which is the same sign as y, as it should be, from equation 48. Cos α and cos β are positive.

$$\begin{aligned}\cos(\pi-\alpha-\beta)&=-\cos(\alpha+\beta)\\&=(-1+\tan\alpha\tan\beta)\cos\alpha\cos\beta\\&=(1-x^2-y^2)\,\frac{\cos\alpha\cos\beta}{y^2}\end{aligned}$$

which is the same sign as $1-x^2-y^2$ and proportional to it (see equation 47). The quantity $2k\pi$, where k is an integer, may be added to equation 51.

Therefore

$$\tanh^{-1}(x+iy)=\frac{1}{4}\ \text{logn}\ \frac{(1+x)^2+y^2}{(1-x)^2+y^2}+\frac{i}{2}\left\{(2k+1)\pi-\ \tan^{-1}\frac{1+x}{y}-\tan^{-1}\frac{1-x}{y}\right\} \tag{52}$$

where the principal values of $\tan^{-1}$ are taken and where x and y may be positive or negative (reference 3, page 115, and reference 5, number 715).

An alternative method of computation is to use equation 46 directly. Multiple values will be obtained since expression 23 for logn has a term $i2k\pi$.

Example 6

$$\tanh^{-1}(-2+i3)=-0.1469+i1.339$$

putting $k=0$ in equation 52

$$\text{or} = -0.1469+i4.4806$$

putting $k=1$.

The choice of the alternative formulas involving logarithms of complex quantities, which have been given in each case in this paper, depends to some extent on the practice and preference of each individual. While the algebraic logarithmic formulas are shorter to write than the others, the solution of the numerical problems in this paper has seemed longer by the logarithmic formulas.

The use of logarithmic formulas for obtaining results involving inverse functions is given in equations 125, page 71 and 428, page 179 of volume 2 of "Communication Networks," by E. A. Guillemin (reference 4).

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A Practical Discussion of Problems in Transformer Differential Protection

P. W. SHILL
MEMBER AIEE

Synopsis: Some conditions which have to be met with in protecting transformers by the usual current-differential method produce special problems. Those involving the protection of interconnected wye or zigzag transformers and those involving Scott-connected transformers are dealt with at some length. There is also a discussion of tolerances in the selection of current transformers and the factors to be considered in the selection of relays for the purpose.

ELECTRIC-power systems are becoming more and more complex insofar as their protection is concerned. Until now there has been little difficulty in obtaining almost anything that one might desire for the usual run of problems, such as the protection of machines, transmission lines, transformers, and so forth. Even systems, which have grown to the point where they have generators, transmission lines, transformers, and other equipment with widely diverse characteristics connected together, have been successfully protected. The war has now changed things, so that today we are having to bring back into service various pieces of apparatus which have been considered obsolete, and which one hoped would remain in that class forever. To connect old apparatus into a well-designed power system and protect them both with equipment not too well-suited to the purpose is a real problem for the engineer responsible for relay protection. Often the circuit is needed in service very quickly, but the delivery dates for relays, current transformers, and so forth, cannot be promised for many months ahead. There is also the question of priorities.

It is quite probable that some old two-phase equipment will have to be rehabilitated and put back into service. For this reason, the question of the protection of Scott-connected transformers by the current-differential method should receive some attention.

While there have been on the market several types of relays and current transformers which, when properly applied, give very satisfactory results, we are now faced with the fact that we must use considerable ingenuity to "get by" with such things as we may have on hand or can procure readily.

A few years ago much time and money were spent on current-differential protections to see that the various component parts of the circuit were built as closely to their theoretical ideals as possible. Current transformers of exact special ratios and with matching characteristics were specially purchased and installed. Current-transformer cable connections were carefully tested so that their resistances might be closely matched. Relays of very special types were considered to be absolute essentials. In fact, there was, generally speaking, considerable superstition in some places about this type of problem.

Today, with our accumulated data based on experience and more accurate knowledge of those conditions which must be satisfied in order to ensure a reliable protection, many of the old ideas have been greatly modified, and we know that we can do things which at one time were considered to be quite hazardous to the continuity of service.

A transformer differential protection is much more likely to operate when it should not do so than not to operate when it should do so. So that, provided the relays are in working order and their current windings are not short-circuited, they will operate if they can get sufficient current at any time. The chief concern with differential relays is to see that they do not respond too readily to transient currents and to be sure that their multiple trip circuits are in an operable state at all times. So sensitive is the usual current-differential protection that a loose connection on one current-transformer wire has been known to operate the relay connected with it.

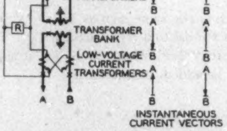
A
B
INPUT
OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
R
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
A
B
A
B
A
B
A
B
A
B
A
B
A
B
INSTANTANEOUS CURRENT VECTORS

Figure 1. Single-phase transformer differential protection

The chief sources of trouble for which the designer must be on the watch when working up the studies for a differential protection are:

1. The possibility of the generating capacity of the system being so reduced under light load conditions that the fault current available in the protected zone may be insufficient to operate the relays.

2. The possibility that the relays may be so sensitive that they may operate during periods of transient differential currents, which may be produced by the magnetic characteristics of the transformer and of the current transformers, at times of switching, or of heavy faults which draw current through the protected zone but are themselves outside of it.

In the past there was some doubt about the suitability of bushing-type current transformers for use in differential current schemes. It is now accepted that, except in the cases of those with turn ratios too small to be reliable for any kind of use, bushing-type current transformers are as reliable as wound ones for relay work, especially when the relays are required to work, that is, during periods of heavy current flow due to fault conditions.

Fundamental Principles

Let us now review the more important basic conditions which must be satisfied in all reliable current-differential protective schemes for three-phase transformers.

1. Current transformers must be provided in every phase connection to the transformer or transformer bank, so that all current entering and leaving the windings by normal paths may be properly measured and accounted for in the balancing of the differential circuit.

2. Delta-connected transformer windings ordinarily are provided with current transformers with wye-connected secondary windings. They are the most accommodating of all circuits for which to provide differential protection, because, provided proper attention is paid to the ratio of the current transformers, it is immaterial whether their current-transformer secondary circuits be wye-connected or delta-connected.

3. Any transformer windings which are connected in wye or zigzag and are so arranged that current can return to any phase through a neutral connection will at times have some current returning which is not measured by the phase current trans-

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P. W. SHILL is relay protection engineer with the British Columbia Electric Railway Company, Ltd., Vancouver, British Columbia, Canada.

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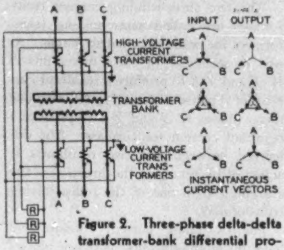

A B C
INPUT OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANS-FORMERS
INSTANTANEOUS CURRENT VECTORS
A B C
R
R
R

Figure 2. Three-phase delta-delta transformer-bank differential protection

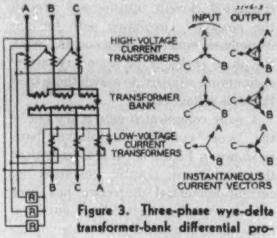

A B C
INPUT OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
INSTANTANEOUS CURRENT VECTORS
B C A
R
R
R

Figure 3. Three-phase wye-delta transformer-bank differential protection

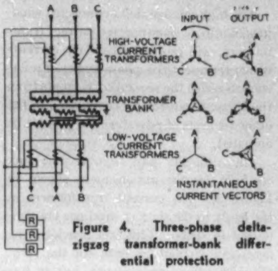

A B C
INPUT OUTPUT
HIGH-VOLTAGE CURRENT TRANSFORMERS
TRANSFORMER BANK
LOW-VOLTAGE CURRENT TRANSFORMERS
INSTANTANEOUS CURRENT VECTORS
C A B
R
R
R

Figure 4. Three-phase delta-zigzag transformer-bank differential protection

formers. This would cause an unbalance in the differential circuit. This unbalance will take the form of zero-phase-sequence components in the current-transformer secondary circuits which measure the currents in the wye-connected or zig-zag-connected transformer windings. Currents which do not sum to zero in delta-connected current-transformer secondary circuits will circulate the difference current around the delta. This circulating current is for all practical purposes the sum of the zero-phase-sequence components and corresponds to the power current returning through the neutral connection. The currents leaving from the delta and entering into the differential circuit will be a true measure of the phase power currents and will balance out satisfactorily. Because of these facts, it is generally accepted that circuits connected to wye- or zigzag connected transformer windings *must* have a delta circulating path for the zero-phase-sequence components in their current-transformer secondary circuits. This is necessary to ensure that neutral currents will not upset the differential current balance and thus operate the relays for some external condition when there is no fault in the differentially protected transformer zone.

4. The instantaneous vector sum of all the currents from the current-transformer leads connected to the relay for any one phase should be as nearly as possible zero at all instants of time when no internal fault exists. For this reason the phase angles between all the currents must be theoretically either zero or 180 degrees, depending upon the relation of the power transformer windings to each other, when no internal fault exists.

Table I

Full Load of Transformer Bank=15,000 Kva 3-Phase

Current-Transformer Ratios Figured for 25 Per Cent Overload. Base 60 Kv

		Current-Transformer Ratios			
Kilovolts	Amperes	Required	Available	Current-Transformer Secondary Amperes	Normal Relay Current Amperes
63	137	172/5	200/5	4.3	—0.2
61.5	141	175/5	200/5	4.38	—0.12
60	144	180/5	200/5	4.5	0
58.5	148	185/5	200/5	4.63	+0.13
57	152	189/5	200/5	4.73	+0.23
12.2	710	888/5	1,000/5*	4.44	—0.06
12	722	902/5	1,000/5*	4.51	+0.01
11.8	734	918/5	1,000/5*	4.59	+0.09
4.32	2,004	4,340/5g	4,000/5	5.43	+0.93
4.16	2,082	4,508/5	4,000/5	5.64	+1.14
4	2,165	4,687/5	4,000/5	5.86	+1.36

g Equals $2{,}506 \times \sqrt{3}/5$ to compensate for the delta connection.

* Auxiliary current-transformers 15/5 amperes current step-down to compensate for the delta plus inverted delta connection.

5. The current transformers in all connections to the transformer bank must have their performance characteristics so well-matched that transient differential secondary currents may be held to a minimum at all times when no internal fault exists.

Conditions 1, 2, and 3 can usually be arranged without much difficulty. Conditions 4 and 5 are ordinarily speaking never possible to be achieved to within their closest theoretical limits.

The Current Connections

The simplest case of all is that of a single-phase two winding transformer as shown in Figure 1. The three-phase two-winding delta-delta-connected transformer bank shown in Figure 2 is quite commonplace. Another case quite as common is the wye-delta-connected transformer bank shown in Figure 3. These three cases serve to illustrate how conditions 1, 2, and 3 are usually satisfied.

It should be noted that no neutral current or ground fault current can return to the delta-connected windings of a transformer bank, except through the admittance of the windings from the cores to their conductors. This current is always of a very small magnitude and can be ignored for the purposes of studies in current-differential protection.

It should also be noted that by putting wye-connected current transformers on the delta side of the transformer bank and delta-connected current transformers on the wye side of the transformer bank, the phase angles of the secondary currents can quite easily be adjusted so that they will satisfy condition 4.

The case of the delta-zigzag transformer bank shown in Figure 4 is one of the newer ones to come up, and, as will be seen by a study of its vector diagrams, is a hybrid one. The phase relations of the high-voltage and low-voltage circuits are the same as those for a delta-delta-connected transformer bank. There is a path for zero-phase-sequence current through the neutral connection of the zigzag winding; therefore, the current transformers on this side must have their secondary circuits connected in delta. We must not shift the phase relations of the current-transformer secondary currents on the delta side, and so these current transformers must have their secondary windings connected in delta also. This differs from the arrangements shown in Figures 2 and 3.

The three-winding delta-zigzag-wye transformer bank, shown in Figure 5, presents an interesting problem. Let us look at the connections of the windings and from the foregoing discussion decide what the requirements will be for each set of windings of the transformer bank.

1. The high-voltage winding is delta-connected and, therefore, will have current transformers with wye-connected secondary windings.
2. The intermediate-voltage windings are connected in zigzag, and, therefore, the current transformers *must* be delta-connected.
3. The low-voltage windings are wye-connected, and, therefore, the current transformers *must* be delta-connected.

Now the vector diagrams for the transformer bank show that the intermediate-voltage and the low-voltage windings have their output currents 30 degrees out-of-phase with each other; therefore, they cannot ordinarily both have current transformers with delta-connected secondary circuits and at the same time have their phase angles adjusted to a balanced condition. Furthermore, the high-voltage windings and the intermediate-voltage windings have their output currents in-phase with each other, and, therefore, the phase angle must not be disturbed as would be the case with dissimilar current-transformer connections.

The high-voltage delta-connected windings, having wye-connected current transformers, will provide secondary currents which represent the phase angles of reference to which the secondary currents from the current transformers connected to the other two sets of power windings must be adjusted.

This difficulty can be overcome successfully by the arrangement shown in Figure 5. The current transformers in the leads to the zig-zag windings are connected in delta. This has provided the necessary circulating path for the zero-phase-sequence components, but it has shifted the phase angles of the currents. A set of auxiliary current transformers must now be used to shift the phase angles back again. It should be noted that normally there will be a one to three step-up in current from the secondary currents in the original current transformers to the output of the auxiliary current transformers into the differential-relay circuit. This factor and any other adjustments in ratio that may be necessary can be taken care of by selecting auxiliary current transformers of a suitable ratio.

To determine the ratio required let:

I_p = the current in amperes from the zigzag windings of the power transformers

I_r = the current in amperes required for the current differential circuit

$R_1/1$ = ratio of the current transformers in the leads from the zigzag windings of the power transformers

$R_2/1$ = ratio of the auxiliary current transformers

Then

$$R_2=\frac{I_p\cdot(\sqrt{3})^2}{R_1\cdot I_r}$$

The low-voltage wye-connected windings present no difficulty. The delta-connected current transformers provide the necessary circulating path for any zero-phase-sequence components, while the phase angles of the secondary output currents are adjusted to match those from the high-voltage side.

A more complicated case is the one of the "Scott"-connected transformer bank as shown in Figure 6. It will be noticed that the worst possible condition for current-differential protection has been considered. The high-voltage circuit is three-phase four-wire. The low-voltage circuit is two-phase four-wire and also has the mid-points of the phase windings of the transformers interconnected and grounded. Here are possibilities of three-phase neutral current and also of two-phase neutral current. Obviously, the three-phase current transformers must have their secondary windings delta-connected.

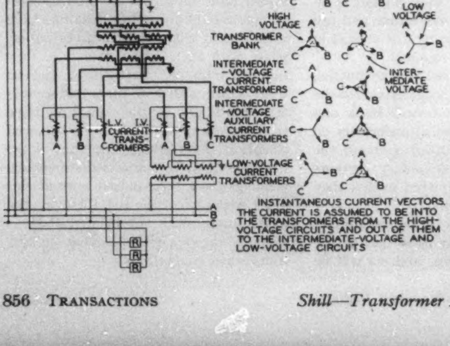
The two-phase circuit presents the real problem. We have to achieve two things:

1. To arrange that the over-all ratio of the current transformers is such that the final output will be of the right magnitude for the current differential circuit.

2. To change these output currents from two-phase to three-phase.

Of course the fundamental idea must be the same as that of the "Scott" connection, but it is not so easily accomplished with current transformers as with power transformers. The transformer producing the current in the phase W-X is the teaser of the transformer bank. Therefore, the current transformers in the circuit W-X must perform the same function in the current-differential circuit, by producing the quadrature components for B and C phases as well as providing the current for A phase.

Three auxiliary current transformers are required: two of them must have three windings, the other needs only two windings.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HIGH VOLTAGE
TRANSFORMER BANK
LOW VOLTAGE
INTERMEDIATE-VOLTAGE CURRENT TRANSFORMERS
INTER-MEDIATE VOLTAGE
INTERMEDIATE-VOLTAGE AUXILIARY CURRENT TRANSFORMERS
L.V. I.V. CURRENT TRANSFORMERS
LOW-VOLTAGE CURRENT TRANSFORMERS
R
INSTANTANEOUS CURRENT VECTORS. THE CURRENT IS ASSUMED TO BE INTO THE TRANSFORMERS FROM THE HIGH-VOLTAGE CIRCUITS AND OUT OF THEM TO THE INTERMEDIATE-VOLTAGE AND LOW-VOLTAGE CIRCUITS

Figure 5. Three-phase delta-zigzag-wye transformer-bank differential protection

The two three-winding current transformers are called phase-mixing transformers for want of a better name. As will be seen, one of them mixes phases W-X and Y-Z to produce a resultant current for B phase. Similarly, the other one mixes phases W-X and Y-Z to produce a resultant current for C phase. The difference between the two resultants is achieved by reversing the direction of the current Y-Z in one of the phase-mixing transformers.

The two-phase current transformers have no interconnection between phases W-X and Y-Z. Therefore, any two-phase neutral current components when passing through the auxiliary current transformers will produce three-phase zero-phase-sequence components in the output circuits.

By connecting the output windings of the auxiliary current transformers in delta, a circulating path is provided for the zero-phase-sequence components, and the phase angles of the output currents are adjusted to match those from the three-phase side.

Let us now get on to the determination of the current transformers required.

Let

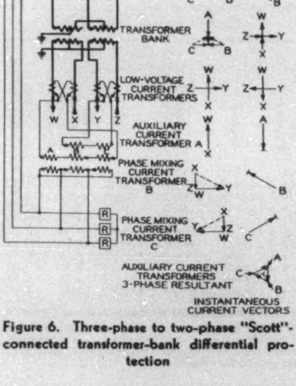
I_h = the current in amperes in the three-phase side of the power transformers

I_l = the current in amperes in the two-phase side of the power transformers

I_{w-x} and I_{y-z} = the currents in amperes in the respective current-transformer secondary windings on the two-phase side

I_r = the current in amperes required for the current-differential circuit

$R_1/1$ = ratio of the current transformers in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W
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AUXILIARY CURRENT TRANSFORMER A
PHASE MIXING CURRENT TRANSFORMER B
PHASE MIXING CURRENT TRANSFORMER C
R
AUXILIARY CURRENT TRANSFORMERS 3-PHASE RESULTANT
INSTANTANEOUS CURRENT VECTORS

Figure 6. Three-phase to two-phase "Scott"-connected transformer-bank differential protection

Table II

Full Load of Transformer Bank = 5,000 Kva
3-Phase–2-Phase
Current-Transformer Ratios Figured for 25 Per Cent Overload. Base 60 Kv

Kilovolts	Amperes	Phase	Current-Transformer Ratios: Required	Current-Transformer Ratios: Available	Current-Transformer Secondary Amperes	Normal Relay Current Amperes
63	46	3	100/5[g]	100/5	5	−0.2
61.5	47	3	102/5	100/5	5.1	−0.1
60	48	3	104/5	100/5	5.2	0
58.5	49	3	106/5	100/5	5.3	+0.1
57	51	3	110/5	100/5	5.5	+0.3
6.86	364	2	455/5	$\frac{1{,}000}{2}/5$†	4.55*	−0.65
6.6	379	2	474/5	$\frac{1{,}000}{2}/5$†	4.74	−0.46
6.34	394	2	493/5	$\frac{1{,}000}{2}/5$†	4.93	−0.27

[g] Equals $58\times\sqrt{3}/5$ to compensate for delta connection.

* Three-phase current the same value after passing through phase-mixing current transformers.

† Two 1,000/5 current transformers with secondary windings in parallel = 500/5.

the three-phase leads of the power transformers

$R_2/1$ = ratio of the current transformers in the two-phase leads of the power transformers

$R_3/1$ = ratio of the auxiliary current transformer for A phase

then

$$R_1=\frac{I_h\cdot\sqrt{3}}{I_r}$$

$$R_2=\frac{I_t\cdot 2}{I_{w-x}}\text{ or }\frac{I_t\cdot 2}{I_{y-z}}$$

and

$$R_3=\frac{I_{w-x}\cdot\sqrt{3}}{I_r}$$

Now when $I_{w-x}=I_{y-z}=I_r$

then the current input for B and C phases equals

$$0.866I_{y-z}+j0.5I_{w-x}$$

and the ratios of the phase-mixing transformers when expressed in terms of amperes will be

$$1.154I_{y-z}/2I_{w-x}\Big/\frac{I_r}{\sqrt{3}}$$

This expression will hold good for any values of I_{w-x}, I_{y-z}, and I_r, even if they are not equal.

Generally speaking, a transformer differential protection should cover not only the transformer bank but as much more of the station equipment as possible. In Figure 7 there is shown an arrangement which can be considered as typical. Here there is shown a three-winding transformer together with its necessary circuit breakers and connections which are all included under the one differential protection. A fault anywhere in the circuits between the transformer and the six groups of current transformers, including the transformer itself, will cause the differential relays to operate. In large stations the bus bars indicated and the circuit breakers connected to them may be separated by considerable distances, so that the connections may include quite long pieces of cable. The differentially protected zone would then reach out to cover equipment situated all over the station installation.

Disturbing Factors

The foregoing discussion of the current-transformer circuits is based on the assumption that all instruments and machines with magnetic cores behave absolutely normally at all times. They do so only after the currents have reached their steady state. Unfortunately, they behave very abnormally during the times that transformers or machines are being cut in and out of service, and also during the times of heavy faults anywhere on the system which can greatly disturb the current conditions in any particular protected zone.

The most usual abnormal conditions are referred to as

1. Through-current transients.
2. Transformer charging-current transients.

Through-current transients are caused by faults which draw very heavy currents through the transformer bank, and the transient magnetic characteristics of the circuit are such that, for a few cycles, the current transformers on one side of the transformer bank do not deliver secondary currents which will balance those from the other side.

Transformer charging-current transients are caused by the inrush of the charging current of the transformer bank at the instant that the first circuit breaker closes. Obviously, the current must appear in the current transformers on one side of the transformer bank only when only one circuit breaker is closed. Fortunately this current persists for only a few cycles also.

Without discussiong these disturbing factors in further detail, let us say that the problem that confronts one from both of these conditions is to get the relays to ignore these transient currents and still remain sensitive enough and fast enough to clear any faults in their zone of protection before excessive damage can result from them.

Here again the war conditions enter into the picture. Old transformers often take very high charging inrushes of current. Current transformers of older types often have saturation curves and d-c time constants which differ widely from those which are built today. In fact, many of them were built when little attention was paid to standardization of performance. Therefore, one may be forced by circumstances to make use of current transformers which have characteristics differing very widely one from another. These conditions are almost sure to produce excessive current-transformer transient differential currents.

There are quite a few types of relays on the market which are arranged to be restrained during transient conditions and are designed on very sound theoretical bases for the purpose. Unfortunately, these relays are quite expensive to put into temporary installations. Also it is becoming more and more difficult to obtain relays of ordinary standard types, let alone those which are of quite special types, as these restrained relays are.

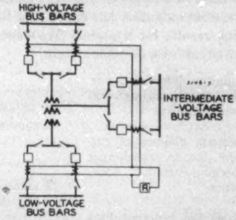

Figure 7. Grouping of current transformers for transformer-bank zone differential protection

Single-phase connections shown

Tolerable Differential Currents

There are some compensating factors which help to simplify the problem. It is quite obvious that insofar as the relays are concerned, either the transformer charging current, or the through transient current will produce the greater differential current. Therefore, if we can restrain them for the greater current, we shall have no trouble with the smaller one.

In case of a fault in the protected zone, the damage done will be a function of the time that the fault persists and of the watts that are poured into it. If the impedance to a fault be high, and the power fed into it be therefore low, one may stretch out the time it may be allowed to persist without doing any excessive damage.

It is quite usual in practice to find that current transformers cannot be arranged so that the differential current through the relays is zero at all times. There will almost always be some permanent differential current flowing which will be added to the true differential current under fault conditions. Insofar as the relays are concerned, this permanent differential current is the same as a permanent fault of very high impedance. A transient differential current will be the same as a transient fault, usually of relatively high impedance.

The Relays

Let us assume that one has not much choice of the current transformers and relays that one can obtain for any given differential protection. Let us assume also that, in a hypothetical case, mathematical investigation has yielded the following results for a normal five-ampere relay circuit on a 60-cycle system.

Maximum differential current due to through-current transients	—12 amperes for 20 cycles
Maximum differential current due to transformer charging-current transients	—15 amperes for 15 cycles
Normal 125 per cent load differential current through relays	—2 amperes
Maximum fault differential current under minimum system generating capacity	—5 amperes

If we can obtain both time delay and instantaneous relays of suitable types, we can adequately protect the transformer bank by using one instantaneous and one time relay per phase and set them as follows:

Instantaneous relay:	Pickup at 20 amperes
Time delay relay:	Pickup at 2.5 amperes Operate at 14 amperes in 30 cycles or at 17 amperes in 23 cycles.

The actual setting of the time-delay relay will depend upon its inherent characteristics. It must be determined by test. Let us suppose that for a given relay setting we get the following performance:

Operate at 14 amperes in 30 cycles.
Operate at 17 amperes in 15 cycles.

and for another setting we get

Operate at 14 amperes in 35 cycles.
Operate at 17 amperes in 23 cycles.

Then the second setting should be the one to be selected, because it gives ample time clearance for both through-current transients and transformer charging-current transients. The first setting comes too close to the anticipated time for transformer charging-current transients to persist and might result in false operations.

Almost always the fault current will be high enough to operate the instantaneous relay, but the time-delay relay is necessary to provide against faults at times when the short-circuit current to the fault would be sharply limited by the system setup. Transient currents will not operate the instantaneous relays, because they cannot reach a high enough value. They may, however, start the time-delay relays in operation, but they will die out before the relays can close their contacts. The low value of pickup current of the time-delay relays—0.5 amperes above the normal 125 per cent load differential current—will ensure that they will operate under minimum fault conditions and will not pick up under normal conditions. It is true that the time will be long in clearing, but the limited power available at the fault will limit the extent of the damage that can be done. Most manufacturers of relays now combine these two types of relays in one case.

In some places it is customary to use some sort of additional suppresser circuit to handle transient conditions. The suppresser circuit is usually arranged to shunt away part of the transient differential current from the usual percentage differential type of relays and slow up their action until the transient conditions have passed. This arrangement has one great disadvantage. There is no instant of time when a fault may show up in a transformer bank or its connections in the protected zone more than the one when the first circuit breaker closes and the charging current rushes in. A suppresser circuit makes the relays far less sensitive while it is in operation than they would be normally. Therefore, any fault developing before the suppresser circuit has timed itself out will require a much greater fault current to operate the differential relays than would otherwise be necessary. At times with low generator capacity in service this may cause a complete failure of the differential protection. Some suppresser circuits, which operate on straight voltage for their timing, could never time out during heavy faults in the protected zone, due to the voltage around the zone falling to a very low value, perhaps almost to zero.

On the whole, considerable tolerance can be allowed in the selection of current transformers and relays for current-differential schemes as will be seen from Tables I and II. Table I is worked out for a delta-zigzag-wye transformer bank such as the one shown in Figure 5. Table II is worked out for a "Scott"-connected bank as shown in Figure 6.

While it is always better to have instruments such as relays perform a function for which they have been especially designed, there are often quite a few applications for relays of certain types which were not contemplated at the time the relays were originally thought of. Fortunately, ordinary overcurrent relays can, with care, be made to suffice quite well for transformer differential protection, if time and money are important factors in getting the installation into service.

Conclusions

The most important and, in fact, the essential thing to make sure of in the design of a current-differential protective circuit is that the instantaneous directions of the various currents which must be matched are right and that they cancel out fairly well at the relays. This requirement naturally presupposes that there will be no phase-angle shifts between the currents coming into the relay for any one phase at any time. The exact balance of the magnitudes of the various currents is not very critical. The exact matching of the characteristics and ratios of the current transformers, which are used, is not necessary under ordinary circumstances. It may be necessary where unusually high sensitivity is required. While special types of relays for this work are normally available and give good results, it is not essential that one have anything but some reliable instantaneous and time-delay overcurrent relays in order to provide adequate protection in most cases. Necessary phase-angle shifts of currents, in order to adjust the instantaneous directions of the currents reaching the relays, can be made with auxiliary current transformers. These should have as low an internal burden as possible, in order not to upset the ratios of the power current transformers with which they are connected during heavy current periods. In current transformer circuits connected to transformer windings which have a return path for current through a neutral connection, a delta circulating path for the zero-phase-sequence components in the phase leads must be provided.

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Some Air-Blast Circuit-Breaker Installations in Canada

H. W. HABERL
ASSOCIATE AIEE

R. A. MOORE
NONMEMBER AIEE

THIS paper gives a brief report on certain installations of air-blast circuit breakers now in service in Canada, including results of interrupting tests and operating experience.

There are in service in Canada 23 air-blast breakers of 69 kv, 138 kv, and 230 kv of the types illustrated. There are 37 breakers in service on 13 kv and 4 kv. These breakers are in service in ten different stations in various localities.

Figure 2 illustrates an installation of two 230-kv breakers in a station, carrying a load of about 250,000 kva per breaker. Two identical breakers are in service in another city.

Figure 4 shows six 138-kv breakers in a switchyard built for ten air-blast breakers. This switchyard was built in 1934, and 138-kv air-blast breakers have been in service continuously since that time in this installation.

Figure 5 shows three 138-kv air-blast breakers in a city station of 135,000-kva transformer capacity.

In Figure 6a is shown a 138-kv breaker in a downtown city station of 80,000-kva transformer capacity, fed by 120-kv underground cables. This station is built for air-blast breakers exclusively and will have an ultimate installation of 24 15-kv and 36 4-kv breakers.

Figure 7 shows one of two 110-kv breakers at a generating station in the north country (above latitude 48).

Air-blast breakers of 69-kv design are shown in Figure 8, in service in a city station of 40,000-kva transformer capacity. In Figure 11 is shown one of five 69-kv breakers in a terminal station where the short-circuit duty is 1,100,000 kva, this being the recorded interruption on test of one of these breakers in this station.

Air-blast breakers in service on 11-kv generator voltage are shown in Figures 13 and 14. Figure 15 shows a group of seven 4-kv breakers now in service in Western Canada.

Interrupting Tests

The type of 230-kv breaker illustrated has been tested at the station where these breakers are in service, located near the center of three large power systems operating in parallel with a combined generating capacity of about 2,750,000 horsepower. The maximum short-circuit duty at this location is 2,500 amperes at 230 kv or 1,000,000 kva, which is the recorded interruption on test of this breaker as shown in oscillogram, Figure 3.

The breaker is rated at 2,500,000 kva or 6,250 amperes at 230-kv three-phase, an amount of short-circuit current not available for test in Canada. However, this breaker has six interrupting elements in series per pole, and tests have been made on one of these elements interrupting 5,385 amperes with 58.8 kv across the element (see oscillogram Figure 6). This test voltage, 58.8 kv, is about 2.67 times the voltage impressed on each of the elements when interrupting a short-circuit current (assuming line-to-neutral voltage across each pole and equal voltage across each of the six elements in series). If the voltage across one pole is assumed to be 1.5 times line-to-neutral voltage, then the above ratio of 2.67 would become 1.78; that is (under the last assumption) the single element was tested at 1.78 times its rated voltage and interrupted 5,385 amperes which is 86 per cent of 6,250 amperes, its rated current. Multiplying 1.78 by 0.86 gives 1.54; that is, the test is an indication that the breaker would interrupt 6,250 amperes with a voltage across one element 1.54 times the normal (equally divided) voltage, and with a voltage across the same pole of 1.50 times the line-to-neutral voltage. It will be noted that the only extrapolation in the above is from 5,385 amperes test current to 6,250 amperes rated current, that is, an assumption of the same kilovolt-ampere capacity at 6,250 amperes. The assumption of 1.5 times line-to-neutral voltage across one pole is one which has been commonly used, and the assumption of 1.54 times the equally divided voltage across one element is believed to be more conservative than necessary. The oscillogram shows that the arc voltage (six arcs in series) is so low that it could be applied all across one element and be only a small fraction of 22 kv (the equally divided voltage across one element). Hence, if there is any practical inequality of voltages across the six elements, it could only occur after the interruption (and until the opening of the series isolating switch, while the blast of air is still passing between the contacts). The electrostatic field is quite uniform, being determined by the metal roof 30 by 48 inches, and a metal bedplate of the same size, 60 inches below it, supported on insulator stacks, about seven feet above the grounded structure. The insulating structure housing the contacts and arc chutes has a large horizontal cross section. The six sets of contacts occupy identical locations in this structure. Such conditions are conducive to uniform division of potential across the contacts.

Tests on the 69-kv breaker (oscillogram Figure 9) also have a bearing on the interrupting capacity of the 230-kv breaker. The interrupting element in the 69-kv breaker is the same as in the 230-kv breaker, except that the width of the 69-kv contact (and arc chute) is eight inches instead of five inches as in the 230-kv breaker. The 69-kv breaker has interrupted 10,000 amperes on a three-phase short circuit on a 63-kv circuit, which test subjected the single element of the 69-kv breaker to a transient voltage of

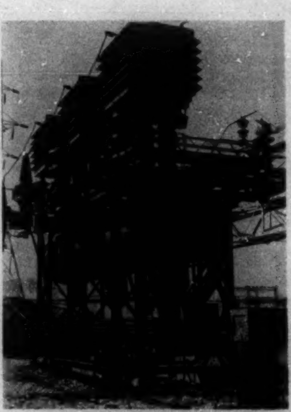

Figure 1. The first 138-kv air-blast breaker of this type

In service from 1934 until 1940

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H. W. Haberl is protection and relay engineer, Montreal Light, Heat, and Power Consolidated, Montreal, Quebec, Canada.

R. A. Moore is switchgear engineer with the English Electric Company of Canada, Ltd.

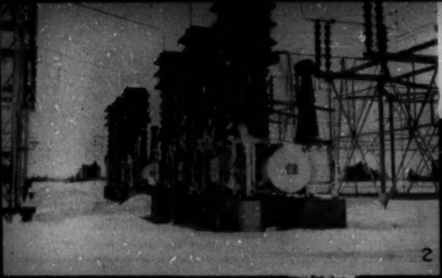


Figure 2. Two 230-kv air-blast circuit breakers in service on long 230-kv lines

One breaker carrying approximately 250,000-kva load. Four of these breakers in service in Canada

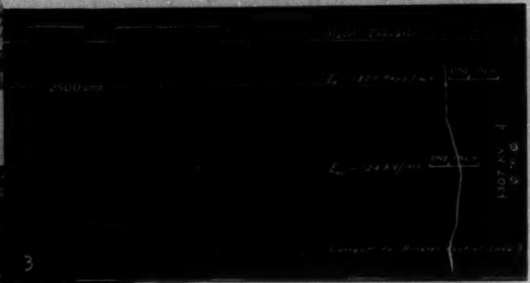


Figure 3. "Make-break" test on 230-kv breaker on 230-kv line, interrupting maximum current available 2,500 amperes

Arcing time one-half cycle (see text for additional tests showing interrupting capacity above 2,500,000 kva)

Figure 4. Switchyard built for ten 138-kv air-blast breakers

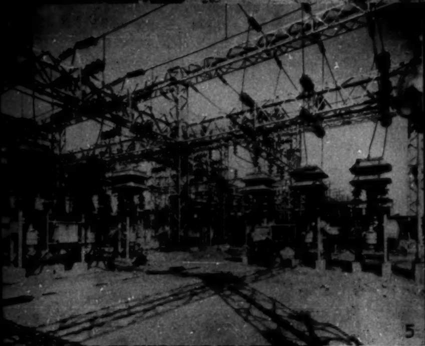
Six have been installed, controlling 200,000-kw load

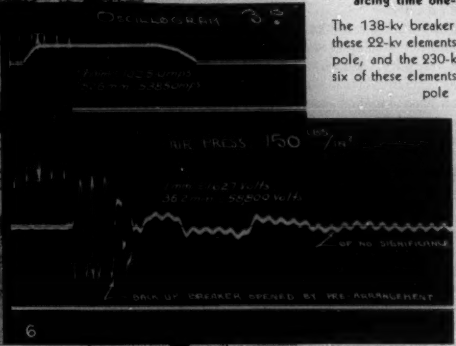
Figure 5. Receiving station switchyard with three 138-kv air-blast breakers installed, operating on 150 pounds per square inch

Closing time 13 cycles—opening time 3 cycles

Figure 6. "Make-break" test applying 58.8 kv across one 22-kv interrupting element, interrupting 5,385 amperes, arcing time one-half-cycle

The 138-kv breaker has three of these 22-kv elements in series per pole, and the 230-kv breaker has six of these elements in series per pole





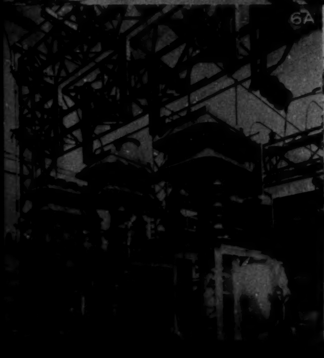


Figure 6a. 138-kv breaker on an underground 120-kv cable-fed substation located in a downtown area

This breaker is within ten feet of a busy street

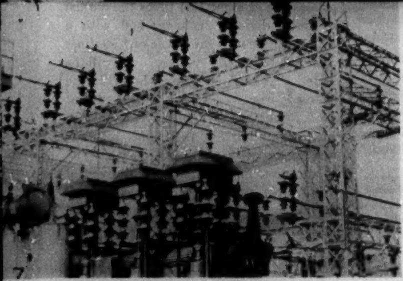


Figure 7. Two 110-kv breakers at generating station in northern Quebec

These breakers have two interrupting elements in series per pole. Addition of one interrupting element per pole would increase voltage rating to 138 kv and interrupting capacity to 2,000,000 kva

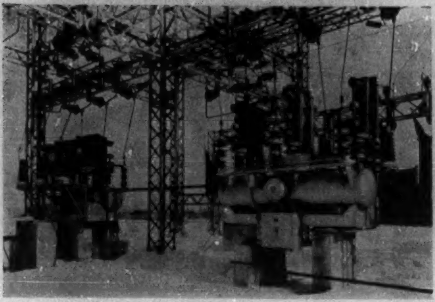


Figure 8. Two 69-kv breakers in city station

Switchyard laid out for nine of these breakers. Each pole has one interrupting element eight inches wide; otherwise the same as the elements in 138-kv and 230-kv breakers, which are five inches wide

36.4 kv. Reducing 36.4 kv in the ratio of five inches to eight inches gives 22.75 kv as the voltage at which the five-inch element should interrupt 10,000 amperes. If the voltages are divided equally in the 230-kv interruption, each element would be subjected to only 22 kv, but assuming 1.5 times line-to-neutral voltage across one pole, then one element would be subjected to 33 kv. Reducing 10,000 amperes interrupted on test, in the ratio of 22.75 kv to 33 kv, gives 6,900 amperes as the interruption at 230 kv, which is 2,750,000 kva. In this case we are interpolating, from the higher test current of 10,000 amperes to the lower current (6,900 amperes) which would justify a slightly higher kilovolt-ampere rating than we have assumed in the voltage ratio calculation.

It will be noted that none of the tests on this style of interrupter showed what is the maximum interrupting capacity. Each of the tests was simply made at the maximum current and voltage available, and none of the tests indicated that the interruption was near the limit of capacity of the breaker.

The 138-kv breakers illustrated have identically the same interrupting elements as the 230-kv breakers, with three elements in series per pole (instead of six). The tests above referred to are, therefore, directly applicable to the 138-kv breaker. The current-interrupting rating is practically the same (6,250 amperes), and the voltage per element is practically the same.

Tests were made also on the 69-kv breaker by applying 58.8 kv across one pole, interrupting 5,580 amperes, which was the maximum current available. The significance of these tests is the liberal overvoltage capacity shown (see oscillogram Figure 10).

In general the tests on all of the breakers above referred to have shown arcing time of one-half cycle. The contacts have shown negligible burning and did not require cleaning up before putting them in service. The arc did not extend outside of the cooling grid. All of the tests were "make-break" and the disconnecting switch contacts making the short circuit performed satisfactorily (the motion of the switch blade being very fast), the arc was not objectionable, and the contacts did not require cleaning up.

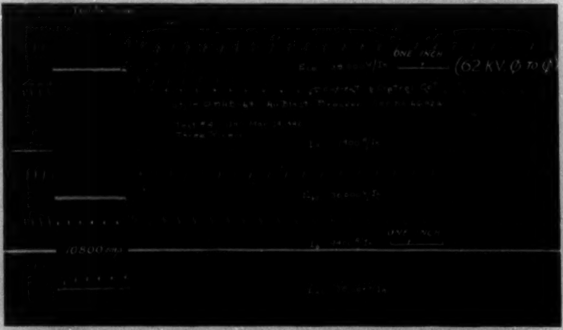
The tests were witnessed by disinterested visiting engineers from various power companies, and there was general agreement that no distress was shown in the interruptions.

Operating Experience

Service experience with air-blast breakers in Canada (and the United States) dates from 1934 when the first 138-kv breaker was put in service in the switchyard of a large generating station. It remained in service continuously for about six years interrupting all short circuits satisfactorily. Its isolating switch was very slow, and the workmanship on its interrupting contacts was relatively crude, finally resulting in overheating while carrying load, and it was superseded by breakers of improved design and workmanship. Breakers of the later style have been installed in the same station, and there are now six installed. This first installation demonstrated that there were no serious difficulties in operating and maintaining air-blast breakers of that voltage outdoors in a location subject to climatic extremes of moisture and temperature. The activated alumina twin air dryers installed in that station in 1934 are still in satisfactory operation serving six breakers. The early experience showed the importance of extracting the oil vapor as well as the moisture from the compressed air. There was much improvement made in the detailed technique of determining when the alumina needed reactivating; the color-changing alumina was a practical help, but not essential; the experienced operators got along quite well with the plain white product. The oil vapor (from the lubrication of the compressor pistons) requires a good tight woolen blanket to filter it out. If the blanket does not fit tightly in the container, the oil vapor will blow by. If oil vapor is allowed to pass into the breaker valves (especially the pressure-reducing valve), it will cause the small parts to stick. Consideration has been given to the use of oilless compressors, using carbon piston rings, but the maintenance of the carbon rings and extra cost of such compressors does not appear attractive nor justified. The

Figure 9. Three-phase "make-break" test on 69-kv breaker in 63-kv circuit, interrupting maximum current available 10,000 amperes, equals 1,090,000 kva

Opening time $3^3/_4$ cycles, including one-half-cycle arcing (see text for additional tests indicating higher interrupting capacities)



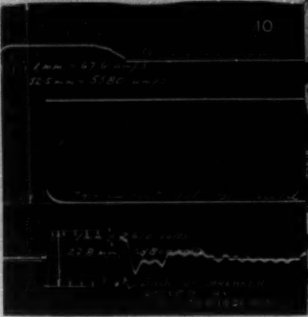
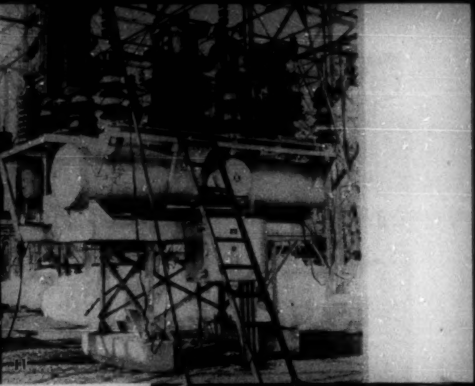
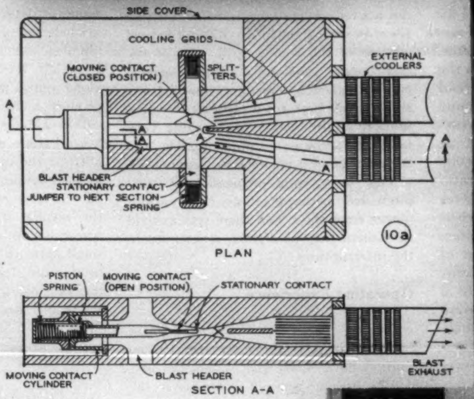


Figure 10. "Make-break" test applying 58.8 kv across one 40-kv interrupting element of 69-kv breaker

Interrupting maximum current available, 5,580 amperes, one-half cycle arcing




SIDE COVER
COOLING GRIDS
MOVING CONTACT (CLOSED POSITION)
SPLITTERS
EXTERNAL COOLERS
A
A
A
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BLAST HEADER
STATIONARY CONTACT
JUMPER TO NEXT SECTION
SPRING
10a
PLAN
PISTON SPRING
MOVING CONTACT (OPEN POSITION)
STATIONARY CONTACT
MOVING CONTACT CYLINDER
BLAST HEADER
BLAST EXHAUST
SECTION A-A

Figure 10a. Details of interrupting element

Two elements in series on 110-kv breaker, three elements in series on 138-kv breaker, and six elements in series on 230-kv breaker

Figure 11. 69-kv breaker 20 minutes after short-circuit test

One pole completely taken down, and inspection complete, showing ease of inspection

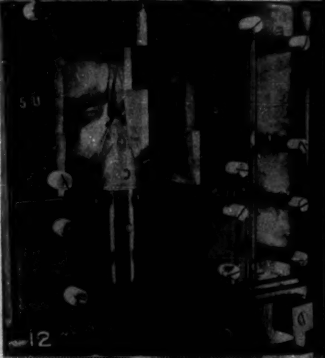

Figure 12. "Inside information" on a 69-kv air-blast breaker after interrupting four short circuits at or above rated capacity

Showing ease of inspection and minimum burning. Moving contact pulled down for inspection

woolen filter has been found quite satisfactory.

There are now in service in Canada 23 air-blast breakers of 69, 138, and 230 kv, in seven different localities. Their service record is equivalent to about 21 "breaker years."

In one instance certain new-style arc-chute details were installed without sufficient interrupting tests, because of the desire to meet promised shipping dates, and certain new parts were found necessary after the tests. In other instances, water had accumulated in the air piping and was not blown out before connecting the breaker, resulting in a flashover in one of the small porcelain "closing" tubes, requiring only replacement of the tube. Aside from such experience, the straight work of installing air-blast breakers does not require any greater skill or labor than installing oil breakers; in fact, the absence of any mechanical linkage between poles of the air-blast breakers eliminates the close mechanical adjustments required in installing oil

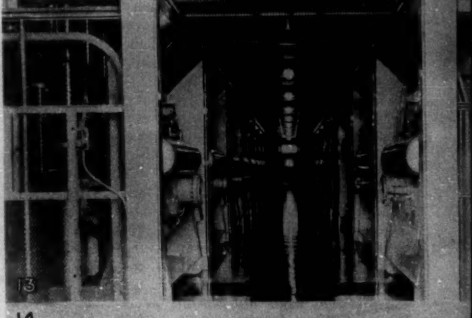

Figure 13. Delle breakers installed in a large powerhouse

Main generator and transformer breakers operating on 11 kv rated 1,000,000 kva. This station is completely equipped with air-blast breakers

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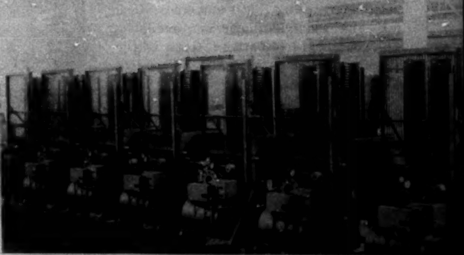

Figure 14. Factory picture, English Electric Company (Canada) of Delle breakers for four-kilovolt service

These breakers are now in service in western Canada breakers. Also there is no item in the air-blast job corresponding to the job of filtering large quantities of oil during installation of oil breakers.

In the 69-kv, 138-kv, and 230-kv breakers herein illustrated the contacts and other parts requiring occasional inspection are very accessible. The contacts can be removed and replaced in a small fraction of the time required for corresponding inspection of oil-breaker contacts.

In these breakers the interrupters are supported on standard switch-type insulators (the same as used for bus supports and disconnecting switches). The breakage of the porcelain on such supports does not impair their mechanical strength, and there is no danger of the interrupter falling.

In the experience of several years of testing various styles of interrupter contacts and arc chutes to determine their interrupting limitations, the observers have been impressed with the fact that a failure to interrupt does not result in serious damage. After such a failure to interrupt, the contacts require resurfacing or replacement of arcing tips, certain parts of the arc chute require cleaning up or replacement, but the remainder of the breaker remains intact.

The 138-kv and 230-kv breakers illustrated are readily adaptable to single-pole operation (automatically opening and reclosing one pole only to clear a line-to-ground fault), because there is no mechanical connection between the three poles. The 69-kv breakers have mechanical connection, but this can readily be dispensed with in case single-pole operation is desired.

The opening time (including arcing time) of the 138-kv breaker is within three cycles. The other two breakers take about three quarters of a cycle longer in opening. This can be shortened by about one cycle in the case of the 69-kv breakers by omitting the mechanical tie between the poles. The opening time is also influenced to some extent by the adjustment of certain small valves controlling the opening and closing of the blast valve, so that the opening time of the blast valves and the breaker can be somewhat reduced below the above figures.

Regarding closing time, the breakers as now adjusted take about 12 cycles to close in the case of the 69-kv breaker, about 15 cycles for the 138-kv, and about 19 cycles for the 230-kv. This time is also susceptible of reduction by certain adjustments.

In cases where extremely fast reclosing is desired, this can be arranged by certain adjustments which advance the time of shutting off the blast and applying closing air pressure, it being unnecessary to wait until the isolating switch has completed its opening motion.

It has been noted that all of the tests recorded by the oscillograms were "make-break" interrupting tests. The construction of these breakers is such that the opening and clearing time is the same whether the operation is a "make-break" or a "break" performance, as the breakers are provided with pneumatic tripfree valves.

As shown by the illustrations and remarks concerning the 138-kv breaker, even after one of these breakers has been installed and operated, if changes should be made in the system either by increasing the voltage or increasing the interrupting duty, it is a relatively simple matter to add one or two supporting insulators and one or two interrupting sections, thereby increasing the voltage and interrupting capacity of the breaker without discarding any of the parts. For instance, a system might initially operate at 110 kv and in later years be raised to 138 kv or 180 kv by the additions to the breaker as indicated.

The same remarks apply to the 230-kv breaker in regard to a possible increase in voltage from 230 kv to 280 kv for instance, which could be accomplished by adding one or two supporting insulators.

Low-Voltage Air-Blast Breakers

Figures 13, 14 and 15 show installations of 15-kv and 4-kv air-blast breakers in Canada. The first few breakers of this type were imported from the Delle Company in France. The other breakers of the same design were built by the English Electric Company of Canada. The interrupting capacity has been established by the tests made in the testing laboratories in France and by certain tests made in the testing laboratories of the I-T-E Circuit Breaker Company in Philadelphia. Further tests have been made on these breakers at the stations where they are installed. Certain improvements in detail have also been made by the Canadian company to meet Canadian requirements.

The 15-kv breakers illustrated have been operating satisfactorily for about two years on 11 kv in a large generating station which is equipped with air-blast breakers exclusively.

References

1. Mechanical Simplicity of Air-Blast Breakers, H. W. Haberl, Otto Jensen. AIEE Transactions, volume 60, 1940, September section, page 869.

2. Discussion by H. W. Haberl of Circuit Interruption by Air Blast. AIEE Transactions, volume 59, 1940, page 1129.

History of A-C Wave Form, Its Determination and Standardization

FREDERICK BEDELL
FELLOW AIEE

Synopsis: With the birth of the transformer and the first distribution of alternating currents, wave form assumed interest, and methods were developed for its determination, chiefly the point-by-point method of instantaneous contact, mechanical oscillograph, cathode-ray oscillograph, and the oscilloscope with stabilized time axis. The point-by-point method, by which were made the first major contributions, is now practically superseded by oscillograph and oscilloscope, each finding increasing use in its field.

With the determination of wave form accomplished, demand arose for its standardization corresponding to expanding applications. No single standard being suited to all applications, different standards have been developed in different fields, as in power, communication, and insulation. While it is desirable that standards, once set up, remain fairly stable, they should be subject to review and occasional change to keep in step with technological advances. Minor revision in communication is in progress. Although standards in other fields do not appear ideal, no immediate revision is recommended. Forty references are appended.

Alternating Current in the Late 80's

THE distribution of alternating current as we now know it began in 1885–86. Previous to that time, alternators were used to operate arc lamps in lighthouses, each machine operating one large arc lamp, but there was no distribution. Late in 1885, William Stanley made his first constant potential transformer, and early in 1886, using six transformers for operation in parallel distributed current at Great Barrington, Mass., to a score of customers at a distance of 4,000 feet. The secondary voltage was 100 volts. Current was supplied from a Siemens alternator, designed for 12 amperes 500 volts, imported for the purpose. At the time of this first installation, it was pointed out by electricians that "if a high potential primary circuit of 500 volts or more were used to distribute electricity throughout a community, there was a grave fire and life danger." A few years later a bill was introduced[1] but not passed in the Virginia legislature to limit a-c pressure to 200 volts, alternating current being considered (by those opposing its use) to be more deadly than direct.

Stanley's installation at Great Barrington was put in regular service in March 1886, and operated until summer, when an attendant dropped a screw driver into the alternator and ruined it. Meanwhile, on April 6, 1886, George Westinghouse, accompanied by W. L. Church, F. L. Pope, W. C. Kerr, and others, had seen the system in operation and determined to actively enter the a-c field. The manufacture of a new type of alternator designed by Stanley, the radial-pole type thereafter generally used, was undertaken in Pittsburgh. The story is well told by Stanley[2] himself with interesting side lights with credit to others.

The following winter the first commercial installation was made at the station of the Buffalo Electric Company. The question of the determination of a-c wave form then arose as an engineering problem. Before considering wave form, we should note the meagerness of knowledge then possessed concerning the behavior of alternating currents.

In the late 80's many misconceptions impeded progress. Series *versus* parallel operation of transformers was much discussed without adequate understanding. In 1886, at the very time that the parallel operation of transformers was successfully accomplished as we have just seen, a distinguished cantor lecturer held such a connection to be impracticable, maintaining that a separate lead to each transformer would be necessary, or each transformer should have a special regulating apparatus.

Referring to constant current operation with transformer primary windings in series, the chief of an electrotechnical testing station explained:[3] "When no secondary current is flowing, the electromotive force in the primary and secondary coils is a maximum. (Quite correct.) We have consequently this disproportion that *the smaller the output of the apparatus the greater the energy consumed.* With the secondary circuit open, and a constant exciting current, the energy used could be as much as ten times as great as under full load." Power factor was overlooked or unknown.

The proponents of the series connection of transformers operated with constant primary current reversed the argument with the same misconception. They held that inasmuch as primary electromotive force decreased with increase of secondary load, power input also decreased (primary current being constant), with the happy result that *as more power was taken out, less power was put in*—surely a condition to be desired. Here again, power factor was neglected or unknown. Yet, without meters for measuring power, power factor or phase, such misconceptions should be expected; they merely reflected the state of the art at the time.

The United States Patent Office had shown a like misconception when in 1883 it refused[4] to grant a patent on a transformer on the ground that it would be impossible to get a larger current out of the secondary winding than was supplied to the primary winding, but in 1886 a patent was allowed for this very thing.

In the Early 90's

Many of the misconceptions of the 80's trailed along into the early 90's. In the field of alternating currents there was a growing collection of isolated facts, some understood and others not, but there was no broad foundation on which to stand. This was the situation, as recalled by the author in 1890 and 1891, when he first became interested in alternating currents and in wave form. The open-magnetic-circuit transformer was still discussed. The merits of the "nonpolar" transformer with its closed magnetic circuit was not yet fully recognized. The capacity "effect" with current before the electromotive force that produced it (the effect before the cause) and the Ferranti "effect," with electromotive force received at the far end of a cable greater than the electromotive force applied, were found baffling.

Two "systems" of a-c distribution were then in use, apparently well-established: the "high-voltage" *constant potential* (Westinghouse) system operating transformers with primary circuits of 1,000 volts and 2,000 volts, with secondary circuits wound for 50 volts and 100 volts for supplying incandescent lamps in parallel;

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FREDERICK BEDELL is professor emeritus of physics, Cornell University, Ithaca, N. Y., and consulting physicist, R. C. Burt Laboratory, Pasadena, Calif.

The author is indebted to A. C. Crehore, his first co-worker, and to later co-workers to some of whom reference is made; to L. F. Blume of Pittsfield, O. E. Buckley and H. S. Osborne of New York, L. W. Chubb and R. D. Evans of Pittsburgh, who furnished material and references; and to the staff of the California Institute of Technology.

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and the *constant current* system, about 10 or 15 amperes for supplying arc lamps in series. Transformers were limited to small sizes, the dictum from high sources that transformers larger than two kilowatts would not be economical being generally accepted. Higher voltages and the use of a-c motors were in the offing. The attempted use of iron wire for a-c transmission has been reported.

It was in 1890–91 that P. N. and L. L. Nunn installed the first commercial transmission of alternating current for power, to operate a 3,000-volt synchronous motor at the Gold King Mine at an altitude of 11,500 feet near Telluride, Colo., from water power 3,000 feet lower at a distance of three miles. The situation at that time was vividly described by P. N. Nunn in an address (mimeographed) before AIEE Los Angeles section, May 8, 1934, on "Early Experiences in the Power Industry," from which the following is quoted:

> "In spite of its shortcomings, alternating current seemed the most feasible. A pair of conventional alternators were installed, one as generator, the other as motor, identical to assure identical 'wave forms,' *whatever that might be.*
>
> "In 1890 alternating current was just plain *freak;* it did not follow Ohm's law and 'clogged' itself in its circuits. Bedell and Crehore had doped out its laws and demonstrated their concepts in 100 pages of solid calculus.
>
> "Wattmeters had not been developed, nor had the term 'power factor' been adopted into the vernacular."

Evidently wave form, power factor, and the clogging effect of impedance were dimly discernible. The need of a standard wave form, "whatever that might be," was thus early recognized. From this first power transmission it was a far cry to the 287,500-volt transmission with its magnificent equipment at Boulder Dam today. The increase from 3,000 volts came slowly with gradual increases in demand for more and more power from greater distances, with notable jumps to 33,000, 40,000, and 60,000 volts. With high voltages and long distances arose problems of insulation and interference, in which questions of wave form play such important part.

Going back to horse-and-buggy days, with constant-current and constant-potential systems both in use, the question arose as to whether incandescent lamps and arc lamps could be supplied from the same system. Early in 1891 a promoter, an ardent believer in the future of the constant-current system,* sought the aid of A. C. Crehore and the author in the development of a constant-current to constant-potential transformer for operating incandescent lamps from such a system. Easier said than done, but with the enthusiasm of youth, the problem was tackled. In this connection the development of the principles governing the flow of alternating currents was undertaken, while at the same time experiments were started on a high-voltage constant-current arc-light circuit, the only supply available.

From the experimental work much was learned, although it was never completed. As at Great Barrington it was learned that a short circuit by a screw-driver could wreck a constant-potential generator; so the lesson was now learned that an open circuit caused by the slipping of a connector from a mercury cup could wreck a constant-current system and cause a general blackout! A mercury-cup contact is ill-suited for high-voltage experiment. Permission for experiment was withdrawn. The work, thus summarily ended, was never renewed; meanwhile a better solution had been found by Elihu Thomson—the "tub" transformer with floating secondary winding delivering constant current from a constant-potential primary winding. Constant-potential primary distribution had become firmly established.

The theoretical work, on the other hand, the development of the principles governing the flow of alternating current, gave better results, opening a fruitful and ever widening field with direct bearing on a-c wave form. This work, prepared initially without thought of publication, formed the basis for the first paper by Bedell and Crehore[5] presented before the Institute at its annual convention (then general meeting) in Chicago just 50 years ago. In this the principles governing current flow in transient as well as in steady conditions were first fully developed. The sequel published in book[6] form later in the same year defined the limitation to telephony (page 201) due to change in wave form of a complex wave along a line[6a] with distributed capacity and the modification produced therein by self-induction, successfully accomplished later by M. I. Pupin with the use of loading coils. In it were included the development of vector methods for solving a-c problems and the extensive use of circle diagrams, now so common.

The first use was here[5,6] made of $j=\sqrt{-1}$ in a-c analysis. Its use in astronomy and other fields had long been known, the symbol $\sqrt{-1}$ as a sign of perpendicularity appearing in a memoir to the Royal Society of Arts and Letters of Denmark by Caspar Wessel[7] in 1797. It is to A. E. Kennelly,[8] however, that credit[9] should be given for bringing out its full significance in the application of complex quantities to a-c problems, and to C. P. Steinmetz for so ably extending its usefulness.

Vector methods and circle diagrams for solving a-c problems were not infrequently criticized, when they were first developed, as being dependent upon the so-called "sine assumption," whereas in fact electromotive forces produced by a-c generators *are not true sine waves.* To this criticism with its part truth there was no categorical answer. As a circle is defined by three points, a circle diagram permits the determination or predetermination of the performance of a-c machines and systems with a minimum amount of observation and calculation. Experimental diagrams[10] were early found to check** closely with theory. The wide use of the circle diagram today is evidence of its value.

The assumption of equivalent sine waves has likewise proved adequate[11] for many cases. For other cases more elaborate methods,[12] sometimes involving vectors in more than two dimensions, have been developed. These methods, also, have been criticized on the ground that electromotive forces produced by most a-c generators *are practically sine waves;* so that these more elaborate methods are unnecessary. Again a part truth! Both assumptions, sine wave and nonsine wave are open to criticism, but each has its field of usefulness. What allowable limits should be set to the departure from a sine wave remains to be determined.

Wave-Form Determination

H. J. Ryan[13] in 1889 made an extensive study of wave forms of a closed-magnetic-circuit transformer under different operating conditions, using a synchronous contact maker to obtain instantaneous readings point by point. In this way complete wave forms of currents and voltages and their phase relations were obtained, showing definitely the behavior of such a transformer. By this method studies were made of generator wave form by Tobey and Walbridge[14] and the wave forms of an open-magnetic-circuit transformer by Bedell, Miller, and Wagner,[15] using a liquid-jet contact maker, with some modifications in methods of measurements and the use of capacitors to improve power factor. The open-magnetic-circuit transformer with its large magnetizing current and low power factor could not survive.

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* Well into the present century engineers of eminence believed that constant current would be the ultimate in power transmission.

** It is interesting to note that such a close check, even though not complete, could be made without wattmeter, phase, or power-factor meter and with limited instruments for current and voltage measurements. Primary and secondary voltages were determined by reading, with telescope and scale, the elongation of two fine German-silver wires; primary and secondary currents, similarly, with two coarse wires. Very competent were the three observers named in the reference, with later careers of note.

The use of a synchronous commutator[16] in place of a contact maker eliminated error caused by duration of contact and made possible the direct determination of flux. The mechanical plotting of points on a synchronous drum was introduced by E. B. Rosa[17] to eliminate tedious plotting by hand. All methods employing synchronously driven mechanism, however, had their day. Besides being cumbersome and inconvenient, they were limited to commercial frequencies and at best gave only points rather than continuous curves.

Meanwhile, a parallel development, starting with the optical study of the excursions of a telephone diaphragm, had led to the oscillograph,[17a] perfected by Blondel, Duddell, and others, employing a suspended element light enough to follow closely the rapid changes in a quantity under observation. There is no need for expanding on the wide and continued usefulness of the oscillograph in the determination of wave form. Point-by-point methods were thus outmoded. The moving element of an oscillograph, however light it may be, has *some* inertia which, though practically negligible for many purposes, limits its ability to follow very rapid changes in the quantity under observation. A vibrator with no weight at all would be most desirable.

It had long been known that a cathode-ray beam would be deflected by a magnetic or electric field. Ryan[18] grasped at this fact and, with a special cathode-ray tube made for him by Mueller Uri, constructed and used the first cathode-ray oscillograph, an oscillograph in which the moving "part" had no weight and could accurately follow the changes in whatever quantity was under observation. A new field was thus opened.

In the cathode-ray oscillograph the spot of light caused by the cathode ray impinging on a fluorescent screen became a curve, by persistence of vision, when the cathode-ray beam was deflected simultaneously by two fields at right angles, one field being set up by the variable under observation, and the other by a known variable of reference. Various Lissajous figures were thus produced. In studying a-c wave form, Ryan used a known sine wave for reference, the resultant curve being a smooth ellipse in case the wave under observation was also a true sine wave. Departure from an ellipse indicated departure of the unknown a-c wave from a sine wave. The observed curve was then laboriously replotted with time as an axis, as there was then no means for obtaining this directly.

The apparatus used by Ryan was cumbersome. The tube required 5,000–10,000 volts accelerating potential, obtained from a motor-driven Wimshurst machine. Furthermore, it required the maintenance of low vacuum, obtained from elaborate vacuum apparatus with frequent attention. These inconveniences were overcome in the low-voltage hot-cathode tube of Johnson,[19] with an accelerating potential of only 300–500 volts and greater sensitivity. The tube contained a small amount of gas and required no re-evacuation during its life. The general use of the cathode-ray tube for determining wave form thus became possible.

To obtain a linear time axis, as now commonly used, required a saw-toothed wave for the wave of reference, instead of the sine wave used by Ryan. Various means for developing such a wave were advanced, some mechanical, as from a synchronously driven rheostat, and some electrical from various types of circuit. Mechanical means were cumbersome and limited in frequency range and never came into general use. Electrical means for obtaining a saw-toothed wave, properly synchronized and stabilized, led to the oscilloscope[20,21] with linear time axis so widely used today.[22,23] To maintain curves stationary, a stable linear sweep circuit is essential. With the oscilloscope practically no energy is drawn from the circuit under test.

With wave form determined, many methods and machines have been developed for its analysis, when plotted either in rectangular or polar[24] co-ordinates, into its harmonic components. Early attempts were made to determine the separate harmonic components in an a-c wave by direct electrical measurement. Some success was obtained by resonance[25] and by other means, as by passing currents of various harmonic frequency through one coil of a split dynamometer, the current to be analyzed through the other. The results, however, were meager, as the harmonic components without amplification, were too small to give significant measurement. Amplification, however, has made possible the development of many successful analyzers that give the components of an a-c wave directly by electrical measurement.[26–29]

Wave-Form Standardization

With the expanding use of alternating currents, the need for the standardization of wave form arose, and the adoption of some factor or factors that would indicate quantitively the degree of departure from a sine wave. Form factor, the ratio of effective to average value, although useful in connection with transformer loss, had no general significance, widely different wave shapes having the same form factor.[30] Other factors were from time to time proposed for this purpose, including distortion factor, peak factor, harmonic factor, curve factor, and deviation, and in some cases, after discussion, were sanctioned by the standards committee. Each factor had its own[31] significance as the numerical measure of the departure of an irregular wave from a pure sine wave, varying each in its own way with variation of amplitudes, phase, and frequencies of the harmonic components of the wave. Each, therefore, had special usefulness for special purposes. Whether a single factor could be found, sufficiently satisfactory for all purposes, was a question.

It was generally agreed that a sine wave of electromotive force at generator terminals or on a transmission line is best for most purposes, and that methods for prescribing allowable departure therefrom should be determined. In 1915 the standards committee, through a subcommittee* on wave form, undertook a study of the subject to ascertain what standard or standards could be specified that would be most suitable in characteristics and practical in application, avoiding tedious analysis and cut-and-try methods as far as possible.

ADMITTANCE STANDARDS

As the troubles caused by a departure from a sine wave depend in many cases upon the frequency of the harmonic or harmonics present, the assignment of penalties to different harmonics according to their frequencies appeared to be an obvious way to make the penalty fit the crime. For doing this, an admittance type of wave-form standard[32] appeared well suited, with the possibility of assigning different allowable weights or penalties to harmonics of different frequencies according to their behavior or misbehavior. The admittance of a circuit is readily measured, being proportional to current. The admittance of a circuit with capacitance (C) increases, and in a circuit with inductance (L) decreases, with the frequencies of any harmonics in the applied electromotive force. With L and C both in the circuit, the admittance reaches a resonant peak at a particular frequency, the broadness of resonance being controllable by the resistance (R). Values of L and C can, accordingly, be selected for resonance at a particular resonant frequency, giving maximum penalty to a harmonic of that frequency, the admittance and penalty tapering off on each side, more or less rapidly, according to the value of R.

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* Membership of the subcommittee: F. Bedell, chairman; L. W. Chubb, F. M. Farmer, H. S. Osborne, and L. T. Robinson.

The shape of the tapering slopes on the two sides can be controlled to a certain extent by employing a composite, instead of a simple circuit, with R, L, and C in the admittance standard. Desirable penalties can thus be assigned to different harmonic frequencies according to the degree of crime. The possibility of better weighting thus obtained led to the development of a composite circuit, instead of a simple circuit as an admittance standard, despite the advantage that the latter could be readily duplicated with common laboratory equipment.

The degree of crime, however, and hence the penalty to be assigned, is different in different fields of application, as in power transmission and machinery, in communication or insulation. It soon developed that penalties could not be uniformly prescribed in all fields, and no one universal wave-form standard, however desirable on account of simplicity, would prove generally acceptable. Special standards thus appeared to be necessary to meet practical conditions in each case.

In Inductive Co-ordination

The admittance type of wave-form standard was found to be particularly suitable in the inductive co-ordination field. After extensive studies of induction problems involving power and telephone systems, a *telephone interference factor*, TIF, was proposed[33] in 1919 and a TIF meter, of the admittance type, for measuring power-system wave shape in terms of its influence on telephone circuit noise. Definite weightings were determined, based on the interfering effects of different frequencies, depending in part on the telephonic equipment in use and in part on the characteristics of the human ear. In 1935 changes in telephonic equipment and new studies led to new weightings,[34] and the name telephone interference factor was changed to telephone influence factor, as more appropriate. In 1941 weightings[35] were again revised, with corresponding changes in the admittance net work of the TIF meter. Wave-form standards as applied to inductive co-ordination problems are thus being well cared for.

In the Power Field

In the power field, deviation and distortion factors are both in use, but not extensively. Deviation is included in some specifications, and acceptance tests are made to see that the specification is met. Distortion factor* is more rarely encountered, being sometimes used by designers for calculating performance of machines. Good wave shape is a matter of evolution, attained by experience. With the increase in size of machines good design for wave form becomes less difficult.

By American Standards Association[36] definition, 1.217–10.95.420:

> "The *deviation factor* of a wave is the ratio of the maximum difference between corresponding ordinates of the wave and of the equivalent sine wave to the maximum ordinate of the equivalent sine wave when the waves are superposed in such a way as to make this maximum difference as small as possible."
>
> "The deviation factor of the open-circuit terminal voltage wave of synchronous machines shall not exceed ten per cent unless otherwise specified." (Rule 3.220)

By ASA definition, 1.218–10.95.430:

> "The *distortion factor* of a voltage wave is the ratio of the effective value of the residue after the elimination of the fundamental to the effective value of the original wave."

To determine deviation, as defined, requires a curve of wave form and the use of cut-and-try methods. Deviation is not directly measurable. In contrast with the admittance type of standard, it takes no account of the frequencies of the harmonic components, a characteristic which may be an advantage or a disadvantage according to the use that is made of it. If there were simple means for its determination, deviation would serve well as a general standard, but no such means are available.

Distortion factor, also, takes no recognition of frequencies, although giving total harmonic content. It can, however, be determined by analytical processes, and indirectly from measurement without requiring a curve of wave form, advantages in its favor.

In Dielectric Tests

In dielectric tests, by ASA rule 2.122:

> "The wave shape of the test voltage shall be of acceptable commercial standards: that is, it shall come within the deviation specified as allowable in paragraph 3.220. The test shall be made with a voltage having a crest equal to $\sqrt{2}$ times the test voltage specified."

More recent AIEE Standards[37] specify further in paragraph 3 of appendix:

> "With the test specimen in circuit, the crest factor (ratio of maximum to mean effective) of the test voltage shall not differ by more than five per cent from that of a sinusoidal wave *over the upper half of the voltage range.*"

Crest voltages and crest factor may be determined by the use of a synchronous commutator[38] or rectifying tube, specification for a crest voltmeter being given in paragraph 4-80, AIEE Standards.[37]

In a Standards subcommittee report,[39] the use of crest deviation factor (the departure of the crest factor from 1.414 calculated in percentage) is suggested, and it is recommended that, as deviation requires a trace of wave form, it be not used for specifying wave form in dielectric power-factor measurements, and that distortion factor be adopted in its stead.

Summary

Since 1890, when alternating current was considered just plain "freak," not obeying Ohm's law and "clogging itself in its circuits," our knowledge (with or without calculus) has become greatly advanced and the importance of wave form, "whatever that may be," so well recognized as to require most careful standardization.

A fuller discussion of wave-form standardization would here be out of place. Standards must always be subject to revision with technological development, such revision being made only after a strong need has developed. This is not the occasion to suggest revision.

The author has outlined the history of wave form, without venturing a prediction as to the future. The paper merely runs a thread through a maze of material with no pretention to completeness, the story being more fully told in the references and their extensive bibliographies.

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* Distortion factor is frequently used in the communication field in rating high-quality program and broadcasting equipment.

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Design and Operation of High-Voltage Axial Air-Blast Circuit Breakers

ARMIN K. LEUTHOLD
MEMBER AIEE

Synopsis: The air-blast high-voltage circuit breaker has been developed and applied on high-voltage systems of 150 and 240 kv. Both single-phase and three-phase automatic high-speed reclosing can be carried out with this new type of circuit breaker. The principles of design and operation are explained and the construction of two high-voltage air-blast circuit breakers rated at 150 kv and 220 kv, installed in Canadian power-distribution plants, is described and supplemented by a discussion of performance tests and oscillograms. The construction of an air-blast circuit breaker with axial blast, designed for extremely high voltage and high interrupting capacity, is explained, and results of various tests discussed.

THE air-blast type of circuit breaker was developed very rapidly from the indoor low-voltage type to the outdoor high-voltage type of circuit breaker, as extensive test experience clearly showed the many distinct advantages of applying compressed air for arc interruption. Besides the elimination of the oil hazard, the application of compressed air assures a very fast arc-interrupting ability and enables extremely fast tripping and automatic high-speed reclosing, by means of which the ideal protection of high-voltage power-transmission lines can be achieved.

The description of the following high-voltage air-blast circuit-breaker installations will show to what extent this type of breaker has been developed.

CONSTRUCTION AND OPERATION OF 150-KV AIR-BLAST CIRCUIT BREAKER

The construction of the 150-kv air-blast breaker is illustrated in Figure 1. The breaker consists of three individual poles which are pneumatically interconnected or controlled by a common closing and tripping shaft. Each pole comprises a compressed-air tank (1), with a control valve and piston assembly (2, 3, 4), and a porcelain insulator column (5), on which is mounted the arcing chamber (6), which contains two arc breaks (7), and an air-saving piston arrangement (8). A movable isolating contact (9) is connected in series with the arcing contacts and is operated by a rotating insulator (10). The stationary isolating contact (11) is supported on an insulator (12) which can be designed to accommodate, at the same time, the current transformers for the necessary protective relays and instruments.

To obtain a maximum arc-interrupting effect, an optimum distance between the arcing contacts is required and it is, therefore, necessary to connect a movable isolating contact in series with the arcing contacts in order to provide a satisfactory isolating distance between the live breaker parts in the open breaker position. This separation of the arc-interrupting and isolating functions allows the arcing contacts to be designed relatively small and light, and this, in turn, makes it possible to obtain a very fast opening as well as a high-speed reclosing of the breaker by means of operating the arcing contacts only.

In cases where automatic high-speed reclosing operation is required, a separate insulator tube (13) is applied to supply compressed air for the automatic high-speed reclosing of the arcing contacts.

BREAKER OPERATION

In the open position of the breaker, the isolating contact (9) is open while the arcing contacts (7) are held closed by spring pressure.

When the breaker is closing an auxiliary control valve becomes energized and feeds air from the breaker tank to the isolating contact piston (4), which closes the movable isolating contact (9) by means of a bevel gear drive and the rotating insulator (10).

When the breaker is tripped, compressed air is fed through an auxiliary control valve from the breaker tank to an auxiliary piston (3) in order to open the main blast valve (2), by means of which air flows with high velocity through the insulator column (5) into the arcing chamber (6) and opens the arcing contacts (7) pneumatically and extinguishes the arc at an early current zero. After the arc is extinguished, the air-saving piston (8) closes, and full air pressure is maintained between the open arcing contacts to provide a high dielectric medium which prevents reignition of the arc during the time when the movable isolating contact (9) opens. The pneumatic control is so arranged that, as soon as the isolating contact is fully open, the main air-blast valve closes again, and only the arcing contacts move back into closed position because of spring pressure.

In case automatic high-speed reclosure is applied, the arcing contacts open as on a normal tripping operation, but without operating the movable isolating contact (9), and they are held open under air pressure until, by means of an auxiliary air supply through the reclosing insulator tube (13), these contacts reclose again.

In case of a permanent fault, the breaker immediately opens again after the first automatic reclosure, in a similar way as on a normal three-phase tripping, by the opening of the arcing and isolating contacts, and will remain in open position.

150-Kv Air-Blast Circuit-Breaker Installation in Canadian Power Plant

Figure 2 shows the air-blast breaker installation of the Aluminum Company at Arvida. This breaker, designed for a service voltage of 150 kv and an interrupting capacity of 1,800,000 kva, has been in service since 1939. The large insulators in the foreground, upon which the stationary isolating contacts are mounted, also contain the current transformers which are necessary for the protective relays; otherwise the construction is the same as described previously. Automatic reclosing operation was not requested, and, therefore, this breaker was not equipped with reclosing device.

ARCING CHAMBER

The construction of the arcing chamber of this breaker is illustrated in Figure 3. Two arc breaks are connected in series which operate as follows:

> When the main air-blast valve has opened, compressed air flows under high velocity through the hollow breaker insulator column and ducts into the outside space of the lower and upper arcing chambers, by which means the arcing contacts (1) move their full stroke away from the stationary arcing contacts (2), thereby striking the arcs. Compressed air rushes through the hollow movable arcing contacts and through the muffler (3) to the open air and envelopes the arcs by a stream of high-velocity air which extends the arcs while, by forceful cooling due to air expansion, the arc paths become deionized to such an extent that the arcs extinguish at an early current zero.

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ARMIN K. LEUTHOLD is engineer with the Brown Boveri and Swiss Electric Company of Canada, Ltd., Montreal, Quebec, Canada.

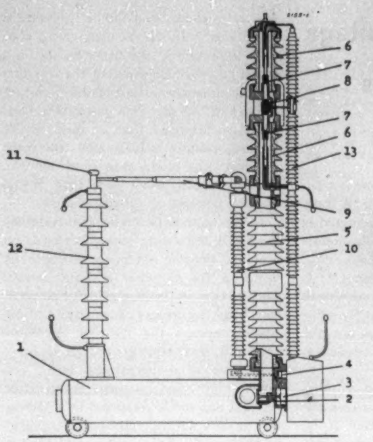


Figure 1 (left). Construction of 150-kv air-blast circuit breaker with double arc break

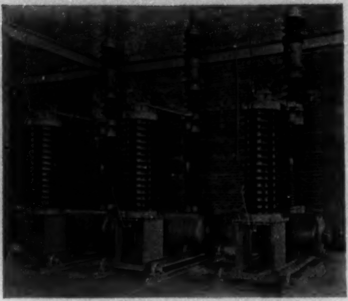


Figure 2 (right). 150-kv air-blast circuit-breaker installation of the Aluminum Company of Canada, Ltd.

When the arcing contacts move into the open position, some ports (4) become uncovered, through which compressed air can flow to the underside of the air-saving pistons (5), and, therefore, these pistons move against the arcing chamber partition, thus preventing a further escape of compressed air from the arcing chamber to atmosphere. A pneumatic time lag retards the closing of the air-saving pistons long enough to ensure positive extinction of the arcs. At the same time, when compressed air is fed into the arcing chamber, it is also admitted to an auxiliary piston which opens, with a time delay of a few cycles, the movable isolating contact. After the latter is fully open the main blast valve closes, and the air-saving piston returns to open position.

OPERATION

The operation of this type of air-blast circuit breaker is illustrated in oscillogram Figure 4, where a make-break operation is recorded. On a single-phase short-circuit at a voltage of 142 kv, a current equal to a three-phase interrupting capacity of 638 megavolt amperes was interrupted. As indicated on this oscillogram, the short-circuit was initiated at (1), at (2) the breaker tripping coil was energized, at (3) the arcing contacts were separated, and at (4) the arc interruption was completed. Including the arcing time of 0.007 second, the short-circuit interrupting time of the breaker proper (protection relay time excluded) amounted to 0.046 second.

The first high-voltage-type air-blast breaker in Canada was installed at the power distribution plant at Arvida in 1939 and has operated up to the present time very satisfactorily without any failure. A second 150-kv air-blast circuit breaker was installed at the same power distribution plant in 1941. This second breaker has also given excellent service. Experience has shown that no appreciable arcing-contact deterioration is caused even after several short-circuit interruptions. The inspection of the arcing contacts can be carried out in a few minutes, and the maintenance work on these breakers is very little compared to that on an oil circuit breaker.

150-KV AIR-BLAST CIRCUIT BREAKER WITH AUTOMATIC RECLOSING DEVICE

Figure 5 illustrates the same type of air-blast breaker rated 150 kv but equipped with additional devices for automatic reclosing. Several breakers of this type are installed outdoors in European power distribution plants and have given very successful operating results on applying automatic high-speed single and three-phase reclosing.

Oscillogram Figure 6 illustrates a short-circuit interruption with high-speed reclosing which was carried out with this type of breaker on an overhead line of 150 kv. On the upper oscillogram the interruption of an arcing short circuit is shown with automatic reclosing. Experience has shown that restriking of transient system faults could be prevented in most cases if the breaker stayed open approximately 10 to 15 cycles. Therefore, the breaker reclosing time was set in such a way that the breaker was delayed in reclosing approximately 0.19 second. With this time delay in regard to the automatic reclosing, very satisfactory operating results were obtained. At a voltage of 150 kv, a current of 2,200 amperes was interrupted. The total short-circuit interrupting time of the breaker proper (protective relay time excluded) was 0.043 second.

The lower oscillogram of Figure 6 shows the interruption of a metallic permanent short circuit. After the first arc interruption is finished at (2), the breaker recloses automatically only once at (3), and then immediately opens a second time by a final trip and stays open.

BREAKER OPERATION AT LOW TEMPERATURE AND UNDER HEAVY ICE FORMATION

In order to check that the movable breaker parts and piston mechanism operate reliably even under heavy snow and

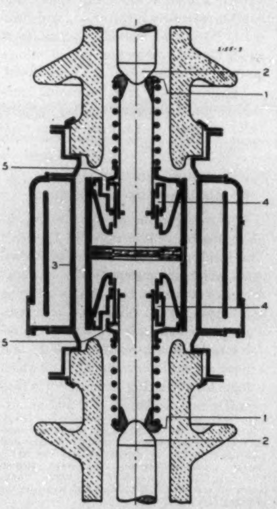


Figure 3. Arcing chamber of 150-kv air-blast circuit breaker

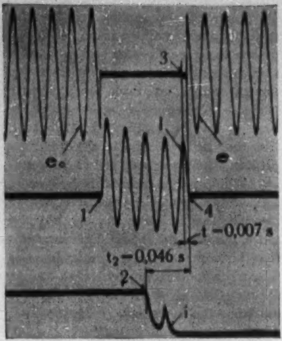


Figure 4. Make-break operation on a single-phase short-circuit by 150-kv air-blast circuit breaker

e_o—Line voltage (142 kv)
e —Recovery voltage (130 kv)
I —Fault current (2,450 rms amperes)
i —Tripping-coil current

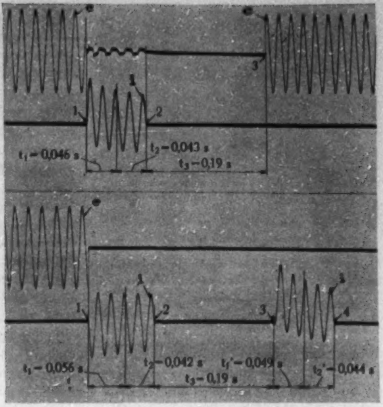


Figure 6. Short-circuit interruption with automatic reclosing by 150-kv air-blast circuit breaker

e—Line voltage (150 kv)
I—Fault current (2,200 rms amperes)

ice formation, extensive tests under similar conditions as shown in Figure 7 have been carried out with various high-voltage air-blast breakers, and it was found that, even under extremely heavy snow and ice formation, all the movable breaker parts operated very satisfactorily, both as to tripping and closing of the breaker.

In order to prevent condensation and ice formation inside the outdoor air-pipe system and breaker parts, special air-drying equipment was applied through which the compressed air is filtered and dried. Furthermore, an additional outdoor-erected air-storage tank was used in order to refill the breaker air tank, at each breaker operation, with air at approximately the same temperature as the breaker parts.

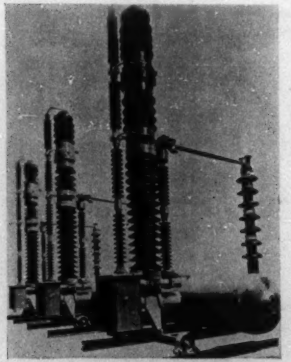


Figure 5. 150-kv outdoor air-blast circuit breaker with automatic high-speed reclosing devices

220-Kv Air-Blast Circuit Breaker Installed Outdoors on a Canadian High-Voltage Power Line

Figure 8 shows a 220-kv air-blast high-speed circuit breaker installed in the outdoor switching station of the La Tuque power plant of the Shawinigan Water and Power Company. This breaker is rated for an arc-interrupting capacity of 2,500,000 kva at 220 kv and is equipped with additional devices for carrying out single-phase or three-phase automatic high-speed reclosing.

CONSTRUCTION

Each breaker phase has a separate undercarriage upon which two large insulator columns are mounted; to each breaker undercarriage are attached four air receivers and a complete control valve and piston assembly as required for the operation of each individual breaker pole. Each insulator column consists of a large foot insulator upon which a double arcing-contact chamber is mounted, thus forming four arc breaks in series for each breaker phase. Inside the foot insulator are located two porcelain pressure tubes for supplying compressed air for the arc interruption and for the high-speed reclosing. Furthermore, to each insulator column is attached a movable isolating arm which is operated by means of rotating insulator and bevel gear drive from a separate piston on closing and normal tripping. Otherwise, the design of each insulator column is similar to that described on the 150-kv air-blast circuit breaker.

ARCING CHAMBER

The arcing chamber, mounted on top of the foot insulator, is illustrated in Figure 9. Two movable arcing contacts (1) and two stationary contacts (2) are mounted inside two porcelain insulators (3), which are surrounded by capacitance tubes (6) which control the potential

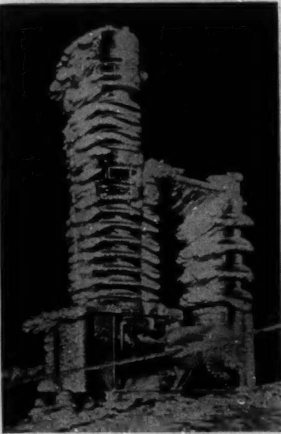


Figure 7. Air-blast circuit-breaker tests under heavy snow and ice formation

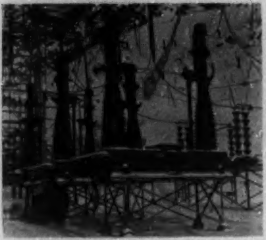


Figure 8. 220-kv air-blast circuit-breaker installation of The Shawinigan Water and Power Company

distribution between the individual arc breaks. The movable arcing contacts (1) are operated by means of a differential piston (7) and a lever arrangement (5); the latter assures simultaneous opening of the arcing contacts in series. In the intermediate piece (4) and also in the cast-iron piece (8) located at the top and at the bottom of the fixed arcing contacts, are the air-saving pistons (9), through which the compressed air in the arcing chamber escapes to atmosphere.

OPERATION

When the breaker is tripped by the opening of the main blast valve, compressed air is supplied through the large porcelain pressure tube (10) and ducts into the outside space of the lower and the upper arcing chambers and also through a duct into the intermediate piece (4) behind the differential piston (7). As soon as the air pressure is built up, this differential piston opens the arcing contacts by compressing the contact spring (11), and a stream of air flows with high velocity through the hollow arcing contacts and the air-saving pistons to atmosphere, which creates a rapid deionization of the arcing path and extinguishes the arc at next zero passing. At the same time part of the compressed air also flows through a special duct into the air-filling volume (12), which closes the air-saving pistons with a certain time delay, thus preventing a further escape of compressed air to the outside after the arc is extinguished. Closing the air-saving pistons, the compressed air re-establishes a high dielectric strength between the arcing contacts and, therefore, prevents reignition by the recovery voltage. At the same time, when compressed air is supplied to the arcing chambers, air is admitted also to the isolating contact piston which opens, with a short time delay, the movable isolating contact under no-load condition. After the complete opening of the isolating contacts, the main blast valve closes again, the arcing contacts close, and the air-saving pistons open again by spring pressure.

On tripping with automatic high-speed reclosing, only the arcing contacts open as previously described—but without opening the movable isolating contact. The arcing contacts are held open under air pressure until, by means of an auxiliary reclosing piston, air is admitted through the porcelain pressure tubes (13, 14) to the differential piston (7) and air-saving pistons (9), which simultaneously reclose the arcing contacts and open the air-saving pistons.

The following operations are carried out by the breaker protective and control devices:

> By means of control relays, the breaker opens at the first tripping impulse, either single or three-phase, by the arcing contacts only, and recloses automatically upon an auxiliary reclosing relay impulse with a definite time-delay which can be adjusted to any value most suitable for the particular network. In case of a single-phase tripping by a permanent single-phase short-circuit, the breaker automatically recloses only once on the faulty phase, and immediately opens again by a final three-phase trip with the arcing and isolating contacts and stays open.
>
> By means of a control and minimum air-pressure valve, it is assured that the breaker operates only if the available pressure of the compressed air, stored in the breaker tanks, is in the range of the admissible operation pressure. If on any reclosing the air pressure drops lower than normal, the reclosing function will not be carried through, and the breaker opens immediately by a three-phase final tripping, which will be initiated by means of a contact pressure gauge.
>
> If while closing the breaker, an urgent trip is required, the air to the closing piston will be shut off immediately by means of an anti-pumping valve, which allows the tripping operation to be carried out without delay. By these various protective and control devices, it is assured that the breaker will always carry out the best suitable breaker function in connection with the particular operating condition.

TEST RESULTS

Field tests have been carried out with this installation where the principle functions were recorded simultaneously on all three breaker poles. Furthermore, by high-voltage short-circuit tests, the arc interrupting ability of the breaker was checked.

The upper oscillogram of Figure 10 illustrates the operation of this breaker in case of a permanent single-phase fault. Only the faulted phase opens initially, followed by automatic reclosure after a short time delay. Thereupon, the breaker immediately opens again, this time on all three phases, both arcing and isolating contacts, and remains opened. As shown on the lower oscillogram of Figure 10, a similar sequence is carried out for a permanent three-phase fault, except that in this case all three phases are opened on the first as well as on the second opening. The recorded potential of the travel indicator, which was attached to the rotating insulator of the movable isolating contact, shows that at the first interruption the movable isolating contact did not move, while at the second, final interruption this contact opened a few cycles after the arcing contacts parted.

From these oscillograms, Figure 10, the following breaker speeds were obtained: The breaker opening time until the arcing contacts part is $2^{3}/_{4}$ to 3 cycles. The automatic reclosing is delayed 23 cycles on single-phase and 16 cycles on three-phase circuit interruption, but this delay can be set to any desired value by means of a relay with an adjustable time-delay device. The movable isolating contact parts approximately four cycles after the opening of the arcing contacts.

For testing the arc-interrupting ability of this breaker, special short-circuit tests were carried out after the installation of this breaker was completed. At first, sev-

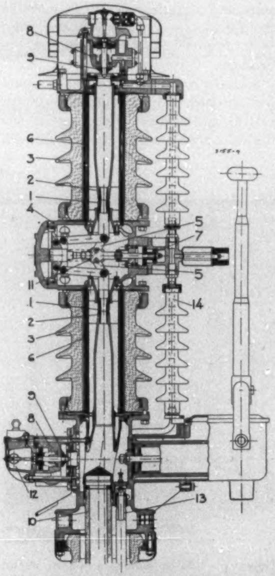


Figure 9. Arcing chamber of 220-kv air-blast circuit breaker

eral short-circuit tests (single-phase-to-ground) were carried out during which the line voltage was gradually raised from 220 kv to 260 kv. A fault current of approximately 280 to 330 amperes was interrupted without any difficulty, with an arcing time of less than one cycle.

In addition, similar short-circuit tests were carried out on the network with the power plants connected in parallel so as to obtain the maximum possible short-circuit capacity. With all these networks in parallel, the breaker interrupted a short circuit which was equal to a three-phase interrupting capacity of approximately 1,000,000 kva as illustrated in oscillogram Figure 11. The time required for the protective relay and the auxiliary contacts of the control circuit of the particular test arrangement was $3^{1}/_{2}$ cycles, the breaker opening time proper, including arcing time, was only $4^{1}/_{2}$ cycles, and the arcing time was less than one cycle.

Tests also were carried out with all networks in parallel where the breaker was switched twice into a short circuit by automatic high-speed reclosing with a time delay of only 17 cycles between the first and the second arc interruption, and twice a short-circuit current of 2,740/2,330 amperes at approximately 220 kv was interrupted satisfactorily with an arcing time at both interruptions of approximately one-half cycle. At the first interruption (which was equal to a three-phase interrupting capacity of approximately 1,000,000 kva) the arcing contacts opened without opening of the isolating contacts and, therefore, the full recovery voltage of approximately 220 kv had to be held between the small distance of the four arc breaks in series until the breaker automatically reclosed again.

All these tests, including the test with automatic reclosing, had so little influence on the large network system that these short circuits could hardly be noticed at the different power stations. Full stability of the networks was maintained through all these various short-circuit tests.

After several weeks of satisfactory operation with this breaker, a flashover took place inside the small porcelain pressure tube for automatic reclosing, and this put the breaker out of operation until the replacement of the damaged parts. It is assumed that probably condensation on the surface of this porcelain tube led to this flashover.

From the various tests and operating results we may conclude that these high-voltage air-blast breakers of 150 and 220 kv have shown the advantage of interrupting large short-circuit capacities with an air stream of high velocity and the facility of carrying out automatic high-speed single and three-phase reclosing.

Latest Design of an Air-Blast Circuit Breaker for Extremely High Voltage and High Interrupting Capacity

Based on experience from the high-voltage air-blast circuit breakers installed in various power plants and on extensive high-voltage arc-interrupting tests carried out in their large breaker test room,

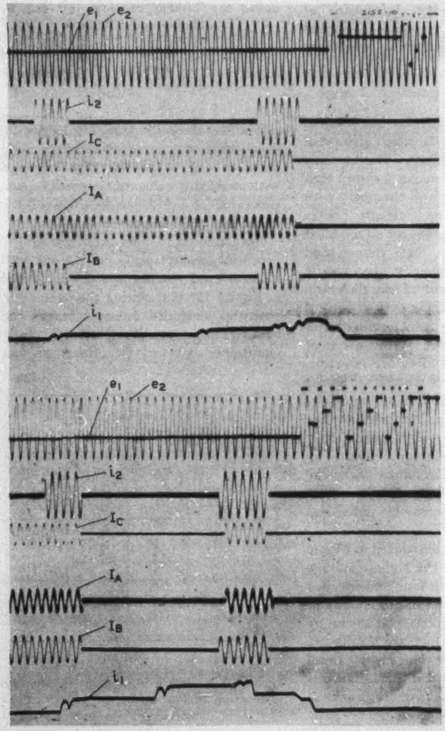
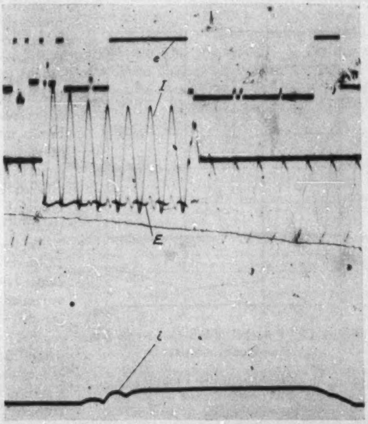


Figure 10 (left). Automatic single-phase and three-phase reclosing with final three-phase tripping by 220-kv air-blast breaker

i_1—Breaker control impulses
I_B, I_A, I_C—Current of breaker phases
i_2—Tripping relay impulse
e_1—Travel indicator potential
e_2—Timing wave (60 cycles)

Figure 11 (below). Interruption of a single-phase short-circuit to ground at 220 kv by air-blast breaker (interruption equal to three-phase interrupting capacity of 1,000,000 kva)

i—Breaker control impulses
E—Compensated voltage across fault
I—Fault current phase *B*
e—Travel indicator potential



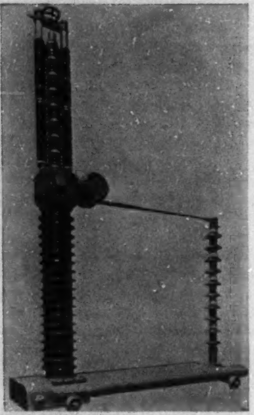


Figure 12. Air-blast circuit breaker for extremely high voltage and high interrupting capacity with eight arc breaks in series

Brown, Boveri and Company have developed, in addition to the previously described air-blast breakers of 150 and 220 kv, an air-blast breaker for extremely high voltage and high interrupting capacity as illustrated in Figure 12.

CONSTRUCTION

On this breaker eight potential-controlled arc breaks are connected in series, which open simultaneously by means of compressed air, and which provide an extremely short arc interruption. In regard to the various pistons and other breaker parts, the design in principle is similar to the previously mentioned design but simplified to a large extent. The inspection of the individual arcing contacts can be carried out in a few minutes, since it only requires loosening the spindle of a press clamp for taking off the contacts for inspection.

This breaker is also equipped with high-speed reclosing devices, by means of which single and three-phase high-speed reclosing can be carried out as previously described.

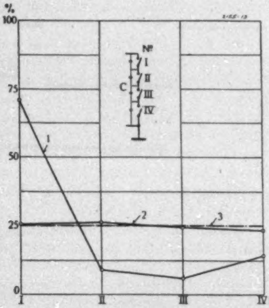


Figure 13. Potential distribution across four arc breaks in series

1. Without potential control
2. With potential control
3. Ideal distribution of potential

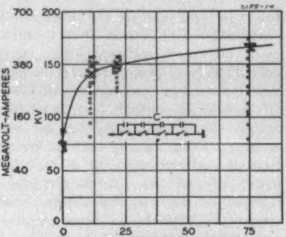


Figure 14. Increase in interrupting capacity and interrupting voltage in function of the control capacitance (C) across the four arc breaks in series

BREAKER TESTS

The design of this breaker has resulted from the following tests, which give proof of the interrupting ability of this breaker. Extensive investigations have shown that the application of arcing chambers with potential-controlled multiple arc breaks presents the best solution for an air-blast breaker of extremely high voltage and high interrupting capacity, because it allows re-establishment of the dielectric strength across the arcing path at several breaks simultaneously, which extinguishes the arc very effectively. With the rise of the recovery voltage after arc interruption, the distribution of the voltage across the arc breaks is influenced by the charging currents of the capacitances across the breaks and to earth and, therefore, it is of paramount importance to assure an equal distribution of the voltage across the different arc breaks by means of connecting small capacities across the individual breaks.

The test results, as illustrated in Figure 13, show clearly that the highest voltage across one break (with four uncontrolled arc breaks in series) can rise as high as 71 per cent of the total voltage (as shown by curve 1) while, if all four arc breaks are potential-controlled by means of small capacitances, very equal potential distribution between the four arc breaks is attained (as shown by curve 2).

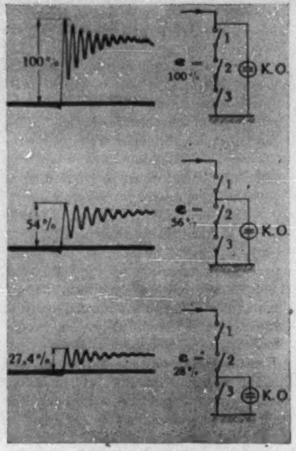


Figure 15. Oscillations of the recovery voltage across potential-controlled arc breaks during the breaker opening, recorded by cathode-ray oscillograph

e—Distribution of the potential during the oscillation
KO—Cathode-ray oscillograph

The test data, illustrated in Figure 14, show that the admissible interrupting capacity can be considerably increased with increasing values of the small capacitances which are connected between the individual arc breaks.

Furthermore, it has been proved by potential measurements carried out with a cathode-ray oscillograph (as illustrated in Figure 15) that during the opening of potential-controlled arcing contacts, the equal distribution of the potential is fully maintained and, therefore, the stresses on

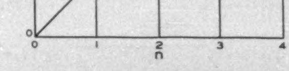


Figure 16. Increase in interrupting capacity in function of the number of potential-controlled arc breaks

Method for A-C Network Analysis Using Resistance Networks

WALDO E. ENNS
ASSOCIATE AIEE

THE mathematical solution of a-c network problems, of the type known as load distribution and voltage studies, is difficult and tedious for complex networks.

Where available, the a-c network analyzer or calculator affords the means of solving this type of problem, as well as many other problems, with good speed and accuracy.

It is the purpose of this paper to describe a new method of calculating a-c network problems and to illustrate with a practical example how a conventional d-c board, comprised of resistance circuit elements, may be used to solve load distribution and voltage studies. The writer has developed a new type of d-c board which would facilitate the use of the method described in this paper, but for the purpose of the present paper the application is limited to the existing types of d-c boards.

The scope of the present paper is restricted to the analysis of three-phase networks with balanced loading on the three phases. If the application to load distribution studies is understood, it is believed the use of the method for short-circuit studies will be evident. For short-circuit work this method can be used to advantage if the resistance of the network circuits is appreciable, so that a network setup representing reactance does not give sufficiently accurate results, and where a setup representing impedance is inaccurate because of differences in ratio of X to R for the circuits forming the network.

In contrast with the a-c network analyzer, inherently this method is not exact, but by successive approximations an exact solution may be obtained. Fortunately the first approximation is close enough for most practical problems, and rarely is it necessary to go further than the second approximation to obtain the desired accuracy. As the illustration shows, the method is simple and is quicker than other methods in analyzing complex networks with the exception of the a-c analyzer. Therefore the writer believes that the method of this paper as applied to a d-c board can be a useful tool for engineers in system planning work.

Simple computations are involved to obtain the voltage drop in a radial circuit with known line constants and loading, particularly when the circuit may be treated as a lumped impedance.

When a current with inphase component $(+I_p)$ and quadrature component $(+I_q)$ flows through a circuit of resistance R and reactance X, the inphase component of voltage drop is $V_p=I_pR-I_qX$, and the quadrature component is $V_q=I_qR+I_pX$. The components of current and voltage drop refer to the same reference axis.

If a current equal to I_p is passed through a resistance equal to R, the voltage drop I_pR can be measured. If a current equal to I_q is passed through a resistance equal to X, the voltage drop I_qX can be measured. Likewise passing I_p through X and I_q through R, I_pX and I_qR can be measured, and, by using the equation for V_p and V_q, the values of the components of voltage drop are obtained. These simple equations are the basis for the method of this paper.

When these equations are applied to network analysis, it is necessary to choose a reference voltage at one point in the network to which all voltage and current components refer for phase position. For simplicity, a network with balanced loading, is analyzed on a single-phase-to-neutral basis. To obtain a correct solution, it is necessary to satisfy Kirchhoff's laws for a-c network, which may be expressed as follows:

(*a*). The algebraic sum of the components of current toward any junction point is zero.

(*b*). The algebraic sum of the components of voltage around any closed path in the network is zero.

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WALDO E. ENNS is electrical engineer with Portland General Electric Company, Portland, Oreg.

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each individual arc break are in proportional relation to the total interrupting capacity.

As a result of these tests, a definite conclusion can be drawn regarding the performance of the breaker under its rated interrupting capacity by applying the proportional interrupting capacity on one break only. This contention has been proved by the test data shown in Figure 16 where it is shown that the maximum admissible interrupting capacity increases practically in proportion to the number of potential-controlled arc breaks. For high-voltage circuit breakers, relatively high interrupting capacities are required which far exceed the maximum short-circuit capacity available at the present test plants. It is, therefore, of great importance to note from these tests that a breaker with potential-controlled multiple arc breaks can be tested accurately by interrupting the partial load with one break only, which enables one to judge what the performance of this breaker would be under full rated interrupting capacity.

Therefore, judging from these test results, it should be possible today to design, for example, an air-blast circuit breaker for a voltage as high as 500 kv without requiring a test plant of such a high voltage.

Conclusion

The construction of the various high-voltage air-blast breakers and the operating and test data show that today this type of breaker is developed to such an extent that very valuable operating results can be obtained. In summarizing, we can conclude that, besides lowering the maintenance cost, the stability of the transmission systems can further be increased because of the breaker interrupting speed and the possibility of carrying out high-speed reclosing.

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In the practical network problem which follows, the compliance with Kirchhoff's laws is illustrated; however, it should be noted that in this particular problem, the transformers involved have the same ratio, so that there is no boost or buck in the voltage in any closed path in the network. The method of treating boost or buck in a closed path will be discussed later in the paper.

Application of Method to Network Problem

Following is an outline of procedure for making a load distribution and voltage study for a complex network with the aid of a conventional d-c calculating board and illustrated with a practical problem. The network shown on Figure 1 consists of 116 miles of 115-kv and 395 miles of 57-kv three-phase transmission circuits. The actual systems involved have more circuit mileage which is radial with respect to the network shown and so is not included in the problem. The estimated loads and generation for the December 1942 peak are used in the study with a total megawatt input in excess of 200 megawatts; however due to simplification by subtracting local load from generation, the net total input is reduced to 157 megawatts for the purpose of the network problem.

1. One-Line Diagram of Network

Figure 1 is the one-line diagram of the network simplified somewhat from the actual system to eliminate radial lines which are set up as loads at the network busses.

The method of lettering busses and numbering circuits as shown on Figure 1 is a satisfactory means of simplifying the recording of data and lessening the chance of making errors in the direction of current flow or voltage drop in the network circuits.

2. Circuit Constants

The circuit constants R, X, B, and G are required as shown in Table I. The constants are on a common phase voltage base of 57 kv in the problem but may be on any suitable common voltage or kilovolt-ampere base, if the currents are on the same base. (Where parallel circuits interconnect busses, the constants of the equivalent circuit will simplify the board setup, and if the parallel circuits have ratios of X/R which are substantially different, it is essential that the equivalent circuit be used, otherwise additional successive approximations may be necessary to obtain the desired accuracy in the study.)

3. Loads and Estimated Generation

As shown in Table II and on Figure 1, the real and reactive power should be tabulated at all load busses. Wherever generator or synchronous condenser inputs to the network occur at load busses, the input and load real and reactive power should be combined to give the net load or generation at that point. It should be noted that in Table II the leading reactive power at load busses is moved into the generation column, so that all reactive will be at the same algebraic sign, negative.

In the problem the voltage at the electrical load center of the system is 57.7 kv, so that the reference voltage to neutral is chosen as $33.33+j0$ kv. Bus P on Figure 1 is the main receiving station for the generating plants and would be expected to have a voltage phase angle approximately that of the major loads, so that it is chosen as the reference bus. Therefore all voltage and current components for the network will refer to this same reference voltage, $E_n'=33.33+j0$ kv. Since the components of voltage to neutrals E_p and E_q are unknown at the other load busses of the network, E_n' is assumed to exist at all load busses, so that the current components I_p and I_q can be computed as shown in Table II. At generator busses remote from the load center, the values of I_p and I_q are computed on the basis of estimated inputs to the network and estimated voltages, but, as shown in Table II, these input currents in total must equal the sum of the load currents.

4. Setup on D-C Board to Determine Voltage Drops

The resistance units of the d-c board are now set up to simulate the resistance or R network on the basis of the R values of Table I. The positive bus of the board is connected through a rheostat to a common bus from which all I_p input busses are fed through individual rheostats. The negative bus of the board is connected to a common bus to which all output I_p currents flow from their respective load busses of the network through rheostats. The board is then ready for adjustment to set up I_p input and output currents in some convenient proportion to the actual I_p currents shown in Table II. It is important to es-

Figure 1. One-line diagram of actual system network showing estimated net load and generation at each bus

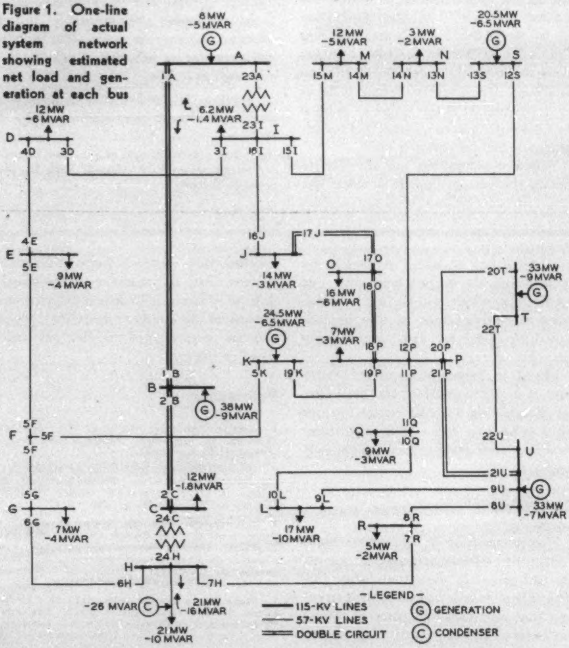
8 MW
-5 MVAR
G
A
1A
23A
12 MW
-5 MVAR
M
15M
14M
3 MW
-2 MVAR
N
14N
13N
20.5 MW
-6.5 MVAR
G
S
13S
12S
12 MW
-6 MVAR
D
4D
3D
6.2 MW
-1.4 MVAR
23I
I
3I
16I
15I
16J
17J
J
14 MW
-3 MVAR
4E
E
5E
9 MW
-4 MVAR
O
17O
18O
16 MW
-6 MVAR
20T
33 MW
-9 MVAR
G
T
22T
24.5 MW
-6.5 MVAR
G
7 MW
-3 MVAR
18P
12P
20P
K
5K
19K
19P
11P
21P
P
1B
B
2B
G
38 MW
-9 MVAR
5F
F
5F
5F
11Q
Q
10Q
9 MW
-3 MVAR
22U
U
21U
9U
8U
G
33 MW
-7 MVAR
5G
G
6G
7 MW
-4 MVAR
12 MW
-1.8 MVAR
2C
C
24C
24H
10L
9L
L
17 MW
-10 MVAR
8R
R
7R
5 MW
-2 MVAR
H
6H
7H
-26 MVAR
C
21 MW
-16 MVAR
21 MW
-10 MVAR
- LEGEND -
115-KV LINES
57-KV LINES
DOUBLE CIRCUIT
G GENERATION
C CONDENSER

Table I. Network Problem—Circuit Constants on 57-Kv Base

Circuit Designation	Resistance, R Ohms to Neutral	Reactance, X Ohms to Neutral	Conductance $G = R \div (R^2+X^2)$	Susceptance $B = X \div (R^2+X^2)$
1*AB*	0.5	2.0	0.1175	0.4710
2*BC*	1.0	3.5	0.0755	0.2640
3*ID*	2.5	4.0	0.1125	0.1798
4*DE*	12.0	12.5	0.0400	0.0417
5*EF*	3.5	4.0	0.1240	0.1420
5*FK*	3.5	4.5	0.1077	0.1384
5*FG*	15.0	23.0	0.0199	0.0305
6*GH*	9.0	14.5	0.0309	0.0498
7*HR*	12.0	15.5	0.0313	0.0405
8*RU*	17.0	24.0	0.0197	0.0278
9*UL*	9.0	13.5	0.0342	0.0513
10*LQ*	5.0	7.0	0.0676	0.0946
11*QP*	1.5	4.0	0.0822	0.2190
12*PS*	11.0	14.5	0.0332	0.0438
13*SN*	8.5	10.5	0.0467	0.0577
14*NM*	8.0	9.5	0.0520	0.0617
15*MI*	3.5	5.0	0.0940	0.1340
16*IJ*	4.5	7.0	0.0650	0.1010
17*JO*	1.5	2.0	0.2400	0.3200
18*OP*	1.0	1.5	0.3080	0.4620
19*PK*	2.5	7.0	0.0453	0.1267
20*PT*	11.5	34.0	0.0089	0.0264
21*PU*	4.5	9.0	0.0450	0.0890
22*TU*	5.0	15.0	0.0200	0.0600
23*AI*	0.5	4.5	0.0244	0.2192
24*CH*	1.0	8.0	0.0154	0.1231

Table II. Network Problem—Loads and Generation for Setup on D-C Calculating Board

Bus	Loads		Generation (Estimated)		Load Current		Generated Current	
	Megawatts	Megavars	Megawatts	Megavars	I_p	I_q	I_p	I_q
A	—	—	8.0	−5.0	—	—	80	$-j41$
B	—	—	38.0	−9.0	—	—	380	$-j74$
C	12.0	−1.8	—	—	120	$-j18$	—	—
D	12.0	−6.0	—	—	120	$-j60$	—	—
E	9.0	−4.0	—	—	90	$-j40$	—	—
F	—	—	—	—	—	—	—	—
G	7.0	−4.0	—	—	70	$-j40$	—	—
H	21.0	*	—	−16.0	210	*	—	$-j135$
I	6.2	*	—	−1.4	62	*	—	$-j12$
J	14.0	−3.0	—	—	140	$-j30$	—	—
K	—	—	24.5	−6.5	—	—	245	$-j54$
L	17.0	−10.0	—	—	170	$-j100$	—	—
M	12.0	−5.0	—	—	120	$-j50$	—	—
N	3.0	−2.0	—	—	30	$-j20$	—	—
O	16.0	−6.0	—	—	160	$-j60$	—	—
P	7.0	−3.0	—	—	70	$-j30$	—	—
Q	9.0	−3.0	—	—	90	$-j30$	—	—
R	5.0	−2.0	—	—	50	$-j20$	—	—
S	—	—	20.5	−6.5	—	—	193	$-j52$
T	—	—	33.0	−9.0	—	—	300	$-j73$
U	—	—	33.0	−7.0	—	—	304	$-j57$
Totals	150.2	−49.8	157.0	−60.4	1,502	$-j498$	1,502	$-j498$
Estimated loss =	6.8	−10.6						
Estimated input =	157.0	−60.4						

*Leading or + megavars at loads is shown as − megavars generation.

At loads $I_p = 1{,}000 \times \text{mw}/57.7\sqrt{3} = 10 \times \text{mw}$.

At loads $I_q = 1{,}000 \times \text{mvar}/57.7\sqrt{3} = 10 \times \text{mvar}$.

For input or generation to network, estimated bus voltages are used to compute I_p and I_q from the estimated megawatts and megavars input, but it is essential for the d-c board setup that the totals of I_p and I_q input are equal to the totals supplied to loads.

tablish a small current through the board at the start by adjusting the rheostat between the positive bus and the common input bus to have a high value of resistance as compared to the network resistances, so that it will act as a current limiter or, in effect, help maintain an almost constant total current to the network. Actually it would be better if a constant current supply could be used, but the current limiting rheostat referred to is satisfactory for the purpose. After the I_p currents into the network at generator busses and out of the network at load busses have been adjusted, by means of their respective generator and load rheostats, to be proportional to the desired values of Table II, the resistance of the current-limiting rheostat may be reduced to the point where a substantial current flows through the network. With more current it is then possible to adjust generator and load rheostats slightly to give the values of currents, still proportional to those of Table II, which are to be the basis for the readings of $I_{pr}R$ voltage differences between bus *P* and all other busses of the network. For example ($I_{pr}R$) measured from bus *A* to bus *P* multiplied by the constant for the d-c board for this set of readings is equal to −635 volts and is recorded in Table III. The board constant depends on the proportionality factors between board resistances and actual network resistances, between board currents and actual network currents, and also on the type of scale calibration on the voltmeter, so that the data for this study are simplified by the elimination of the board constants from the tabulations.

In a similar manner the circuit resistance units are adjusted to simulate the reactance or *X* network on the basis of the *X* values of Table I. Again by use of the current-limiting rheostat the board current is reduced, and some minor adjustments in generator I_p and load I_p currents will make them proportional to those of Table II, and the same procedure as described for obtaining $I_{pr}R$ voltage readings is followed to obtain $I_{px}X$ voltage differences between all busses and reference bus *P*. These readings are in Table III.

In like manner ($-I_{qx}X$) readings can be obtained. However it will be noted in this problem, and in most problems of

Table III. Network Problem—Measured Components of Voltage Drop Between All Busses and Reference Bus *P*

*	$(+I_{pr}R)$	$(-I_{qx}X)$	$V_p = I_{pr}R - I_{qx}X$	$(+I_{px}X)$	$(-I_{qr}R)$	$V_q = I_{px}X + I_{qr}R$
AP	−635	+865	+230	+125	+335	−210
BP	−595	+1,055	+460	+340	+380	−40
CP	−895	+1,135	+240	−605	+435	−1,040
DP	−960	+40	−920	−1,130	+80	−1,210
EP	−760	+70	−690	−820	−70	−750
FP	−405	+235	−170	−315	+25	−340
GP	−1,200	+620	−580	−1,840	+110	−1,950
HP	−1,075	+1,435	+360	−1,825	+505	−2,330
IP	−710	+260	−450	−735	+255	−990
JP	−550	−60	−610	−730	−10	−720
KP	+195	+345	+540	+445	+85	+360
LP	−275	−455	−730	−395	−305	−90
MP	−820	+40	−780	−900	+90	−990
NP	−120	+90	−30	−100	+120	−220
OP	−280	−90	−370	−440	−50	−390
QP	−170	−240	−410	−375	−105	−270
RP	−575	+865	+290	−880	+230	−1,110
SP	+890	+360	+1,250	+1,120	+320	+800
TP	+1,730	+1,050	+2,780	+4,410	+380	+4,030
UP	+1,020	+450	+1,470	+1,785	+175	+1,610

* ($+I_{pr}R$) is measured with $+I_p$ currents flowing through *R* network.
($-I_{qx}X$) is measured with $+I_q$ current flowing through *X* network.
($+I_{px}X$) is measured with $+I_p$ current flowing through *X* network.
($-I_{qr}R$) is measured with $+I_q$ current flowing through *R* network.

Table IV. Network Problem—Computation of Components of Current Flowing in Each Circuit

Circuit Designation	Voltage Difference		$I_p = GV_p + BV_q$			$I_q = GV_q - BV_p$		
	V_p	V_q	GV_p	BV_q	I_p	GV_q	BV_p	I_q
1*BA*	+230	+170	+27.0	+80.0	+107.0	+20.0	+108.0	−88.0
2*BC*	+220	+1,000	+16.8	+264.0	+280.8	+75.5	+58.1	+17.4
3*ID*	+470	+220	+52.9	+39.6	+92.5	+24.7	+84.4	−59.7
4*ED*	+230	+460	+9.2	+19.2	+28.4	+18.4	+9.6	+8.8
5*FE*	+520	+410	+64.5	+58.2	+122.7	+50.8	+73.9	−23.1
5*KF*	+710	+700	+76.4	+97.0	+173.4	+75.4	+98.5	−23.1
5*FG*	+440	+1,610	+8.8	+49.1	+57.9	+32.0	+13.4	+18.6
6*HG*	+970	−380	+30.0	−18.9	+11.1	−11.1	+48.4	−60.2
7*HR*	+70	−1,220	+2.2	−49.5	−47.3	−38.3	+2.8	−41.1
8*UR*	+1,180	+2,720	+23.2	+75.8	+99.0	+53.6	+32.9	+20.7
9*UL*	+2,200	+1,700	+75.3	+87.2	+162.5	+58.1	+113.0	−54.9
10*QL*	+320	−180	+21.6	−17.0	+4.6	−12.2	+30.2	−42.4
11*PQ*	+410	+270	+33.7	+59.1	+92.8	+22.2	+90.0	−67.8
12*SP*	+1,250	+800	+41.6	+35.0	+76.6	+26.6	+54.8	−28.2
13*SN*	+1,280	+1,020	+52.0	+59.0	+111.0	+47.6	+74.0	−26.4
14*NM*	+780	+770	+40.6	+47.5	+88.1	+40.0	+48.2	−8.2
15*IM*	+360	0	+33.9	0.0	+33.9	0	+48.2	−48.2
16*IJ*	+160	−270	+10.4	−27.3	−16.9	−17.6	+16.2	−33.8
17*OJ*	+240	+330	+57.6	+105.8	+163.4	+79.1	+76.9	+2.2
18*PO*	+370	+390	+114.1	+180.2	+294.3	+120.4	+170.8	−50.4
19*KP*	+540	+360	+24.5	+45.6	+70.1	+16.3	+68.5	−52.2
20*TP*	+2,780	+4,030	+24.9	+106.5	+131.4	+36.0	+73.4	−37.4
21*UP*	+1,470	+1,610	+66.1	+143.4	+209.5	+72.5	+1[illegible].0	−58.5
22*TU*	+1,310	+2,420	+26.2	+145.0	+171.2	+48.4	+78.6	−30.2
23*AI*	+680	+780	+16.6	+171.0	+187.6	+19.0	+149.0	−130.0
24*HC*	+120	−1,290	+1.8	−159.0	−157.2	−19.9	+14.8	−34.7

this type, there is some modification of the connections between the network and the input and output busses for the I_q current setup due to the fact that loads with leading or positive reactive power are set up as generators of negative reactive power. In this problem this change is necessary for the connections to busses *H* and *I*. The reason why the voltage differences between all busses and reference bus *P* are designated as $-I_{qr}X$ voltages is that $+I_q$ currents are made to flow in at the input bus and out at the output bus, whereas actually they should be set up as $-I_q$ currents to conform to the signs of Table II.

Similarly by using the *R* network and $+I_q$ currents the $-I_{qr}R$ voltage differences are obtained.

Referring again to Table III, the inphase voltage difference between all busses and the reference bus *P* is $V_p = I_{pr}R - I_{qr}X$ for each case, and the quadrature component of voltage difference is $V_q = I_{pr}X + I_{qr}R$.

5. COMPUTATION OF COMPONENTS OF CURRENT FLOWING IN EACH CIRCUIT OF THE NETWORK

Referring to Table IV, the voltage differences between the bus terminals of each circuit are derived from the V_p and V_q values of Table III. For example, V_p for circuit 1*BA* is obtained by subtracting V_p for *AP* from V_p for *BP* in Table III which gives $+460-230=+230$ volts. In like manner, V_p and V_q for all circuits of the network are derived.

Next, as shown in Table IV, the equations $I_p = GV_p + BV_q$ and $I_q = GV_q - BV_p$ are used to determine the inphase and quadrature components of current which must flow in each of the circuits of the actual a-c network to give the V_p and V_q voltage differences derived from the d-c board setups. The values of conductance *G* and susceptance *B* are those shown in Table I for each circuit of the network.

6. COMPUTATION OF BUS VOLTAGES AND REAL AND REACTIVE POWER

As noted at the start of the solution, the reference bus *P* was established as having a voltage to neutral of $33.33+j0$ kv. From Table III the voltage differences to neutral of components V_p and V_q were obtained between all busses and bus *P*. To determine the voltage to neutral at every other bus, these voltage differences are added algebraically to the reference voltage. This is done, and the results are tabulated as E_p and E_q in Table V.

The current components I_p and I_q in Table V are not exactly the same as the current components given in Table II which were the basis of the d-c board setup, but instead are the algebraic sum of the components of current away from each generator bus and toward each load bus, so that Kirchhoff's law will be satisfied. For example at generator bus *A* the actual input current is equal to the sum of currents 1*AB* and 23*AI* expressed in their components. From Table IV, I_p for 1*AB* is −107.0 amperes, and I_p for 23*AI* is +187.6 amperes, and the algebraic sum is +80.6 amperes as shown in Table V. All I_p and I_q current components given in Table V are obtained in this manner.

The computations of real and reactive power in megawatts and megavars are obtained by using equations megawatts = $0.003\,(E_pI_p + E_qI_q)$ and megavars = $0.003\,(E_pI_q - E_qI_p)$ as shown in Table V.

Table VI is added to show the computation of the phase voltages at all busses and to give a direct comparison of the derived values of real and reactive power, which completely satisfy the current distribution of Tables IV and V and the voltages of Tables V and VI, with the problem setup values of Table II.

The comparison shows that for all

Table V. Network Problem—Computation of Real and Reactive Power

Bus	E_p Kv	E_q Kv	I_p Amperes	I_q Amperes	Real Power in Megawatts = $0.003(E_pI_p + E_qI_q)$	Reactive Power in Megavars = $0.003(E_pI_q - E_qI_p)$
A	33.56	−0.21	80.6	−42.0	8.16	−4.18
B	33.79	−0.04	387.8	−70.6	39.31	−7.09
K	33.87	+0.36	243.5	−75.3	*28.33	*−9.05
S	34.58	+0.80	187.6	−54.6	19.37	−6.12
T	36.11	+4.03	302.6	−67.6	31.97	−10.98
U	34.80	+1.61	299.8	−62.5	30.99	−7.97
C	33.57	−1.04	123.6	−17.3	12.53	−1.36
D	32.41	−1.21	120.9	−50.9	11.95	−4.50
E	32.64	−0.75	94.3	−31.9	*9.33	*−2.92
F	33.16	−0.34	−7.2	−18.6	*(−0.70)	*(−1.86)
G	32.72	−1.95	69.0	−41.6	*7.02	*−3.68
H	33.69	−2.33	193.4	+136.0	18.58	+15.12
I	32.88	−0.99	78.1	+11.7	7.67	+1.39
J	32.72	−0.72	146.5	−31.6	14.47	−2.80
L	32.60	−0.09	167.1	−97.3	16.35	−9.46
M	32.52	−0.99	122.0	−56.4	12.09	−5.16
N	33.30	−0.22	22.9	−18.2	2.30	−1.81
O	32.96	−0.39	130.9	−52.6	13.02	−5.05
P	33.33	0.0	100.5	−58.9	10.05	−5.88
Q	32.92	−0.27	88.2	−25.4	8.75	−2.44
R	33.62	−1.11	51.7	−20.4	5.28	−1.89

* Bus *F* was not set up as a load so that the real and reactive power at *F* may be moved to busses *K*, *E*, and *G* in amounts proportional to the admittance of the circuits between *F* and its adjoining busses. This is done in Table VI.

practical purposes the first solution is adequate for the analysis of this network problem. The greatest difference between derived and setup values of real and reactive power occurs at load buses *O* and *P* which are connected by a very low impedance circuit of $(1+j1.5)$ ohms. If the two loads are added together, the derived total is $23.07-j10.93$ megavolt-amperes as compared to a setup total of $23.0-j9.0$ so that the effect of the differences of the two loads would be negligible in the network. However, in case greater accuracy is desired, it is possible to repeat the solution with corrected values of I_p and I_q to obtain derived values of real and reactive power which check almost exactly with the setup values. The method of correcting the I_p and I_q currents is described in the section which follows, but the solution is not repeated, because it would add nothing of value to the paper.

Procedure for Second Solution

Unless a particularly accurate second solution were desired, the values of real and reactive power tabulated in Table VI would be compared, and the difference in the derived and setup values would form the basis for estimating the changes in I_p and I_q which would result in improved accuracy. However, a more exact procedure is given in the following example.

At bus *O* from Table II the load setup was $16.0-j6.0$ megavolt-amperes instead of the derived value of $13.02-j5.05$ megavolt-amperes, which satisfied the current distribution and voltages of the first solution. The voltage components for bus *O* from Table V are $E_p=+32.96$ and $E_q=-0.39$, and since these voltage components will not change appreciably in the second solution, the values of I_p and I_q can be computed with the equations:

$$I_p=(E_pP-E_qQ)\,1{,}000/(E_p^2+E_q^2)3$$

$$I_q=(E_qP+E_pQ)\,1{,}000/(E_p^2+E_q^2)3$$

In these equations *P* and *Q* are in megawatts and megavars and E_p and E_q are in kilovolts to neutral.

Whence $I_p=160.6$ amperes and $I_q=-62.5$ amperes.

In Table II the current components to the load at bus *O* were set up $I_p=160$ and $I_q=-60$; however in Table V the derived values were $I_p=130.9$ and $I_q=-52.6$. This difference is due to variations in X/R ratios in the circuits of the network, and about the same difference would occur in the second solution. Therefore, a corrected value of $I_p=160.6+(160-130.9)=189.7$ would be used for the second solution. The corrected value of $I_q=-62.5+(-60+52.6)=-69.9$ would also be used for the second solution. This same procedure could be followed at all other busses of the network to obtain corrected values for a second and more accurate solution to the problem.

Table VI. Network Problem—Computation of Phase Voltage at Busses; Comparison of Result of Table V With Values Set Up in Table II

Bus	Eϕ in Kv $=1.732\sqrt{E_p^2+E_q^2}$	Loads* Megawatts	Loads* Megavars	Generation* Megawatts	Generation* Megavars
A	**109.3	— —	— —	8.16 (8.0)	−4.18 (−5.0)
B	**110.0	— —	— —	39.31 (38.0)	−7.09 (−9.0)
C	**109.4	12.53 (12.0)	−1.36 (−1.8)	— —	— —
D	56.3	11.95 (12.0)	−4.50 (−6.0)	— —	— —
E	56.6	8.99 (9.0)	−3.82 (−4.0)	— —	— —
F	57.5	— —	— —	— —	— —
G	56.8	6.96 (7.0)	−3.84 (−4.0)	— —	— —
H	58.6	18.58 (21.0)	— —	— —	−15.12 (−16.0)
I	57.0	7.67 (6.2)	— —	— —	−1.39 (−1.4)
J	56.8	14.47 (14.0)	−2.80 (−3.0)	— —	— —
K	58.7	— —	— —	28.63 (24.5)	−8.25 (−6.5)
L	56.5	16.35 (17.0)	−9.46 (−10.0)	— —	— —
M	56.4	12.09 (11.8)	−5.16 (−5.0)	— —	— —
N	57.7	2.30 (3.0)	−1.81 (−2.0)	— —	— —
O	57.2	13.02 (16.0)	−5.05 (−6.0)	— —	— —
P	57.7	10.05 (7.0)	−5.88 (−3.0)	— —	— —
Q	57.1	8.75 (9.0)	−2.44 (−3.0)	— —	— —
R	58.4	5.28 (5.0)	−1.89 (−2.0)	— —	— —
S	60.1	— —	— —	19.37 (20.5)	−6.12 (−6.5)
T	62.9	— —	— —	31.97 (33.0)	−10.98 (−9.0)
U	60.3	— —	— —	30.99 (33.0)	−7.97 (−7.0)
Totals	—	148.99	−48.01	158.43	−61.10

* Problem values set up in Table II are given in brackets.

** Turns ratio of 115/57-kv transformers = 1.88/1.

Application of Superposed Method of Setup to the D-C Board

In cases where the d-c board does not have a sufficient number of rheostats to set up all of the generators and loads, it is possible to set up any proportion of the generators and loads at one time, so long as the total input current of the several generators adds up to the total output current to the several loads. The same voltage measurements from all busses to the reference bus would be required for each such setup, and the sum of the voltages thus obtained would give the same result as though all generators and loads had been included in one setup. This method requires additional readings, but because of the smaller number of generator and load rheostats which must be adjusted to obtain correct input and load currents, the current adjustment is simplified.

Use of Method With D-C Board With Fixed Resistors

If a d-c board is of the fixed resistor type, it may be possible to adapt it for use with this method, provided it has enough rheostats to use for generator and load-current adjustment.

Most networks have many circuits with X/R ratio almost equal. If the board is set up to simulate *X* only, this network is already available. Assume that 75 per cent of the circuits of a particular network have an X/R ratio of 2/1 or approximately so, and that only 25 per cent vary much from this ratio. If a means is provided for changing this 25 per cent to have the 2/1 ratio for setups requiring the *R* network, then the method may be used. The board constant for the *R* network would then be one-half that of the *X* network.

Use of Method Where Line Capacitance Must Be Considered

In the network problem of this paper, the effect of shunt capacitance of the lines was neglected, because the amount of leading megavars of the circuits would not change the solution appreciably. However, if it is necessary to consider line capacitance, it is suggested that the capacitance + megavars for each line be computed on the basis of estimated voltages at the start of the problem. In the usual manner one half of the megavars of all lines terminating at a bus would be allocated at that bus, and an adjustment in net load or generator megavars at that bus would be made to obtain the corrected megavars from which the I_q component of current would be computed.

Setup of Transformers With Different Ratios in a Loop Circuit

In a paper presented before the Portland section of the AIEE, the writer included an example to show how a series boost or buck may be provided from a separate source of direct current inserted at the point of boost or buck in the loop circuit, but rather than lengthen this paper, the method will be indicated only.

In most networks the quadrature component of voltage with respect to the reference voltage is small as compared to the inphase component, so that the effect of any normal boost or buck on the E_q component is negligible. However, if one transformer has a ten per cent boost or buck with respect to other transformers operating in closed paths with this transformer of different ratio, then a separate d-c source should be inserted at the point of boost or buck in the X network setup with I_q current flowing. This gives the desired effect to the inphase voltage readings for V_p. In the problem of this paper the voltage to neutral is approximately 33 kv at bus I, so if a boost of ten per cent were required in transformers $23AI$, the series potential required would be 3,300 divided by the board constant. It would be inserted between bus A and the resistance unit representing X of the transformers $23AI$. If the potential source had appreciable internal resistance, the $23AI$ unit setting would be reduced by this amount. As noted before, it would be used only in the setup for reading $I_{qx}X$ voltage differences.

Setup Using Ammeter Instead of Voltmeter on D-C Board

Although the measurement of the $I_{pr}R$, $I_{px}X$, $I_{qr}R$, and $I_{qx}X$ voltages between all busses and the reference bus is recommended, it is possible to read current distribution in each of the four network setups on the d-c board and compute the voltage differences for all circuits in each setup, and obtain the same results.

Conclusions

The scope of this paper has been quite limited with respect to the broad field of a-c network analysis, but it is believed that the network problem and the discussion show how this new method can be applied with the aid of a conventional d-c board to solve complex problems of load distribution and voltage conditions for a-c networks in a relatively simple manner and with good speed.

In the application of the method to the conventional d-c board, it probably will become apparent that various combinations of resistance networks could be set up simultaneously, which, together with proper metering arrangements could be used to reduce the analysis from four board setups to one—thus eliminating the greater part of the computation work. Patent application has been made for the method as applied to such simplifying resistance network combinations.

As an indication of the time required for a study of this type, the writer unaided set up the board and took the four sets of readings required in eight hours. An additional six hours was required to make all of the computations and tabulate the results. If the outline of procedure given in the paper is followed, it is believed that other engineers will find the method to be a useful tool in their system planning work.

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3. ELECTRICAL CHARACTERISTICS OF TRANSMISSION CIRCUITS (third edition, 1926), **William Nesbit.** Paralleling of Transmission Circuits. Pages 183–4.

4. LOAD STUDIES ON THE D-C CALCULATING TABLE, **W. C. Hahn.** *General Electric Review*, part 1, July 1931, page 444; part II, August 1931, page 482. (This is very complete and shows how the d-c board may be used for load studies, but it follows an entirely different approach to the problem of d-c board setup than that given in the present paper.)

Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada

D. M. FARNHAM
MEMBER AIEE

O. W. TITUS
MEMBER AIEE

Synopsis: This paper describes the 120-kv oil-filled cable system of the Montreal Light, Heat, and Power Consolidated, installed during 1941 and in present operation as a link in a large interconnection scheme.

The paper describes the functions of the various sections of the system and the reasons for the choice of this method of effecting the interconnection. The steps required in designing the cable system are outlined and the general principles governing each such step discussed.

A short section deals with manufacture followed by a section covering the organization required for installation and the methods followed. The final section deals with operating practice, especially the basis of loading.

System

THE 120-kv oil-filled cable system of the Montreal Light, Heat, and Power Consolidated as at present installed may properly be divided into the following sections: (Figure 1):

1. Station *A* to station *B*—two circuits
2. Station *B* transformer cables—two circuits
3. Station *B* to station C—one circuit

The first and third parts are much the most important in size, but the second part, while relatively small, presents a use of high-voltage cable of particular interest to transformer station engineers.

In sequence of installation one circuit plus a spare phase of section 1 were purchased and installed initially on one contract, the remaining two phases of that section and all of sections 3 on a second contract. Section 2 was on a third contract but was installed during the same period as section 3.

Section 1 (known as circuits *A* and *B*), takes power from the 110-kv bus of station *A* directly to the new station *B* for service to an important business and industrial section of the city. Station *B* departs from the general substation scheme of the utility, in that it is not part of their 60-kv ring system, but is fed practically direct from the generating source, station *A* acting as a switching station. The present availability of underground cable suitable for 110-kv operation permitted locating the new station at its proper load center without double transformation or further loading or reinforcement of the already busy 60-kv ring. It relieves the ring as well and permits further load growth with existing facilities.

Section 2 takes power from the 110-kv bus of station *B* to the transformers, at present two, with provision for an ultimate of four. Underground feed was chosen as providing a substantial saving in steel structure, greater compactness and better appearance and much less exposed high-voltage circuit. Also when combined with underground cable on the low-voltage side, it permits a virtually "dead-front" station, excepting under the easily isolated and relatively small high-voltage bus section which lies in one 25-foot-wide strip along the south side of the station property.

Section 3 (known as circuit *C*), takes power from the 110-kv bus of station *B* to station *C* and acts in combination with circuits *A* and *B* as a direct tie between the Beauharnois system of the Montreal Light, Heat, and Power Consolidated supplying station *A* and the system of the Shawinigan Water and Power Company, and through it to the Saguenay Power Company system. It also will serve as an additional circuit to station *B*.

This direct tie between the first two systems was made in order to place the entire generating capacities of these three large companies on one interconnection, from which power for wartime industrial requirements could be drawn by the industry most vitally needing it.

The Shawinigan and Saguenay systems were already connected together by high-voltage transmission lines with ample capacity to carry any loads expected. Between the Beauharnois and Shawinigan systems the connection consisted of a 60-kv double ring tied through transformers to the 110-kv system of the Shawinigan Company and through other transformers to the 110-kv Beauharnois system (Figure 2). Thus the power that could be transferred from the Beauharnois system to the interconnection of the Shawinigan and Saguenay systems was limited by the Montreal Light, Heat, and Power Consolidated 60-kv ring and transformer capacity which was partially used by the primary power requirements of the Montreal Light, Heat, and Power Consolidated system.

The original suggestion was to construct an overhead 110-kv tie around the metropolitan area of Montreal between the Beauharnois system and the Shawinigan system. This would have involved the building of about 15 miles of steel tower transmission line over territory where the right-of-way problems were difficult and uncertain, especially as to the cost and time required to negotiate.

The second suggestion was to extend the 120-kv oil-filled cable system then nearing completion under the first contract mentioned above (circuit *A* of section 1) to tie with the 110-kv transmission system of the Shawinigan Company. This required the adding of two phases between stations *A* and *B*, which combined with the spare phase already installed for circuit *A* to become circuit *B* plus about five miles of additional cable circuit across the metropolitan area.

The cable tie was selected on the basis of several factors, most important among which were:

Initial cost
Time required
Stability
Freedom from lightning troubles
Present and future public reaction

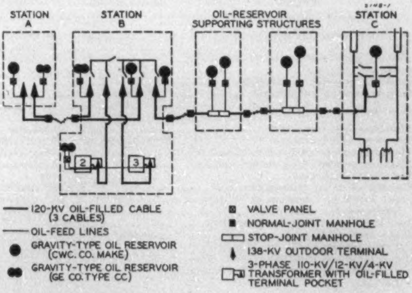
STATION A
STATION B
OIL-RESERVOIR SUPPORTING STRUCTURES
STATION C
2
3
120-KV OIL-FILLED CABLE (3 CABLES)
OIL-FEED LINES
GRAVITY-TYPE OIL RESERVOIR (CWC. CO. MAKE)
GRAVITY-TYPE OIL RESERVOIR (GE CO. TYPE CC)
VALVE PANEL
NORMAL-JOINT MANHOLE
STOP-JOINT MANHOLE
138-KV OUTDOOR TERMINAL
3-PHASE 110-KV/12-KV/4-KV TRANSFORMER WITH OIL-FILLED TERMINAL POCKET

Figure 1. Single-line diagram of system

Paper 42-148, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 8, 1942.

D. M. FARNHAM is assistant distribution engineer, Montreal Light, Heat, and Power Consolidated, Montreal, Quebec, Canada. O. W. TITUS is chief engineer, Canada Wire and Cable Company, Ltd., Leaside, Ontario, Canada.

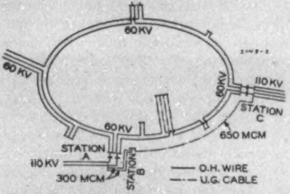

Figure 2. Interconnections between Montreal Light, Heat, and Power Consolidated and Shawinigan Water and Power Company systems

Since these three contracts constitute the most important underground cable installation in Canada and its highest-voltage underground system (the previously highest being 66-kv cables constituting part of the above 60-kv ring installed during 1927, 1931, and 1937 by the suppliers of the present cable), and since the type of cable is new to Canada, descriptions of the elements of an oil-filled cable system and the problems and steps in its design and installation seem warranted.

Cable-System Design

Underground cable systems above 75 kv fall into classifications of:

(*a*). Oil-filled cables in which practically all gas is removed from the cable, insulation, impregnant, joints, and terminals, ensuring that none of these elements are permitted to come into contact with a gas thereafter and so engineering the system that no part of it will be subjected to pressures less than atmospheric during service. It necessitates the use of an oil-impregnant fluid at all operating temperatures and oil channels giving access for the impregnant to all parts of the cable system.

(*b*). High-gas-pressure systems[1,2] in which ionization of gas pockets in the insulation is prevented by keeping them at such high gas pressures that their critical corona voltage substantially exceeds any electric stress gradients in the insulation. The usual pressures are 200 to 250 pounds per square inch. There are several such installations in service up to the 132-kv range but mostly at 66 kv. The pressures employed demand the use of steel as a containing element which gives the added advantage of excellent mechanical protection but requires great care in protection against corrosion, a serious problem where buried in earth over relatively long distances.

As the oil-filled class of system is well adapted to installation in conventional duct systems in busy city streets, and as there is in excess of 1,200 miles in service throughout the world at voltages up to 220 kv, this was the type chosen for this cable system. It is the type dealt with hereafter in this paper.

The design of an oil-filled cable system involves the following elements of choice, design and engineering:

1. Type of installation, that is, duct or buried directly in earth.
2. Number of conductors per cable.
3. Size of oil channel.
4. Size of conductor.
5. Thickness of insulation.
6. Thickness of sheath.
7. Spacing between oil-feed points.
8. Type of oil feed, that is, gravity or balanced pressure.
9. Normal joint design.
10. Pothead design.
11. Stop-joint design.
12. Size of oil-feed pipe lines.

1. DUCTS

As the cable system lies beneath the streets of one of the largest cities of Canada and under some of the busiest streets of that city, the well-accepted practice of installing the cable in a duct system was the choice. Diameters for the 300,000-circular-mil and 650,000-circular-mil cables were approximately $2^1/_4$ and $2^1/_2$ inches respectively, for which $3^1/_2$ inch and 4 inch fiber ducts were chosen.

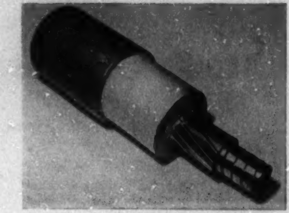

Figure 3. Cable construction

The duct-line route was selected for the most level contour (Figure 14), always desirable for oil-filled cable. Several surveys were made to determine such a route, which fortunately was one of the most direct, although through one of the busiest sections of the city.

The total length of the conduit run was approximately 36,000 feet. Work was commenced for the ducts for the second phase of the work, approximately 125,000 duct feet, in early June 1941 and completed by the end of September of the same year.

All manholes were built in such a way as to allow a minimum horizontal cable offset between duct mouth and joint of 42 inches. This was based on recom-

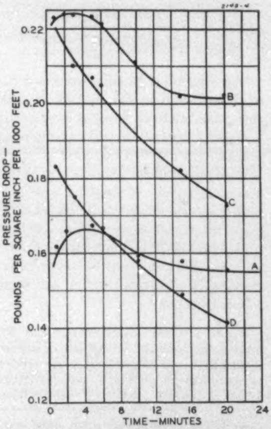

Figure 4. Curves of transient pressure drop per 1,000 feet versus time, due to dropping load and dielectric loss

Curve *A*—From full load
Curve *B*—From 75 per cent load
Curve *C*—From 40 per cent load
Curve *D*—From 25 per cent load

Table I

Circuits	Station A to B	Station B to C
Operating voltage (for calculations)	120 kv	
Frequency	60 cycles	
Number loaded cables per duct bank	6	3
Insulation thickness*	0.500 inch	0.500 inch
Lead thickness (for calculations)	0.125 inch	0.133 inch
Duct centers (for calculations)	$5^1/_4$ inches	$6^1/_2$ inches
Duct configuration	*A B C* *C B A*	*C* *B A*
Maximum daily load factor	75 per cent	
Maximum daily loss factor	$62^1/_2$ per cent	
Maximum ambient temperature of earth	20 degrees centigrade	
Maximum permissible copper temperature	70 degrees centigrade	
Duct thermal constant	1.04	
Thermal resistivity of insulation	550 ohms	
Power factor of insulation at 70 degrees centigrade	0.75 per cent (guaranteed value)	
Specific inductive capacity of insulation	3.7	
Sheath currents	Flowing	Not flowing
Current amperes	375	600
Conductor area circular mils	300,000	650,000

* See Table II.

mendations of Herman Halperin of Chicago, whose advice on this and other features is gratefully acknowledged.

Records of cable pulls on section 1 indicated that for straight runs a coefficient of friction of about 0.25 to 0.3 could be used for calculating cable pulls. On this basis it was decided to attempt to locate manholes up to 1,000 feet apart. As constructed, the maximum length is 975 feet, 8 inches, and the average length for section 3 is 814 feet. For this reason particularly rigid inspection of ducts was made to eliminate all bad or doubtful pieces of ducts or couplings. The duct supply, however, was found to be extraordinarily good.

Ducts were laid in a concrete envelope and in a manner to provide drainage to manholes. An attempt was made to maintain a 30-inch cover at least. In all but a very few cases the manholes have drainage connections to the city sewer system. In consequence, very little time had to be spent pumping water.

One section of duct run extends under a ship canal. Here the duct mass has a 6-foot cover to the bottom of the canal. Considerable care was taken in this construction to insure the best reinforced concrete job possible, not only because it passed under a ship canal, but further because the bottom on which the mass was constructed was quicksand. This section was built of 3,000-pound concrete reinforced with steel rods. The slump of the concrete mix was watched especially carefully.

2. Number of Conductors

Cable dimensions practically preclude the use of multiconductor cables at 120 kv. Three single-conductor cables per circuit consequently were the necessary decision, and design and calculations were based accordingly.

3. Oil Channels

There are in common use for single-conductor cables, two oil channel sizes, namely 0.500 inch and 0.690 inch.

The smaller diameter core results in a substantial saving in cable cost, but the larger core permits feeding nearly twice as far as the smaller core. The use of the smaller core increases substantially manhole, joint, reservoir, and miscellaneous accessory and installation costs. At the same time it restricts the choice of the type of reservoir and adds to the number of points requiring operating supervision and inspection.

It was decided to employ the 0.690-inch oil channel, especially since section 1 was the only part originally contemplated, and, as it was 8,800 feet long, this permitted its being fed from the ends alone.

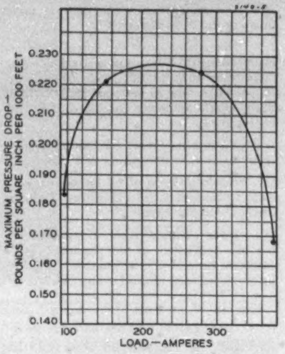

Figure 5. Curves of maximum transient pressure drops, versus load prior to interruption

4. Size of Conductor

The size of conductor was determined by accepted methods[3] based on thermal limitations. Bases for calculations were as set forth in Table I.

At the time of designing the system as far as station *B*, generous factors of safety for a single circuit determined the choice of 300,000-circular-mil conductors. The addition of the second circuit brought the capacity of this part of the system to approximately 140,000 kva.

A load of about 30,000 kva is expected at station *B* during the war period, leaving an available capacity of 110,000 kva or more to pass on to station *C*. It was therefore decided to continue the tie line for this capacity, which gave a cable size of 650,000 circular mils as indicated by Table I, the load and loss factors having been determined as follows:

The shape of the cable load curve was the Montreal, Light, Heat, and Power Consolidated's system load curve inverted and superimposed on a flat load curve whose value was the difference between the system peak and its generating capacity. This gave a load factor less than 75 per cent and a loss factor of above 50 per cent. Such being the case, it was decided to make all calculations on the more or less standard basis of 75 per cent load factor and $62^{1}/_{2}$ per cent loss factor, which were expected would provide the additional capacity required to carry weekend surplus power.

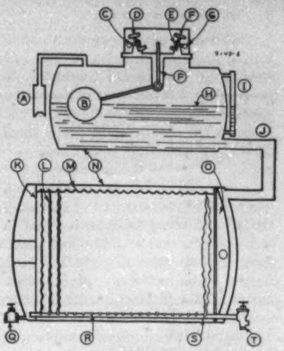

Figure 6. Schematic diagram of new gravity-type oil reservoir (Canadian design)

A.	Activated alumina dehydrator
B.	Float
C.	Oil-level relay
D.	Low level
E.	High level
F.	Mercoid switch
G.	Counterweight
H.	Gauge oil level
I.	Oil-level indicating gauge
J.	Pipe-connecting tanks
K.	End plate for cell unit
L.	Oil cells
M.	Cell spacers
N.	Galvanized steel tank
O.	End plate for cell unit
P.	Actuating arm
Q.	Gauge oil drain valve
R.	Cell oil pipes
S.	Stainless-steel tube manifold
T.	Packless diaphragm valve

5. Thickness of Insulation

As other system characteristics precluded a higher voltage than 120 kv ever being used, an insulation thickness of 0.500 inch was chosen.[4]

Table II. Cable Dimensions

		300,000 Circular Mils	650,000 Circular Mils
Steel channel	Inside diameter (inches)	0.69	0.69
	Outside diameter (inches)	0.79	0.79
Copper conductor	Outside diameter (inches)	0.98	1.17
Paper insulation	Thickness (inches)	0.50	0.50
	Outside diameter (inches)	1.98	2.17
Lead	Thickness (inches)	0.125–0.156	0.133–0.164
	Outside diameter (inches)	2.23 –2.29	2.44 –2.50
Weight per foot	Pounds	7.15 –8.46	8.16 –9.40

6. THICKNESS OF SHEATH

After consultation with several authorities on stresses permissible in lead cable sheaths (a lead sheath containing approximately 0.06 per cent of copper was used), it was decided not to exceed a fiber stress in the sheath under static conditions of 125 pound per square inch.

The elevation of the oil reservoirs having been determined by considerations dealt with in the next part of this paper, the actual cable profile showed that internal oil pressures would be such that in some sections the cable sheaths would require to be increased. Sheath thicknesses for the 300,000-circular-mil cable varied from 0.125 inch to 0.156 inch and for the 650,000-circular-mil cable from 0.133 inch to 0.164 inch.

Figure 3 illustrates the cable construction.

7. SPACING BETWEEN OIL-FEED POINTS

The making of this decision involved some of the most extensive and interesting calculations of the entire design. The governing consideration is that on dropping load the cable cools and demands oil. During this cooling period a "transient" drop in oil pressure occurs at all points away from the oil supply and is greatest at the point most remote from supply, that is, halfway between feed points.

It is a standard of oil-filled cable system design that no point in the system shall fall below one pound per square inch above atmospheric pressure (in order to prevent ingress of air or moisture if the cable sheath be punctured). When the "transient" drop for unit distance under worst conditions is known and correlated with the pressure head permissible without overstressing the sheath (which sets the reservoir elevation or pressure), the maximum feeding distance is determined.

Steps in determining the "transient" are, in brief:[5]

(*a*). Determination of the thermal capacitances of the cable elements.

(*b*). Determination of the thermal resistances of the cable elements.

(*c*). Determination of the rate of cooling of each element from representative initial conditions such as full load, 75 per cent, 50 per cent, and 25 per cent loads at minimum earth ambients (assumed 0 degrees centigrade).

(*d*). Determination of oil-volume demand (proportional to first power of *c* above).

(*e*). Determination of oil temperatures at each instant and the viscosities of the oil at these temperatures.

(*f*). Determination from *d*, *e*, and fluid friction coefficient of oil channel, of the transient drop for a unit length at successive intervals for each initial loading condition.

Curves of pressure drop against time for dropping from various loads are shown in Figure 4. It will be observed that maximum drop occurs as late as three minutes after zero time.

Figure 5 of maximum pressure drop versus initial loads is then drawn from the maximum values obtained from the curves of Figure 4. It will be seen that worst conditions obtain in dropping about one-half to two-thirds full load during winter conditions.

As pressure heads in the order of 15 pounds per square inch are quite safe for normal lead sheaths of these diameters, it was felt that there were ample margins of safety in using feeding distance of up to 5,000 feet, that is, spacing between feed points of 10,000 feet. This proved suitable for conditions in the field, as station *B* is 8,800 circuit feet from section *A*, and there were suitable locations for oil-reservoir structures at approximately 9,000 feet and 18,000 feet from station *B*, and station *C* lies about 27,000 feet from station *B*.

8. TYPE OF OIL FEED

Two general types of oil reservoirs are available to the design engineer.

(*a*). *Gravity type* in which the requisite pressure to reach all the system is obtained by locating the reservoir at an elevation determined by the cable profile and the feeding distance, as the oil in the reservoir is at atmospheric pressure (plus the slight back pressure of the cell walls). In practically all cases, excepting for unusual profiles, gravity reservoirs must be mounted above the ground surface and cannot be used unless above-surface space is available at the feed points.

(*b*). *Balanced Pressure type* in which the requisite pressure is obtained by gas confined within the reservoir shell (plus auxiliary gas tanks if necessary) and external to the oil cells. The use of such reservoirs requires careful gas volume calculations and adjustments and introduces certain extra operating and maintenance items. However, they can be mounted in manholes and must be used when above-surface reservoir space is not available.

In the systems covered by this paper above-surface space fortunately was available adjacent to desired feeding points. The gravity type of oil feed was chosen.

Figure 6 illustrates one type of gravity-type reservoir employed. This reservoir is a new development not previously used. It was designed and manufactured in Canada and has particularly low dead oil volume and cell back pressure. It is of 36 gallons (U. S.) capacity between plus 0.5 pound per square inch and minus 0.1

Figure 7 (left). Gang-treating normal joints

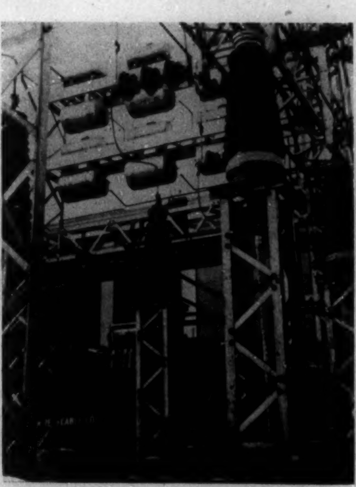

Figure 8 (right). Station *B* potheads, oil reservoirs, and oil pipe lines

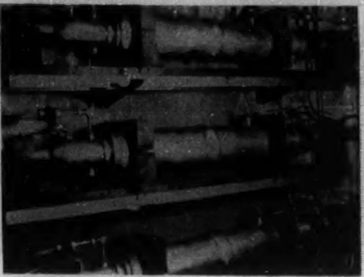

Figure 9 (left). Stop joints, cradles, and piping (manhole 1040)

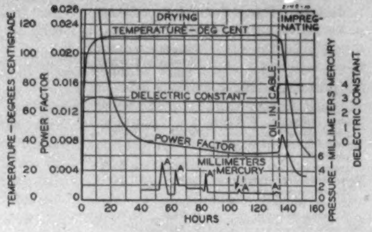

Figure 10 (right). Evacuation and impregnation curves

pound per square inch cell pressures. The smaller tank acts as a riser for the gauge oil to permit keeping the oil cells of the main reservoir completely immersed at all times and out of contact with the air.

The low dead oil capacity is due to both sides of each cell nesting together. Tests of numerous individual cells gave a life of 1,500 cycles not to failure. Each cycle was the full yearly travel of the cell. The daily working would not be 25 per cent of this amount.

9. Normal Joint

Normal joint design is similar to that for high-voltage solid-type cable with provision by means of hollow cores, three way valves, and oil ports for flushing out of any cable oil overheated during the soldering operations and establishing a permanent oil channel through the connector for servicing cable sections lying beyond the joint. Sharp depressions in the connector body which might set up high electric stresses were filled in by lead discs cast on the job and dressed carefully to the contour of the adjacent surface.

The initial joints were imported from the United States. The later ones were made in Canada (see Figure 7). Both were of designs in common use in the United States.

10. Pothead Design

Pothead design requires provision for relieving end stresses such as by combination of stress cones and barrier tubes, access to the cable core for oil to enter and leave the cable, and means of shutting off the core of the cable from the pothead body while erecting, evacuating, and filling the pothead. Two designs in common use in the United States were employed. On account of the low temperatures to be anticipated in the district, extra large oil ports were provided in the connector fittings. Here again part were manufactured in United States, but the later and larger quantities were made in Canada (see Figure 8).

11. Stop Joints

Stop joints serve two purposes. One is to segregate the oil systems where contours require that one section, having for example a higher elevation than an adjoining section, needs its reservoirs mounted at a higher elevation. At the same time troubles in one section are not communicated to adjacent sections. The second main function is that these joints act as a feed point and provide means of ingress and egress for the oil in the cable core. In the design utilized the stop joint essentially is a pair of cable terminals placed end to end, joined by a suitable connector block, and enclosed within a copper sleeve. Reinforcement insulation and enclosure within oil permits these terminals to be quite compact and readily mounted within a manhole. These were imported from United States.

Oil entry fittings are provided at each end of the joint to provide independent oil feeds to the cable sections on both sides of the joint. Impregnated wooden cradles support these long joints (see Figure 9).

12. Size of Oil-Feed Pipe Lines

Due to the very low temperatures likely to be encountered in the locality, and acting on the recommendation of Herman Halperin, it was decided to make exposed oil-feed lines from the reservoirs 1.5-inch copper pipe (Figure 8). Those parts of the oil-feed lines to the stop joints between the valve panel (where it is close to the foot of the structure) (Figure 9) and the stop joints were of one-inch soft copper pipe pulled into fiber ducts.

Manufacture

The manufacture of these oil-filled cables, although only slightly more complex, was different from the manufacture of solid-type cables, chiefly in that drying and impregnating of the paper insulation was done after the core was lead-sheathed. This is possible because of the presence of the oil channel within the conductor.

As may be seen from Figure 3 the conductor is of a novel make-up. Instead of the usual round wires, in single or multiple layers, 12 segments, carefully designed to fit snugly around the steel channel and present a smooth outer periphery, were stranded with a ten-inch right-hand lay.

An advantage for this conductor is that it inherently reduces the electrostatic stresses formed unless remedial measures are used at the crests of wires in conventional stranding.

After insulating, the core was heated at atmospheric pressure for five to six hours, sufficiently long to dry the insulation slightly and loosen the paper tapes so that the possibility of tearing the tapes during leading would be reduced. Leading followed immediately after the preliminary heating. The cable dimensions were as listed in Table II.

Evacuation and impregnation is effected through pipes wiped by fittings to the ends of the cable length, the sheath acting as the vacuum container. A typical impregnation curve with electrical and absolute pressure values is given in Figure 10.

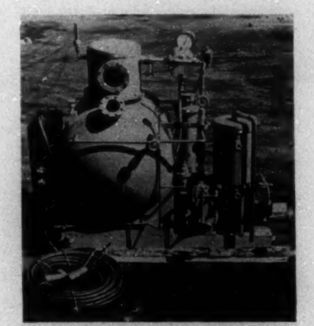

Figure 11. Portable degasser, operating side

Table III. Typical Test Values of Cable

	300,000 Circular Mils	650,000 Circular Mils
Insulation resistance, megohm miles at 15.5 degrees centigrade	6,000	5,000
Conductor resistance, ohms per 1,000 feet at 25 degrees centigrade	0.0355	0.0163
Power factor of insulation at 25 degrees centigrade:		
20 volts per mil	0.0038	0.0031
Working pressure	0.0043	0.0036
190 volts per mil	0.0046	0.0039
Ionization	0.0008	0.0008
Power factor of insulation at working pressure:		
22 degrees centigrade	0.0045	0.0040
80 degrees centigrade	0.0055	0.0048
60 degrees centigrade	0.0040	0.0043
40 degrees centigrade	0.0043	0.0038
Room temperature	0.0044	0.0040
Dielectric watts loss per foot, three-phase, 20 degrees centigrade	0.70	0.70
Dielectric constant at 35 degrees centigrade	3.94	3.94

After cooling, the cables were tested based on Association of Edison Illuminating Companies specifications.[4] Typical results are given in Table III.

Following the tests, the inside end of the cable length was connected through a flexible pipe to degassed oil storage reservoirs housed in the cable drum (Figure 13); then a pulling eye was fitted to the outside end of the length. The pulling eyes were provided with a threaded opening through which oil connections can be made.

With the pulling eye wiped on, the cable was ready for its final washing. A sufficient quantity of oil was washed through the cable core to remove any trace of burnt oil present in the cable because of the wiping operations incidental to fitting the pulling eye. When the power factor of the oil reached a value similar to that of the degasser storage tank oil, and the volume of oil the cable would expel from ten pounds pressure was under the specified limit showing the almost total absence of dissolved gas, the cable was made ready for shipment.

Installation

As this system was sold on an installed basis, the manufacturer was responsible for the cable and accessories until the cable went into operation. Under the manufacturer's engineer in charge, an organization was set up in which a cable-installation company contracted to do the handling, trucking, pulling, and such similar items, to supply top-grade skilled splicers, helpers, and laborers, and to supervise that phase of operation. Great credit is due to this company, as the success and speed with which the work was done was in no small measure due to their lengthy experience and to the high quality of workmanship of their skilled staff.

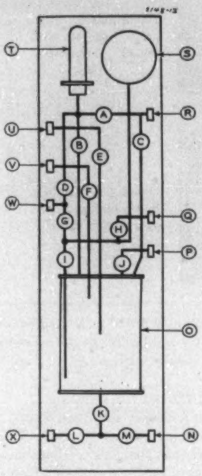

Figure 12. Treating bottle, valves, and connections

Treating panel packless diaphragm valves

A. Jumper vacuum
B. Bottle manometer
C. Bottle vacuum
D. Cable manometer
E. Oil return *A*
F. Oil return *B*
G. Cable oil
H. Oil line
I. Bottle oil
J. CO_2
K. Bottle bottom
L. Joint drain
M. Drain

Inlets and outlets

N. Drain outlet
O. Oil-test bottle
P. CO_2 inlet
Q. Oil inlet
R. Vacuum inlet
S. Compound gauge 30 inches-0-30 pounds
T. Absolute manometer
U. Oil return *A*
V. Oil return *B*
W. Vacuum oil outlet
X. Joint drain

Again under the engineer in charge was a staff of inspectors and engineers. The former checked each operation and dimension as performed. The latter were responsible for "treating" that is, evacuating and filling) all accessories, checking accessories as received, keeping reservoirs filled, and operation of the degasser. They were assisted by truck drivers who became skilled in handling the degasser and in many other duties. In addition, the utility maintained their own staff of inspectors checking and co-operating with the foregoing.

The installation of an oil-filled system involves planning for, training for, or executing the following:

1. Provision of suitable stock room, field office, tools, supplies and equipment, especially portable degassing equipment.
2. Provision for temporary source of degassed oil for feeding cable after pulling into ducts and before jointing.
3. Training of personnel, technical, semitechnical, and skilled tradesmen, in handling of cable and accessories, joint making and accessory installation.
4. Transport of cable and accessories from delivery point to point of installation.
5. Test check of cable or accessory for any leaks or imperfections before starting installation.
6. Actually installing, jointing, and so forth.
7. Continuous inspection and supervision of all operations.
8. Keeping of detail records of each individual operation, progress records, charts, and a daily log.
9. Making field check tests.

The basic considerations are that no air or other gas shall remain in, or in contact with, the cable oil, nor shall any overheated oil remain in the system. All training and inspection keep these prime necessities in view at all times. As detail procedure and instruction sheets are quite lengthy, this article can give only the general principles and procedure followed.

1. TOOLS AND EQUIPMENT

(*a*). A portable degasser, a vital piece of equipment, (Figure 11) serves as a source of high vacuum (less than one-millimeter absolute pressure) and as a source of warm degassed oil. The same special oil used in impregnating the cable is taken to the field in 50-gallon sealed drums. Here it is tested for breakdown strength and color, drawn into the portable degasser, filtered, and sprayed under high vacuum into the main tank of the degasser. It is then recirculated, through a rigidly controlled heat exchanger, and again sprayed into the high vacuum until it reaches approximately 60 to 65 degrees centigrade and is thoroughly degassed. A viewing window in the dome of the degasser permits ready inspection of the oil condition. Vacuum and pressure gauges, thermometers and thermostatic controls, automatic cutoffs, and similar safety devices make this degasser a highly reliable and efficient instrument. During the larger contract two degassers were employed, one mounted in a trailer, the second in a truck.

(*b*). A "treating bottle" and its associated valve panel (Figure 12) are employed at the joint or accessory being treated. These embody a system of valves and vacuum and pressure gauges by which the oil or vacuum from the degasser can be applied to the accessory being treated, the degree of treatment checked, and oil from the degasser checked for quality before being admitted to the cable system.

(*c*). Extra-heavy-walled one-half-inch (inside diameter) special rubber hose is used as vacuum line from the degasser to the work. Soft copper tubing three-eighths inch (outside diameter) is used as the oil line.

(*d*). A plentiful supply of three-eighths inch (outside diameter) soft copper tubing and flare fittings such as nuts, tees, crosses, unions, and so forth was maintained in the field stock room with the degasser and with the inspection crews.

Spare manometers, both atmospheric and absolute types, pressure-vacuum gauges, mercury, a small hand-operated high-grade vacuum pump, appropriate wrenches, spanners, and miscellaneous tools were found essential.

(*e*). Report forms were set up in great detail, individual forms covering the following:

Pulling.
Manhole and reservoir inspection.
Normal joints.
Stop joints.
Terminals.
Degasser log.
Truck log.
Progress charts.

2. Temporary Source of Degassed Oil

As the permanent reservoirs were available at the time cable started arriving at the site, these reservoirs were mounted at or close to their permanent locations. Cable deliveries were scheduled so that lengths adjacent to the reservoirs were first pulled in and connected to the reservoirs, the next lengths connected to the first by temporary oil lines, and so on. This eliminated the use of numerous temporary reservoirs and introduced many economies such as less frequent recharging, handling of reservoirs into and out of manholes, besides the investment cost in such temporary reservoirs. No troubles were encountered, nor was it felt that there was added risk with this technique as compared with practice common in United States.

3. Training of Personnel

Before jointing started, a school was instituted in which actual joints were made under simulated manhole conditions. Jointers and inspectors were required to do each operation of their respective duties in this school. All personnel handling the degasser were required to practice its operation until thoroughly conversant with it.

4. Test Checks

Each cable length at the site was checked by means of an "expulsion" test (Figure 13) to ensure that it was properly impregnated in the factory and that no gas had entered the cable thereafter.

CANADA WIRE & CABLE CO

Figure 13. Expulsion test on reel length

Each reservoir, pothead, feed pipe, and valve panel, before installing and after installing but before filling with oil, was checked for leaks by evacuating and watching carefully for loss of vacuum when blanked off from the source of vacuum.

5. Installation

(*a*) *Cable.* Cable pulling was done by standard methods with careful records of pulling tensions and atmospheric conditions (some of this was done at temperature in the order of 0 degrees Fahrenheit). As the pulling eye enters the manhole toward which the cable length was being pulled, it was connected through a fitting screwed into the pulling eye to a source of degassed oil. The other end of the cable was then disconnected from the reel oil reservoir and pulling completed.

Table A. Coefficients of Pulling Friction

	At Average Temperatures of	
	Cold Weather Minus 7 C	Warm Weather 23 C
Maximum	0.520	0.370
Minimum	0.190	0.120
Average	0.312	0.258

Coefficients of friction computed from recorded pulling tensions are shown in Table A.

(*b*) *Reservoirs.* After erection and with valves installed, reservoirs were checked again by blank off. In the case of the Canadian-built reservoir of new design, calibration was effected as follows:

The cells were completely collapsed and degassed by being placed under vacuum. Gauge oil was then poured in through the riser tank until at the normal zero on the gauge glass. The volume of oil contained by the cells, when at operating zero, (minus 0.1 pound per square inch) is five gallons for these 36-gallon reservoirs. Accordingly, five gallons plus or minus an amount to allow for the contraction or expansion of the gauge oil between ten degrees centigrade (assumed normal temperature) and its temperature at the time of charging were drained from the gauge oil. The reservoir thus calibrated was then charged with degassed oil to whatever volume was required for the system at the time.

(*c*) *Oil Lines.* The permanent oil lines were of copper pipe with joints of the soldered capillary type. Each oil line was heated to remove moisture, tested by blank off for vacuum tightness, flushed with clean oil at 110 degrees centigrade and, if not for immediate use, was backfilled with dry carbon dioxide gas. When about to be placed in service the pipe was flushed with degassed oil, reevacuated, filled with degassed oil, and connected to a source of degassed oil fed from a reservoir.

(*d*) *Joints and Potheads.* As these were points at which the cable insulation and newly applied insulation were necessarily exposed to the atmosphere, special means were employed to remove such contamination. This paper, because of lack of space, does not go into details of splicing and erection, but outlines the added requirements above ordinary solid-type cable joints consequent on the type of cable and the principles followed in ensuring that the quality of the cable was not impaired.

1. All oil overheated by soldering operations must be flushed out.
2. There must not be a stoppage of oil channels when work is completed.
3. No sharp stress points may be left.
4. Proper contours must be maintained.

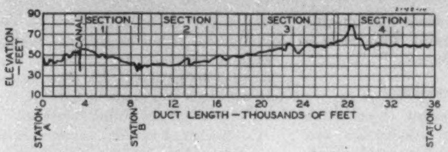

Figure 14. System profile of 120-kv duct line

5. There must be no leaks, even the most minute.

6. Dissolved and occluded gas must be removed.

7. The accessory must be left filled with good degassed oil.

These last two requirements are accomplished by the so-called "treatment." The joint or pothead was connected through the treating bottle to the portable degasser. Vacuum was applied until the accessory had reached a high degree of vacuum and so maintained until, on being blanked off, the completeness of the treating process was indicated by the lack of rise in absolute pressure. Drainage of oil coming out of the insulation during this operation was effected through a drain connection at the lowest point in the accessory. Such drainage, of course, must be into a vacuum.

The accessory was then filled with degassed oil from the portable degasser and connected to the system excepting in the instance of joints when this first filling was drained out, the bottom joint fitting permanently sealed, and the joint retreated and refilled with degassed oil. The top fittings of normal joints were then permanently sealed, as they have no source of oil other than from the cable itself.

In the 300,000-circular-mil section joints were treated immediately after the splicer finished. In the 650,000-circular mil section the splicers completed the three joints in each manhole. The three joints were then "gang" treated (that is, all treated at the same time). (See Figure 7.) This routine permits most effective use of all personnel and equipment and results in substantial time saving provided the skilled mechanics do first-class work. During this phase of operations the gangs were organized for continuous work from 8 a.m. Monday to 8 a.m. Saturday, using three eight-hour shifts.

On account of the small number of stop joints and of other limiting circumstances in the case of potheads this routine was not followed in these cases. Each such accessory was treated as soon as the splicers were finished.

7. Inspection and Supervision

The keeping of detailed and continuous inspection of all operations and constant backchecking was a highly important element in the success of the work. It was found that the skilled mechanics welcomed this inspection and co-operated thoroughly with the inspectors. Besides entering it in the proper report, all unusual results or circumstances were required to be reported directly to the engineer in charge.

8. Keeping of Records and Log

Detail reports of personnel doing the work, time in and out, time each subdivision of the operation was completed, values of pulling tension, vacua, weather conditions, and so forth were turned in by the inspectors or engineers at the completion of the operation. These reports were then typed by the office man, and from them and other data, the "daily log" and progress charts were entered. From these data it can be determined what man did any one operation and under what conditions. Progress charts and records served their usual function of assisting in seeing that no operation was neglected and in correlating progress on all parts of the work and on preparation of progress invoices.

9. Field Check Tests

As work progressed, expulsion, impregnation, and oil-flow tests were made frequently. The former two tests checked that no air or other gas had become dissolved or locked in the system. The last named ensured that there was no stoppage or constriction in the oil channels, such as from a wrongly positioned connector valve in a joint, or from solder working into an oil channel.

Operation

Contracts for the first and second sections of this work were placed in May 1940. As they were approaching completion in May 1941, the contract for the third and much larger section was placed. The whole system received voltage tests and was placed in initial service on December 14, 1941, two weeks ahead of the original schedule.

In order to transmit as much power as possible when available, that is, during the off-peak hours, it was decided to rate the cables as follows: Since the original calculations were based on a $62^1/_2$ per cent daily loss factor, it seemed satisfactory to allow a total daily watts loss from all sources equal to that established by using the standard formula for current-carrying capacity and to use as maximum current that established by using the factory measured resistance of the conductor as indicating the copper area rather than the circular-mil area. Since more power was available between midnight and 8 a.m., curves were made to show the maximum current that could be carried during these hours, depending on the current carried during the previous 16 hours, and to give a loss factor of $62^1/_2$ per cent with no current higher than the maximum allowable by calculation.

Records of ground temperature, idle duct temperatures, sheath temperatures in manholes, duct-mouth movements, and oil demands are being taken, and it is hoped that in the future these will provide some valuable information.

Sheath-bonding transformers of the Halperin and Miller design are being installed on circuit *C* which will increase the present load-carrying ability of the cables a considerable amount and at the same time allow for the saving and transmission of the load that is presently taken by the sheath losses. Because of the time element these were not in the main contracts, but provision was made for these transformers by including solder-seal sheath insulators in the joint casings.

The oil level in reservoirs is supervised by a simple alarm system operated by high- and low-level switches at each reservoir. The high or low oil level, as soon as it exists, is known by a station operator who is able to report the section in trouble to the proper persons.

A further precaution is being taken with reservoirs in the form of a dehydrator attached to the reservoir breather. This consists of a cylinder filled with activated alumina through which the air in contact

Table IV. Bill of Principal Materials

Exclusive of Spares

	Produced in Canada	Imported
Cable		
300,000 circular mils	53,400 feet	
650,000 circular mils	81,500 feet	
Number of lengths	201	
Joints		
Normal	124	56
Stop		6
Terminals	21	9
Reservoirs		
36 gallons	24	
17 gallons		12
9 gallons		2
Oil-level relays	24	8

with the gauge oil (that is, the oil outside the reservoir cells) must pass before entering the reservoir. It is felt that by removing the moisture in the air, less sludging will take place around the reservoir cells. The size of these cylinders is such that, with expected load cycles, the alumina should not have to be reactivated more often than once a year.

Load has been carried continuously since January 2, 1942. Operating data are being collected, such as duct temperatures, oil demands, cable movement at duct mouths, shifting of joints, and so forth. Sufficient information of this nature to draw conclusions will require considerable time to collect.

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1. 120-Kv High-Pressure Gas-Filled Cable, **I. T. Faucett, L. I. Komives, H. W. Collins, R. W. Atkinson.** AIEE Transactions, volume 61, 1942, September section, pages 658–65.

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4. Specifications for Impregnated-Paper-Insulated Lead-Covered Cable, "Oil-Filled" Type (third edition). Association of Edison Illuminating Companies, February 1938.

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A New Single-Pole Service Restorer

E. E TUGBY
ASSOCIATE AIEE

Synopsis: The widespread and general use of three-phase automatic service-restoring equipment on low-cost low-revenue distribution lines has created a field for a single-pole service-restoring device having continuously adjustable characteristics for trip current and time delay, and powered by a prestored source of energy. The device described in this paper utilizes a new and unique application for the prestoring of the operating energy and the restoration of the utilized operating energy after transient fault conditions.

A STUDY of the requirements for a source of operating energy for this new single-pole service-restoring device indicated a need for a source that would be independent of fault current and have available a dependable source of prestored energy when a fault occurs.

The successful operation of a very large number of service restorers powered by manually wound or motor-wound torsional springs logically led to the investigation of a similar source.

From this investigation was developed a new and unique application of previously tried and proved principals. In a paper presented by A. E. Brock at the Pacific Coast convention in August 1941, an improved service restorer using a motor of an entirely new type for rewinding the operating spring was described.

This Vibratorque motor is adapted to this new application, but, instead of the customary secondary source of energy being provided, the motor is arranged to be directly in series with the primary line, thus making the new unit a completely self-contained device.

The operating energy of the service restorer is contained in a motor-rewound flat spiral spring where enough energy is stored to provide four opening operations and three reclosing operations on a sustained fault without rewinding, and an infinite number of operations on transient faults, as the Vibratorque motor restores the expended energy upon reclosing the circuit after each transient fault.

Figure 3 shows a schematic diagram of the entire mechanism.

Rating

This single-pole service restorer is rated at 15 kv 50 amperes continuous current-carrying capacity, with a maximum interrupting rating of 1,000 amperes when using the 25–50-ampere trip coils. When the lower current-rating trip coils are used, the interrupting rating is reduced to 40 times the rating of the trip coils installed.

The interrupting ability of this new service restorer has been amply proven by a comprehensive test program where currents of 1,490 amperes at 15 kv were interrupted on a full cycle of three reclosures and four openings.

A complete oscillographic record was made of the tests for record and study.

Two thousand operations were performed at moderate short-circuit currents considerably in excess of continuous current rating, on a 15-kv system, and, on being inspected, the service restorer was found in condition to perform many more operations.

A complete series of impulse tests was performed, and the results recorded on a cathode-ray oscillograph, to verify operation under lightning conditions, and in every case the restorer flashed over from bushing to ground outside the case.

Arc-Extinguishing Devices

The successful and co-ordinated performance of rural lightly loaded low-cost distribution lines under fault conditions depends primarily upon the rapid extinguishment of the arc and the high speed of the interrupting device protecting the circuit.

As the prestored spring operating energy is restrained by a roller-type latch,

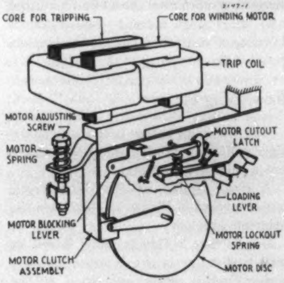


Figure 1. Diagram of Vibratorque motor

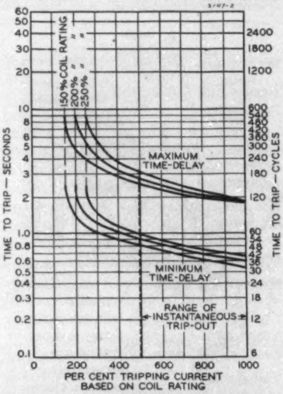


Figure 2. Time-current characteristic curves

Paper 42-147, recommended by the AIEE committee on protective devices for presentation at the AIEE Pacific Coast convention, Vancouver, British Columbia, Canada, September 9–11, 1942. Manuscript submitted June 1, 1942; made available for printing July 3, 1942.

E. E. Tugby is sales engineer, Pacific Electric Manufacturing Corporation, San Francisco, Calif.

Table I. Typical Interrupting-Capacity Tests

Test Item	Number of Tests	Amperes	Average Arcing Time (Cycles)	Duty
1	10	104	1.0	O—5 sec—*OCO—5 sec—OCO—9 sec—OCO
2	6	640	1.1	O—7 sec—OCO—7 sec—OCO—7 sec—OCO
3	6	1,459	0.99	O—7 sec—OCO—7 sec—OCO—7 sec—OCO

* OCO—Open, close, open.

and the fault current affects only the latching mechanism, the operating speed of the arc-interrupting device is independent of fault current. The moving blade travels with a high rate of speed to assure quick extinguishing of the arc in the expulsion chamber, and an actual test performed has shown arc time from 0.65 to 1.2 cycles of arc while interrupting approximately 1,500 amperes at 12.5 kv to ground. The blade tips of arc-resisting Elkonite also assure a minimum of material vaporization, thereby minimizing oil carbonization and consequently reducing maintenance and inspection.

A test run of in excess of 2,000 operations at a moderate short-circuit current and full voltage failed to reveal serious burning of contacts which gave complete proof of the adequacy of the design.

Rewinding Motor

The rewinding motor is of a unique design and has a number of novel features not available heretofore.

A cross section of this Vibratorque motor (shown in Figure 1) illustrates the principle involved.

In referring to this drawing, it will be seen that an armature is mounted on a spring that is rigidly supported at one end and has an adjustment to limit the travel of the spring at the other end. At right angles to the armature is an extension carrying a clutch that engages both faces of a flat hard steel disc. The clutch consists of two hardened steel-bearing rollers engaging in a tapered slot, so co-ordinated as to provide the most efficient transfer of energy from the vibrator to the steel disc, with the least friction on the return stroke. The armature is alternately attracted by an a-c magnet coil assembly consisting of two identical coils mounted on a laminated solenoid core, and returned by a spring. The reciprocal motion of the vibrator alternately causes the clutch to engage on the disc which travels forward a small increment, then to disengage and move back to its original position. The disc rotates in a rapid series of small increments which are transmitted through gears to the main spiral operating spring, where the energy is stored for use in operating the service restorer.

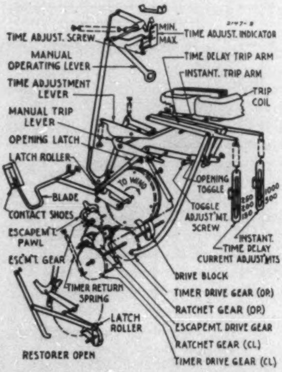
The speed of the output shaft is a function of the weight and size of the armature, ampere turns of the coil, and characteristics of the spring which supports the armature.

The Vibratorque motor develops compound characteristics and with a change in load assumes a moderate output speed change. The magnet coils which also act as trip coils are, as previously stated, connected in series with the line and arranged in two halves to give flexibility. Since they are in series with the line, no external source of operating energy is necessary, and, as the rewinding energy is provided during normal operating conditions, the stored energy is instantly available to operate the device during fault conditions.

The motor will operate successfully with the smallest coils when a current as low as one-half ampere flows through the line.

The magnet coils also perform a dual duty as trip coils and are available in ratings from 3–6 amperes to 25–50 amperes. It is to be noted that pairs of coils have a dual value; as, when the coils are connected in series, they have their lowest rating, that is, 3–6 ampere coils would carry three amperes, but if they were connected in parallel, they would have their higher rating of six amperes as a unit, with each coil carrying three amperes only.

When the Vibratorque motor has fully wound the operating spring to its required tension, the vibrator spring is automatically locked up against the pole pieces. This is controlled through a differential arrangement on the outer case surrounding the operating spring. A scroll is cut in this outer case, in which rides a stud projecting from a bar which thus travels across the face of the case and assumes a position with direct relation to the amount of spring operating energy available. With the spring fully wound, the projection is in a position near the outer periphery of the spring house, and the bar projects beyond the case. When this point is reached, a catch that is normally held under tension is released, and sufficient pressure is applied to the armature support spring to force it up against magnet coils and prevent further vibration. When the spring energy is expended, the differential bar moves down, under the action of the projection in scroll and re-engages the catch, which



TIME ADJUST. SCREW
MIN.
MAX.
TIME ADJUST. INDICATOR
MANUAL OPERATING LEVER
TIME DELAY TRIP ARM
INSTANT. TRIP ARM
TIME ADJUSTMENT LEVER
TRIP COIL
MANUAL TRIP LEVER
OPENING LATCH
LATCH ROLLER
TO WIND
OPENING TOGGLE
BLADE
TOGGLE ADJUST'MT. SCREW
250
200
150
1000
500
CONTACT SHOES
ESCAPEM'T. PAWL
INSTANT.
TIME DELAY
CURRENT ADJUST'MTS
ESC'M'T. GEAR
DRIVE BLOCK
TIMER DRIVE GEAR (OP.)
TIMER RETURN SPRING
RATCHET GEAR (OP.)
LATCH ROLLER
ESCAPEMT. DRIVE GEAR
RATCHET GEAR (CL.)
RESTORER OPEN
TIMER DRIVE GEAR (CL.)

Figure 3. Diagram of mechanism

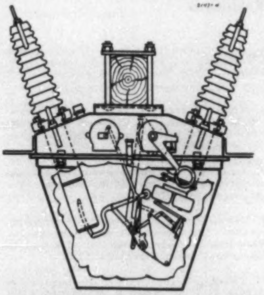
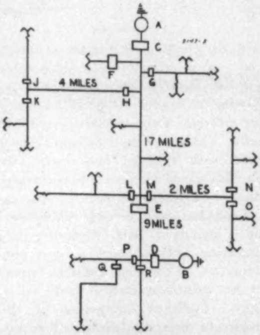


Figure 4. Cutaway section of complete unit



A
C
F
G
J
4 MILES
H
K
17 MILES
L
M
2 MILES
N
O
E
9MILES
P
D
Q
R
B

Figure 5. Typical sectionalized system

releases the spring to vibrate and again supply power to the operating spring.

The center of the coil spring is connected to a shaft which carries on its outer end an operating lever, which in turn is connected to the moving contact blade through a connecting rod.

This blade is normally held in the closed position by the operating spring, restrained by a roller latch mechanism. This type of latch mechanism has been found to give positive latching while requiring a minimum of current for tripping without any diminution of the tripping speed.

The latch may be released by one of the three following methods:

1. By the trip coils actuating the time-delay mechanism.
2. By the trip coils actuating the instantaneous high-current trip mechanism.
3. By the operation of the manual trip mechanism.

All three of these schemes act upon a single common toggle independently and, in the case of the first two, are independently adjustable over a wide range.

Escapement-Type Delay Mechanism

As the complete service restorer was designed for low-cost protection, it was imperative that all working parts be utilized to the fullest degree. Since both tripping and reclosing involved time delay, it was decided that one unit serving both operations could perform these duties admirably, and a mechanism was accordingly built with a rugged design that would handle both duties successfully and reliably.

This time-delay mechanism consists essentially of a gear train with an escapement pawl, which provides a time delay that is independent of temperature conditions.

The time-delay driving gear has a ratchet on either side, and each ratchet is connected to a smaller gear on a shaft. On one side this ratchet gear is driven by the opening gear which is connected to the opening toggle, and the other ratchet gear is actuated by the main blade through a suitable mechanism to control the reclosing time delay.

The correct initial starting position is always assured by a cam return spring that brings the gear train back to normal starting position. If the fault clears before the restorer opens, the time delay will immediately return to its initial position ready for a second fault, thus providing a high degree of accuracy of protection. The reclosing time delay is actuated by a projection on the main-blade operating lever. This projection initially engages the flat spring mounted on a cam which initiates the movement of the gear train. After a short travel, the main-blade operating lever engages the cam, and the driving effort of the main operating spring, transmitted through the main-blade operating lever, tends to rotate the cam, but rotation is restrained by the time-delay escapement.

The reclosing time schedule is fixed at seven seconds, but the tripping time delay may be varied. The tripping time is dependent upon the amount of travel of the escapement, which in turn may be limited by a time-adjustment lever. This lever adjusts the position of the trip arms relative to the trip-coil pole faces and is in its turn controlled through a linkage to the manual rewinding lever. This lever is adjustable to its final position by a set screw mounted external to the housing. Calibration points are provided to indicate the maximum and minimum adjustments, which may be easily adjusted in the field with a screw driver.

Manual Rewinding Mechanism

A manual rewinding mechanism is provided and so arranged that the restorer will not reclose until sufficient spring energy is stored for one closing and one opening operation, thus providing a valuable safety factor when closing in against a fault.

If there is no fault on the line, the rewinding motor then takes up its duties and completes the rewinding of the spring to its proper operating tension.

Time-Current Characteristics

Typical time-current characteristics are shown on Figure 2 where it may be noted that minimum trip is arranged for 150 per cent of rated coil current. That is, in the dual ratings with the coils of a 3–6-ampere combination connected in series for a three-ampere maximum current, the restorer would trip at 4.5 amperes, or if the trip coils were connected in parallel for six amperes continuous current, then the restorer would trip at nine amperes. The two sets of curves for minimum and maximum time delay represent the limits of the lever setting previously described, and each family of curves represents the current adjustment obtainable.

It should also be noted that at 500 per cent of rated coil current, an instantaneous trip mechanism comes into play, and the restorer may be tripped instantaneously. This minimum trip may be easily adjusted from 500 per cent to 1,000 per cent if a higher instantaneous trip value is desired. The current adjustments are both internal to the restorer and may be made with a screw driver as calibration marks are provided, and an indicator may be set for each adjustment.

Mounting

This new restorer is designed for either single-arm or direct-pole mounting and may be readily arranged for either type mounting as required.

Construction

Referring to Figure 4, it will be seen that this new single-pole service restorer consists essentially of a pivoted blade actuated by a small coil spring, mounted on the lower end of the one bushing and an expulsion contact mounted on the lower end of the other bushing. Both bushings are mounted, diverging outwardly in the cover, and are of the standard stud type. A slow-speed motor rewinds the operating spring after each reclosure, and this makes available an infinite number of reclosures on transient faults. On a sustained fault, the prestored spring energy is dissipated at such a rate that, after the fourth spring operation following three reclosures, the restorer will not reclose and is "locked out." To reclose the restorer after lockout, it is necessary to manually rewind with the manual rewinding handle for four or five strokes, to store enough energy for one closing and one opening operation before the restorer closes and the motor begins its rewinding cycle.

Application

The problem of providing high-quality protection against faults, and the maintenance of a reasonable continuity of service at a cost comparable with the revenue obtainable with long lightly loaded low-revenue lines have been brought to the fore more rapidly and forcefully with the shift of population from the cities to the country.

The availability of modern low-cost electric appliances in these rural areas has further heightened the pressure to maintain high-quality service, and, inasmuch as the average rural circuit is of the radial feeder type, it is of utmost importance that faults be localized as much as possible and confined to the least number of customers.

Due to the nature of the territory in which these rural lines are located and the types of low-cost construction used, the prevalence of faults due to lightning disturbances and to trees swinging lines together in wind storms and whipping under sleet conditions is very high.

Studies made in many parts of the country show that an average of 80 per cent of faults may be cleared, and service restored on a first reclosure, which thus justifies the use of reclosing devices on a rural system.

The development of the three-pole service restorer described at the AIEE 1941 Pacific Coast convention was one answer to the problem of high-quality protection for the main feeder, and the development of the single-pole unit described in this paper complements the three-pole unit to relieve the three-phase service restorers of a large number of operations, and to localize a sustained fault to any one particular feeder.

An interesting application is illustrated in Figure 5 based on a co-operative system with widely scattered individual customers and on a moderately heavy load. *A* and *B* are two interconnected generating stations with three-pole motor-rewound service restorers at *C*, *D*, and *E* in the three-phase interconnecting power lines, and at *F* to protect a large load. At restorer *E* potential and current transformers were installed in the main line, and power directional relays were provided to prevent generating station *B* from carrying the entire load if generating station *A* were out of service. Single-pole restorer *G* protects one branch line that is in a reasonably clear area and would not be subject to serious faults. Single-pole restorers *J* and *K* protect long lines radiating through heavily wooded area and are in cascade with a single-pole restorer at *H*. Similarly, single-pole restorer *M* also is in cascade with restorers *N* and *O* to protect another area in trees. Restorers *P*, *Q*, and *R* protect feeders in the vicinity of generating station *B*. As no fuses are to be used for sectionalizing protection on this system, the problem of co-ordination was simply one of superimposing the time-current characteristic curves of single-pole service restorers on those of the three-pole service restorer and arranging tripping time and current settings so as to provide a high degree of selectivity.

At each distribution transformer installation, individual fuse protection was provided; therefore, the problem resolved itself into a group of individual circuit calculations to balance all factors to obtain the greatest flexibility, at the same time restricting outages to the smallest area.

A check was made of the connected capacity on each branch feeder, and it was found that, in any individual leg to be protected, the connected transformer capacity did not exceed 20 kva. On the basis of a load factor of 50 per cent the load current would not exceed 1.5 amperes per circuit, which was well under the continuous current-carrying capacity of the smallest trip coil available.

Therefore all single-pole service restorers used were to be equipped with the same size trip coil, namely 3–6 amperes, and were arranged where there were two in cascade so that the unit nearest the source had the trip coils connected in multiple, for six amperes continuous current-carrying capacity, and those furthest out were arranged with the coils in series for three amperes continuous current-carrying capacity. It was then necessary to make field adjustments as to the time delay and instantaneous trip at various points to properly co-ordinate the entire system.

The three-pole service restorers *C* and *D* at the generating station were each equipped with a first instantaneous tripping action, followed by time-delay tripping on subsequent tripping operations.

It is thus to be seen that each branch line is fully protected by a single-pole restorer which will trip after a time delay. The main-line protective restorer at the generating stations, being equipped with instantaneous initial trip and instantaneous first reclosure, will operate to protect the entire system against heavy transient faults in the main feeders.

The single-pole restorers would be arranged so that the tripping time delay is less than that of the main-line restorer on the second trip out; thus under sustained fault conditions the single-pole restorer will take over the duty of isolating the fault, and the remainder of the system continues with uninterrupted service. Under transient fault conditions the single-phase restorers will clear a fault on the branch lines, of lesser magnitude than that required to operate the main-line restorer at either *C* or *D* and will automatically restore the expended operating energy upon the line circuit being re-energized.

In the case of the system under discussion, after three transient faults had been cleared, each subsequent operation of the single-pole service restorer may be considered as saving the expense of a service man with his transportation to and from the maintenance base to patrol the line and restore service. This saving of labor and transportation offers a considerable reduction in maintenance expense and at the same time provides a very high quality of service in rural areas, comparable to that obtainable in suburban areas, at a very low initial cost.

Stability Study of A-C Power-Transmission Systems

JOHN GRZYBOWSKI HOLM
MEMBER AIEE

Part I

FOR the operation of power-transmission systems of medium and long length, the problem of stability is becoming increasingly important. This is true in the steady state of the system as well as in the transient state, during and after the occurrence of a fault on the system. Since the design of the transmission system is primarily responsible for its proper operation, and since the a-c network analyzer offers the best means for system analysis, the present study was undertaken for the purpose of correlating the design and operational data by means of the a-c network analyzer. For this purpose several power-transmission systems were selected, and their steady-state- and transient-stability characteristics studied. Thus the steady-state- and transient-stability limits of the system were obtained on the analyzer.

As a result of the study, curves were constructed from which it is possible to determine, at various voltages, the stability limits of transmission systems of various lengths, to select the proper voltage for a certain transmission project, and to determine the kilovolt-ampere capacity of generators to be installed, in order that certain steady-state- or transient-stability limits might be attained. Curves were suggested for rapid estimating of system steady-state- and transient-stability limits. Certain means for increasing the stability limits were studied, such as the intermediate synchronous condenser, the resistance in the neutral of the sending transformer, and the reactor on the generator bus. The necessity of the application of these means to the various systems, and the need for generators of special design with lower-than-normal transient reactance and higher inertia constant are shown.

General Principles of System Design

Thirty-one power-transmission systems were designed for the purpose of purely academic study. The systems transmit blocks of power of 50,000, 100,000, 250,000, 500,000, and 800,000 kw (at the receiving end) over distances of 50, 75, 150, 250, and 500 miles, at sending-end voltages of 69, 138, 230, and 345 kv, at 60-cycle frequency. The power is transmitted from hydroelectric power plants to distant metropolitan load centers, and the transmission lines were designed so as to transmit the predetermined block of power. In line with the trend toward high standards of reliability and security of operation, the systems were required to deliver a 100 per cent block of power under a double line-to-ground fault at the sending-end bus, cleared in 10.5 cycles with a consequent loss of a circuit, or a section of it if the circuits are sectionalized. Each system thus has a minimum of two circuits. The block of power transmitted under these conditions and delivered at a corona power loss as small as is economically justified, is taken as the rating of the system.

The long-line theory of line design was used throughout, and the general circuit constants were calculated for the line alone, for a circuit, or for a section of it. In several cases the transmission of a certain block of power over a certain distance was made at two different voltages in order that the effect of the voltage on system stability could be shown.

Transmission-Line Data

All systems were designed to operate at standard voltages at both ends of the system. The voltage drop of approximately five per cent in the line was maintained by means of synchronous condensers at the receiving end of the system.

On all systems under study, only copper conductors were used—both the standard concentric stranded conductor and the hollow copper conductor of the *HH* type manufactured by the General Cable Corporation. The corona power loss was calculated by the Carroll-Rockwell method.[1-3] In view of the great difference in the corona power loss of the two types of conductors, it became necessary to establish conditions for the use of either of the two conductors. A rather conservative criterion was established by which the hollow-type conductor was used whenever the cross section of the concentric stranded copper conductor, of the diameter necessary to secure the desired corona power loss, was about 50 per cent greater than the cross section required of this type of conductor to carry the emergency current of the line.

In every other respect the lines are of conventional design.[4-6] In view, however, of the effect that certain design factors have on system stability limits, it is pointed out that all systems have high-voltage busses at both ends, and all are equipped with a single continuous-type tower-to-tower counterpoise. The list of the systems designed, the types and sizes of conductors, and the corona power losses are given in Table I. Normal and emergency circuit conditions of the transmission lines are represented in Figure 1.

System Characteristics and Representation on the A-C Network Analyzer

The basic study of systems on the network analyzer was made for those with generators of standard design, and not equipped with devices for stability improvement, in order that the stability limits might be obtained for systems with standard equipment. Subsequently the various means for improvement of stability were investigated (see part II) and applied where needed.

The generators were not equipped with damper windings for this study, and their saturated synchronous reactance was taken at 80 per cent of the unsaturated direct-axis synchronous reactance assumed to be 100 per cent. The transient and negative-sequence reactances were taken at 35 and 45 per cent, respectively. The transformer reactances were taken at standard values, with 12.5 per cent for the 345-kv units. For the basic study, the high-voltage neutral of transformers was solidly grounded in all systems. The saturated synchronous reactance of the receiving-end synchronous condensers was taken at 128 per cent, and the transient and negative-sequence reactances were taken at 40 and 24 per cent, respectively.

The transmission systems feed into a metropolitan-system load. The magnitude of this load is assumed to vary in-

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JOHN GRZYBOWSKI HOLM is with the Stone and Webster Engineering Corporation, Boston, Mass.

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versely with the block of power transmitted, ranging from 400,000 kw for the 50,000-kw systems to 1,600,000 kw for the 800,000-kw systems. The power factor of the load is 0.85 lagging. In the circuit representation for the network-analyzer study, the metropolitan load was represented by an equivalent synchronous motor with proper constants and excitation voltage. Such a representation follows from the study of the input characteristics to the metropolitan-load system which, when plotted in the form of synchronous motor charts, resemble the characteristics of the synchronous-motor charts.[7] The reactance of the equivalent synchronous-motor load was taken at the same value for both the steady-state- and the transient-stability studies. Similarly the positive- and the negative-sequence reactances of the load were taken equal.

The inertia constant of generators and synchronous condensers was taken at standard values for the sizes of machines assumed.[8] The inertia constant of the load was calculated for the proportion of turbogenerators and synchronous motors known to be representative of large metropolitan-load centers, and ranged from about 5.15 to 5.60 kw-sec per kva.

System reactances, as represented on the a-c network analyzer, are given in Table II.

Synchronous Condensers at the Receiving End of Systems

Synchronous condensers were placed at the receiving end of every system, of sufficient capacity to maintain rated voltage under all conditions from full load to no load, at an approximate ratio of 1.05 of the voltages at the system ends. The leading kilovolt-amperes were determined for the 100 per cent load under emergency conditions. At the 1.0 to 0.5 lead-to-lag ratio of this condenser, the lagging kilovolt-amperes measured at no load and under normal circuit conditions, were always below the maximum allowed by this ratio.

Table I. List of Systems Designed—Type and Size of Conductors—Corona Power Loss

System Identification Number	Block of Power Transmitted to the Receiving End (Kw_r)	Transmission Distance (Miles)	Transmission Voltage at Sending End (Kv_s)	Number of Circuits in System	Conductor			Corona Power Loss Three-Phase Loss Per Mile of One Circuit (Kw)
					Type	Cross Section (Circular Mils)	Outside Diameter (Inch)	
0-1*a*	50,000	50	69	2	Concentric stranded copper	250,000	0.574	0.52
0-2*a*	50,000	50	138	2	Hollow, *H H*-type (*UM*)	138,000	0.600	0.89
0-3*a*	50,000	75	69	2	Concentric stranded copper	350,000	0.679	0.40
0-4*a*	50,000	75	138	2	Hollow, *H H*-type (*UM*)	138,000	0.600	0.89
0-5*a*	50,000	150	138	2	Concentric stranded copper	250,000	0.574	1.63
0-6*a*	50,000	250	138	2	Concentric stranded copper	300,000	0.628	1.30
0-7*a*	100,000	50	138	2	Hollow *H H*-type	212,000	0.600	0.89
0-8*a*	100,000	75	138	2	Concentric stranded copper	250,000	0.574	1.63
0-9*a*	100,000	150	138	2	Concentric stranded copper	400,000	0.725	0.865
0-10*a*	100,000	150	230	2	Hollow, *H H*-type (*UM*)	265,000	0.950	0.75
0-11*a*	100,000	250	230	2	Hollow, *H H*-type (*UM*)	265,000	0.950	0.75
0-12*a*	250,000	50	138	2	Concentric stranded copper	850,000	1.062	0.36
0-13*a*	250,000	50	230	2	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-14*a*	250,000	75	230	2	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-15*a*	250,000	150	230	2	Hollow, *H H*-type	400,000	0.950	0.75
0-16*a*	250,000	250	230	2	Concentric stranded copper	650,000	0.929	2.07
0-17*a*	250,000	250	345	2	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-18*a*	250,000	500	345	2	Hollow, *H H*-type (*NM*)	587,000	1.400	0.61
0-19*a*	500,000	50	230	3	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-20*a*	500,000	75	230	3	Hollow, *H H*-type (*NM*)	316,000	0.950	0.75
0-22*a*	500,000	150	230	3	Concentric stranded copper	700,000	0.964	1.72
0-23*a*	500,006	150	345	2	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-24*a*	500,000	250	345	2	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-25*a*	500,000	500	345	3	Hollow, *H H*-type	650,000	1.400	0.61
0-26*a*	800,000	50	230	3	Concentric stranded copper	750,000	0.998	1.44
0-27*a*	800,000	50	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-28*a*	800,000	75	230	3	Concentric stranded copper	800,000	1.031	1.22
0-29*a*	800,000	75	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-30*a*	800,000	150	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-31*a*	800,000	250	345	3	Hollow, *H H*-type (*UM*)	505,000	1.400	0.61
0-32*a*	800,000	500	345	4	Hollow, *H H*-type	900,000	1.400	0.61

The system identification number is kept throughout the entire study.

Hollow, *H H*:type copper conductor, General Cable Corporation patents.
Hollow, *H H*-type—standard size.
Hollow, *H H*-type (*NM*)—normal minimum size.
Hollow, *H H*-type (*UM*)—undercut minimum size.

Corona power loss calculated for an elevation of 3,300 feet above sea level and a temperature of 50 degrees Fahrenheit. The equivalent-delta spacings are 10.28 feet for 69-kv lines, 16.67 feet for 138-kv lines, 32.13 feet for 230-kv lines, and 47.88 feet for 345-kv lines.

The kilovolt-amperes of the receiving-end condensers, as obtained from measurements on the a-c network analyzer, are given in Table III. The average leading kilovolt-amperes per kilowatt of the line rating is 1.05 kva in the 50- to 150-mile lines. In the 250- and 500-mile systems, equipped with intermediate synchronous condensers, the average is 0.85 kva per kilowatt of system rating.

The analysis of the figures obtained substantiates the common experience, to the effect that the receiving-end condenser kilovolt-amperes required per transmitted kilowatt increase as the transmission distance or the block of transmitted power increases, and decrease with an increase of the transmission voltage, the other parameters remaining unchanged. The data which have been obtained for a large variety of systems make it possible to estimate the required kilovolt-amperes of receiving-end synchronous condensers for numerous systems, either by direct use of the figures given or by their judicious interpolation.

It should be borne in mind, however, that the systems designed do not make use of the rotating equipment within the metropolitan-load area into which they feed, and are thus made independent of it. Should such use be made, the condenser kilovolt-amperes may be reduced.

The receiving-end condenser unquestionably contributes somewhat toward the steady-state- and transient-stability limits of the system, and hence toward the system rating. The degree of this contribution, however, is difficult to determine rigorously (if, indeed, it can be determined at all), in view of the difficulty in maintaining the same voltages at the ends of the system in the two cases where the system has, and where it does not have, a condenser at its receiving end.

Procedure and Approximations in the Stability Study

The determination of the steady-state- and transient-stability limits was made according to the standard accepted procedure.[9] The steady-state-stability study was made for normal circuit conditions of the system. From the stability limit obtained on the network analyzer, the steady-state-stability limit is calculated for emergency circuit conditions. This calculation is made on the basis that the steady-state limit changes inversely with the total system reactance. The transient-stability limit was determined by the point-by-point method for the condition of a double line-to-ground fault near the sending-end bus. The negative-sequence-

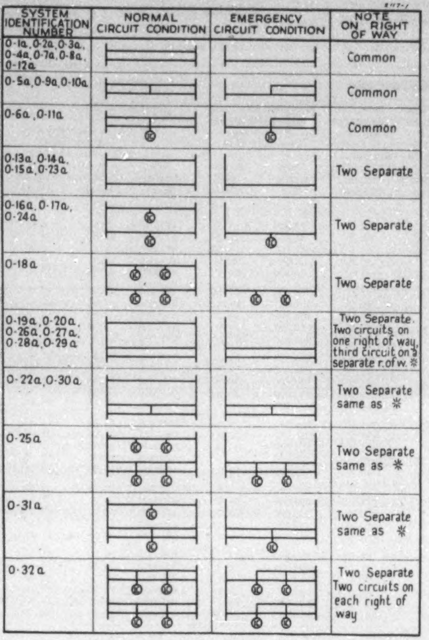

SYSTEM IDENTIFICATION NUMBER
NORMAL CIRCUIT CONDITION
EMERGENCY CIRCUIT CONDITION
NOTE ON RIGHT OF WAY
0-1a, 0-2a, 0-3a, 0-4a, 0-7a, 0-8a, 0-12a
Common
0-5a, 0-9a, 0-10a
Common
0-6a, 0-11a
IC
Common
0-13a, 0-14a, 0-15a, 0-23a
Two Separate
0-16a, 0-17a, 0-24a
IC
Two Separate
0-18a
IC
Two Separate
0-19a, 0-20a, 0-26a, 0-27a, 0-28a, 0-29a
Two Separate. Two circuits on one right of way, third circuit on a separate r. of w. ✱
0-22a, 0-30a
Two Separate same as ✱
0-25a
IC
Two Separate same as ✱
0-31a
IC
Two Separate same as ✱
0-32a
IC
Two Separate Two circuits on each right of way

Figure 1.[12] **Normal and emergency circuit conditions of transmission lines (single-line diagrams)**

IC indicates an intermediate synchronous condenser. Receiving-end synchronous condensers not shown. For commentary of identification numbers of systems, see Table I

and zero-sequence-impedance networks of the system were formed as seen from the place of fault.

Regular simplifications were introduced for the system study on the network analyzer. Of these the following are the most important: The saliency of machines was not accounted for, exciter action was not introduced, and only the first swing of machines was considered. In this part of the paper the stability limits of the 50- to 150-mile systems are reported as obtained in the basic study on the network analyzer, that is, for systems of standard equipment with no devices for stability improvement. The basic study of the 250-mile and 500-mile systems shows that it is absolutely impractical to operate lines of this length with standard equipment, when no devices of any kind are employed for the increase of stability limits. In the present study the intermediate synchronous condenser (see part II) was adopted as the basic device necessary for the satisfactory operation of the 250-mile and 500-mile systems. Therefore, the stability limits of these systems are reported in this part of the paper as obtained for systems equipped with intermediate synchronous condensers, with machines of standard design, and otherwise with standard equipment. The additional means which might be necessary to bring the transient-stability limit of the system to the desired level are discussed in part II.

Discussion of the Steady-State-Stability Limit

The steady-state-stability limit under both normal and emergency circuit conditions is given in Table IV and for normal circuit conditions is represented in Figure 2 as a function of the length of the transmission line. Similarly the stability limit may be represented as a function of the sending-end voltage or of the kilovolt-ampere capacity of installed generators.

The family of curves obtained is susceptible to interpolation and may be used for the following purposes:

1. To determine the steady-state-stability limit of the system when the line length, the system voltage, and the installed generator capacity are known.

2. To select an appropriate transmission voltage for a certain project when the design is to be based on the magnitude of the steady-state-stability limit.

3. To determine the kilovolt-ampere generator capacity which must be installed in a system of a certain length operating at a certain voltage, if a certain steady-state-stability limit is desired. Also to determine the maximum transmission distance when the generator kilovolt-ampere capacity and the system voltage are known.

4. To give an indication of conditions where the steady-state-stability limit may be within the economical region, and hence an indication of the order of magnitude of the kilovolt-ampere generator capacity which can be economically installed.

Figures on the steady-state stability obtained on the network analyzer on a wide variety of systems clearly demonstrate the relative effect on the stability limit of the system reactance and the transmission voltage. In the heavy lines the large kilovolt-ampere generator capacity has an effect on the steady-state-stability limit which is greater than that exerted by the transmission voltage. Since the systems designed represent fairly economical combinations of the block of power transmitted and the transmission distance, it can be seen that in the longest systems the steady-state-stability limit is more affected by changes in the mileage than by changes in the system voltage. This again has a bearing on the economics of the system.

Curves for Estimating the Steady-State-Stability Limit

Further correlation of data obtained on the a-c network analyzer makes it possible to express the steady-state-stability limit as a function of a quantity which for convenience is denoted as β, thus providing a family of curves for quick estimation of the stability limit of the system. The interpretation is simple and provides a sufficient degree of accuracy in estimating the stability limit of a great variety of trunk-line systems differing in details from those investigated.

The stability limit is plotted as a function of the quantity

$$\beta = \frac{E_s \cdot E_r}{X} \qquad (1)$$

where E_s and E_r are, respectively, the sending- and receiving-end line-to-line voltages in kilovolts, and X is the total system reactance as taken for the steady-state calculations and expressed in ohms on the system generator base. More specifically, X is the sum of the generator saturated synchronous reactance, the reactances of the sending-end transformer, the transmission line under either normal or emergency conditions as the case might be, the receiving-end transformer, and the load reactance as taken for the steady-state study.

Table II. System Reactances

(As Represented on the A-C Network Analyzer for System-Stability Study)

System Identification Number	Generators (Waterwheel)			Sending-End Transformers Delta-Grounded-Wye		Transmission Line		Receiving-End Transformers Grounded-Wye-Delta-Grounded-Wye		Metropolitan-System Load		
	Total System Kva	Synchronous Reactance X_s'—Saturated (Ohms)	Transient Reactance X_d' (Ohms)	Total System Kva	Reactance (Ohms)	Total Line Reactance of All Circuits Under Normal Circuit Conditions (Ohms)	Total Line Reactance of All Circuits Under Emergency Circuit Conditions (Ohms)	Total System Kva	Reactance (Ohms)	Load Assumed (Kva)	Load Representation as Equivalent Synchronous Motor: Resistance (Ohms)	Load Representation as Equivalent Synchronous Motor: Reactance (Ohms)
0-1*a*	55,200	69.0	30.2	55,200	6.05	19.3	38.6	53,500	5.7	471,000	3.1	4.65
0-2*a*	52,400	291.0	127.0	52,400	30.9	19.85	39.7	50,800	29.1	471,000	12.35	18.5
0-3*a*	57,700	66.1	28.9	57,700	5.8	27.85	55.7	57,200	5.35	471,000	3.1	4.65
0-4*a*	53,300	286.0	125.0	53,300	30.4	29.8	59.6	51,900	28.5	471,000	12.35	18.5
0-5*a*	53,500	285.0	124.6	53,500	30.3	61.8	91.5	50,800	29.1	471,000	12.35	18.5
0-6*a*	54,700	279.0	122.0	54,700	29.6	100.8	151.0	51,300	28.85	471,000	12.35	18.5
0-7*a*	105,800	144.2	63.0	105,800	15.3	19.9	39.8	102,600	14.4	647,000	8.95	13.45
0-8*a*	107,400	142.0	62.0	107,400	15.1	31.0	62.0	104,900	14.2	647,000	8.95	13.45
0-9*a*	110,100	138.5	60.6	110,100	14.7	59.7	88.5	103,800	14.4	647,000	8.95	13.45
0-10*a*	106,100	400.0	175.0	106,100	54.9	61.6	91.25	103,800	51.3	647,000	24.9	37.4
0-11*a*	110,000	386.0	168.7	110,000	53.0	99.1	148.65	103,800	51.3	647,000	24.9	37.4
0-12*a*	270,000	56.4	24.7	270,000	6.0	18.7	37.4	264,600	5.6	1,030,000	5.65	8.45
0-13*a*	260,200	162.5	71.0	260,200	22.3	20.6	41.2	254,000	20.9	1,030,000	15.65	23.45
0-14*a*	266,000	159.4	69.6	266,000	21.9	30.6	61.2	256,500	20.8	1,030,000	15.65	23.45
0-15*a*	287,000	147.8	64.5	287,000	20.3	60.5	121.0	279,000	19.1	1,030,000	15.65	23.45
0-16*a*	274,000	154.5	67.5	274,000	21.2	104.5	209.0	264,600	20.1	1,030,000	15.65	23.45
0-17*a*	267,200	356.0	156.0	267,200	55.6	101.1	202.2	256,500	53.0	1,030,000	35.25	53.0
0-18*a*	277,000	345.0	151.0	277,000	53.8	200.5	401.0	256,500	53.0	1,030,000	35.25	53.0
0-19*a*	532,000	79.3	34.7	532,000	10.9	13.75	20.6	508,000	10.5	1,470,000	10.95	16.45
0-20*a*	541,500	78.0	34.1	541,500	10.7	20.45	30.6	513,000	10.35	1,470,000	10.95	16.45
0-22*a*	570,500	74.0	32.4	570,500	10.2	41.5	62.3	553,000	9.6	1,470,000	10.95	16.45
0-23*a*	541,000	176.5	77.2	541,000	27.55	60.5	121.0	529,000	25.7	1,470,000	24.7	37.1
0-24*a*	557,500	171.0	74.7	557,500	26.65	101.3	202.6	529,000	25.7	1,470,000	24.7	37.1
0-25*a*	547,500	174.0	76.1	547,500	27.2	133.2	200.0	518,500	26.2	1,470,000	24.7	37.1
0-26*a*	842,000	50.4	22.1	842,000	6.9	13.9	20.9	821,000	6.5	1,882,000	8.55	12.85
0-27*a*	833,000	114.5	30.0	833,000	17.85	13.8	20.75	812,500	16.75	1,882,000	19.3	28.95
0-28*a*	889,000	47.6	20.85	889,000	6.55	20.9	31.3	838,000	6.35	1,582,000	8.55	12.85
0-29*a*	840,000	113.4	49.5	840,000	17.7	20.4	30.6	812,500	16.75	1,882,000	19.3	28.95
0-30*a*	868,000	110.0	48.0	868,000	17.15	40.3	60.5	821,000	16.55	1,882,000	19.3	28.95
0-31*a*	870,000	110.0	47.9	870,000	17.1	67.4	101.2	829,000	16.4	1,882,000	19.3	28.95
0-32*a*	885,000	107.6	47.0	885,000	16.8	100.65	134.2	829,000	16.4	1,882,000	19.3	28.95

Reactances are calculated on the system base and are represented on the high-voltage side.

The curves are represented in Figure 3 for normal circuit conditions of the transmission line. Separate lines are drawn for the systems equipped with intermediate synchronous condensers.

While it is true that the kilovolt-ampere generator capacity has an effect on the system stability limit, this does not preclude the possibility of using the curves for transmission systems whose relative generator and line reactances are different from those which served as a basis for the β curves. The curves are applicable to any fairly economical combinations of kilowatts, mileages, and voltages, and may be used with a degree of accuracy well within normal engineering estimates. They cannot be expected to give reliable figures (if, indeed, any can be given) in obviously uneconomical and mpractical cases such as the transmission of 50,000 kw over a distance of 500 miles at a voltage of 345 kv, or the transmission of 800,000 kw over 250 miles at 138 kv.

The metropolitan-system load of the transmission line for which the β curves are used has unquestionably an effect on the system-stability limit, but, as may be seen from the systems investigated, the load reactance constitutes a relatively small percentage of the total reactance X in the equation 1. Considering that the load on the systems designed was carefully determined with a view to representing actual cases as nearly as possible, it may be said that the method of proposed β curves may be used with the customary degree of engineering accuracy for systems having loads such as are encountered in actual practice. The gaps between the separate β curves could have been filled, had a greater variety of cases been investigated, or had other voltages been used. In the expression for β, excitation voltages could have been made use of instead of the sending- and receiving-end voltages. This, however, would introduce a great many additional calculations which are not justified by the ultimate accuracy possible of attainment.

Discussion of the Transient-Stability Limit

In the process of determining the transient-stability limit on the a-c network analyzer, it was found that the transient-stability limit of a system B may be calculated to a high degree of accuracy from the known transient-stability limit of a system A similar to it in configuration, by using the following expression:

$$(\text{Transient-stability limit})_B = \frac{P_A}{P_B} \cdot \frac{X_A}{X_B} \cdot \left[\frac{E_B}{E_A}\right]^2 \cdot (\text{transient-stability limit})_A \quad (2)$$

where P is the system rating determined solely from consideration of the conductor size and installed transformer capacity, X is the total system transient reactance of the emergency circuit, and E is the nominal system voltage. The subscripts refer to system A or B. More specifically, X is the sum of the generator transient reactance, the reactances of the sending-end transformers, the transmission line under emergency circuit conditions, the receiving-end transformers, and the load. Equation 2 gives a good check with the data obtained by actual measurement on the analyzer, the error not exceeding five per cent. In applying it, the intermediate condensers, if any, may be disregarded. This is explained by the comparatively small increase in the transient-stability limit caused by these condensers (see

Table III. Kva of Synchronous Condensers at the Receiving End of Systems as Determined on the A-C Network Analyzer

System Identification Number	Total Condenser Kva	Condenser Kva Per Kilowatt of Line Rating, Kva/Kw_r	Condenser Kva Per Kilowatt of Steady-State-Stability Limit Under Normal Circuit Conditions, Kva/Kw of SSSL-N	Remarks
0-1*a*	58,700	1.17	0.61	
0-2*a*	42,750	0.86	0.39	
0-3*a*	67,600	1.35	0.83	
0-4*a*	50,900	1.02	0.51	
0-5*a*	37,750	0.76	0.49	
0-6*a*	32,100	0.64	0.44	One *IC* at midpoint
0-7*a*	95,600	0.96	0.49	
0-8*a*	110,000	1.10	0.60	
0-9*a*	105,900	1.06	0.79	
0-10*a*	54,900	0.55	0.34	
0-11*a*	61,100	0.61	0.37	One *IC* at midpoint
0-12*a*	298,000	1.18	0.71	
0-13*a*	235,000	0.94	0.51	
0-14*a*	253,000	1.01	0.61	
0-15*a*	352,000	1.41	1.09	
0-16*a*	285,000	1.14	0.97	One *IC* at midpoint
0-17*a*	203,000	0.81	0.60	One *IC* at midpoint
0-18*a*	214,000	0.86	0.70	Two *IC* equally spaced
0-19*a*	485,000	0.97	0.58	
0-20*a*	526,000	1.05	0.70	
0-22*a*	688,000	1.38	1.17	
0-23*a*	639,000	1.28	1.00	
0-24*a*	537,000	1.07	0.91	One *IC* at midpoint
0-25*a*	410,000	0.82	0.84	Two *IC* equally spaced
0-26*a*	817,000	1.02	0.68	
0-27*a*	688,000	0.86	0.52	
0-28*a*	960,000	1.20	0.82	
0-29*a*	735,000	0.92	0.63	
0-30*a*	870,000	1.09	0.91	
0-31*a*	772,000	0.96	0.88	One *IC* at midpoint
0-32*a*	602,500	0.75	0.84	Two *IC* equally spaced

For commentary on system identification numbers, see Table I.

Approximate sending- and receiving-end voltage ratio 1.05.

Condenser kilovolt-amperes given leading.

IC denotes intermediate synchronous condenser placed for 100 per cent load.

part II of paper), the increase itself being counterbalanced by the error inherent in equation 2. Thus, by a judicious use of this expression, a saving of time may be effected in estimating the transient-stability limit of one system through the known stability limit of another similar system.

The transient-stability limit of the designed systems is given in Table IV. The table also gives the stability limit for zero-duration time of a double line-to-ground fault. This calculation is made by using the appropriate curve of Figure 11 of the First Report of Power-System Stability.[8] The latter curve represents a long and heavy line, and it has been checked on the writer's systems, resulting in data confirming it.

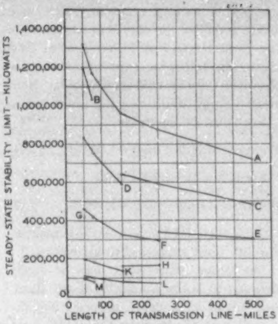


Figure 2. Steady-state-stability limit

Normal circuit conditions

A—800,000—345	*G*—250,000—138
B—800,000—230	*H*—100,000—230
C—500,000—345	*K*—100,000—138
D—500,000—230	*L*— 50,000—138
E—250,000—345	*M*— 50,000— 69
F—250,000—230	

(First figure represents kilowatts transmitted; the second, kilovolts of sending-end voltage)

The 250-mile lines are equipped with one intermediate synchronous condenser; the 500-mile lines, with two intermediate condensers, in capacity sufficient for 100 per cent load

In Figure 4 the transient-stability limit is represented as a function of the length of the transmission line. In like fashion it may be shown as a function of the sending-end voltage, or of the kilovolt-ampere capacity of installed generators. The representation is similar to that of the steady-state-stability limit (see Figure 2). The curves are also susceptible to interpolation and may be used for similar objectives (see discussion of the steady-state-stability limit).

The relative effect, however, of the system reactance and the transmission voltage is different in the case of the transient-stability limit from that of the steady-state limit. In the transient state the generator reactance is much smaller and the line reactance greater than in the steady state. Therefore, while in the steady state the generator reactance predominates even in the longer lines, in the transient state the reverse is true; here the line reactance is of greater significance, the line length assuming the controlling influence. In the greater majority of cases the total system reactance in the transient state is smaller than in the steady state under normal circuit conditions. As an indirect result, the transmission voltage is a considerably more powerful factor in fixing the magnitude of the transient-stability limit than in fixing that of the steady-state limit.

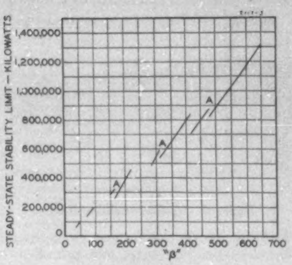


Figure 3. Estimating curves for the steady-state-stability limit

Normal circuit conditions

Curves for systems with installed generator capacity of 55,000 to 950,000 kva, transmitting power to a metropolitan-load center 50 to 500 miles distant, at sending-end voltages of 69, 138, 230, and 345 kv. All systems with receiving-end synchronous condensers in sufficient kilovolt-ampere capacity to maintain a five per cent voltage drop in the line. The 250-mile lines are equipped with one intermediate synchronous condenser; the 500-mile lines, with two intermediate condensers, in capacity sufficient for 100 per cent load. Curve sections denoted *A* are for systems with intermediate condensers. No other means are used for the increase of system stability limits. For explanation of quantity "β," see text of paper

Curves for Estimating the Transient-Stability Limit

As a result of further analysis, the transient-stability limit is represented as a function of the quantity β similar to that used for the representation of the steady-state limit. Thus a curve is obtained for the rapid estimation of the transient-stability limit.

In this case the quantity β has a different meaning from that of the denominator in equation 1, in that X is the total system reactance as taken for the transient-stability calculations, expressed in ohms on the system generator base. The transient reactance of generators and the transmission-line reactance under emergency circuit conditions must now be taken.

The curve thus obtained (Figure 5) permits the estimation, within engineering accuracy, of the transient-stability limit for a system with a double line-to-ground fault cleared in 10.5 cycles. For reasons mentioned above, there is no need for a

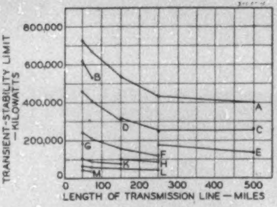


Figure 4. Transient-stability limit

Double line-to-ground fault at the sending end cleared in 10.5 cycles, with loss of circuit or a section of it. For explanation of identification letters, see Figure 2. For circuit condition after fault is cleared, see Figure 1

separate curve for lines equipped with the intermediate synchronous condensers.

The discussion of the β curve and its application in the case of the steady-state-stability limit applies equally well to the transient-stability limit. In addition, it may be said that since the inertia constant of machines in the metropolitan-load center was chosen so as to be representative of actual systems, an additional argument exists for the statement that the curve may be used on systems with loads met in actual practice.

The transient-stability limit given by the curve of Figure 5 may be expressed as follows:

$$\text{Transient-stability limit} = 800 \cdot \beta \text{ kilowatts} \quad (3)$$

with β as defined above. When use is made of the relationship between the severity of various types of faults, such as are given by the curves of Figure 11 of the First Report of Power-System Stability, the following expressions may be obtained for the transient-stability limit at 10.5 cycles' fault-clearing time:

For a line-to-line fault

$$\text{Transient-stability limit} = 1{,}040 \cdot \beta \quad (4)$$

For a line-to-ground fault

$$\text{Transient-stability limit} = 1{,}100 \cdot \beta \quad (5)$$

with β as defined above, and the transient-stability limit expressed in kilowatts.

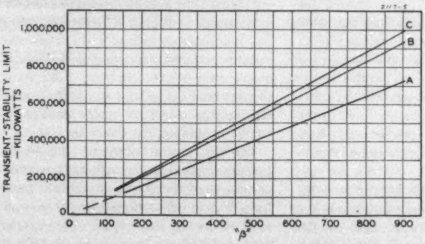
The above expressions may be easily modified to apply for faults of any duration, by using a curve representing the variation of the transient-stability limit with the clearing time of the particular fault. For this purpose, the curves of the First Report of Power-System Stability may be used for trunk transmission lines.

Figure 5. Estimating curves for the transient-stability limit

Sending-end fault cleared in 10.5 cycles

A—Double line-to-ground
B—Line-to-line
C—Line-to-ground

In using the curves for estimating purposes, no distinction is made between systems with and without intermediate synchronous condensers. For general note, see Figure 3



Criteria of the Margin of the Transient-Stability Limit Over the Test Load

In cases where the transient-stability limit of a system is determined on the a-c network analyzer, the better the guesses as to the probable stability limit of the system, the fewer the total number of swing curves to be taken. The procedure may be shortened considerably if use is made of the relative time when, on the one hand, the maximum angle between the generator and the load machines is reached, and on the other hand, the change in the rotor angle of the generator, $\Delta\delta_n$, becomes negative.

When the angle between the machines reaches its maximum before $\Delta\delta_n$ becomes negative, there is a comparatively large margin between the transient-stability limit of the system and the test load. When the maximum machine angle is reached simultaneously with $\Delta\delta_n$ passing through zero, there still is a margin—though a small one—between the stability limit and the test load. When, however, the angle $\Delta\delta_n$ becomes negative before the maximum value of the angle between the machines is reached, then, practically speaking, there is no margin, and the test load may be taken as equal to the transient-stability limit of the system. Observation of the time relationship given makes it possible to reduce considerably the effort required for the study of the system on the network analyzer.

Part II

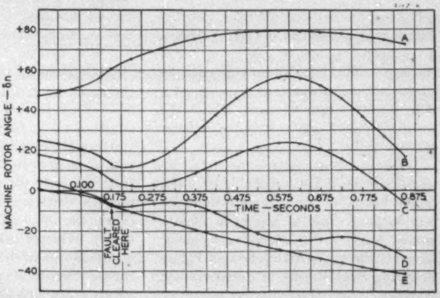
From the data on stability limits of the transmission systems investigated on the a-c network analyzer, given in part I of the paper, it may be seen that only a handful of the systems will deliver the 100 per cent block of power to the metropolitan-system load under the fault conditions prescribed, without the use of special design features. Special designs must be applied to the great majority of systems in order that their transient-stability limits may be increased.

It is the purpose of part II of this paper to investigate, on the a-c network analyzer, some of the means for improving the stability limits of transmission systems, and then to apply the various means to the systems whose transient-stability limits had to be raised to the 100 per cent rating.

Figure 6. System swing curves, taken on the network analyzer (system 0-25a)

13 per cent resistance in sending-transformer neutral. Double line-to-ground fault at the sending end. System stable

A—Generator
B—Intermediate condenser at point two thirds from receiving end
C—Intermediate condenser at point one third from receiving end
D— Receiving-end condenser
E—Load



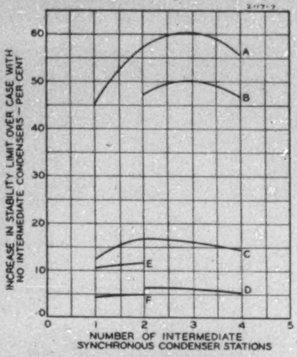


Figure 7. Effect of intermediate-synchronous-condenser stations on system stability limit

Intermediate-condenser stations so located as to divide the line into sections of equal length

A—Steady-state-stability limit—500-mile line
B—Steady-state-stability limit—500-mile line. Reactors substituted for condensers
C—Transient-stability limit—500-mile line
D—Transient-stability limit—500-mile line. Reactors substituted for condensers
E—Steady-state-stability limit—250-mile line
F—Transient-stability limit—250-mile line

Table IV. System Steady-State and Transient-Stability Limits

System Identification Number	Steady-State-Stability Limit: Normal Circuit Conditions (Per Cent)	(Kw)	Emergency Circuit Conditions (Per Cent)	(Kw)	Transient-Stability Limit (Double Line-to-Ground Fault): At 10.5 Cycles Fault-Clearing Time (Per Cent)	(Kw)	At Zero Fault-Duration Time (Per Cent)	(Kw)	Remarks
0-1*a*	193	96,500	162.5	81,250	89.2	44,600	131.5	67,500	
0-2*a*	220	110,000	208	104,000	129	64,500	190	95,000	
0-3*a*	162	81,000	120	65,000	74	37,000	109	54,500	
0-4*a*	200	100,000	190	95,000	115	57,500	169.5	84,750	
0-5*a*	155	77,500	145.5	72,750	99.5	49,750	146.6	73,300	
0-6*a*	147	73,500	132.5	66,250	84.5	42,250	124.5	62,200	One *IC* at midpoint
0-7*a*	196	196,000	177	177,000	105	105,000	154.5	154,500	
0-8*a*	183	183,000	159.5	159,500	91	91,000	134	134,000	
0-9*a*	134	134,000	120	120,000	76	76,000	112	112,000	
0-10*a*	160	160,000	152	152,000	99.7	99,700	147	147,000	
0-11*a*	164	164,000	152	152,000	88.7	88,700	130.8	130,800	One *IC* at midpoint
0-12*a*	167	417,000	138.5	346,000	74	185,000	109	272,500	
0-13*a*	184	460,000	168.5	421,000	97	242,500	143	357,000	
0-14*a*	167	417,000	148.5	371,000	84.2	210,500	124.2	315,000	
0-15*a*	129	322,500	106	265,000	63	157,500	92.8	232,000	
0-16*a*	118	295,000	89	222,500	47.9	119,900	70.5	176,500	One *IC* at midpoint
0-17*a*	135	337,500	116	290,000	71	177,500	104.6	261,500	One *IC* at midpoint
0-18*a*	122.5	306,000	95.5	239,000	54.5	136,300	80.2	200,500	Two *IC* equally spaced
0-19*a*	167	835,000	158	790,000	91.7	459,000	135.2	675,000	
0-20*a*	151	755,000	140	700,000	81.6	408,000	120.3	601,500	
0-22*a*	118	590,000	104	520,000	61	305,000	89.7	444,000	
0-23*a*	128	640,000	108	540,000	62.7	314,000	92.4	462,000	
0-24*a*	118	590,000	92	460,000	49.5	247,600	72.9	364,500	One *IC* at midpoint
0-25*a*	97	485,000	83	415,000	52.5	262,500	77.3	386,500	Two *IC* equally spaced
0-26*a*	149	1,194,000	138	1,105,000	77.4	619,500	114	912,000	
0-27*a*	165	1,321,000	159	1,272,000	90.8	727,000	134	1,074,000	
0-28*a*	129	1,033,000	116.5	932,000	66.4	531,000	97.8	782,000	
0-29*a*	146	1,170,000	138.5	1,110,000	83.6	669,500	123.4	988,000	
0-30*a*	120	960,000	109.5	876,000	67.1	537,000	98.9	792,000	
0-31*a*	109.5	876,000	96	768,000	53.9	431,200	79.3	634,000	One *IC* at midpoint
0-32*a*	90	720,000	80	640,000	49.8	398,200	73.4	587,000	Two *IC* equally spaced

Systems have generators of normal design and are not equipped with devices for stability improvement, except with intermediate synchronous condensers, as indicated under "Remarks."

IC indicates an intermediate synchronous condenser, placed for 100 per cent load.

For list of systems, see Table I.

The intermediate synchronous condenser, the resistance in the neutral of the sending transformer, and the reactor on the generator bus are the three devices for increasing the stability limits which were investigated. They were chosen, because the available data in the literature on their application are either incomplete or nonexistent, and also because they were within the means available for the type of academic study made by the writer.

The Intermediate Synchronous Condenser

There are relatively few power-transmission systems on which intermediate synchronous condensers are installed. Wherever this is the case, however, satisfaction is generally expressed with the part the intermediate condenser plays in the system operation. Since in most systems the installation of the intermediate condenser was made previous to the time when the full significance of the problem of transient stability was realized it may be presumed that the primary reason for installing the condenser was its contribution to the steady-state-stability limit and to the maintenance of voltages at certain points of the transmission line. Indeed, the studies published by Baum,[10,11] Fortescue,[12,13] Fortescue and Wagner,[14] Evans and Bergvall,[15] Petersen,[16] Piloty,[17–19] and Rüdenberg[20,21] deal exclusively with the effect of the intermediate condenser on the steady-state-stability limit of the system. None of these studies were made on the network analyzer, and consequently the method of approach necessitated simplifying assumptions; for instance, in their study of the intermediate condenser, Wagner and Evans[22] assumed an infinite bus (that is, a bus to which so large a synchronous capacity is connected that its frequency and voltage are absolutely unaffected by any conditions external to that bus). There are practically no published data on the effect of the intermediate condenser on the transient-stability limit. Summers and McClure[23] mention the subject briefly, and Tchernycheva and Lavrov[24] discuss the effect of the intermediate condenser on the system stability limit for the case of a three-phase fault.

From the study of system operation it appears that all intermediate condensers on a line may operate either primarily leading or primarily lagging; or that some will operate mostly leading, requiring a 1.0 to 0.5 lead-to-lag ratio, while others will operate only lagging, requiring a 1.0 to 1.0 lead-to-lag ratio. This depends on the length of the line, on the number of condenser stations, and on the ease with which the line carries its 100 per cent load. For instance, on the 500-mile lines with two intermediate condensers, both condensers have a 1.0 to 1.0 lead-to-lag ratio; while with three condenser stations, two condensers have a 1.0 to 1.0 lead-to-lag ratio and the third has a ratio of 1.0 to 0.5. In every case the larger condenser kilovolt-amperes are required under emergency circuit conditions. The kilovolt-ampere capacity of the intermediate condensers and the total synchronous-condenser capacity for the systems under design, for the number of intermediate-condenser stations as finally adopted, are given in Table V.

Table V. Kva of Synchronous Condensers in the 250- and 500-Mile Systems (Receiving-End and Intermediate Condensers) as Determined on the A-C Network Analyzer

		Intermediate Synchronous Condensers			Total Synchronous Condensers in System		
System Identification Number	Receiving-End Synchronous Condensers Total Kva	Total Kva	No. of Intermediate Condenser Stations	Condenser Lead-to-Lag Ratio	Total Kva	Condenser Kva Per Kw of Line Rating Kva/Kw_r	Condenser Kva Per Kw of Steady-State Limit Under Normal Circuit Conditions Kva/Kw of SSSL-N
0-6*a*	32,100	20,500	One	1.0:1.0	52,600	1.05	0.72
0-11*a*	61,100	69,750	One	1.0:1.0	130,850	1.31	0.80
0-16*a*	285,000	349,000	One	1.0:0.5	634,000	2.54	2.15
0-17*a*	203,000	132,500	One	1.0:1.0	335,500	1.34	0.99
0-18*a*	214,000	313,400	Two	1.0:1.0	527,400	2.11	1.72
0-24*a*	537,000	608,000	One	1.0:0.5	1,145,000	2.29	1.94
0-25*a*	410,000	369,000	Two	1.0:1.0	779,000	1.56	1.61
0-31*a*	772,000	386,700	One	1.0:0.5	1,158,700	1.44	1.32
0-32*a*	602,500	325,500	Two	1.0:1.0	928,000	1.16	1.29

For commentary on system identification numbers, see part I, Table I.

Condenser kilovolt-amperes given leading.

Intermediate synchronous condensers located so as to divide line into sections of equal length, and placed for 100 per cent load.

The swing curves of two intermediate condensers on a 500-mile line loaded almost up to its transient-stability limit are shown in Figure 6.

The results of a study of the effect of the intermediate synchronous condensers on the stability limits of the 250-mile and 500-mile systems may be summarized as follows: On an average 250-mile line, with one intermediate-condenser station in the system, the steady-state-stability limit is about ten per cent greater than in the system with no intermediate condensers. The stability limit of a 500-mile line, under the same conditions, is 45 per cent greater. On a 250-mile line carrying its 100 per cent load very easily, the beneficial effect of the intermediate condenser may be considerably greater; for instance, in the system 0-11*a*, which typifies a light line, the steady-state-stability limit is increased by about 20 per cent.

The increase in the transient-stability limit caused by one intermediate-condenser station is far smaller than the increase in the steady-state limit. On an average 250-mile line the increase is less than five per cent; on a 500-mile line it is about 12 per cent. Thus it is seen that the intermediate condenser is more effective on the longer lines than it is on the shorter ones.

As the number of intermediate-condenser stations on the line is increased, the stability limits increase until a certain theoretical optimum is reached. On a 250-mile line the difference in stability limits when one or two intermediate stations are installed is so small that from the economic viewpoint one station will give the best solution. On a 500-mile line, however, the largest increases in the steady-state-stability limit seem to take place with a different number of condenser stations than is best for the transient-stability limit. Economic computations, however, will show that two stations are the optimum. Two stations will increase the steady-state-stability limit by about 57 per cent and the transient limit by about 16 per cent.

Irrespective of the number of intermediate-condenser stations in the system, their location is most advantageous when they divide the entire line into sections of equal length; thus, for a 250-mile line, a mid-point condenser gives the best result.

Doubling the size of the intermediate condenser required to maintain specified voltages increases both the steady-state- and the transient-stability limits by slightly less than ten per cent over the case of a condenser of normal size. The same general result is obtained when intermediate condensers of lower reactance are used.

Study of the substitution of reactors for lagging intermediate condensers was made on systems where all condensers operate fully lagging, as well as on those where some condensers operate lagging while others are leading. The results show that when lagging condensers are replaced by reactors, the stability limits are considerably lowered, especially the transient-stability limit. While in a certain 500-mile system the increase in the steady-state limit obtained from reactors was only ten per cent less than that obtained from synchronous condensers, the increase in the transient-stability limit was less than half that obtained with condensers.

Reactors therefore can be substituted for lagging intermediate condensers only when the steady-state stability is considered of greater importance than the transient stability, and provided the lower initial cost and lower losses make the over-all economic balance in favor of the reactors. The effect of intermediate condensers on system stability limits is represented in Figure 7.

When the load in a 500-mile system drops below the 100 per cent level for which the intermediate condensers are provided, then the voltage at the intermediate points may rise considerably above the desired level and thus endanger the insulation of the system and the continuity of operation. This may also take place in some, though not all, 250-mile lines. Intermediate condensers of sufficient capacity for the 100 per cent load are insufficient to prevent the rise in voltage in these lines with smaller loads. On a 500-mile line, with a 50 per cent load, the intermediate-condenser capacity needed is about double that required for the 100 per cent load; on the 250-mile line it is approximately 25 per cent greater, when equal voltages are to be maintained with both the 50 and 100 per cent loads. It is not economical to provide intermediate condensers large enough to keep normal voltages at very low loads; yet they should be placed for a load smaller than 100 per cent. This will put the system in a more favorable operating condition and will facilitate the charging problem of the line. A corresponding increase in the stability limits of the system will result; this will compensate for the additional cost of the condenser.

Resistance in the Neutral of the Sending Transformer

It has been recognized for some time that the grounding of the neutral of the sending transformer through a resistance has a distinctly beneficial effect on the transient-stability limit of the system. Although this method of grounding has been applied to existing systems, the published data on its effect on the transient-stability limit are very meager. It has been reported,[8] however, that with the sending-end double line-to-ground faults, which are also assumed for the present study, the same percentage gain in the transient-stability limit is obtained when either the neutral of the sending-end

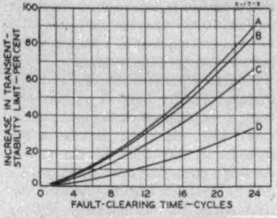


Figure 8. Effect on transient-stability limit of a 13 per cent resistance in the sending-transformer neutral

Double line-to-ground fault at the sending end

A—50-mile line
B—75-mile line
C—150-mile and 500-mile lines
D—250-mile line

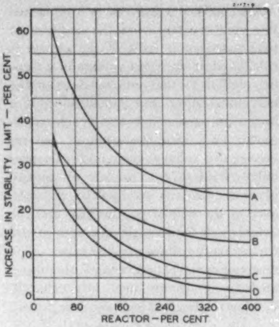


Figure 9. Effect of shunt reactors on generator bus on stability limits of 250-mile to 500-mile lines

Lines equipped with intermediate synchronous condensers

A, B—Steady-state-stability limit
C, D—Transient-stability limit

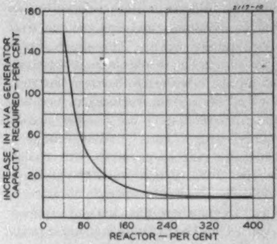


Figure 10. Effect of shunt reactors on generator bus on required kilovolt-ampere generator capacity on 250-mile to 500-mile lines

Lines equipped with intermediate synchronous condensers

transformer alone is grounded through a resistance, or when, in addition to it, the neutral of the receiving-end transformer is grounded through a reactance. Hence, so far as the transient stability is concerned, the grounding of the receiving-transformer neutral is not necessary with the type of fault being considered.

In grounding the sending-transformer neutral of a 50-mile line with various resistors ranging from 5.8 to 17.5 per cent, it was found that the smallest angle between the machines at the ends of the system at the transient-stability limit occurs with a 13 per cent resistance. This optimum resistance, however, is not particularly critical, since the observed variations in the angles between the machines with the various resistor sizes are accompanied by very small differences in corresponding transient-stability limits. This leads to the conclusion that the ultimate selection of a resistance—whether it should be 13 per cent or should differ somewhat from it—must be based on considerations other than stability; namely, on the effect of the size of the resistance on the insulation of the transformer. On this basis, in all systems designed, the study of the effect on the transient-stability limit of grounding the sending-transformer neutral was made with grounding through a 13 per cent resistance.

The increase in the transient-stability limit obtained at various fault-clearing times on lines from 50 to 500 miles along is given in Figure 8. The curves may be judiciously interpolated for any length of line up to and somewhat exceeding 500 miles.

It is seen from the data obtained that the resistance in the sending-transformer neutral makes a very substantial contribution to the transient-stability limit. The percentage increase secured on a 50-mile line is the largest; it then decreases as the line length increases, reaching a minimum on lines somewhat over 250 miles in length. This is due to the fact that as the line gets longer, its zero-sequence impedance increases. A decrease in the zero-sequence current follows and causes in turn a smaller I^2R loss in the grounding resistor. As a consequence, the power output of the generator under fault conditions is not maintained so well on the longer lines as on the shorter. The resultant increase in the transient-stability limit in the 250-mile lines is smaller than in the 50-mile lines.

Shunt Reactors on the Generator Bus

It was suggested some years ago that shunt reactors be used on the generator bus[25] in order to increase the stability limits of the system, and views were expressed to the effect that this method has some real possibilities. There are, however, no published data on the subject.

The writer does not know of any case where such reactors are installed on systems in this country. From a sketchy description[26,27] of the Kembs–Creney 220-kv, 173-mile line in France, it appears that there is a 15,000-kva shunt reactor on the generating bus of the station at Kembs. A statement is made that the reactor renders a very satisfactory service in helping to maintain stability of the line.

The shunt reactor on the generator bus is connected directly from the low-voltage sending-end bus to ground. The reactor increases the total system transfer reactance, it lowers the power factor on the generator, and increases its excitation voltage. The increase in the total system transfer reactance lowers the system stability limit, while the increase in the excitation voltage raises it. As a result, an important increase in the system stability limit is obtained. Since the generators must supply the transmission line with the same amount of active power when the generator bus is loaded with reactors as when they are not used—and this without overheating during the operation with full-load normal field currents—the installed kilovolt-ampere capacity of generators, in the case when reactors are used, must be greater than when there are none.

The study of the shunt reactor on the generator bus was made on the 250-mile and 500-mile systems on the network analyzer and was checked by analytical calculations. Reactors of from 40 to 400 per cent were placed on the 250-mile systems, equipped with one intermediate condenser, and on the 500-mile systems with two condensers. When the reactor was connected to the generator bus, the kilovolt-ampere generator capacity was increased to satisfy the new conditions. The increases in the steady-state- and transient-stability limits obtained on these systems are represented in Figure 9.

It is seen that the shunt reactor on the generator bus greatly increases the system stability limits. In the systems investigated, a 40 per cent reactor increased the steady-state limit as much as 60 per cent, and the transient limit as much as 37 per cent. The increase in the transient limit is considerably smaller than that in the steady-state limit; still it is very substantial. Figure 9 gives two curves for the steady-state- and two for the transient-stability limits. The upper of each pair of curves is obtained for systems where the relationship between the reactor and the system reactance is such that the introduction of the reactor, accompanied with a corresponding change in generator reactance, causes little change in the total system transfer reactance, while the lower of each pair of curves is for systems where this change is appreciable. Thus the percentage increase in the steady-state- or transient-stability limits obtained from a reactor of a certain size lies between the upper and lower curves of a pair, depending on the system. As the size of the reactor increases, its effectiveness declines. The curves of Figure 9 may be used for estimating the range of the increase in stability limits caused by the presence of a reactor on the bus.

A part of the increase in the stability limits of a system with a shunt reactor on the bus is due to the larger kilovolt-ampere capacity of generators required by the reactor. A study of the two factors, made separately on a system having a steady-state-stability limit of 118 per cent, with a normal kilovolt-ampere generator capacity, gave the following results: With a reactor on the bus and the generator capacity correspondingly increased, the steady-state limit rose to 157 per cent; when the reactor was removed, with the increased generator capacity still connected to the system, the stability limit dropped to 132 per cent. The contribution of the reactor itself to the stability limit is plainly evident.

The increase in the steady-state-stability limit caused by the shunt reactor on the bus, as measured on the network analyzer, may be checked in the following fashion: The ratio of the generator excitation voltages for the system with and without a reactor on the bus is first determined. This may be calculated from the voltage vector diagram. The increased kilovolt-ampere generator capacity and the actual angle between the line current and the reference voltage (line sending-end voltage) must be properly represented. The ratio of the total system transfer reactance, without a reactor on the bus, to that with it is then calculated. The product of the two ratios will indicate the increase in the steady-state-stability limit. The result differs from the measured figures by 15 to 20 per cent. This is due to the fact that the excitation voltage is determined for the condition of 100 per cent load, and not at the pull-out for which the stability limit is determined on the network analyzer.

The pull-out condition is approached to a greater extent when the steady-state maximum power limit of the system is calculated at the rated voltage in a manner similar to that employed when the maximum power limit is determined of a long transmission line and synchronous-motor load, with the sending- and receiving-end voltages given and reactances only considered.[9] The kilovolt-ampere generator capacity required by the presence of the reactor on the bus, and the actual angle between the line current and the reference voltage must be taken. The receiving-end condenser and the intermediate condensers may be neglected. The line sending-end leak also is neglected, and the receiving-end leak may be assumed to equal infinity. This method of calculation does not represent a rigorous solution; still it gives a satisfactory check with the data obtained on the network analyzer.

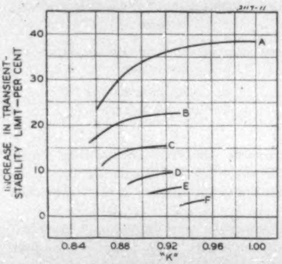


Figure 11. Curves for estimating the increase in transient-stability limit due to shunt reactors on generator bus

Based on study of 250-mile to 500-mile systems with intermediate synchronous condensers. For explanation of quantity *k* see text of paper

Reactor size

A—40 per cent
B—90 per cent
C—130 per cent
D—200 per cent
E—260 per cent
F—400 per cent

The kilovolt-ampere generator capacity required by shunt reactors on the bus is easily calculated when the angle by which the line current leads the sending-end voltage is known. On the 250- to 500-mile lines studied, with one or two intermediate condensers, this angle varies within narrow limits. The increase in generator kilovolt-amperes required by reactors of various sizes in the systems under design is given in Figure 10. With a 40 per cent reactor the increase is over 150 per cent, dropping rapidly as the size of the reactor increases. With a change in the number of intermediate-condenser stations in the line, the generator kilovolt-amperes required by a reactor on the bus will undergo a slight change.

From the effect of shunt reactors on the transient-stability limit of various systems, a set of curves of Figure 11 is obtained from which the increase in the transient-stability limit of the system due to the presence of a reactor on the bus may be estimated when the ratio *k* of the total system transfer reactance without the reactor to that with a reactor is calculated. In determining the ratio *k*, the transient reactances of the generators and the reactance of the line under emergency circuit conditions must be taken. For the system transfer reactance, with the reactor on the bus, the increased generator kilovolt-amperes should be taken as required by the reactor, and the architrave of the equivalent π circuit of the entire circuit must be known. It is also possible to determine from the curves the proper size of reactor to be placed on the generator bus when it is desired to increase the transient-stability limit of the system by a certain percentage, when the system reactances are known. The curves may be applied to double line-to-ground, line-to-line, and line-to-ground faults, and are subject to interpolation.

The increase in the steady-state- and transient-stability limits caused by the presence of the shunt reactor on the generator bus is substantially independent of the length of the line. The increase in the transient-stability limit caused by the reactor is essentially independent of the type of fault and of the fault-clearing time.

In view of the large increase in the kilovolt-ampere generator capacity required by some shunt reactors on the generator bus, the economic benefits from the increase in stability limits due to their presence must be balanced against the cost of the reactors, as well as the expense of the incremental generating capacity which must be installed, including associated housing and other equipment. It should be borne in mind, however, that it is necessary to increase the capacity of the prime-mover equipment only to the extent of supplying the losses in the incremental generating capacity and in all reactors. In so far as reactors are concerned, air-core units may be used. In general, the installation of shunt reactors on the generator bus would be economical in cases of which the following may be representative:

1. When the reactors require a comparatively small increase in generator capacity.

2. When they are to be installed in a system where the total installed generating

capacity is small, so that the addition to it of the high percentage required by the reactors is not excessive.

3. When a transmission line is fed from a bus of a large generating system supplying a large local load at a small annual load factor, so that reactors may be installed without adding to the existing generating capacity.

Other Methods for the Increase of Stability Limits

Various means have been proposed to increase the stability limits of transmission systems. Of these, such methods as shunt static condensers, series synchronous condensers, inductive compensating generator, series loading resistors, quadrature booster transformers, tuned line, neutralizing networks, frequency changers, and other conversion equipment have not been used in the present study. High-speed exciters are not used, because they bring about a very small gain in the stability limit at fast fault-clearing times. The construction of additional circuits or the shortening of the line sections provides only a slight increase in the stability limit, and hence both are hardly economical.

Series capacitors are another major device proposed to overcome stability limitations of long lines. It has recently been shown[23] that series capacitors offer great promise so far as the steady-state-stability limit is concerned. They should, however, be studied under fault conditions in order not only to ascertain to what degree they are effective in overcoming the transient-stability limitations, but also to make sure that they operate properly at the high voltages which under fault conditions may take place in the 230- and 345-kv lines. The means for such an investigation were not at the disposal of the writer, and so this device was not applied to the systems reported in this study.

Four additional methods which are used for the increase of stability limits of the systems designed are:

1. The decrease of the transient reactance of the generators.
2. The increase of their inertia constant.
3. Generator damper windings.
4. High-speed switching.

All these methods are well known; nevertheless the following will be pointed out:

The increase in the transient-stability limit, because of the decrease of the generator transient reactance, is essentially independent of the fault-clearing time, while that obtained from the increase of the inertia constant does depend upon it. On the other hand, the beneficial effect obtained from lowered transient reactance is usually secured at a slightly higher cost. In some systems the inertia of the receiving-end machines may be controlled by the system designer; in the systems of this study the constants of the receiving-end rotating equipment are assumed to be fixed. Damper windings of both high and low resistance serve to improve the transient-stability limit; the former provide negative-sequence damping while a dissymmetrical fault lasts, while the latter have an appreciable positive-sequence damping action after the fault is cleared. Whenever, in the systems reported, dampers are said to be required, they are assumed to be of high resistance. It is felt, however, that the type of dampers to be used must be decided on the merits of the individual case, and that this has little effect on the magnitude of the stability limit. It should be borne in mind that the increase in the transient-stability limit due to the dampers depends on the switching time as well as on the type of grounding of the sending-transformer neutral. By far the most effective factor affecting the transient-stability limit of a system is the fault-clearing time; the high-speed circuit breaker may hence be considered the major device for this purpose. The effect of the fault-clearing time on the increase of the stability limit, with various types of faults, is well known and has already been reported.

In every long transmission system the tower-footing and fault resistance makes a small contribution to the increase of its transient-stability limit. In the zero-sequence network represented on the network analyzer, the tower-footing resistance was omitted. Still, in each system a small fraction of the ultimate limit of its transient stability is credited to the improvement caused by the tower-footing and fault resistance.

Design Features Required by the Transient-Stability Conditions Imposed

From the transient-stability limit of systems at 10.5 cycles fault-clearing time, as given in Table IV (part I of paper), it will be seen that there is a group of six systems, namely, systems 0-2*a*, 0-4*a*, 0-5*a*, 0-7*a*, 0-10*a*, and 0-13*a*, whose transient-stability limit is either very close to 100 per cent or exceeds it. These systems therefore require no special devices for the increase of stability limits. In fact, in some cases the fault-clearing time could exceed 10.5 cycles and still the condition of 100 per cent transient stability would be met. In spite of this, standard eight-cycle circuit breakers are provided for these systems, thus securing in some of them a margin between the rating assigned and the transient-stability limit. The additional generators and transformers which this would permit are not installed at this time; however, these systems will be somewhat more economical when full advantage of transmission capacity is taken. The problem of system expansion is not considered in this study.

The remaining systems are divided into two groups. To the first group belong systems whose transient-stability limit is below 100 per cent but which have a reasonable margin—from 16.0 to 62.5 per cent—between the line rating and its steady-state-stability limit under emergency circuit conditions. These are systems 0-1*a*, 0-3*a*, 0-6*a*, 0-8*a*, 0-9*a*, 0-11*a*, 0-12*a*, 0-14*a*, 0-17*a*, 0-19*a*, 0-20*a*, 0-26*a*, 0-27*a*, and 0-29*a*. Here, as a rule, the high-speed circuit breaker is used to increase the transient-stability limit; and in addition to this, where it is found necessary, either the generator transient reactance is reduced from 35 per cent standard value to 30 per cent, or a damper winding is added. In some systems of this group it sufficed to lower the generator transient reactance to 30 per cent, without having to apply high-speed circuit breakers. On the three 250-mile lines the mid-point intermediate condenser of kilovolt-ampere capacity necessary for a 50 per cent system load has been installed. This is resorted to primarily for the purpose of increasing the transient-stability limit rather than for any other reason.

To the second group belong systems with a small margin between the line rating and its steady-state-stability limit under emergency circuit conditions. These are systems 0-15*a*, 0-16*a*, 0-18*a*, 0-22*a*, 0-23*a*, 0-24*a*, 0-25*a*, 0-30*a*, 0-31*a*, and 0-32*a*. Here the steady-state-stability limit under emergency circuit conditions is either 4.0 to 8.5 per cent above the line rating or 4.5 to 20.0 per cent below it. In these systems the steady-state-stability limit is raised first by lowering considerably the generator transient reactance. This affects the short-circuit ratio of the generator, and through it the steady-state-stability limit of the system. For the same purpose, the generator bus is loaded with shunt reactors, where necessary, with a corresponding increase of the kilovolt-ampere generator capacity. On all the 250-mile and 500-mile lines of this group, intermediate condensers are installed for a 50 per cent load on the system.

Table VI. Special Design Features Necessary for the Transmission of 100 Per Cent Rated Power at a Double Line-to-Ground Fault

System Identification Number	Fault-Clearing Time Required (Cycles)	Circuit-Breaker Opening Time (Cycles)	Generator Transient Reactance (Per Cent)	Generator Inertia, Per Cent of Standard	Generator Damper Winding	Grounding Sending-Transformer Neutral (Per Cent Resistance)	Shunt-Reactor on Generator Bus (Per Cent Reactance)	Increase in Generator Kva Required by Reactor on Generator Bus (Per Cent)	Note on Intermediate Synchronous Condensers (IC)
0-1*a*	9.5	8.0	30.0						
0-2*a*	9.5	8.0							
0-3*a*	4.5	3.0							
0-4*a*	9.5	8.0							
0-5*a*	9.5	8.0							
0-6*a*	9.5	8.0	30.0						One *IC*
0-7*a*	9.5	8.0							
0-8*a*	9.5	8.0	30.0						
0-9*a*	5.5	4.0							
0-10*a*	9.5	8.0							
0-11*a*	9.5	8.0							One *IC*
0-12*a*	4.5	3.0			Dampers				
0-13*a*	9.5	8.0							
0-14*a*	7.5	6.0							
0-15*a*	9.5	8.0	17.5	200		13			
0-16*a*	4.5	3.0	17.5	200	Dampers	13	52	100	One *IC*
0-17*a*	4.5	3.0							One *IC*
0-18*a*	9.5	8.0	17.5	200		13	125	20	Two *IC*
0-19*a*	9.5	8.0	30.0						
0-20*a*	7.5	6.0	30.0						
0-22*a*	7.5	6.0	20.0	200		13			
0-23*a*	9.5	8.0	17.5	200	Dampers	13			
0-24*a*	5.5	4.0	17.5	200		13	70	62	One *IC*
0-25*a*	9.5	8.0	17.5	200		13	90	40	Two *IC*
0-26*a*	5.5	4.0	30.0						
0-27*a*	9.5	8.0	30.0						
0-28*a*	9.5	8.0	22.5	200		13			
0-29*a*	7.5	6.0	30.0						
0-30*a*	9.5	8.0	25.0	200	Dampers	13			
0-31*a*	7.5	6.0	17.5	200	Dampers	13	110	27	One *IC*
0-32*a*	7.5	6.0	17.5	200		13	90	40	Two *IC*

Where not specifically indicated, generator transient reactance and generator inertia are standard (as taken in the study).

Per cent reactor on generator bus is on the generator base prior to the increase in generator kilovolt-amperes required by the reactor.

Intermediate synchronous condensers placed in kilovolt-ampere capacity for 50 per cent load.

Although here the primary purpose is to prevent the voltage from rising with a smaller than 100 per cent load, the installed intermediate condensers serve well the purpose of increasing the system steady-state-stability limit. The margin between the system rating and its steady-state-stability limit under emergency conditions having been improved, means are applied which increase primarily the transient-stability limit, such as grounding of the sending-transformer neutral, increased generator inertia, damper windings, and high-speed circuit breakers. It will be noted that the transient-stability limit of these systems as originally obtained is well below 100 per cent; hence the application of several devices is required. System 0-28*a*, although by definition not in the second group of systems, is treated substantially in a similar manner.

Since the opening time of the high-speed circuit breakers is not standardized, their speeds have been specified as required by the system analysis. In every system, from one to three per cent of the stability limit attained is credited to the tower-footing and fault resistance.

Table VI gives the list of the various methods for overcoming stability limitations which were applied to the systems studied. With these design features the transient-stability limits are brought to the 100 per cent system rating with a double line-to-ground fault. Additional calculations will show the changes necessary when other types of faults are considered. The methods applied in particular cases may be somewhat modified, according to the preference of the designer.

The table shows very interesting examples of the various methods required for the improvement of stability when either the magnitude of the block of power transmitted, the transmission voltage, or the length of the line is modified. The results of changing the transmission voltage alone may be observed in comparing system 0-3*a* with 0-4*a*, 0-12*a* with 0-13*a*, or 0-16*a* with 0-17*a*. In case 0-16*a*, for example, it was necessary to apply practically all methods of improvement in order to transmit 250,000 kw at 230 kv over a distance of 250 miles; in case 0-17*a*, however, when 345 kv was used as the sending-end voltage, the same result was accomplished with generators of standard design using three-cycle circuit breakers.

Summary and Conclusions

Results of the system study may be summarized as follows:

1. The steady-state-stability limit is given for a number of systems transmitting 50,000 to 800,000 kw (at the receiving end) over distances ranging from 50 to 500 miles at sending-end voltages of 69 to 345 kv. When represented in the form of curves, the data permit the determination by interpolation of the stability limit of various systems whose transmission distance, voltage, and power are within the range investigated.

2. For the same systems, the transient-stability limit is obtained for the condition of a double line-to-ground fault near the sending-end bus, cleared in 10.5 cycles, with the loss of a circuit or a section of it. The data may be used to determine by interpolation the transient-stability limit of various systems in a manner similar to that used in determining the steady-state limit.

3. The transient-stability limits of two systems of similar configuration are related by a simple expression (equation 2) permitting the calculation of the transient-stability limit of one system from the known transient-stability limit of another system when basic data on both systems are available.

4. A set of curves is suggested from which it is possible to estimate rapidly the steady-state-stability limit of transmission systems of a certain type.

5. Curves and expressions are given from which the transient-stability limit of a system may be estimated for double line-to-ground, line-to-line, and line-to-ground faults (equations 3, 4, and 5) cleared in 10.5 cycles. The possibility of modifying the expressions to be used with faults of any duration is indicated.

6. It is suggested that possibly the time required for determining on the network analyzer the system transient-stability limit may be shortened, by making observations on the angles of the system machines.

7. The intermediate synchronous condenser is far more effective in increasing the steady-state-stability limit of the line than in increasing its transient-stability limit. While on a 500-mile line the optimum number of intermediate-condenser stations increases the steady-state-stability limit by 45 per cent, it increases the transient-stability limit by only about 12 per cent.

8. The intermediate synchronous condenser is more than twice as effective on the 500-mile line as it is on the 250-mile line.

9. On a 250-mile line the optimum number of intermediate-condenser stations is one; on a 500-mile line, two. The location of

intermediate-condenser stations is best where they divide the line into sections of equal length. Doubling the size of the intermediate condenser raises the system stability limits by about ten per cent.

10. Substitution of reactors for lagging intermediate condensers lowers considerably the system stability limits.

11. The primary purpose in placing the intermediate synchronous condensers on long lines should be to raise the steady-state-stability limit of the line, and to prevent the voltage at the intermediate points from rising with smaller than 100 per cent loads. Since the kilovolt-ampere capacity of the intermediate condenser required by the 100 per cent load is smaller than that required by lighter loads, the intermediate condensers should be installed as required by the latter condition.

12. The resistance in the neutral of the sending transformer increases substantially the transient-stability limit. Within the range of the systems investigated, the increase is the largest on a 50-mile line, and reaches a minimum on lines about 250 miles long. As the line length approaches 500 miles, the effectiveness of the resistance increases.

13. Within the investigated range of resistances in the sending-transformer neutral—from 5.8 to 17.5 per cent—no particularly critical value affecting the stability limit has been found, thus leading to the conclusion that selection of the resistance size should be based on considerations other than stability.

14. The shunt reactor on the generator bus increases the stability limits very appreciably. The increase obtained in the steady-state-stability limit is considerably greater than that secured in the transient-stability limit. With a 40 per cent reactor, the steady-state limit increases about 60 per cent, while the transient-stability limit increases more than 35 per cent. The larger the reactor, the smaller its effectiveness. Curves are given showing the increases in stability limits obtained from reactors of various magnitudes.

15. The increase in the stability limits caused by the shunt reactor on the generator bus is essentially independent of the length of the line, the type of fault, and the fault-clearing time.

16. The shunt reactor on the generator bus requires that the installed kilovolt-ampere capacity of generators be correspondingly increased. The smaller the reactor, the larger the required additional generating capacity. Curves are given showing the size of the shunt reactor to be placed when a certain increase in the transient-stability limit is desired; a curve is also given for the required additional kilovolt-ampere generator capacity.

17. The transient-stability limit of each system studied has been raised, where required, to the desired 100 per cent value by applying the various methods serving this purpose. The methods depend on the margin between the desired rating of the system and its steady-state-stability limit under the emergency circuit condition. The results illustrate (Table VI) the effect of the line length and its transmission voltage on the means to be applied.

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Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperatures

V. M. MONTSINGER
FELLOW AIEE

P. M. KETCHUM
ASSOCIATE AIEE

Synopsis: The paper points out that during the period of the war the need for obtaining maximum transformer overload capacity is very great, particularly under rare emergency conditions. From a thermal standpoint, the amount of overload is limited by the hottest spot in the transformer winding. To obtain accurate data

1. On the actual hot-spot temperatures under rated load conditions
2. On the increase of the hot spot with load under both ultimate moderate overload and heavy short-time overload conditions

approximately 50 heat runs were made on several power transformers of different ratings, representing a wide variety of design conditions. The results of the tests are given in the paper.

The subject of how heavy overloads affect the life of transformers, is discussed and temperature limits somewhat higher than those given in the American Standards, Guides for Operation of Transformers, are suggested. Calculated short-time overloads are given to show the effect of transformer characteristics on overload values.

THE subject of overloading power transformers has been discussed in several AIEE papers,[1–8] and the American Standard *C*-57 gives recommendations on overloading transformers continuously in cool ambients, and in applying short-time overloads under recurrent and emergency conditions.

When the recommendations were first drawn up by the AIEE transformer subcommittee, considerable conservatism was used in the curves for short-time overloading, for two reasons:

1. It was a departure from a long-standing practice of permitting no overloading, a practice based on the assumption that temperature was the only factor that caused the aging of insulation. That is, "time" had not yet been recognized as an important factor. In fact, until 1922 the AIEE Standard stated that the name-plate rating must not be exceeded, "whatever be the ambient," and 105 degrees centigrade was considered the ceiling for hottest[a] spot temperatures for class *A* insulation until as late as 1934 or 1935.

2. The overload curves were purposely made safe for transformers having the highest hot spots under overload conditions.

While the practice of overloading transformers has been on the increase during the past few years, the importance of taking advantage of the maximum overload capacity has never been so great as it is today. It is important in two ways:

1. In many cases the demand for additional electric power has caught up with or even exceeded the present transformer capacity—when operated in the usual manner—and additional capacity is needed. A great many loading curves show that the load factor of most power transformers ranges from 50 to 65 per cent. A large reservoir of capacity is therefore available which could be used without causing a serious shortening of transformer life. This problem was discussed by one of the authors in 1940.[8]

2. We now have the important problem of taking care of rare emergency conditions caused either by electrical failure or by a saboteur. In the past it has generally been considered necessary to carry a spare transformer or a spare bank of transformers to pick up the load in case of a failure. Today many users are considering the use of, and are relying on, the short-time overload capacity of their transformers to carry them through rare emergency conditions. In fact, under some conditions it may be economical and necessary to use up 25 per cent, 50 per cent, or more of the transformer life to prevent a shutdown. To do this intelligently requires reliable information on how to calculate hot-spot temperatures and how the life of the transformer is affected by heavy overloads. The purpose of this paper is to discuss this problem.

I. Hot-Spot Temperatures in Transformers

Heat Runs Made

Since the rate of the deterioration of insulation in a transformer is governed by the hot-spot temperature in the winding, it is quite important to have accurate data on:

1. The value of the hot-spot temperature in various classes of transformers at rated load.

2. A practical method of calculating the hot-spot temperature with changes in the load under both ultimate and short-rime overload conditions.

To obtain such data, approximately 50 heat runs were made during the past year on three transformers of different ratings, one of which was operated

(*a*). As self-cooled.

(*b*). As forced-air-cooled.

The transformers were of the following ratings:

1. *H*-60-2,000 kva—34,500-volt class.
2. *H*-60-2,500 kva—34,500-volt class.
3. OAP[b]-60-2,500/3,125 kva—50,000-volt class.

Some of these tests were made using the loading-back (bucking) method, and some using the short-circuit method, to supply the losses.

Measurement of Hot-Spot Temperatures

Before the heat runs were made on the above transformers, a series of tests was made on a few coils in which thermocouples were embedded between turns to determine the most practical method of measuring the hot-spot temperature, since it is not practical to insert thermocouples between the turns of transformer windings. Figure 1 gives time-temperature curves for three heat runs with 1,500, 2,240, and 4,510 amperes per square inch. These tests showed that a thermocouple inserted under a spacer and in contact with the top surface of a horizontal coil represented very closely the hot spot in that coil. The coil second from the top was used for hot-spot measurements in each of the transformers mentioned above. No extra insulation, such as tape often placed on buffer coils, was used on these coils.

Effect of Change in Viscosity of Oil on Temperature Rise

To determine the effect of changes in viscosity of the oil with temperature on the winding rise over adjacent oil, several tests holding a constant loss were made on the previously mentioned small coils immersed in oil, whose temperature ranged from 35 to 100 degrees centigrade. The curves shown in Figure 2 were derived from these test data, together with the data given in Figure 21b, page 314, of reference 4. These curves are in close

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[a] Referred to hereafter as "hot spot."

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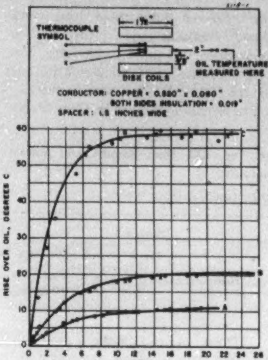
V. M. MONTSINGER is research engineer and P. M. KETCHUM is in the power transformer engineering department, both with General Electric Company, Pittsfield, Mass.

[b] Oil-immersed air pressure.

agreement with similar data given in reference 5.

Determination of Average-Winding Rise at Shutdown

All initial winding temperatures were obtained by cooling curves taken for at least ten minutes' duration. A comparison of the values tested and values calculated by the American Standards Association method (based on Figure 13 of reference 8) showed exceptionally close agreement, being within 0.1 to 0.5 degree centigrade in practically all cases. Extreme care was taken to obtain accurate cold winding temperatures.



RUN	AMPS./SQ.IN.	θ_u °C	B*	FINAL OIL TEMP. °C
A	1500	10.5	5.08	75
B	2240	20.5	4.50	67
C	4510	59.0	2.72	86

TIME CONSTANT CALCULATED FROM θ_u AND LOSS,

SOLID LINES SHOW CALCULATED RISE.

Figure 1. Hot-spot exploration tests in disk coils

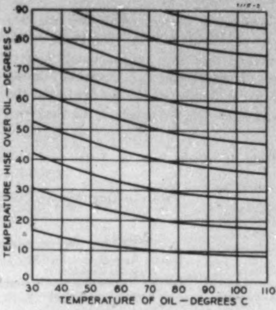


Figure 2. Effect of viscosity on temperature rise of transformer windings

These curves are for 10-C transformer oil and constant loss. The values ranging from 25 to 70 degrees centigrade oil temperature are based on Figure 21b, page 314, of reference 4. From 70 to 110 degrees centigrade, the curves are based on tests made with coils shown in Figure 1 of this paper

Table I. Comparison of Tested and Calculated Top-Oil Rise of the OAP-60–2,500/3,125-Kva Transformer

Loading-Back Run			Short-Circuit Run			
	Rise of Top Oil Over Ambient (Degrees Centigrade)			Rise of Top Oil Over Ambient (Degrees Centigrade)		
Hours	Test	Calculated	Hours	Test	Calculated	
		$B=3.7$			$B=3.0$*	$B=3.7$
1	8.3	8.1	0.75	10.9	9.4	7.8
2	14.3	14.2	1.75	19.9	18.9	16.2
3	19.4	18.9	2.75	25.8	25.6	22.6
4	23.3	22.4	3.75	30.1	30.2	27.3
6	28.4	27.2	5.75	35.2	36.6	33.8
10	32.5	31.6	Load interrupted at this point			
Ultimate	33.9	33.9	Ultimate	42.9	42.9	42.9

* Core weight not used in deriving this value.

Determination of Hot-Spot Temperatures

The determination of hot-spot temperatures in transformers either by tests or by calculation, must be considered in two stages:

1. Oil rise over ambient.
2. Hot-spot rise over oil.

The first stage is easier to determine than the second stage. The principal purpose in making the tests was to investigate the second stage—the hot-spot rise over oil and its variation with load.

Methods have been available[4,7] for several years for calculating either ultimate or transient oil rises. That is, for ultimate conditions $\theta = KW^n$ where $n = 0.8$, and for transient conditions,

$$\theta = \theta_u \left(1 - \epsilon^{-\frac{T}{B}}\right) \tag{1}$$

Where

θ = oil rise in degrees centigrade
θ_u = ultimate rise, degrees centigrade
ϵ = 2.718
T = time in hours
B = time constant[c] expressed in hours

$$= \frac{\theta_u C}{W}$$

$$C = \frac{3.5 \text{ lb (copper + core + } {}^2/_3 \text{ tank)} + 90\ G}{60}$$

W = loss in watts
G = gallons oil (U. S.)

There are two points, however, in the calculation of transient oil rises that need mentioning. When using equation 1, it was found that the calculated oil rises checked quite well the test rises obtained by the loading-back method, but did not check the test values obtained by the short-circuit method of loading. The principal difference in the two methods of loading is, of course, that in the loading-back method the temperature of the core keeps well ahead of the temperature of the oil due to excitation losses. On the other hand, in the short-circuit method, the temperature of the core lags the oil temperature. It was found that if the core weight was not used, the calculated values checked quite well the short-circuit test values. For example: for the OAP-60–2,500/3,125 kva size tested, $B = 3.7$ when the core weight was included, and 3.0 when the core weight was omitted. The calculated and tested top oil rises using the two methods of loading, are shown in Table I.

Short-circuit runs are, of course, of no importance under service conditions. They are discussed here, because it is more convenient to use this method when making heat runs (*a*) to measure hot-spot temperatures, and (*b*) to obtain transient oil-rise data (sometimes requested by customers); and it is well to remember that equation 1 should be modified (by omitting core weight) when calculating the oil rise. This same modification has been found necessary in other cases where the short-circuit method was used to obtain transient oil rise data.

The other point found was that for heavy short-time overloads the best results are obtained by using the time constant corresponding to that for ultimate rated load, although theoretically it should be based on ultimate (calculated) conditions using the initial loss in

$$B = \frac{\theta_u C}{\text{loss}}$$

and the final oil rise θ_u for the ultimate loss corresponding to the final winding temperature. While this method can be used for moderate overloads as was done in reference 7, it did not give even approximately correct results for the 200 per cent and 270 per cent loads. The method that gave approximately correct results was to base θ_u in equation 1 on the calculated ultimate rise for the loss at the end of the overload and use the time constant derived from rated load conditions. This was done for the two overloads shown in Table II, which gives comparative tested and calculated oil rises for the *H*-60–2,500-kva transformer.

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[c] Time to reach 63 per cent of the ultimate rise, or time to reach ultimate rise if all heat is stored.

The calculations given in Table II show that fairly accurate results were obtained using the rated load (total loss) time constant of 3.17, and indicate that results close enough for practical purposes can be obtained by the simpler method 1 instead of method 2 (described in the footnote). Heat runs made on other transformers confirmed these findings. However, if the initial rise is changing (that is, not ultimate for a given loss) and is well along—over 30 to 40 per cent of the ultimate rise—method 2 should be used. The use of the same (rated load) time constant simplifies the calculations of transient oil rises for short-time overloads.

Table II. Comparison of Tested and Calculated Top Oil Rises—Short-Circuit Runs—2,500-Kva Transformer

$B = 3.17$—Time Constant Computed by Omitting Core Weight

	Method 1			Method 2	
Time (Hours)	Top-Oil Rise Over Initial Rise (Degrees Centigrade)		Time (Hours)	Top Oil Rise Over Ambient (Degrees Centigrade)	
	Test	Calculated		Test	Calculated*
Total Loss Run (Final Loss 26.9 Kw)					
0	0.0	0.0	0+1.0	11.9	11.9
2	14.8	15.0	2+1.0	26.7	26.7
4	22.0	22.6	4+1.0	33.9	34.6
6	27.0	27.0	6+1.0	38.9	38.8
8	29.4	29.2	8+1.0	41.3	41.0
10	31.0	30.4	10+1.0	42.9	42.4
Ultimate	31.8**	31.8	Ultimate	43.7	43.7
			Time to reach 11.9 C rise = 1.0 hour		
200 Per Cent Rated Load Current Run (Final Loss 91 Kw)					
0	0.0	0.0	0+0.35	12.0	12.0
1	25.3	27.9	1+0.35	37.3	40.4
2	46.7	48.5	2+0.35	58.7	60.4
2.75	59.3	59.7	2.75+0.35	71.3	72.1
Ultimate (Calculated)		103.0	Ultimate (Calculated)		115.0
			Time to reach 12.0 C rise = 0.35 hour		
Calculated final oil rise = 115 C. Initial oil rise = 12.0 C. $\theta_u = 115.0 - 12.0 = 103.0$ C for method 1, and 115 C for method 2.					
270 Per Cent Rated Load Current Run (Final Loss 170 Kw)					
0	0.0	0.0	0+0.36	20.3	20.3
0.50	23.1	25.1	0.50+0.36	43.4	45.2
0.92	40.4	42.7	0.92+0.36	60.7	63.9
Ultimate (Calculated)		169.7	Ultimate (Calculated)		190.0
			Time to reach 20.3 C rise = 0.36 hour		
Calculated final oil rise = 190 C without resistance correction. Initial oil rise = 20.3 C. $\theta_u = 190.0 - 20.3 = 169.7$ C for method 1 and 190 C for method 2.					

* Calculated by finding time from Figure 3 to attain rise over ambient at start of overload and then continuing from this time on to end of overload. For example: for the total loss runs, it would require one hour to reach 11.9 degrees centigrade rise. One hour is then added to each succeeding time period and 43.7 degrees centigrade is used for θ_u instead of 31.8 degrees centigrade.

** $43.7 - 11.9 = 31.8$

Table III. Summary of Heat Runs Made on *H*-60–2,000-Kva Transformer

Loading-Back Method Used to Supply Losses

	Method of Cooling							
	Self-Cooled					Forced-Air-Pressure (OAP)		
	Per Cent of Self-Cooled Rating							
	100	125	150	200	250	100 133	125 166.5	150 (OAP) 200
Hot-spot temperature	82.5	97.0	114.0	130.0	137.0	85.7	110.1	133.2
Hot-spot temperature (calculated)*	84.0	98.8	116.1	132.0	134.0	87.7	113.0	138.0
Top oil	73.5	83.3	94.6	97.0	84.0	71.1	88.2	102.8
Hot-spot rise over top oil	9.0	13.7	19.4	33.0	53.0	14.6	21.9	30.4
Average difference top and bottom tube header (outside surface)	12.4	14.4	14.4	15.0	15.8	12.4	12.4	14.8
Average winding temperature by resistance	77.6	90.1	104.9	115.2	116.9	79.5	99.9	120.2
Ambient (degrees centigrade)	24.5	23.3	26.5	31.7	28.0	26.5	32.3	30.9
Average high-voltage rise over ambient	53.1					53.0		
Hot-spot rise over average winding rise	4.9	6.9	9.1	14.8	20.3	6.2	10.2	13.0
Average high-voltage rise over average oil temperature	10.3	14.0	17.5	25.7	48.8	14.6	17.9	24.7
Relative copper loss (corrected for temperature)	1.0	1.63	2.45	4.48	7.03	1.78	2.95	4.52
Duration of test	Ultimate	3.5 hr	3 hr	46 min	23.5 min	Ultimate	3 hr 22 min	3 hr 34 min

* By using calculated hot-spot rise over top oil for self-cooled rated load and then varying rise over top oil as $\text{loss}^{0.8}$.

Determining Hot-Spot Rise Over Oil

The determination of the actual hot-spot rise over the oil (and over the average winding temperature) at rated load for different design conditions, and the variation of the hot spot with load, involves several factors not so easy to determine as they are for oil temperature rises. Among these are:

1. Vertical oil gradient as affected by the center of heat[d] with respect to the center of the cooling circuit (radiators, tube headers, and so forth).

2. Accurate determination of the oil temperature adjacent to that part of the winding having the maximum temperature rise.

3. Effect of blanketing of

(*a*). Part of coil area by spacers.
(*b*). Total coil area by tape.
(*c*). Extra turn insulation often used in buffer coils

4. Effect of change in viscosity of oil and change in resistance with temperature—minor factors, as one tends to counteract the other.

5. Transient temperature rise (when the duration of time is under 15 to 20 minutes).

As previously mentioned, it was for the purpose of obtaining more accurate data on these factors (than have been available heretofore) that tests were made during 1941 on five different design conditions—three different designs, one of which was tested under three different vertical oil gradient conditions.

First Series of Heat Runs (Loading-Back Method)

Figure 4 shows the location of thermocouples in the *H*-60–2,000-kva transformer tested first, as self-cooled, and second, as OAP–2,660-kva with temporarily mounted fans. In both series of tests 53 degrees centigrade high-voltage winding rise was produced by blanketing some of the tube headers. To protect the ther-

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[d] Usually a point half-way up the coils and core.

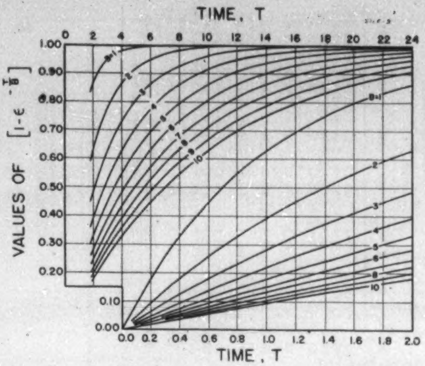


Figure 3. Transient heating curves

mocouples placed under the spacers resting on the next to top coil from dangerous voltages, the coil crossover at this point was grounded during the tests. Figure 4 also shows the vertical temperature gradient of the oil for the 100, 125, and 200 per cent load self-cooled runs. The OAP oil gradients were essentially of the same shape.

Table III gives the final temperature readings. Figure 5 shows in curve form the hot-spot rise:

1. Over average winding temperature.
2. Over top oil.
3. Over high-voltage oil duct temperature.
4. Average winding rise over average oil temperature.

These data show:

1. The hot-spot rise over average winding temperature was between four and five degrees at rated load and varied as loss$^{0.75}$.
2. The hot-spot rise over top oil (and over maximum observed oil) varied as loss raised to approximately 0.8 power.
3. The average winding rise over average oil temperature[e] varied as loss$^{0.7}$.
4. The temperature throughout the main body of oil above the top of the core was approximately constant.

Second Series of Heat Runs (Short-Circuit Method)

The second series of heat runs was made on the transformer rated *H*-60–2,500 kva under three different design conditions, namely:

1. With core and coils resting on bottom of tank—"normal position."
2. With center of heat (half-way up core and coils) raised to be level with center of cooling tubes—"maximum raised position."
3. With center of heat in "intermediate position."

Figure 6 shows the locations of the windings, thermocouples, and vertical oil gradients for each of the three conditions tested. It will be noted that raising the core and coils increased the vertical oil gradients, these being indicated in Table A.

Table A

Position of Core and Coils	Difference Between Top and Bottom Oil Inside Tank (Degrees Centigrade)
Normal	15.0
Intermediate	25.0
Maximum raised	40.0

Figures 7, 8, and 9 show the oil and winding rises for this series of tests. Curve *E* shows the effect of raising the center of heat on the hot-spot rise over the average winding temperature. At rated load the rises, as read from the curves, were:

(*a*). 5.1 degrees centigrade for the normal position.

(*b*). 14 degrees centigrade for the intermediate position.

(*c*). 18 degrees centigrade for the maximum raised position.

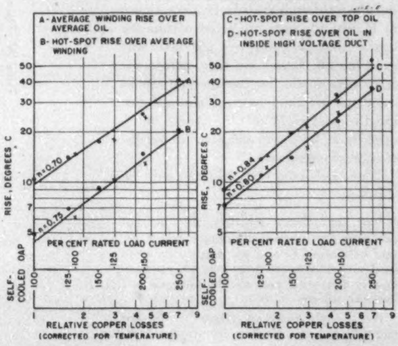
It is, of course, proper to point out that most power transformers fall in the (*a*) condition where the center of heat is well below the center of cooling tubes. The (*c*) condition, where the center of heat and center of cooling tubes are at the same level, is one that seldom exists in practice.

Table IV gives the final readings for 20 of the 32 runs made. The 12 additional tests were check runs of two to three hours' duration, made only for obtaining winding temperatures, and were not run until the top oil rises again became constant.

The Thermal Circuit

The thermal circuit in a self-cooled transformer is composed of the tempera-

Figure 5. Temperature rises during loading-back heat runs on *H*-60–2,000-kva transformer



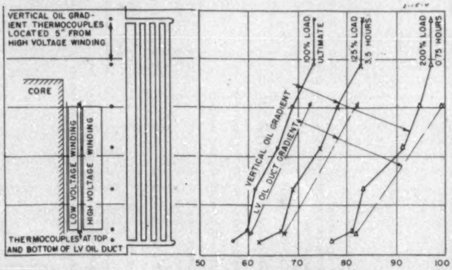


Figure 4. Temperature gradients during loading-back heat runs on *H*-60–2,000-kva transformer

ture rise of the oil flowing upward through the coil stack and the temperature drop in the oil flowing downward through the attached tube headers or radiators.

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[e] The average oil temperature is taken as the top oil minus one-half the temperature drop through the cooling tubes.

The height of the center of heat (center line of core and coils, or of coils only, for short-circuit runs) with respect to the center of the cooling agent, controls the temperature rise and temperature drop of the thermal circuit for ultimate conditions. Also, when the center of heat is well below the center of the cooling agent, the temperature rise and temperature drop in the thermal circuit are small and do not appreciably increase within a reasonable range of loads for ultimate conditions. But, for short-time heavy overloads, while the temperature drop in the cooling circuit may not appreciably increase in value, the temperature rise of the oil flowing upward may greatly increase. For example: for the normal position, there was no appreciable change for all the ultimate runs in either the temperature rise or temperature drop of the thermal circuit. On the other hand, for the 200 per cent and 270 per cent short-time runs, the temperature drop in the cooling tubes remained essentially the same as for the ultimate runs—while the temperature rise of the oil flowing up through the windings increased approximately 100 per cent over the rise for the ultimate runs. This increase in the oil temperature flowing upward increased the hot-spot rise over the average winding temperature, Figure 7, whereas, if the constant temperature drop in the cooling tubes had been the controlling factor, the hot spot would not have increased with load.

When the center of heat was raised, both the temperature rise and the temperature drop in the thermal circuit definitely increased with load under both ultimate and short-time overload conditions. The temperature drops in oil flowing down through the tube headers, shown in Figure 10, were predicted within one degree for the total loss ultimate heat runs for all three conditions before the tests were made, by the thermal circuit calculations (which have been in use during the past eight or nine years) developed by L. Wetherill and given in his discussion of reference 5.

The following conclusions can be stated:

1. For the normal position, the hot-spot rise over the average winding temperature was between five and six degrees at rated load and increased with load at approximately the same rate as the hot-spot rise over the top oil.

For the two raised positions, the hot-spot rise over average winding temperature defi-

Table IV. Summary of Heat Runs Made on *H*-60–2,500-Kva Transformer; Short-Circuit Method Used to Supply Losses

	Position of Core and Coils																			
	Normal								Intermediate						Maximum Raised					
	Load Current (Per Cent of Rating)																			
	58	85	100	112*	125	135	200	270	57	85	112*	135	200	270	85	100	112*	125	200
Hot-spot temperature	43.5	63.7	78.0	85.9	93.3	107.9	128.0	138.0	47.6	65.8	93.7	104.9	107.3	133.3	77.5	93.3	105.7	114.5	125.5
Hot-spot temperature (calculated)**	42.8	62.7	78.6	85.7	93.9	110.0	134.6	145.9	47.8	64.9	92.7	106.2	107.4	134.2	75.0	90.0	98.0	112.4	122.5
Top oil temperature	37.3	54.5	67.2	72.5	78.5	91.6	99.4	87.8	39.8	52.4	75.1	84.4	73.9	87.7	64.4	77.7	87.3	96.4	95.1
Hot-spot rise over top oil	6.2	9.2	10.8	13.4	14.8	16.3	28.6	50.2	7.8	13.4	18.6	20.5	33.4	45.6	13.1	15.6	18.4	18.1	30.4
Difference between top and bottom oil in tube headers	5.4	8.9	9.0	8.5	10.0	10.0	10.6	12.6	10.3	14.9	18.5	21.8	22.3	27.1	21.1	27.5	32.9	34.4	42.9
Temperature rise of oil up through low voltage	7.4	11.1	10.5	11.2	12.1	12.0	20.0	27.6	10.3	18.2	21.0	24.1	33.9	48.8	25.3	33.1	38.7	40.0	66.1
Average high-voltage temperature (by resistance)	—	58.6	—	—	86.2	—	117.2	119.3	40.8	53.3	78.3	89.2	92.2	110.4	64.4	74.0	82.7	91.4	99.0
Hot-spot rise over average winding temperature	—	5.1	—	—	7.1	—	10.8	18.7	6.8	12.5	15.4	15.7	15.1	22.9	13.1	18.7	23.0	23.1	26.5
Ambient temperature	22.4	25.7	28.7	28.8	26.8	30.0	28.1	27.1	22.6	22.3	25.1	23.0	22.5	24.4	28.1	30.4	29.2	29.6	22.8
Top oil rise over ambient	14.9	28.8	38.5	43.7	51.7	61.6	Not constant		17.2	30.1	50.0	61.4	Not constant		36.3	47.3	58.1	68.8	Not constant
Average oil rise over ambient	12.2	24.3	34.0	39.4	46.7	56.6	Not constant		12.0	22.6	40.8	50.5	Not constant		25.7	33.5	41.6	49.6	Not constant
Duration of test (hours)	Ultimate	Ultimate	Ultimate	Ultimate	Ultimate	Ultimate	2.75	0.92	Ultimate	Ultimate	Ultimate	Ultimate	0.92	0.92	Ultimate	Ultimate	Ultimate	Ultimate	2.08

*112 per cent load loss = total (core + copper) loss.

**By using calculated hot-spot rise over top oil at rated load and then varying rise as loss^{n} where $n = 0.8$ for normal position, and 0.53 for intermediate and maximum raised positions.

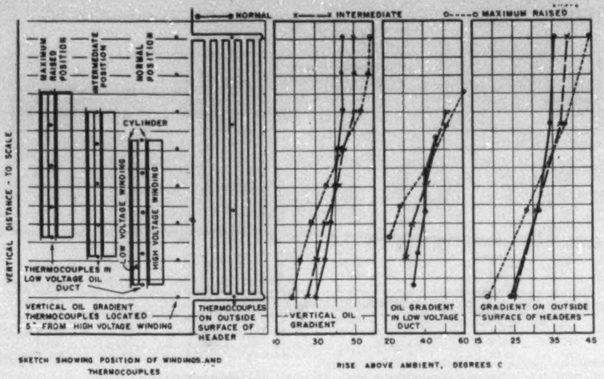

Figure 6. Temperature gradients during short-circuit heat runs on *H*-60–2,500-kva transformer

nitely increased with load for all ultimate conditions, but under short-time overload conditions, the hot-spot rise over average winding was only slightly above the rises for the 135 per cent load ultimate run—see Figures 8 and 9.

2. The ultimate average and top oil rises varied as loss raised to a power ranging from approximately 0.75 to 0.85—the general average being about 0.8.

3. The difference between the top and bottom temperature of the oil in the cooling tubes for ultimate conditions was approximately constant from 100 per cent to 135 per cent load for the normal position, but increased with loss raised to approximately 0.4 power for the intermediate position, and to approximately 0.7 power for the maximum raised position.

4. The hot-spot rise over top oil for both ultimate and short-time overloads varied as loss raised to a power of approximately 0.75 (test points scattered) for the normal position, and approximately as loss$^{0.53}$ for both intermediate and maximum raised positions.

5. The main body of oil above the windings (not core yoke) was at approximately the same temperature.

Third Series of Heat Runs

A third series of heat runs was made on an OAP-60–2,500/3,125-kva transformer in which the losses were supplied

1. By the short-circuit method.
2. By the loading-back method.

Only self-cooled runs were made. The main purpose of these tests was twofold:

1. To obtain comparative tests on short-circuit and loading-back heat runs to determine the effect of the loss in the core (top yoke) on the hot-spot rise over top oil.

2. To obtain additional data on the hot-spot rise over the average winding temperature for both transient and ultimate conditions.

The data for this series of tests are given in Table V, and some of the data are plotted in Figure 11. These data show:

1. The hot-spot rise over the average winding temperature was between five and six degrees at rated load, and increased as loss$^{0.8}$ for both the short-circuit and loading-back runs.

2. The hot-spot rise over top oil (when corrected to the same oil temperature) was approximately the same for both short-circuit and loading-back methods of loading. In other words, the short-circuit method of loading can be used to obtain hot-spot temperatures in transformers. The rise varies as loss$^{0.8}$.

3. The average winding rise over average oil varied as loss$^{0.7}$.

Variations of Hot Spot With Load

Vogel and Narbutovskih[5] reported that the hot-spot rise over average winding rise of a 600-kva transformer was constant from 100 per cent load to 250 per cent load inclusive. These constant hot-spot test values were explained on the grounds that this would be expected since the square root of the product of the losses and oil viscosity was a constant value, which in turn produced an approximately constant difference in the temperature of the top and bottom oil in the radiators. In the tests reported in this paper, Table VI shows the difference between top and bottom cooling tube temperatures and the square root of the product of the loss and oil viscosity for different loads held on the 2,500-kva transformer tested with the center of heat at different levels. These data show that with the core and coils in the normal position this factor was approximately constant except for the 58 per cent load test; but, for the intermediate and maximum raised positions of core and coils, there

Table V. Summary of Self-Cooled Heat Runs Made on OAP-60–2,500/3,125-Kva Transformer

	Test Readings, Degrees Centigrade										
	Short-Circuit Heat Runs						Loading-Back Heat Runs				
	Load in Per Cent of Rating										
	48.6	81.1	101.0	121.3	141.8	228.0	48.6	80.8	99.0	122.0	140.0
Hot-spot temperature	29.2	50.6	61.2	74.8	91.6	125.8	42.7	60.9	69.9	78.6	102.7
Hot-spot temperature (calculated*)	26.3	51.5	64.1	75.3	92.0	134.4	45.6	60.8	73.0	76.4	102.6
Top oil temperature	21.5	40.3	45.9	53.8	62.6	69.4	41.7	51.9	57.3	58.6	79.5
Hot-spot rise over top oil	7.7	10.3	15.3	21.0	29.0	56.4	1.0	8.2	12.6	20.0	23.2
Hot-spot rise over top oil (all rises corrected to 75-degree oil temperature Figure 2)	—	2.9?	8.8	16.1	26.5	55.2	—	3.8	9.1	16.3	24.0
Difference between top and bottom tube headers	4.5	10.8	12.5	15.2	15.9	21.3	10.7	11.6	12.8	14.2	15.6
Average winding temperature	26.5	47.6	55.6	64.7	77.4	108.9	41.6	57.2	64.7	—	93.4
Hot-spot rise over average winding temperature	2.7	3.0	5.6	10.1	14.2	16.9	1.1	3.7	5.2	—	9.3
Average winding rise over average oil temperature	7.2	12.7	15.9	18.5	22.7	50.1	5.2	11.1	13.0	—	51.4
Copper loss (kw)	3.17	9.23	13.62	20.8	30 6	83.0	3.2	9.0	13.8	21.3	29.4
Core loss (kw)	0	0	0	0	0	0	6.13	6.13	6.13	6.13	6.13
Total loss (kw)	3.17	9.23	13.62	20.8	30.6	83.0	9.33	15.13	19.93	27.43	35.53
Duration of run (hours)	3	3	3	3	3	1.1	8.5	6	14.7	2	12.5

* By using calculated hot-spot rise over top oil at rated load and then varying this rise as loss$^{0.8}$.

Table VI. Product of Loss and Kinematic Viscosity for Three Different Positions of 2,500-Kva Transformer

Per Cent Load	Loss (Kw)	Oil Inside of Header (Degrees Centigrade) Top	Bottom	Difference	$\sqrt{\text{Loss}\times\text{Kinematic Viscosity}}$* Top Oil	Avg. Oil	Bottom Oil
Core and Coils—(Normal Position)							
58	6.975	37.2	31.8	5.4	26.4	29.0	29.2
85	14.7	54.4	45.5	8.9	29.7	31.6	34.0
100	20.9	67.0	58.0	9.0	29.7	31.4	33.6
112	26.9	72.5	64.0	8.5	31.4	33.0	35.0
125	32.3	78.7	68.7	10.0	31.6	33.6	35.8
135	40.4	90.8	80.8	10.0	29.5	32.0	34.2
Core and Coils—(Intermediate Position)							
57	6.56	40.7	30.4	10.3	24.4	26.3	28.6
85	14.7	52.2	37.0	15.2	30.6	34.3	38.7
112	26.9	75.4	56.9	18.5	30.0	34.2	38.7
135	40.4	84.8	63.0	21.8	32.0	37.5	43.5
Core and Coils—(Maximum Raised Position)							
85	14.7	64.1	43.0	21.1	25.8	30.0	35.4
100	20.9	77.0	49.5	28.5	26.0	31.0	38.2
112	26.9	86.8	53.9	32.9	26.0	32.5	40.4
123	32.3	95.7	61.3	34.4	27.7	35.4	44.4

*Curve given in Figure 2 of reference 5 used. This curve represents a fairly universal oil.

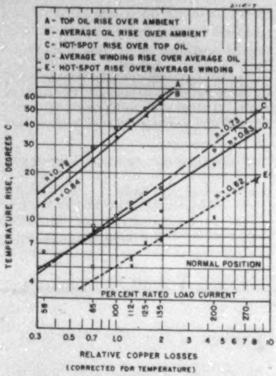

Figure 7. Temperature rises during short-circuit heat runs on *H*-60–2,500-kva transformer

was a constant increase with load. Yet, the hot-spot rise over the average winding temperature increased with load for all ultimate conditions in all three positions. The temperature drops in the tube headers are plotted in curve form in Figure 10.

The procedure to follow in the general case for calculating hot-spot temperatures with windings similar to those in the transformers tested, is illustrated in the following example.

These assumptions are made:

1. 55 degrees centigrade average winding rise at rated load.
2. 45 degrees centigrade top oil rise at rated load.
3. 10 degrees drop in cooling circuit at rated load.
4. 5 degrees hot-spot increase due to extra coil insulation, spacers, and so forth.

If located at the top the hot-spot rise of the extra insulated coil would be

$$55+\frac{10}{2}+5=65 \text{ degrees centigrade}$$

Under overload conditions after 15 minutes' duration, hot-spot rise would be:

$$A+B\left(\frac{\text{loss at overload}}{\text{loss at rated load}}\right)^{0.8}$$

where

A = temperature rise of the top oil

B = difference between the hot-spot and top oil at rated load

Factors Limiting Short-Time Rare Emergency Overloads

Quite often factors other than the life of the insulation limit heavy overloads for transformers. Among such factors to be considered are, contacts, leads, joints, and oil. Effect of heavy overloads on regulation must, of course, be carefully considered.

Excessive expansion may cause oil to run over. Also, oil deteriorates at high temperatures even for short periods of time, particularly if exposed to air. Figure 12 shows calculated hot-spot and top oil rises for a four-hour 200 per cent load following full load, in a case studied recently. In this case the temperature of the oil was considered the limiting factor.

Effect of Change in Resistance and Oil Viscosity on Hot-Spot Temperatures

Figure 12 also shows the difference in the hot-spot rises obtained when no temperature and viscosity corrections are made as compared with the rises obtained with the corrections. The calculations for 200 per cent load (assuming 20 degrees centigrade hot-spot rise over top oil at 100 per cent load) were made as follows:

1. With No Corrections

Hot-spot rise over oil $=20\times2^{1.6}=60.4$ degrees centigrade after 20 minutes' duration

Hot-spot temperature = top oil + 60.4 degrees centigrade for any time after 20 minutes

2. With Corrections

For $2\frac{3}{4}$ hours, oil temperature = 110 degrees centigrade (by Figure 12)

Hot-spot rise over oil =

$$20\left[2^2\times\left(\frac{234.5+177.5}{234.5+95}\right)\right]^{0.8}=72.5$$ degrees centigrade with resistance correction only

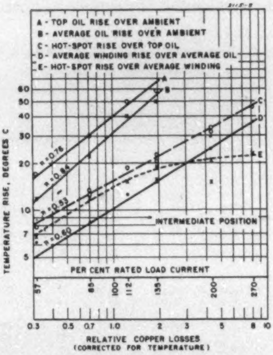

Figure 8. Temperature rises during short-circuit heat runs on *H*-60–2,500-kva transformer

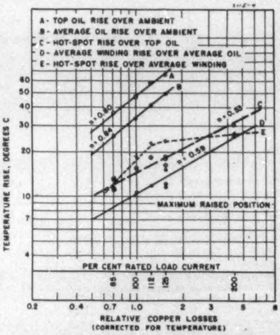

Figure 9. Temperature rises during short-circuit heat runs on *H*-60–2,500-kva transformer

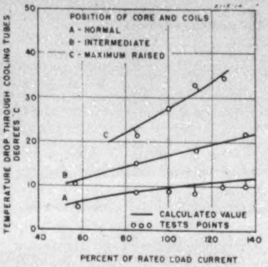

Figure 10. Cooling tube temperatures during short-circuit heat runs on *H*-60–2,500-kva transformer

By Figure 2 if the hot-spot rise is 72.5 in 75 degrees centigrade oil, it would be 67.5 degrees centigrade rise in 110 degrees centigrade oil—a reduction of 5 degrees centigrade due to viscosity correction.

The correct hot-spot temperature is, therefore, 110+67.5=177.5 at $2^3/_4$ hours, as against 110+60.4=170.4 degrees centigrade without the correction.

These curves show that for strictly accurate results both corrections should be made. But, since in making overload calculations the bases of the assumptions are approximations, it should be satisfactory to neglect the corrections. This was done when calculating the overloads shown in Table VII.

Hot-Spot Temperature Indicator Tests

Temperature indicators have been used for several years to indicate hot-spot temperatures in transformers. Since an indicator can be set for any desired hot-spot rise over average winding at rated load, its ability to follow the hot-spot temperature depends

1. On its variation with load current.
2. On the time constant of its heating coil.

To check these variations with load current, a standard indicator heating coil was placed in the top oil of both the 2,000-kva self-cooled /2,667-kva OAP and the 2,500-kva self-cooled transformer.

Figure 13 shows that on the average the indicator rise over top oil varied as the load $\text{current}^{1.70}$ or as $\text{loss}^{0.85}$, whereas (on the average) the winding hot-spot rise over top oil varies as $\text{loss}^{0.8}$. This means that for heavy overloads the indicator will read a little too high, and it may be well to retain this feature. The time constant of the standard indicator heating coil is approximately the same

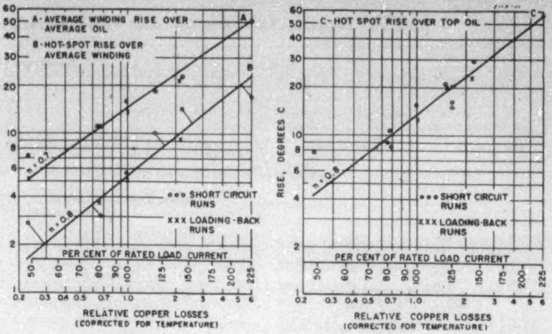

Figure 11. Temperature rises during heat runs on OAP-60–2,500/3,125-kva transformer

as the time constant of transformer windings; consequently the indicator should follow the hot-spot temperature under both fluctuating loads and overloads.

General Conclusions on Steady-State and Transient Temperature Rises

1. For rated load conditions of self-cooled power transformers, the hot-spot rise over average winding temperature by resistance ranges from approximately five degrees centigrade upward. For moderate size and voltage rating (below approximately 50 kv), it naturally comes between five and six degrees. But, for large size and high-voltage rating, steps have to be taken by the designer to limit the hot-spot to ten degrees centigrade above average.

2. For transformers having an OAP rating 1.33 times the self-cooled rating, the hot-spot rise over average winding temperature is approximately 40 per cent greater than that for the self-cooled rating (see Table III).

3. The hot-spot rise over average winding temperature increases with load (for both ultimate and short-time overloads), but no definite rate of rise versus loss can be stated (see Figures 7, 8, 9, and 11).

4. The average winding rise over average oil varies as the loss raised to a power ranging from 0.6 to 0.7.

5. The hot-spot rise over top oil varies as the loss raised to a power ranging from approximately 0.7 to 0.8, generally more nearly the 0.8 power for most transformers.

6. The oil time constant used in calculating the temperature rise of the oil for short-time overloads should be based on the ultimate rise and loss for rated load conditions.

7. The winding time constant used in calculating the temperature rise of windings over oil before conditions become constant, should be based on the calculated ultimate rise and loss for the load under consideration.

8. Either loading-back or short-circuit method of loading can be used to determine both hot-spot and average winding temperatures. When calculating the transient oil rises by equation 1, for the short-circuit method the weight of the core should not be used in deriving the time constant.

9. The temperature rise of both the average and top oil varies approximately as the $\text{loss}^{0.8}$ for ultimate conditions.

II. Effect of Overloads on Life of Transformers

Life Expectancy of Transformers

The estimated life of a transformer is at best only a rough approximation. The dielectric strength of insulation does not deteriorate until it has become embrittled and cracked. It is possible, therefore, for a transformer to continue to operate long after the mechanical life of its insulation is well used up, unless subjected to excessive mechanical stresses (short circuit, handling, or other mechanical shocks), since its normal stresses are quite low. It does not follow, therefore, that a transformer will fail when its insulation is embrittled; on the contrary, if severely strained, it might fail before the mechanical strength is completely used up.

Rate of Aging of Class-A Insulation

Mr. Clark's paper, presented at this session, shows that for temperatures ranging from approximately 115 to 200 degrees centigrade, the rate of aging is doubled for each six to ten degrees centigrade depending upon the percentage of life used up. That is, near the start a nine- to ten-degree rule applies; at the 50 per cent to 25 per cent life levels an eight-degree rule applies; and near the finish a six- to seven-degree rule applies.

Table VII. Emergency Overloads for Self-Cooled Power Transformers Having Different Characteristics*

		Transformer Class												
		1			2			3		4		5		6
		a	b	c	a	b	c	a	b	a	b	a	b	ASA
Hot-spot rise		60	60	60	62.5	62.5	62.5	60	60	65	65	65	65	
Top oil rise		50	50	50	50	50	50	45	45	50	50	45	45	
Time constant		3	5	7	3	5	7	3	5	3	5	3	5	
Hours	Temp. C	Times Rated Load Current												
Following Full Load														
1/4	152	3.00	3.13	3.16	2.67	2.81	2.84	2.46	2.54	2.42	2.51	2.12	2.18	1.8
1/2	144	2.53	2.76	2.81	2.30	2.50	2.53	2.19	2.27	2.13	2.23	1.91	1.97	1.6
1	136	2.10	2.28	2.40	1.95	2.10	2.13	1.88	1.98	1.82	1.94	1.68	1.76	1.4
2	128	1.70	1.86	1.97	1.62	1.77	1.82	1.60	1.69	1.54	1.64	1.47	1.52	1.3
4	120	1.45	1.55	1.62	1.40	1.47	1.53	1.41	1.46	1.33	1.40	1.30	1.35	1.2
8	115	1.32	1.35	1.40	1.28	1.31	1.34	1.32	1.35	1.24	1.27	1.23	1.25	1.15
24	110	1.26	1.26	1.26	1.21	1.21	1.21	1.24	1.24	1.18	1.18	1.17	1.17	1.10
Following No Load (18 Degrees Centigrade Oil Rise)														
1/4	152	3.70	3.94	4.06	3.31	3.50	3.60	3.04	3.13	2.96	3.11	2.62	2.68	2.3
1/2	144	3.10	3.43	3.60	2.85	3.08	3.20	2.67	2.83	2.62	2.80	2.33	2.45	1.9
1	136	2.51	2.78	3.04	2.35	2.60	2.78	2.27	2.46	2.20	2.40	2.03	2.17	1.6
2	128	1.95	2.25	2.45	1.86	2.10	2.29	1.84	2.05	1.77	1.98	1.68	1.84	1.5
4	120	1.57	1.76	1.92	1.50	1.68	1.82	1.52	1.67	1.46	1.58	1.41	1.54	1.35
8	115	1.35	1.44	1.54	1.31	1.39	1.48	1.33	1.40	1.27	1.34	1.26	1.32	1.25
24	110	1.26	1.26	1.26	1.21	1.21	1.21	1.24	1.24	1.18	1.18	1.17	1.17	1.10

* Based on top oil rise over ambient and hot-spot rise over top oil varying as loss$^{0.8}$. Loss ratio 2.5:1 at rated load.

No corrections made for increase in resistance and change in viscosity of oil with temperature. 30 C ambient.

In view of the fact that predicting the life of a transformer, as mentioned above, cannot be exact, the eight-degree rule, which one of the authors has used for several years,[1,3] is a good average value to use.

Mr. Clark's data show also that for temperatures from 75 to 100 degrees centigrade the rate of aging doubles with changes in temperature as low as two to five degrees centigrade. This indicates that it is not possible to use aging data for the higher temperatures and the eight-degree rule to predict the life of a transformer when operating at temperatures of approximately 100 degrees centigrade and below. In fact, Figures 3 and 4 of Mr. Clark's paper indicates that while the life of insulation is a matter of weeks at 120 degrees centigrade, it is a matter of several years if the temperature does not exceed approximately 100 degrees centigrade. While these laboratory tests may not show the actual life of a transformer in weeks and years, they are indicative of the relative life at different temperatures.

Basis for Determining the Life of Transformer Insulation

There is, of course, some question whether laboratory aging tests made on isolated strips of paper in sealed tubes can be applied directly in estimating the life of insulation in a transformer. Such tests should rather be depended upon to furnish the more fundamental data, such as the increase in temperature to double the rate of aging, the effect of the various other factors contributing to the deterioration of insulating materials, and so forth.

In attacking the problem of determining the effect of overloads on the life of transformers, it is felt that for the present it is preferable to rely mainly on past experience in establishing a basis, and then depend upon fundamental laboratory data as an additional aid. For example: we have now had several years' experience in the use of hot-spot temperature limits for emergency overload conditions, and the laboratory tests show how the relative life of insulation is affected by different temperatures.

The hot-spot temperature limits given in section 11.002 of the American Standards Guides for Operation of Transformers under emergency conditions are 115 degrees centigrade for 2 hours, 110 degrees centigrade for 8 hours, and 105 degrees centigrade for 24 hours. When these limits were first set up a few years ago, they were purposely made conservative for the reason that the permitting of overloads on an emergency basis was a departure from a long-standing practice of permitting no overloads, as pointed out in the first part of the paper. Furthermore, no limitations were put on the number of overloads during the life of the transformer.

When the number of operations is limited, it is reasonable to increase somewhat the above American Standards hot-spot limits. Using a rule less than eight degrees for temperatures under 120 degrees centigrade, an eight-degree rule for temperatures from 120 to 192 degrees centigrade, which are in agreement with Mr. Clark's data, and a rule greater than eight degrees above 192 degrees centigrade, in order to reach the present ASA limit of 250 degrees centigrade in five seconds, a schedule of temperatures is suggested in Table B for consideration in setting up emergency overloads.

The use of a rule greater than eight degrees above 192 degrees centigrade appears reasonable, since the actual temperatures (calculated on the basis of all heat stored in the copper) will generally be below the calculated values, for two reasons:

1. The insulation on the conductors will absorb some heat.
2. Most transformer windings start dissipating heat after from three to five seconds' duration.

The temperature limits suggested are in close agreement with values assumed in reference 5 for times of 1/4 hour and longer, being somewhat higher for the short times, and the same for 4, 8, and 24 hours' duration.

Calculation of Emergency Overloads

The overloads for a given hot-spot temperature vary widely, depending on the characteristics of the transformer, such as top oil rise, hot-spot rise over top oil, ratio of losses, and time constant, all of which vary under rated load conditions for power transformers, approximately as shown in Table C.

Based on the temperature limits sug-

Table B

Time, in seconds	5	10	20	30	60	300	
Temperature, degrees centigrade	250	220	200	192	184	165	
Time, in hours	$^1/_4$	$^1/_2$	1	2	4	8	24
Temperature, degrees centigrade	152	144	136	128	120	115	110

gested in Table C, Table VII gives the calculated times rated load current for $^1/_4$ hour to 24 hours inclusive for self-cooled transformers having different characteristics. The present ASA emergency overloads are also shown for comparison. These calculated overloads are given principally for the purpose of showing how they vary for different transformer characteristics. It will, of course, be possible to reduce a group of this kind into three or four classes, since, in several cases, the overload values are of the same order of magnitude. Similar overload values can be calculated in the study of revising the ASA emergency overloads and set up for other types of transformers (OAP, water-cooled, and so forth), which have characteristics different from those for self-cooled transformers.

When calculating the aging during an overload, it may be assumed that the hot spot is maintained during the entire time period. This is not strictly correct, since the hot spot reaches the maximum value only at the end of the time period. The question naturally arises as to how much greater overload could be carried if the aging was based on the actual increasing hot spot and then increased sufficiently to produce the same aging. The increased overload is not so great as might be expected. For example: if we take the 5*a* class of transformer (Table VII) for two hours following full load, and integrate the actual temperature area by the formula given in reference 3, it is found that a maximum temperature of 134.5 degrees centigrade at the end of two hours results by the eight-degree rule in the same aging as 128 degrees centigrade maintained for two hours. The 6.5 degrees centigrade higher hot spot permits a 4.5 per cent greater load. For the eight-hour overload (115 degrees centigrade hot spot) the difference in the overload is only 3.5 per cent, and, for longer times, the difference becomes less and less. Therefore, if it is assumed that the hot spot exists during the entire time period, the error is small and on the safe side.

A Comparison With Laboratory Aging Data

Having arrived at certain temperature limits and overload values, based partly on past experience and partly on the eight-degree rule, it will be interesting to see what per cent of life these proposed temperature limits will use up for a given time period, based on the laboratory tests. The 120 degrees centigrade aging curves shown in Figure 14 of Mr. Clark's paper appear to be the best ones to use for this purpose. For example: using one per cent water content, these curves give the following approximate hours of life at 120 degrees centigrade:

Per cent of original life	50	0
Hours at 120 degrees centigrade	100	430

Taking the 50 per cent life level we have 100 hours/50 per cent = two hours to use up 1 per cent of initial life at 120 degrees centigrade, or one hour to use up one per cent at 128 degrees centigrade which is within eight degrees of the 136 degrees centigrade suggested for one hour. That is, one hour's operation at 136 degrees centigrade would use up between one and two per cent of the life, depending on whether the 136 degrees centigrade temperature was maintained during the entire time, or reached the maximum value at the end of one hour. In the latter case—as pointed out above—the 136 degrees centigrade would be equivalent to several degrees lower continuous temperature. By this process of reasoning and by the assumptions made, the suggested hot-spot limits would use up from one to two per cent of life per operation. Wide variations from these life values would, of course, be obtained for other than one per cent water content. The estimates of life depend entirely upon the assumptions made.

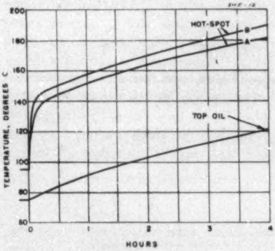

Figure 12. Calculated temperatures for 200 per cent load following full load of transformer having

1. 65 degrees centigrade hot-spot rise
2. 45 degrees centigrade top oil rise
3. 2.7:1 loss ratio
4. Time constant of 5.4
5. In 30 degrees centigrade ambient
6. Rises vary as loss$^{0.8}$

(1–3: at rated load)

Curve *A*—No corrections made for change in resistance and change in viscosity of oil with temperature

Curve *B*—Corrections made for both temperature and oil viscosity

Table C

	Usual Variations	
	Self-Cooled	OAP
Top oil rise over ambient	40 to 50 C	35 to 45 C
Hot spot over top oil	10 to 20 C	15 to 30 C
Loss ratio (copper to iron)	2:1 to 3:1	3.5:1 to 5:1
Time constant	3 to 7	2 to 4.5

At any rate, these laboratory tests have brought out very forcibly a new factor that has not been taken into consideration in the past, namely, water content in the insulation. We seem to make progress in steps. In the early days it was assumed that "temperature" alone was the controlling factor, and later it was recognized that "time" was as important as temperature. Now, we have a third factor, "water content," to take into consideration which appears to be fully as important as "time" and "temperature."

Standardizing Emergency Overloads

Before attempting to standardize emergency overloads, the operators' views are needed on some of the factors discussed above. It is recommended that the

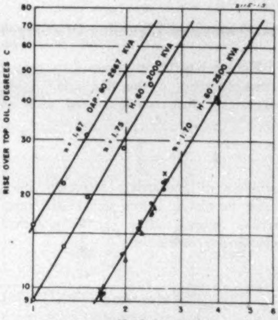

Figure 13. Hot-spot temperature indicator tests

... Normal position
xxx Intermediate position
ΔΔΔ Maximum raised position
Indicator heating coil placed in top oil

problem of preparing emergency overloads be placed before the AIEE transformer subcommittee of the electrical machinery committee.

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Induced Voltages on Transmission Lines

C. F. WAGNER
FELLOW AIEE

G. D. McCANN
ASSOCIATE AIEE

ABOUT the year 1929 it first became appreciated that the lightning discharge is not instantaneous. The recognition of this fact had a profound influence upon the theories of lightning protection, the emphasis being diverted from induced to direct strokes[1] as the agent to be guarded against. With the meager information then available the calculations indicated that induced voltages were unimportant when the finite time of discharge was taken into account.[2] The more accurate information available today calls for a critical analysis of this subject, which is the purpose of the present paper.

A brief résumé of our present knowledge of stroke mechanism will be given, which will form the basis for determining simplifying assumptions to be used in calculating the electric field at the ground. The voltage surges produced on transmission lines by these gradients will then be computed. It will be shown that the over-all influence of the variation of stroke characteristics from the simplifying assumptions is not very great and can, in general, be neglected. With these voltage surges established and from other known time-probability characteristics of lightning strokes, a probability curve giving the frequency of occurrence of induced voltages as a function of their crest magnitude will be computed.

Mechanism of Lightning Discharges

Figures 1 and 2 illustrate the essential characteristics of the discharge[3] based upon the work of Schonland and his associates.[4–8] All strokes to ground or to objects as low as transmission towers originate from charge centers in the base of the cloud. At least 90 per cent of the strokes are of negative character. In the course of the heterogeneous formation of the charges the electric gradient finally reaches a magnitude sufficient to initiate a discharge. The original path is blazed by the "pilot streamer" which propagates toward earth at the most frequent velocity of 0.5 foot per microsecond ($^1/_{30}$ of one per cent of the speed of light). Its path is very irregular, and branching occurs frequently in the direction of propagation. The luminosity is low, and the current does not exceed a few amperes. At intervals ranging from 20 to 90 microseconds "stepped leaders" are formed in this same path. These travel at a much higher velocity of about three per cent of light or 30 feet per microsecond. Upon catching up to the pilot streamer these leaders pause. The current seldom exceeds 200 amperes.[7,9] These initial streamers form an antennalike system upon which negative charge is distributed from the original charge center in the cloud.

All available evidence[3] indicates that only very short upward streamers from the earth occur for strokes to open ground or relatively low objects such as transmission lines. They are probably only a few feet in most cases and certainly do not exceed about 100 feet.

The charge on the stroke channel is then lowered much more rapidly the rest of the way to earth by the very brilliant and rapidly propagating "return streamer" which in its visual manifestation appears as a stroke whose head moves upward toward the cloud at a velocity of about 10 per cent of the speed of light or 100 feet per microsecond. The charge on the channel is discharged continuously as the head progresses, which accounts for the high channel and ground currents shown in the lower portion of Figure 2. The ground current rises to a crest ranging from a few hundred amperes to as high as 160,000 amperes[10] in a time of the order of one to ten microseconds and remains at a relatively high value during the period that the return streamer is rapidly discharging the charge in the space between cloud and ground. The wave shape of this current is determined by the rate of discharge of the stroke channel which is a function of the velocity of the return streamer along the channel and the charge distribution.

After the return streamer reaches the cloud, there may occur a more gradual lowering of charge from cloud to ground down the conducting path produced by the return streamer. Charge tapped by streamers into the cloud from the region in which the stroke was initiated may thus produce a current tail of a few hundred or only a few amperes which, however, may persist for 10,000 or 20,000 microseconds.[10]

Multiple strokes consisting of separate discharges down the same stroke channel may be formed by attraction of streamers from another charge center in the cloud. Streamers from this second charge center form a path to the original charge center, and the charge is then lowered by a "dart leader" down the channel blazed by the first discharge. This seldom shows evidence of the stepped character of the first discharge. Its velocity is of the order of one per cent of the speed of light or ten feet per microsecond. Appreciable branching rarely occurs for such subsequent discharges. As the dart leader reaches the earth, its associated charge is further lowered to earth by a return streamer similar in character to that of the first discharge. About 50 per cent of all strokes are multiple, and as many as 40 separate components have been photographed. The time intervals between

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C. F. Wagner and G. D. McCann are both with the central station engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., Mr. Wagner as manager and Mr. McCann as transmission engineer.

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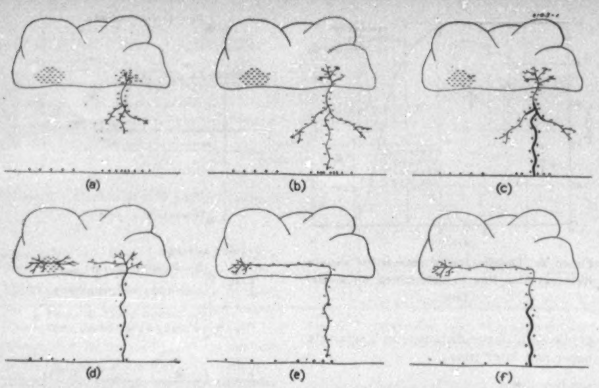


Figure 1. Diagram showing charge distribution at various stages of lightning discharge

(a) Charge centers in cloud
Pilot streamer and stepped leader propagate earthward
Outward branching of streamers to earth
Lowering of charge into space beneath cloud
(b) Process of (a) almost completed
Pilot streamer about to strike earth
(c) Heavy return streamer
Discharge to earth of negatively charged space beneath cloud
(d) First charge center completely discharged
Development of streamers between charge centers within cloud
(e) Discharge between two charge centers
Dart leader propagates to ground along original channel
Dart leader about to strike earth
Negative charge lowered and distributed along stroke channel
(f) Heavy return streamer
Discharge to earth of negatively charged space beneath cloud

components have been found to vary from 0.0005 to 0.5 second with an average of about 0.03 second.

EFFECT OF DISTANCE FROM LINE ON TERMINATING POINT OF THE STROKE

There is a minimum horizontal distance from the line below which all strokes strike the line. As the distance becomes greater, some strokes strike the line and others the ground, and then more and more of them go to ground. Finally all terminate on the ground. The curve of Figure 3 shows this effect as determined from model studies.[11] These studies indicated that for strokes with no appreciable ground streamer the division of strokes is essentially independent of cloud height and a function only of A/h as shown in the figure. The maximum induced voltages occur when the stroke strikes the ground at the minimum distances from the line which are about three times the line height or 75, 150, and 300 feet for line heights of 25, 50, and 100 feet, respectively.

BASIC STROKE MECHANISM FOR INDUCED VOLTAGE CALCULATIONS

Although the lightning discharge is quite complex, and the various factors such as the leader velocities, currents, and charges have a wide range of variation, it has been found that the electric field at the ground and the induced voltage on a transmission-line conductor can be determined quite accurately if the stroke discharge is represented by a fairly simple mechanism.

The basic mechanism for the component of a stroke, illustrated in Figure 4, can be divided into three distinct stages.

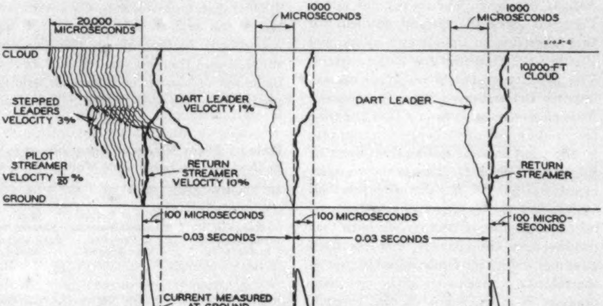
(*a*) *Condition Before the Start of the Discharge.* The charge, $-Q_o$, involved in the initial high-current portion of a single-stroke component (or that lowered onto the stroke channel by the initial leaders) is assumed concentrated in a point source at a height, H, above ground. A height of 2,000 meters or 6,560 feet was chosen, as it was found to be a mean value for cloud heights.

(*b*) *First Stage of Lowering Charge.* At the initiation of the discharge the charge, $-Q_o$, is lowered as a uniform line charge, q coulombs per foot, onto the stroke channel by an initial downward leader moving at the uniform velocity, v, of $^1/_{20}$ of the speed of light or approximately 0.5 foot per microsecond (the pilot streamer velocity). At the end of this period as the streamer reaches earth, the charge in the original charge center is zero. No initial upward streamer from the ground is considered.

(*c*) *Second Stage of Lowering Charge.* At the instant the pilot streamer reaches ground, the return streamer starts propagating upward toward the cloud at the uniform velocity, V, of $^1/_{10}$ that of light or approximately 100 feet per microsecond. It instantaneously discharges each section of the stroke channel reached. The current flowing at ground and at each point of the return streamer is of the same magnitude and constant with time during the period that the return streamer is propagating up to the cloud. A current of 100,000 amperes is used resulting, for the given return streamer velocity, in a leader charge, q, of 1.02×10^{-3} coulombs per foot, an initial leader current, i, of 500 amperes, and (for $H=$ 6,560 feet) an initial cloud charge, $-Q_o$, of -6.66 coulombs.

Figure 2. Diagram showing lightning mechanism and ground current

All velocities expressed in per cent of the speed of light which is 984 feet per microsecond or approximately 1,000 feet per microsecond



In all three stages the earth is represented as a perfectly conducting plane by a system of image charges and currents in an image channel.

Ground Gradients

The electric gradient at any point, P, on the ground for the assumption of perfect conducting plane, has only a vertical component that can be resolved into

(*a*). That due to the instantaneous distribution of charges in the stroke channel.

(*b*). That due to the instantaneous current in the stroke.

When either of these conditions change rapidly (with the speed of light as a criterion), it is necessary to take cognizance of the finite time required for the disturbance to propagate to the point in space under consideration.

Expressions for the calculation of the three stages of the discharge enumerated in Figure 4 have been derived in appendix I with the following results.

(*a*) *Before the Start of the Discharge.* Figure 5 shows the initial field as a function of the horizontal distance, d, from the charge center.

(*b*) *First Stage of Lowering Charge.* The left-hand side of Figure 6 shows the ground gradient due to the electric charges alone for various distances, d. Note that the gradient for strokes near the line increases from the value given in Figure 5 at zero time to the extremely high values, attained at the instant the initial streamer strikes the earth. These are plotted in Figure 7. The contribution of the current during this time is negligible, and the processes involved are so slow that the time for transmission of the effect is negligible.

(*c*) *Second Stage of Lowering Charge-Return Streamer.* The right-hand side of Figure 6 gives the ground gradient for this stage due to the charges alone, and Figure 8 the gradients due to the current. The polarity of the latter gradients are opposite to those produced by the charges. This phenomenon is so fast that the time required for the disturbance to propagate to the point under consideration must be taken into account. This is shown quite clearly in Figure 8. For distances less than 10,000 feet the gradient due to the current is negligible in comparison with that produced by the charges. For the fixed streamer velocities assumed in Figure 4, the relative contribution of the two components is independent of the current, since the charge is also proportional to the current, I.

Special consideration will be given to gradients at large distances in a separate paper at a later time.

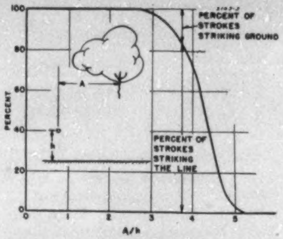


Figure 3. Distribution of strokes for various distances from line as determined by model tests[11]

Voltage Waves on Conductors

If a long horizontal conductor were divided into short sections, and each section insulated from ground and from the other sections, the voltage appearing at any point at any instant would be merely the voltage gradients at the point times the height of the conductor. Actually, however, transmission-line conductors are continuous. If consideration be given first to an extremely long conductor, then the voltages appearing upon the conductor are free to travel in the form of traveling waves. If the conductor is originally at zero potential, and a voltage wave such as shown by the full line in Figure 9a is suddenly induced, then this wave is broken up into two components each of half the value of the original wave, one half traveling to the right and the other half to the left. After the passage of the waves a negative charge remains on the conductor, representing the negative of the positive charge carried away by the two waves. If a second induced wave be applied instantaneously to the conductor before the first wave will have had an opportunity to have propagated beyond the region of the first, the two waves can be added as shown in Figure 9b, giving the resultant wave indicated. The same process can be carried out indefinitely, as shown for four independent waves in Figure 9c. Thus it is necessary only to divide the continuously increasing inducing voltage wave into finite increments for finite increments of time. Each of the increments is then split up into two halves which are superposed upon the previous halves of increments, each displaced in space along the wire in accord-

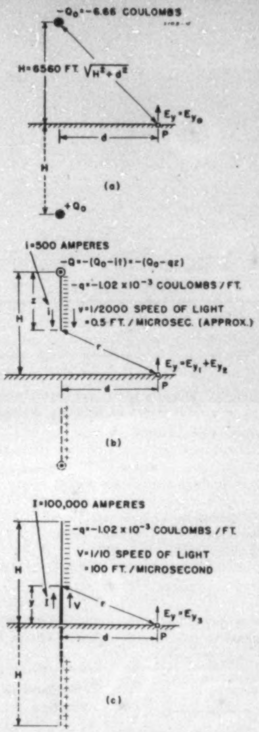


Figure 4. Basic stroke mechanism for no ground streamer

Table I. Mean Values of Return-Streamer Velocity and Corresponding Correction Factors to Be Used in Calculating Crest Voltage Induced by a Stroke With a Given Current Magnitude

Range of Stroke Current in Amperes	Range of Return-Streamer Velocity in Feet Per Microsecond	Mean Velocity for Each Current Range	Correction Factor, K, to Be Applied to Voltages Given in Figure 12* Stroke Distance in Feet			
			150	300	600	1,000
0– 50,000	50–190	120	0.93	0.97	1.00	1.00
50,000–100,000	190–330	260	0.70	0.84	0.97	1.00
100,000–160,000	330–480	400	0.61	0.66	0.85	1.00

* For a given crest stroke current (I) in amperes and crest voltage E_m as given by Figure 12, actual voltage is given by the equation $E_m' = \frac{E_m K I}{100{,}000}$.

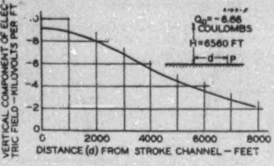


Figure 5. Electric ground gradient just prior to start of discharge

ance with its own increment of time. The increments of field at each time interval as a function of distance, x, along the line can be obtained from curves similar to those of Figure 6 where the distance, d, to be used is the square root of the sum of the squares of the distance along the line and the minimum distance, A, of the stroke from the line. The time intervals which must be chosen to give sufficient accuracy depend upon the rate of change of the field both with time and distance along the line. The greater the rate of change with both of these quantities, the smaller the time intervals have to be. For stroke distances in excess of the minimum at which indirect strokes can occur, as given by Figure 3, the rate of change of field with time is so small for the mechanism of Figure 4 compared to its decrease with distance along the line, in the period the initial streamer is propagating to ground, that the induced voltage is insignificant. The very small increments of voltage, which are negative for negative strokes, and carry away negative charge, move away so rapidly that they do not accumulate an appreciable voltage. If the line is of finite length and open-circuited at both ends with no leakage paths to ground, the successive waves considered in this graphic analysis will produce positive reflections at the line ends which will propagate back to the region under the cloud, return the negative charge, and allow the conductor to rise in potential. However, for actual transmission lines the terminal equipment and other leakage paths will allow charge to flow onto the line and keep it at zero potential during this initial phase of the stroke discharge.

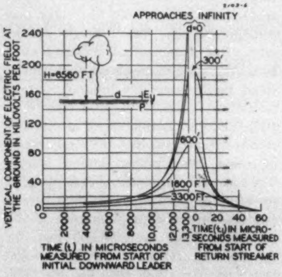


Figure 6. Ground gradient as function of time and distance from stroke channel

Stroke mechanism of Figure 4 ($I = 100{,}000$ amperes)

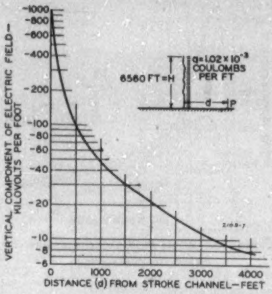


Figure 7. Maximum value of ground gradient that occurs at instant initial streamer reaches earth

Stroke mechanism of Figure 4 ($I = 100{,}000$ amperes)

During the progress of the return streamer, the rates of field change with time are sufficient to induce a voltage. The negative field is decreasing, so that the rate of change is positive, inducing a positive voltage which propagates off along the line carrying away the positive charge accumulated in the previous period of the discharge. Time intervals of 0.2 microsecond were found small enough for accurately calculating the induced voltage for return streamer velocities of the order of 100 feet per microsecond. Figure 10 shows the voltage induced along an infinitely long conductor 50 feet above the ground at successive intervals of time after the start of the return streamer for a stroke 300 feet from the line. The maximum voltage in all cases is induced at or very near the point on the line closest to the stroke channel. The magnitude of the voltage propagating down the line is, before being attenuated by propagation, only slightly less than the maximum voltage and has the same wave shape. In this case it is 86 per cent of the maximum.

In Figure 11 are shown curves of the maximum voltage waves induced by strokes in conductors at various distances from the stroke. These occur at approximately the point in the line nearest the stroke channel. Since the voltage is proportional to the conductor height, the curves can be applied to heights other than 50 feet. Both the time to crest and time to half value increase with stroke distance. They range from 2 to 20 microseconds and 6 to 36 microseconds, respectively, for distances from 150 to 1,600 feet. In Figure 12 are curves showing the maximum crest voltage induced on lines of various heights as a function of stroke distance. The maximum voltage which can be induced by a stroke with a current of 100,000 amperes and the mechanism of Figure 4 is 970 kv for a 100-foot conductor and 1,070 kv for both 50-foot and 25-foot conductors. The voltage is higher for the latter two, be-

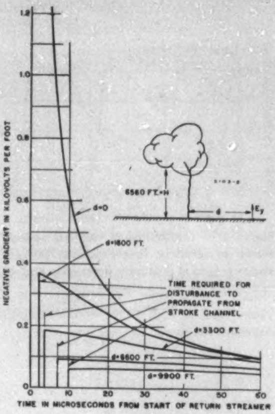


Figure 8. Ground gradient due to current in return streamer

Stroke mechanism of Figure 4 ($I = 100{,}000$ amperes)

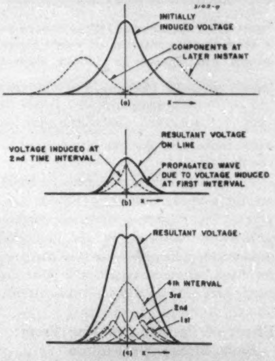

INITIALLY INDUCED VOLTAGE
COMPONENTS AT LATER INSTANT
(a)
x
VOLTAGE INDUCED AT 2nd TIME INTERVAL
RESULTANT VOLTAGE ON LINE
PROPAGATED WAVE DUE TO VOLTAGE INDUCED AT FIRST INTERVAL
(b)
RESULTANT VOLTAGE
4th INTERVAL
3rd
2nd
1st
(c)

Figure 9. Illustration of step-by-step method of graphically calculating voltage induced on transmission-line conductor

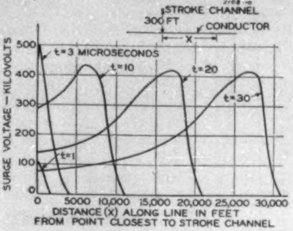


Figure 10. Distribution of induced voltage along an infinitely long conductor 50 feet above ground at successive times after start of return streamer

Stroke mechanism of Figure 4 (I=100,000 amperes), and stroke 300 feet from conductor

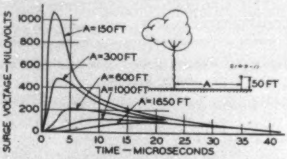


Figure 11. Maximum voltage induced on a conductor 50 feet above ground

Stroke mechanism of Figure 4 (I=100,000 amperes)

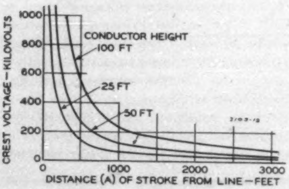


Figure 12. Maximum crest voltage induced on a conductor as a function of height above ground and distance from stroke channel

Stroke mechanism of Figure 4 (I=100,000 amperes)

cause indirect strokes can occur closer to the line.

The voltage curves of Figures 11 to 13 are for a stroke current of 100,000 amperes. The voltage, however, is directly proportional to the current for the stroke mechanism of Figure 4 since the charge, q, for fixed streamer velocities is also directly proportional to the stroke current.

Effect of Factors Differing From Basic Stroke Mechanism

It is now possible to determine the accuracy of the assumed basic stroke mechanism and the effect of the variation of the important factors.

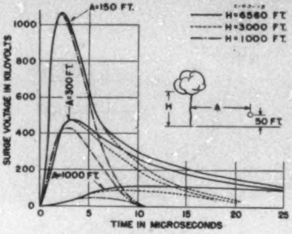


Figure 13. Effect of stroke channel height upon induced voltage

I=100,000 amperes

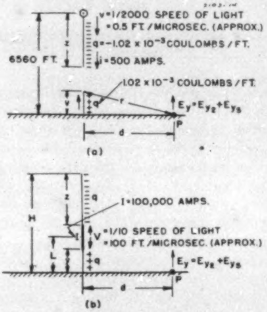

v=1/2000 SPEED OF LIGHT =0.5 FT./MICROSEC. (APPROX.)
q=−1.02 × 10⁻³ COULOMBS / FT.
i=500 AMPS.
6560 FT.
z
1.02 × 10⁻³ COULOMBS / FT.
v
q
r
Ey=Ey2+Eys
P
d
(a)
z
q
I=100,000 AMPS.
H
V=1/10 SPEED OF LIGHT =100 FT./MICROSEC. (APPROX.)
L
q
Ey=Ey2+Eys
P
d
(b)

Figure 14. Mechanism assumed for strokes with upward ground streamers

(a). When downward leader reaches distance $2L$ from ground, upward streamer is initiated with velocity, v, which distributes charge, q, along its channel

(b). After streamers meet, return streamer propagate from meeting point with velocity, V, and annul distributed charge as they proceed

HEIGHT OF STROKE CHANNEL

The only data[11] available pertaining to stroke channel height are that on cloud base heights. Although the cloud source of a stroke may not coincide with the cloud base, the effect of stroke channel height is so small that the difference is not important. For relatively flat terrain the bases of cumulo-nimbus or thunder clouds are usually higher than 1,000 feet with an upper limit of about 30,000 feet and a mean of about 2,000 meters or 6,560 feet. Occasionally, however, strokes to ground occur from cumulus clouds as low as 500 feet, and in mountainous regions the clouds may envelope peaks. The basic calculations have been made for the mean cloud height. Only low channel heights will give a significant variance from the calculations made for a height of 6,560 feet.

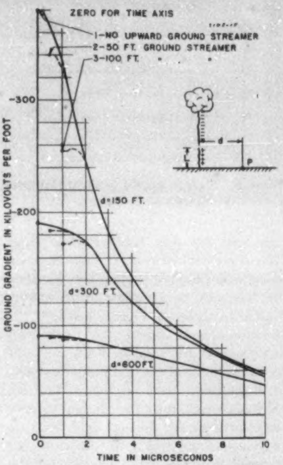


Figure 15. Effect of upward ground streamers on ground gradient during period of return streamer

Mechanism of Figure 14 (I=100,000 amperes)

For the same current and the typical streamer velocity of Figure 4 the minimum channel height for which the variance from the mean stroke length can be neglected depends upon the distance from the stroke channel. For d equal to 150 feet a 1,000-foot stroke gives a maximum ground gradient only ten per cent less than for the mean (6,560) height. For d equal to 300 feet, the corresponding height is about 2,000 feet, and for d equal to 1,000 feet, about 4,000 feet. The percentage effect of channel height on the crest value of the induced voltage is even less. In Figure 13 are shown the effect of cloud height upon the induced voltage for I equal to 100,000 amperes and a constant uniform distribution of charge and return streamer velocity, the total charge, of course, being proportional to height. The approximate minimum cloud heights for which only a ten per cent variance is produced in the crest magnitude of the induced voltage are $H=500$ feet for $d=150$ feet, $H=1{,}000$ feet for $d=300$ feet and $H=3{,}500$ feet for $d=1{,}000$ feet. One thousand feet is the maximum distance for which significant voltages will be induced on lines of practical heights. Lower cloud heights do, how-

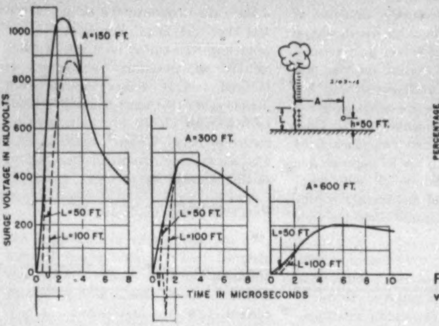


Figure 16. Effect of upward ground streamers upon induced voltage wave for a 50-foot conductor

Stroke mechanism of Figure 14 ($I=100{,}000$ amperes)

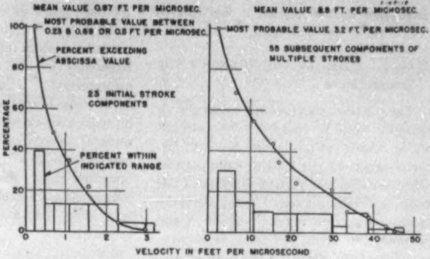


Figure 18. Percentage distribution curves of the effective downward velocity of initial streamers as determined from Boys camera data of Schonland, Malan, and Collens[5]

(a, left). Pilot streamer of first component
(b, right). Dart leader of subsequent multiple stroke components

ever, shorten the duration of the tail of the voltage as shown in Figure 13. The number of strokes to relatively flat terrain which will have low enough heights to cause a significant variance from the crest voltages calculated by the formulas of Figure 4 is very small.

CHARGE CENTERS IN CLOUD

As shown by comparison of Figures 5 and 6, the ground gradient due to the charge in a single charge center, whether it be concentrated at a point or distributed over a finite volume, is negligible compared to the total field during the period in which an appreciable voltage is being induced on a conductor. A large amount of charge in other regions of the cloud may produce an appreciable ground gradient but, so long as it is constant, cannot contribute to the voltage wave on the conductor. The probability of two simultaneous discharges occurring sufficiently close to affect the induced voltage wave is extremely small.

UPWARD STREAMERS FROM GROUND

Upward streamers from ground which meet the initial downward streamers are probably seldom over a few feet (and definitely not over 100 feet) in length for strokes to open ground or to relatively low objects. Calculations have been made of the effect of such ground streamers by expressions which can readily be developed from what is already given in appendix I. The stroke mechanism assumed is shown in Figure 14. It is assumed that the initial streamer from the cloud has the same character as the basic case given by Figure 4. When it reaches a height above earth of twice the ultimate height, L, of the ground streamer, a streamer is initiated from the ground and propagates upward at the same uniform velocity, v, depositing positive charge on its channel of the same density, q, as for the downward leader. When the two streamers meet, return streamers are formed at the junction point which propagate in both directions at the uniform velocity, $V=100$ feet per microsecond. In Figure 15 are plotted curves of the electric field during the period of progress of the return streamer for a 50-foot and a 100-foot ground streamer compared with the case of no ground streamer. After the return streamer has reached earth and thus discharged all of the positive charge on the ground streamer, the field is the same as for the case of no ground streamer for corresponding heights of the return streamer channel above ground. The curves of Figure 15 have been plotted with this corresponding portion coinciding for better comparison. Thus, the time of starting of the return streamer (or the zero for the time axis) in Figure 15 must be shifted a different amount for each length of ground streamer to make the curves coincide. The black dots indicate the points of zero time for each case.

The upward ground streamer decreases the maximum ground gradient. However, calculations of the induced voltage, as shown in Figure 16, indicate that even streamers 50 to 100 feet long have little effect upon the crest magnitude of the induced voltage wave. A 100-foot streamer affects the crest only at distances less than 300 feet. For some cases the field tends to increase before decreasing, and this produces a negative peak on the induced voltages. The higher effective velocity of the return streamer during the initial period decreases the front of the wave somewhat. Since most ground

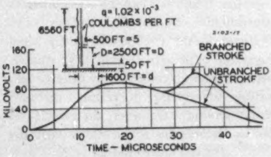


Figure 17. Successive peaks produced by a branch

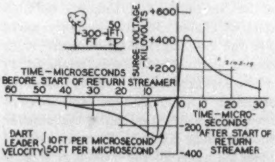


Figure 19. Comparison between negative voltage induced by dart leaders and voltage induced by return streamer

Stroke mechanism of Figure 4 ($I=100{,}000$ amperes)

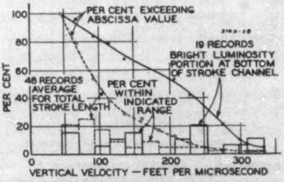


Figure 20. Percentage distribution curves of the effective upward velocity of the return streamer from Boys camera data of Schonland, Malan, and Collens[5]

streamers do not exceed a few feet in length their effect can be neglected.

IRREGULARITIES IN THE SHAPE OF THE STROKE PATH

The basic mechanism assumes a straight vertical channel. For strokes whose mean path is about vertical, the tortuousness of the path is unimportant when the effective vertical velocity of the return streamer is used. Placing the same charge in a given vertical section of the channel also gives the same result, regardless of the shape of the path or distribution. Strokes having an appreciable slant cause a greater variance when the channel is not in a perpendicular plane parallel to the line. If the base of the stroke channel toes toward the line, the magnitude of the ground gradient is smaller, and if it toes away, larger than if the channel were vertical. However, the rate of change of the ground gradient with time will be increased in the first case and decreased in the second case, producing a compensating effect on the induced voltage waves. Model tests indicate that those strokes which strike the ground near the transmission line have channels which are not attracted significantly toward the line. In any statistical analysis the only reasonable assumption is a vertical straight channel.

BRANCHING

Schonland, Malan, and Collens[5] have found two characteristic types of branching, those occurring high enough above ground that they do not reach the earth, and those close to ground which contact the ground and form additional channels at the stroke base. This type of branching has been called by them "root branching." The branches leave the main channel at a distance of from 60 to 500 feet above the ground and are from 60 to 150 feet apart at ground. Branches of the first type of appreciable length seldom reach lower than 2,500 feet from ground. Branching usually occurs for the first component of a multiple stroke, but appreciable branches are seldom present in successive components. Of 57 Boys camera[5] records of first components, 54 had branching with an average of five branches each; and of 33 subsequent components, 14 had an average of one very short branch each while the rest had none.

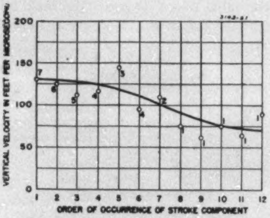


Figure 21. Variation of average return-streamer velocity with order of occurrence of stroke component[4]

Each point is the mean of the indicated number of records

For strokes close to transmission lines, "root branching" will exert some influence on the induced voltage. However, the branches occur practically simultaneously, and for a given configuration of branching an effective location can be determined where practically the same result will be obtained with a single channel. A higher voltage will be induced by an unbranched stroke at the same distance as the nearest branch. If the branches are all the same distance from the line, the difference will be negligible. As shown in the previous section, the character of the stroke within the first 100 feet of ground has little effect on the induced voltage.

Branches 2,500 feet above ground will not appreciably affect the crest magnitude of the ground gradient or the crest magnitude of the induced voltage wave. They may, however, produce additional peaks on the voltage wave. In Figure 17 is shown a calculation made for a horizontal branch 500 feet in length (the average recorded with the Boys camera[5]), 2,500 feet above ground and pointing directly toward the conductor which is 1,600 feet from the main stroke channel. The same uniform charge density is assumed for the branch as for the main channel. It is assumed that when the main return streamer reaches the branch, the branch charge is lowered by a return streamer propagating at the same uniform velocity, $V=100$ feet per microsecond, as for the main channel. This case will produce an abnormally high current peak because of the direction of branching and the high relative charge density on the branch. For the case illustrated in Figure 17, a second peak of 110 kv is produced 33 microseconds after the start of the return streamer. Its magnitude and time of occurrence will be practically the same for shorter stroke distances, so that, to determine the effect of the branch at shorter distances, the increment due to the branching can be superposed on the voltage waves of Figure 11. For d equal to 1,600 feet, the second crest is 22 per cent higher than the first. However, for d less than 1,000 feet, the second crest is less than the first. For most cases of branching, the second crest will be much smaller. Cathode-ray oscillograms of induced voltage waves showing such double peaks that were definitely not due to reflections on the line have been recorded by Perry, Webster, and Baguley.[12] The second crest was in all their records smaller than the first.

VELOCITY OF INITIAL LEADERS

In the basic stroke mechanism it was assumed that the charge was lowered uniformly onto the channel by a single downward moving leader with a constant current. For the first component of a stroke the charge is lowered in steps, some of it by the pilot streamer and some by the stepped leaders. A continual process of recombination of the negative ions with positive ions is probably taking place along the channel. It is this process which very likely causes the building up of sufficient gradient along the channel to start each successive stepped leader. This fluctuation of charge and stepped leader current produces pulses in the ground gradient and has been recorded by Schonland, Hodges, and Collens.[8] It is probably more pronounced in records obtained at large distances but does not alter the mean field. This effect is not present in the dart leaders of successive components, as these leaders are either continuous, or the pulses occur so close together that they can be considered as continuous. Schonland, Hodges, and Collens[8] found evidence that in some cases the pilot streamer of first components slowed up considerably after propagating to within a certain distance above ground. However, the velocity of the leader over the whole stroke path was in all cases too low to induce voltage on a conductor. For most cases the pilot streamer velocity, although having ir-

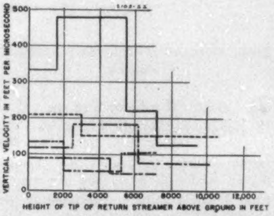
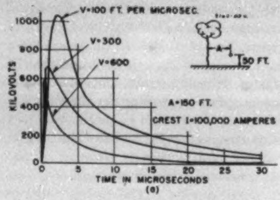
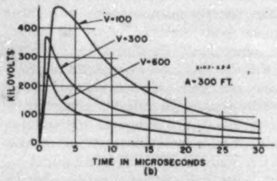


Figure 22. Variation of return-streamer velocity with height above ground for the five strokes recorded by Schonland and Collens[5] for which this analysis could be made



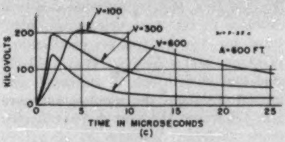
(a). Voltage wave for $A = 150$ feet



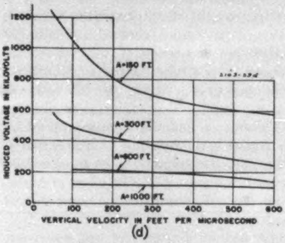
(b). Voltage wave for $A = 300$ feet

Figure 23. Effect of velocity, *V*, of return streamer upon voltage wave induced on conductor

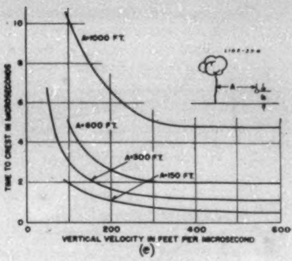
Uniform velocity is assumed and uniform distribution of charge along channel is varied for each case in order to maintain $I = 100{,}000$ amperes



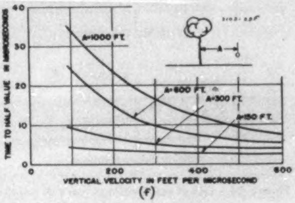
(c). Voltage wave for $A = 600$ feet



(d). Crest magnitude



(e). Time to crest



(f). Time to half value

regularities, had the same mean velocity over the entire stroke path.

An initial leader velocity of 0.5 foot per microsecond was used for the basic calculations. In Figure 18 are percentage distribution curves, plotted from the Boys camera records of Schonland, Malan, and Collens,[5] showing the range of variation of the average effective vertical velocity of both the pilot streamer and the dart leader. These values (as were all vertical velocities) were calculated from the two dimensional velocities obtained from the Boys camera photographs by dividing them by 1.3 the average ratio found[5] to exist between this and the straight line path on the film. Only the extreme upper range of the pilot-streamer velocities is high enough to induce appreciable voltage during this period of the discharge. The dart-streamer velocities are, however, high enough to produce a significant voltage. The most common value for this velocity is ten feet per microsecond and the upper limit is 50 feet per microsecond.

In Figure 19 are plotted the voltages induced by leaders of these velocities on a conductor 50 feet above the ground at a distance of 300 feet. For dart-leader velocities of 10 and 50 feet per microsecond the crest magnitude of the negative voltages are 14 and 63 per cent, respectively, of the subsequent positive crest voltage produced during the return-streamer period. For the extreme pilot-leader velocity of three feet per microsecond it will be about four per cent. The duration of the negative induced voltage is about proportional to the velocity of the leaders as shown in Figure 19.

VELOCITY OF RETURN STREAMER

Figures 20 to 22 give data on the range of variance of the velocity of the return streamer as obtained from Boys camera records.[4,5] Here also the one dimensional vertical velocity is considered. In Figure 20 part of the data are for records for which only the average velocity for the total stroke path could be determined, and part for records for which the average velocity of the initial bright luminosity portion could be measured. This is the portion of the stroke path below the first main branch. As shown by Figure 20, the average velocity for the total path varies from 50 to about 330 feet per microsecond with a most common value of about 80 feet per microsecond. For the bright luminosity portion the range is the same with a most common value of about 240 feet per microsecond. The data[5] from which Figure 20 was plotted did not permit segregating the stroke components in their order of occurrence. This could be done, however, for some of the records as published earlier,[4] and in Figure 21 is shown the variation of average return-streamer velocity with the order of occurrence of the stroke component of a multiple stroke. The velocity of successive strokes is generally less than the previous one with the initial component having the highest value. It has also been found that the stroke luminosity generally decreases with successive components in the same manner, and that the higher velocities are associated with the higher luminosities. Stroke-current measurements show the same general trend, so that the evidence is quite strong that higher return-streamer velocities are associated with higher stroke currents. Also, the higher return-streamer velocities of first components are associated with considerable branching. It is probable that the increased ground gradient produced by the branches accounts for the higher velocities and stroke currents of first components.

In Figure 22 are plotted the velocities of five Boys camera records published by Schonland and his associates[5] for which the distribution of velocity along the channel could be determined. It is questionable that the base of the stroke channel below about 100 feet was recorded. However, the crest magnitude and wave shape of the tail of induced voltages are not affected by the character of the stroke mechanism at the base of the channel. This was well illustrated by the effect of ground streamers, which would have the maximum effect of any condition which would occur at the base. The lowest portion of the stroke channel which need be considered in determining the crest magnitude and the

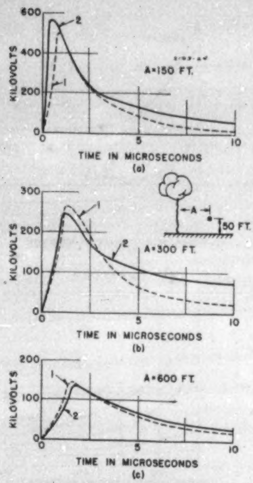


Figure 24. Effect upon voltage wave of return-streamer velocity varying along stroke channel

Curve 1—Uniform charge distribution with streamer velocity given by solid curve of Figure 22

Curve 2—Uniform charge distribution and uniform $V = 480$ feet per microsecond

Crest current = 100,000 amperes for both cases

tail of the voltage wave depends upon the stroke distance from the line. For 150 feet it is about 75 feet above ground, for a distance of 300 feet it is about 150 feet, and for a stroke distance of 600 feet, about 400 feet. In general, the abrupt decrease in velocity of the return streamers in Figure 22 is associated with the streamer reaching a branch point. It probably also varies somewhat between branches but not enough to be recorded. The maximum known effective vertical velocity is the 480 feet per microsecond of the stroke represented by the solid curve of Figure 22. The maximum velocity along the channel also occurred for this stroke and is about 600 feet per microsecond.

Calculations have been made of the effect of return-streamer velocity on the induced voltage wave. In Figure 23 is shown the effect of velocity, with the charge density so adjusted as to maintain the same stroke current. Increasing velocities decrease the crest magnitude, time to crest and duration of the voltage. As shown in Figure 23d, the effect on the crest voltage is most pronounced at the short-stroke distances and lower velocities. For a stroke distance of 150 feet the upper limit of vertical velocity of $V = 480$ feet per microsecond causes a decrease in the crest voltage, as calculated for $V = 100$ feet per microsecond, of 44 per cent. At 300 feet it is 40 per cent and at 600 feet, 19 per cent. For greater distances the effect is unimportant. However, the wave shape is affected more at large distances as shown in Figures 26 and 27.

Calculations have also been made to determine the effect of velocity variation along the stroke channel. Figure 24 shows a comparison of the voltage induced by a stroke having a uniform return-streamer velocity of 480 feet per microsecond and a uniform distribution of charge, q, and the voltage induced by a stroke with the same charge distribution but the velocity given by the solid curve of Figure 22. The effect of variation is small, producing a change in crest voltage of 14 per cent at $d = 150$ feet, 6 per cent at $d = 300$ feet and 8 per cent at $d = 600$ feet. The variation has no effect on the front of the wave and little effect on the time-to-half value, although the extreme tail of the wave is decreased by the decreasing velocity.

The variation of return-streamer velocity along the stroke channel is not important, because it does not vary greatly until it reaches the first branch point which is usually at least 2,500 feet above ground. As pointed out earlier, the character of the stroke mechanism above this height has no important effect on the induced voltage. It is then the velocity of the bright luminosity portion or that below the first branch which should be used for voltage calculations.

Variation of Charge Distribution Along Channel

The two principal factors governing the stroke current flowing at ground are the charge distribution, q, and the return streamer velocity, V. The variations of streamer velocity of the five cases of Figure 22 are sufficient to account for the decrease of current recorded for the tails of lightning-stroke currents. Independent data regarding the time for the current at the ground to decrease to half value are given in Figure 25. The normal variance of this time is from 15 to 100 microseconds, with an average of about 50 microseconds. The solid velocity curve of Figure 22 would, for a uniform charge distribution, give a time-to-half value of 16 microseconds; the other curves give times of about 40 microseconds.

Calculations have been made to determine the effect of nonuniform charge distribution on the induced voltage wave. In Figure 26 are compared the voltages induced by a mechanism with a uniform charge and velocity distribution and by the same uniform velocity distribution but a charge distribution such that the current decays exponentially to half value in 40 microseconds. The effect on the induced voltage wave shape and crest magnitude is slight, as would be expected since the charge cannot decrease greatly in the first 1,000 or 2,000 feet and still give the proper current wave shape.

Rate of Discharge of Stroke Channel by Return Streamer

In the fundamental stroke mechanism of Figure 4, it is assumed that, as the return streamer reaches a given height above ground, it immediately discharges all the charge on the channel at that point. It has been suggested by Bruce and Golde[14] that this is not the case. They believe that the charge, after being lowered onto a given section of the channel by the initial leaders, spreads out radially and thus occupies a larger cross section than is immediately made con-

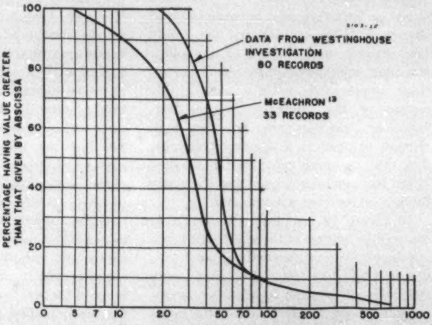


Figure 25. Percentage distribution curves of time-to-half value of lightning-stroke currents

ducting by the return streamer. They further assert "that the darts associated with the leaders of first strokes result from lateral conduction from the initial channel outwards as the tip progresses and the potential at each point behind it rises," and that: "During the return stroke this ionization in each horizontal layer cannot be recombined instantaneously, since the lateral progression of this process will again take time."

Schonland[7] found that the duration of high luminosity at any point after it is reached by either the initial bright portion of the stepped leader (which Bruce and Golde refer to as the "dart leader"), or by the return streamer is of the order of five to ten microseconds. After this the luminosity suddenly diminishes. This is the period in which Bruce and Golde assume the channel to be widening out. Schonland[7] himself has pointed out that luminosity of this duration cannot be due to the excitation period of nitrogen (the principal element involved in the ionization process). Its period is only about 0.1 microsecond.

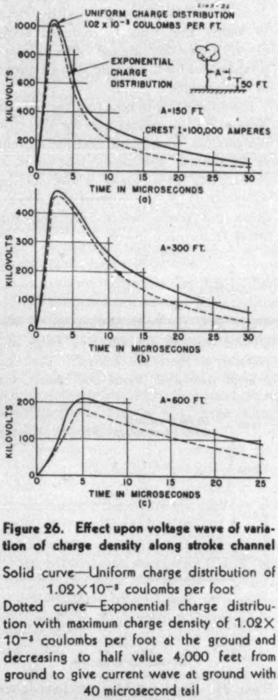


Figure 26. Effect upon voltage wave of variation of charge density along stroke channel

Solid curve—Uniform charge distribution of 1.02×10^{-3} coulombs per foot

Dotted curve—Exponential charge distribution with maximum charge density of 1.02×10^{-3} coulombs per foot at the ground and decreasing to half value 4,000 feet from ground to give current wave at ground with 40 microsecond tail

For both curves $V = 100$ feet per microsecond and crest current $= 100{,}000$ amperes

Such lateral ionization may take place. However, even if the return streamer does (as shown in Figure 27) first blaze a conducting channel of smaller cross section than that occupied by the initial charge on the channel, positive charge must immediately flow onto the outer surface of the initial highly conducting channel to maintain it at the low potential it will have by virtue of being in contact with the earth. For the ground gradient calculations, all of the return-streamer portion of the stroke channel can be considered as being at earth potential as the arc drop is small. As shown in appendix II, the amount of positive charge on a given channel section required to maintain the core of the return streamer at earth potential in the presence of estimated surrounding charges of the downward leader is at least 70 per cent of the negative charge left in the space not yet made conducting by the return streamer. Most of the charge is neutralized rapidly by recombination with charges rising in the ground leader; and even if a remaining charge exists, as Bruce and Golde purport, that is not immediately neutralized by this process, additional positive charge to the extent of at least 70 per cent is drawn into the section to maintain the arc core at earth potential. Thus, positive charge flows up the stroke channel at a rate determined by the return-streamer velocity and charge density, q, on the stroke channel, and the effect is essentially the same as though the combination of the two sets of charges were consummated instantaneously.

Positive Charge on Return-Streamer Channel

The analysis of appendix II considers only the relationship the positive charge on the channel must bear to the negative charge in a horizontal section of the conductor. Symmetry about a vertical axis is assumed, and the end effects at the tip of the return streamer are neglected. Because of electrostatic induction with the negatively charged leader that has not yet been discharged, the upwardly extending channel and ground which are all at the same potential, do not have equal charge distribution. Thus, there must be a net distribution of positive charge on the upward streamer. If this charge were uniform, it would produce no effect on the induced voltage, as calculated by the mechanism of Figure 4 for a fixed stroke current. For the same current, the negative charge on the stroke channel would have to be decreased by an amount proportional to the assumed positive charge, and the net field change with time would be the same. The density of this positive charge on the return streamer should be greatest near the tip and should decrease down the channel. Thus, as the streamer is forming upward, the current at ground feeding the streamer will decrease with time. Since the variation of return-streamer velocity alone is sufficient to account for the measured current wave shapes, the variation of charge along the stroke channel cannot be very great, especially for the first 1,000 or 2,000 feet above ground. It probably does not produce so great an effect as the variation of negative charge for the case illustrated in Figure 26.

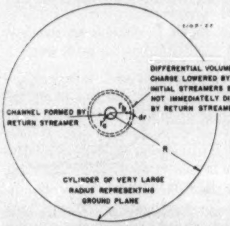

DIFFERENTIAL VOLUME OF CHARGE LOWERED BY INITIAL STREAMERS BUT NOT IMMEDIATELY DISCHARGED BY RETURN STREAMER
CHANNEL FORMED BY RETURN STREAMER
r_b
r_c
dr
R
CYLINDER OF VERY LARGE RADIUS REPRESENTING GROUND PLANE

Figure 27. Diagram showing cross section of stroke channel

Relation Between Maximum Induced Voltage and Voltage Propagating Along Line

In the previous discussion only the voltage induced on the line at the point nearest the stroke channel, or the maximum voltage, has been considered. For the range of variation of the several factors controlling the induced voltage, the ratio between the voltage which propagates away from this point and the maxi-

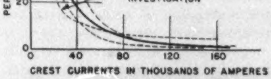


Figure 28. Percentage distribution curves of crest currents at ground in lightning strokes as obtained by various investigators

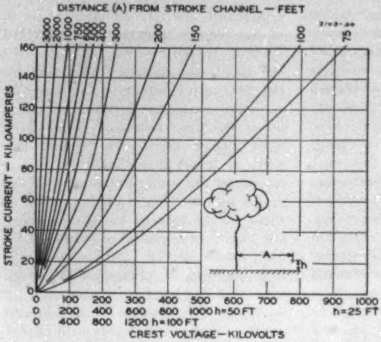


Figure 29. Crest of induced voltage wave as a function of stroke current, distance from channel, and height of conductor

mum (as illustrated in Figure 10) is never less than about 0.8 and is usually greater than 0.9. Thus, for practical purposes it is considered best to use the maximum voltages for both cases.

SUMMARY

The foregoing analysis shows that induced voltages on transmission lines can be calculated to a good degree of accuracy by the stroke mechanism of Figure 4 where the distribution of charge and the return-streamer velocity is assumed constant along the channel. These are the primary factors determining the voltage wave shape and magnitude, and they in turn determine the stroke current. Wave shape is little affected by anything but the return-streamer velocity as summarized in Figures 23e and 23f. As shown in Figure 23d, the effect of velocity upon the crest voltage may be considerable, but it can be taken into account to a good degree of accuracy. The velocity increases with the magnitude of the stroke current. The exact manner in which it does is not known. However, if the measured range of the velocity and the stroke current are each divided into only three equal ranges, and a mean velocity used for each current range, the voltage can be calculated to good accuracy as a function of the crest stroke current only.

Figure 28 gives what is considered to be the best mean probability curve on the relative crest magnitudes of currents in lightning strokes. The maximum current which has been reliably measured is 160,000 amperes.[10] In Table I is shown the segregation of stroke current and return-streamer velocity into the three ranges together with the mean velocities for each range. Correction factors, determined from Figure 23d, applicable to the mean velocities for different distances, are also given in Table I. In Figure 12 are given crest voltage curves based on a stroke current of 100,000 amperes and a streamer velocity of 100 feet per microsecond. The curves of Figure 12 can be used for calculating the induced voltage as a function of crest current, together with the equation and the mean correction factors of Table I. In this manner the curves of Figure 29 were obtained which give the induced voltage as a function of stroke current for various conductor heights and stroke distances.

Effect of Other Conductors and Line Terminal Conditions

As has been shown previously,[27] the voltage induced on an ungrounded conductor is not affected by the presence of other ungrounded conductors. Ground wires, which are perfectly grounded so that their potential remains at zero at all points, reduce the voltage induced on a conductor by the factor $(1-K)$, where K is the coupling factor between the conductor and the ground wires.

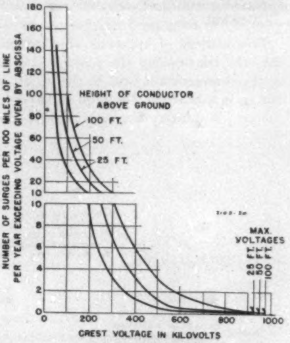


Figure 30. Probability of occurrence of induced voltages

Isokeraunic level = 30

However, for long spans and strokes of high current magnitude occurring close to the line, appreciable voltage appears for a short time on the ground wire. Consider for example, a line with 1,000-foot spans and a ground wire 100 feet above ground. As shown by Figures 29 and 23e, a voltage of 975 kv with a 1.2-microsecond front can be induced at mid-span. The rate of rise is too rapid for the reflection from the towers to limit the crest appreciably. Approximately the same potential is induced on both conductor and ground wire. Until the waves reach a tower, there is little potential difference across them. Upon reaching a tower, the voltage wave on the ground wire is reduced sharply, and thus the voltage across the string of insulators is practically the same as though there were no ground wire present. For shorter spans, reflections on the ground wire induce voltages on the conductor that reduce the crest voltage, the limiting value to which they can be reduced being $(1-K)$ times the original value as mentioned above for the ground wire grounded at many points.

It is interesting to note that the voltage induced on the ground wire for this case can produce appreciable positive current in the towers. For an incident surge, e, propagating to a tower whose footing resistance is R, the current, i_t, flowing down the tower before reflection can arrive from other towers is

$$i_t=\frac{2}{Z+R}\,e$$

where Z is the surge impedance of the ground wire, or the effective surge impedance if there are several. For one ground wire Z is about 500 ohms; for

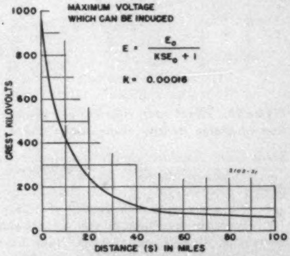


Figure 31. Foust and Menger[30] formula for determination of attenuation of crest magnitudes of voltage waves

two, about 350 ohms. The maximum current would occur if $R = 0$. Then for the case being considered i_t would be 3,900 amperes for one ground wire and 5,600 amperes for two ground wires, respectively. This represents the upper limit of the current which can be induced in the tower of a high-voltage line.

For lines of finite length or containing points of discontinuity such as connected apparatus at various points along the line, the voltage will, of course, be modified by reflections. These can be taken into account by conventional traveling wave theory in which the initial incident wave is that considered here.

Probability of Induced Surges on Transmission Lines

Number of Induced Surges Originating on Each Mile of Line

With a knowledge of the stroke density for a given region or the number of strokes originating from each square mile of sky area, it is now possible to determine the number of surges of a given crest magnitude which will be induced on a line. An average value for the stroke density in a region of flat terrain where the isokeraunic level averages about 30 storm days per year has been found to be ten strokes per square mile.[18] This value and the curve of Figure 3 determine the number of strokes which strike ground within finite bands a given distance from each side of a line of a given height. Figure 28 determines how many of such strokes have a current magnitude of a given value, and Figure 29 how much voltage each induces. By dividing the ground (from the minimum distance from the line for which strokes will strike earth to the maximum distance for which significant voltages will be induced) into such finite bands parallel to the line and adding up the number of strokes in each band which will produce voltages of a given value, probability curves such as given in Figure 30 are obtained. The curves of this figure give the number of surges exceeding each magnitude which is induced at the point nearest the stroke channel per 100 miles of transmission line.

The curves of Figure 30 indicate, as has been pointed out previously,[19] that the number of surges of a significant magnitude which are induced on a transmission line is of the same order of magnitude as the number of strokes which strike it directly. Waldorf[15] has found that on the average 100 direct strokes per mile per year can be expected to lines in the voltage class between 66 and 220 kv with line heights of from 60 to 110 feet and in regions of the same isokeraunic level as given for Figure 30.

The curves of Figure 30 can be used for determining the number of times flashover may occur on a line at points remote from the ends where reflections do not modify the surge voltage. As can be seen from Figure 23f, for distances at which voltages sufficiently great to produce outages (above about 300 kv) are developed, the time to half value varies between 5 and 20 microseconds. The critical flashover voltages for waves of this character are from 10 to 30 per cent higher than for a standard $1^1/_2 \times 40$ wave. Thus, for a 50-foot line with a critical standard impulse value of 300 kv, it is equivalent to 330 to 390 kv on the shorter waves. From Figure 30 this will produce about five outages per 100 miles per year due to induced voltages.

The data of Figure 30 must be distinguished from the total number of surges exceeding each magnitude which will appear at each point on the line, due not only to those originating at the point, but also to those propagating from other points. If it were not for attenuation and reflections, this would be equal to the number per mile times the length of the line in miles. Attenuation is a function not only of voltage magnitude but also of wave shape. However, an estimate of the probability of surges of various magnitudes appearing at any one point on a line can be made with the use of the empirical decrement curve of Figure 31. This curve[20] represents the mean of records obtained on the attenuation of lightning surges. The length of the line and terminal conditions, of course, affect the magnitude of the surges, so that they vary for different line conditions. Each particular line can be studied by traveling wave analysis. Use Figure 30 to determine the number of surges of each magnitude originating from each section of line and Figure 31 to determine its magnitude after propagation. However, two conditions are sufficiently representative that they can be applied to most cases.

Number of Surges at Point Remote From Ends of Line

The first of these is the case for which the line is sufficiently long in both directions from the point under consideration that it can be considered as infinite. Voltage probability curves calculated for this case are given in Figure 32. These were determined by dividing the line on each side of the point being considered into one-mile sections, after deciding from Figure 30 the number of surges of each magnitude which will originate from each section, and from Figure 31 their magnitude by the time they reach the point. A mean distance is assumed for each section. The accuracy of these curves for lines of finite length depends upon the voltage being considered. Figure 31 indicates that no induced surges in excess of 100 kv propagate more than 40 miles; for 200 kv, 25 miles, and for 400 kv, only 10 miles. Thus, the length of line for which these curves may be used varies in an inverse manner with the voltage. For voltages less than 100 kv, the attenuation is so small that for estimating purposes the number of surges of such magnitudes can be considered as proportional to line length and determined directly from Figure 30. These curves are based upon the premise that none of the surges reaching the point under consideration produced flashover before reaching the point.

Number of Surges at Open End of Long Line

If a line is open-circuited at its end, the magnitude of the crest voltage at the end is twice that of the incident surge. This is approximately true for lines with transformers connected to the end. Also, since the line extends in only one direction from the point under consideration, only half as many surges can originate from a given distance from the end as for the previous case. The probability curves of Figure 33 were, therefore, obtained from those of Figure 32 simply by picking a particular voltage, doubling it, and assigning to it one-half the number per year as read from the curves.

Comparison With Field Data

There are very little field data on lightning-surge voltages for which the induced surges can be segregated from those produced by direct strokes. From the data submitted by Cox, McAuley, and Huggins,[21] the authors have chosen the Klydonograph records obtained at the ends of four long lines of voltage class of 110–140 kv without ground wires. Only those records that did not produce flashover were included. The results are plotted by the dotted line of Figure 33. Undoubtedly some of the high-voltage records were due to direct strokes, and at low voltages some surges were not recorded because of the lower limit of sensitivity of the instrument.

Conclusion

The velocity of the initial downward leader of lightning strokes is too slow to produce significant voltages on transmission lines by induction. Only the

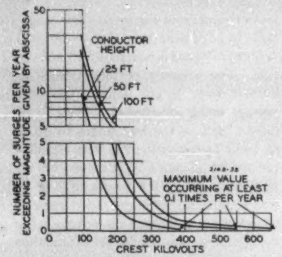


Figure 32. Probability of occurrence of induced voltage at a point remote from the ends of a line

Maximum voltage which can appear:

$h=$ 25 feet—925 kv
$h=$ 50 feet—950 kv
$h=$100 feet—975 kv

return streamers are of importance. The voltages produced by the latter can be determined to sufficient accuracy by representing the return streamer as a vertically propagating conductor whose tip moves with uniform velocity, neutralizing, as it progresses, the uniformly distributed charges on the initial streamer. In spite of the high currents and the short duration of the return streamer, only the charge and not the high rate of change of the current is important. The voltages so produced are proportional both to the height of the transmission-line conductor and the crest of the current in the stroke at ground. It has been found possible to represent strokes by the characteristics enumerated in Figure 4. Of all the factors considered, only variations in velocity of the return streamer affect induced voltages significantly. Return-streamer currents vary with the magnitude of stroke current. By choosing the probability of occurrence of stroke currents and corresponding return-streamer velocities, the probability of occurrence of induced voltages as a function of their magnitude can be determined. Although the voltages induced by lightning on transmission lines may be quite high, those of sufficient magnitude to produce flashover occur so infrequently as to be of little practical importance.

Appendix I. Equations for Electric Field Produced at Ground by Lightning Strokes

During the period in which the initial leaders are propagating toward earth, the change of electrical conditions with time are slow enough that neither the velocity of propagation of the disturbance from the stroke channel to the point at the ground under consideration nor the magnetic effect on the electric field need be considered. However, for the period in which the intense return streamer is propagating up to the cloud, the rapid rate of propagation and high current in the return streamer require the use of electrodynamic equations.

The general solutions to Maxwell's dynamic field equations are given in the practical system of units by the following equations:

$$\boldsymbol{A}_{P,\,t}=0\;1\int\frac{[i]_{t}-\dfrac{r}{c}}{r}dv \qquad (1)$$

$$V_{P,\,t}=9\times10^{11}\int_{v}\frac{[q]_{t}-\dfrac{r}{c}}{r}dv \qquad (2)$$

$$\boldsymbol{E}_{P,\,t}=-\nabla V-10^{-8}\frac{\partial \boldsymbol{A}}{\partial t} \qquad (3)$$

where

dv is a differential element of volume
r is the distance from P to the differential volume (dv)
c is velocity of light (3×10^{10} centimeters per second, or 984×10^{6} feet per second)
t is time in seconds
q is charge density
$\boldsymbol{A}_{P,\,t}$ is retarded magnetic vector potential
$V_{P,\,t}$ is retarded scalar electric potential in practical volts
$\boldsymbol{E}_{P,\,t}$ is electric field in practical volts per centimeter

The quantities $\boldsymbol{A}$, V, and $\boldsymbol{E}$ are given at the point P as a function of time when the integration is carried out over all space occupied by charge except at $r=o$. The retarded values of i and q, $[i]_{t-r/c}$ and $[q]_{t-r/c}$ are their values at the time $(t-r/c)$ at which they produce the field disturbance at P at the time t. The ratio r/c is the time required for the disturbance to propagate the distance r at the speed of light.

1. Electric Field Before Start of Discharge

It is assumed that, as shown in Figure 4a, the charge in any one charge center is confined to a small volume which can be represented by the point charge, $-Q_o$, and that the earth's surface is a perfectly conducting plane as represented by the image charge, $+Q_o$. For this static case the field at the point P has only the vertical component given by the equation where dimensions are expressed in feet.

$$E_{yo}=\frac{5.9\times10^{10}HQ_o}{(H^2+d^2)^{3/2}}\text{ volts per foot} \qquad (4)$$

2. Electric Field Produced by Stroke With No Initial Upward Streamer From Ground

DURING PROGRESS OF DOWNWARD LEADER

As shown in Figure 4b, it is assumed that the charge, $-Q_o$, is lowered onto the stroke channel by the initial leader at a uniform rate given by the leader velocity, v, and the uniform charge density, $-q$, per unit length of the channel. Thus, if z is the length of the initial streamer channel at a time, t, as measured from the start of the leader

$$z=vt \qquad (5)$$

The current flowing in the channel is

$$i=qv$$

The charge left in the charge center is

$$-Q=-(Q_o-it)=(Q_o-qz)$$

The effect of the ground plane for all of the following cases is represented by an identical motion of the image charge along the image channel. This is accounted for by merely multiplying the field due to the real cloud charges or currents by two. The initial streamer current and the initial streamer velocity relative to the speed of light are so small that the magnetic contribution to the electric field and the retarded potential effect need not be considered.

The field due to the charge left in the cloud is

$$E_{y1}=\frac{5.9\times10^{10}(Q-it)}{(H^2+d^2)^{3/2}}\text{ volts per foot} \qquad (6)$$

The field due to the charge on the leader is, when dimensions are in feet,

$$E_{y2}=5.9\times10^{10}\int_0^z\frac{q(H-z)}{r^3}dz \qquad (7)$$

If the charge distribution, q, is uniform

$$E_{y2}=5.9\times10^{10}q\left[\frac{1}{(z^2-2Hz+H^2+d^2)^{1/2}}-\frac{1}{(H^2+d^2)^{1/2}}\right]\text{ volts per foot} \qquad (8)$$

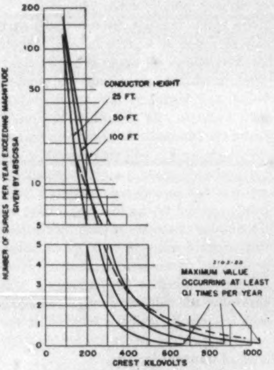


Figure 33. Probability of occurrence of induced voltage at the end of an open-circuit line

Solid curves are calculated
Dotted curve is result of Klydongraph measurements[21]

Maximum voltage which can appear:

$h=$ 25 feet—1,850 kv
$h=$ 50 feet—1,900 kv
$h=$100 feet—1,950 kv

E_{y2} as a function of time is obtained by the substitution of equation 5. The total field during progress of the downward leader is

$$E_y = E_{y1} + E_{y2} \tag{9}$$

At the instant the downward leader reaches the earth Q is zero and

$$E_y = E_{y2} = 5.9 \times 10^{10} q \left[\frac{1}{A} - \frac{1}{(H^2+d^2)^{1/2}} \right] \text{ volts per foot} \tag{10}$$

DURING PROGRESS OF RETURN STREAMER

At the instant that the initial leader reaches earth, the return streamer shown in Figure 4c starts upward at the velocity, V, instantaneously discharging each portion of the channel upon reaching it. The velocity is high enough (0.1 to 0.6 that of light) so that the retarded potential effect must be considered. However, since the return streamer just annuls the initial streamer charge, the only point where q is changing at any instant is at the return-streamer tip whose location is designated by y. Thus, for a given height, y, of the return streamer, the field due to electric charge can be expressed by equation 8 upon the substitution $z = H - y$.

$$E_{y3} = 5.9 \times 10^{10} q \left[\frac{1}{(y^2+d^2)^{1/2}} - \frac{1}{(H^2+d^2)^{1/2}} \right] \text{ volts per foot} \tag{11}$$

The time t_1 (as measured from the start of the return streamer) at which the gradient at P is given by equation 11 for a certain value of y is

$$t_1 = t_2 + \frac{1}{c}\sqrt{d^2+y^2} \text{ seconds} \tag{12}$$

where t_2 is the time required for the return streamer to reach the height y. For a uniform velocity V, $t_2 = \dfrac{y}{V}$

$$t_1 = \frac{y}{V} + \frac{1}{c}\sqrt{d^2+y^2} \text{ seconds} \tag{13}$$

ELECTRIC FIELD CAUSED BY CURRENT IN RETURN STREAMER

As given by equation 1 and Figure 4c, the vector potential at P caused by the uniform distribution of current, $+I$, flowing upward in the return streamer channel and its image, $-I$, is

$$A_y = 0.2I \int_0^y \frac{dy}{r_1}$$

$$A_y = 0.2I \log_e \frac{y+\sqrt{y^2+d^2}}{d} \tag{14}$$

$\mathbf{A}_y$ is independent of the units of length used and the only component of $\mathbf{A}$ which contributes to E_y. Furthermore, its other components are zero. From equation 3

$$E_{y4} = -10^{-8} \frac{\partial A_y}{\partial t_1} \text{ volts per centimeter}$$

$$E_{y4} = -30.4 \times 10^{-8} \frac{\partial A_y}{\partial t_1} \text{ volts per foot} \tag{15}$$

$$\frac{\partial A_y}{\partial t_1} = \frac{\partial A_y}{\partial y}\frac{dy}{dt_1}$$

Thus

$$E_{y4} = \frac{6.08 \times 10^{-8} I[1 + y(y^2+d^2)^{-1/2}]}{y+\sqrt{y^2+d^2}} \frac{dy}{dt_1} \text{ volts per foot} \tag{16}$$

From equations 12 and 13 for uniform velocity, V,

$$cy = cVt_1 - V\sqrt{y^2+d^2}$$

and for the particular case in which $V = 0.1c$

$$y = 0.10101ct_1 - \sqrt{0.000102c^2t_1^2 + 0.0101d^2} \text{ feet}$$

$$\frac{dy}{dt_1} = 0.10101c - \frac{0.000102c^2t_1}{\sqrt{0.000102c^2t_1^2 + 0.0101d^2}} \text{ feet per microsecond} \tag{17}$$

E_{y4} as a function of time can thus be determined from equations 16 and 17.

Appendix II

The negative charge lowered by the initial downward leader may be distributed over a section that is considerably larger than the core of the upward streamer. Since the upward streamer is essentially at ground potential, to maintain this potential a positive charge must move from earth to neutralize the effect of the negative charge. The magnitude of the positive charge can be determined as follows: Assume the negative charge lowered in the streamer to have circular symmetry of density $-q_1$ per square centimeter per centimeter length and that the radius of the arc core of the return streamer is r_a. Without invalidating the general conclusions, the earth may be represented by a cylinder of large radius, R, symmetrical about the streamers. End effects will be neglected. Both the earth cylinder and the return streamer are at zero potential.

The charge on an elemental ring of radius, r_1, and thickness dr_1 is $(-2r_1q_1dr_1)$ or dQ_1. The field due to this charge external to r_1 is

$$E_r = \frac{2dQ_1}{r} \tag{18}$$

The potential of this ring above the outer cylinder is

$$V_1 = \int_R^{r_1} \frac{2dQ_1}{r} dr$$

$$= 2dQ_1 \log \frac{r_1}{R} \tag{19}$$

The potential due to the charge in the elemental ring for all points within r_1, including the upward leader, is also equal to V_1.

Let dq_u be the charge in the return streamer required to neutralize the negative charge on the elemental ring. The potential of the return streamer above the outer ground cylinder is

$$V_u = \int_R^{r_a} \frac{2dq_u}{r} dr = 2dq_u \log \frac{r_a}{R} \tag{20}$$

since the potential of the return streamer is zero.

$$V_1 + V_u = 0 = 2dQ_1 \log \frac{r_1}{R} + 2dq_u \log \frac{r_a}{R} \tag{21}$$

and

$$dq_u = dQ_1 \frac{\log \dfrac{R}{r_1}}{\log \dfrac{R}{r_a}} \tag{22}$$

To form an idea of the magnitude of this quantity, let R equal 200 feet and r_a equal 0.5 centimeter or 0.016 foot; then for r equal to one foot the positive charge is 0.56 of the negative charge; for six inches, 0.635; for three inches, 0.71; for one inch, 0.79; and for 0.016 foot, 1.0. If the density varies inversely as the distance, the incremental charges in incremental rings of equal thickness are equal, and if the radius of the space occupied by the charge in the downward leader is one foot, then the above values should be averaged over the radius, resulting in a positive charge about 70 per cent of the negative charge. If the radius is not so great, or the core has greater diameter or the charge is concentrated nearer the center (which is probably the case) this value would be larger.

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Load Ratings of Cable—II

HERMAN HALPERIN
MEMBER AIEE

Synopsis: Since the author's 1939 paper,[1] further data have been obtained regarding the loading of underground power systems having impregnated-paper-insulated lead-covered underground cable. The results of the studies may be summarized as follows:

1. The factors affecting the setting of maximum safe conductor temperatures are so numerous that no fixed value (or values) can be assumed as applicable to all installations of a given design and size of cable in this country.

2. For some cables, the present temperature limits for normal day-in and day-out operation may be safely exceeded, especially for wartime conditions.

3. For emergency operation safe temperatures may be even higher than listed in the previous paper, especially for wartime operation.

4. Operation in wartimes at special temperatures will mean in some cases substantial shortening of the life of underground circuits and an accompanying increase in service interruptions.

5. Wartime increases in usual maximum daily loading and in load factors may cause large increases in duct and copper temperatures, even if past current ratings are maintained. These temperatures will sometimes exceed those in the present standards.

6. The safe temperature for emergency operation, particularly for extra-high-voltage cables, may be limited by the joints. Also, for all kinds of cable sufficient room must be provided to avoid mechanical damage of cable or joints in manholes with cable movements incidental to emergency loads.

7. Cracking of lead sheaths due to reciprocating cable movement into manholes may limit the temperature range for usual daily loading, but has little effect on the safe emergency loading.

8. For three-conductor solid-type cable, the insulation of the shielded type can safely withstand higher temperatures than the belted type, but the reverse is true as to the allowable daily temperature range with regard to its effect on the sheath in manholes.

9. Cable movement increases with length of conduit sections up to about 250 feet but shows little change with further increases in length up to 1,025 feet.

10. Changes in installation methods and the use of new types of repairs may help to mitigate troubles due to sheath cracking.

11. Copper shielding tape in three-conductor cable with relatively thin insulation has little effect in reducing the thermal drop from the conductor to the sheath.

12. In some cases considerable thermal advantage may be gained economically by making relatively deep installations of conduit.

13. In most cases, the maximum conduit temperature of 50 degrees centigrade given in the previous paper may be safely exceeded.

THE 1939 AIEE paper, "Load Ratings of Cable," by the present author

(*a*). Brought out mainly that the temperatures and allowable loading of underground cable during occasional emergencies could safely be considerably above the past accepted practices.

(*b*). Presented a large amount of detailed data on the limitations due to various factors for different underground cables and conduit systems.

The current war effort and attendant problems on conserving critical materials and labor have increased the importance of this general subject. This paper presents data that are extensions of investigations covered in the previous study and new investigations.

As a general proposition one cannot be dogmatic about the allowable temperatures for a given underground cable circuit. Factors affecting the decision include age and condition of the cable and accessories, effect of heat on the properties of the materials in the circuit, magnitude of usual daily temperature range, size of manhole and training of cable therein, frequency of emergency loading, past history of loading of cable, relation of temperature and temperature range for normal loading to temperature and temperature range for emergency loading, number of cables in conduit, allowable conduit temperatures, thoroughness of field inspection and maintenance work, desired life of the underground circuit, and desired reliability of service. One factor or set of factors may allow one loading while another factor or set of factors for the same cable may allow less loading.

The main purpose of this paper is to present data that, taken along with the data in the first paper and other available information, will aid in deciding the allowable normal and emergency loading for a given cable or system of cables of one design. The data are presented in three parts:

I—Limitations Due to the Insulation.

II—Limitations Due to the Sheath.

III—Heating Characteristics of Cables and Conduits.

I. Limitations Due to the Insulation

Effect of Heat Alone

Since the quality of wood-pulp paper as manufactured had improved since the period of 1920–30 when many tests were made in this country on the effect of high temperatures, it was decided to make further tests. In this series, samples of complete cables of recent manufacture were used in order to make the test conditions more nearly comparable to those encountered in service, instead of using paper tapes as had been usually done in the past.

The samples were 30-inch pieces of three-conductor 500,000-circular-mil solid-type cable from five manufacturers. The ends of each sample were cut square and immediately covered with lead foil which was bound to the sheath with fine copper wire. A lead cap, previously spun to fit the cable, was then placed over each end, beaten to close onto the cable, and sealed to the sheath by lead burning. Tests showed that this method of sealing practically eliminated contamination of the ends of the sample during preparation and also reduced to a negligible quantity the amount of air trapped between the seal and the cable end.

The samples to be aged were placed in ovens—some at 100 and some at 125

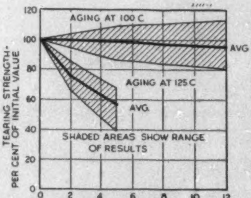

Figure 1. Effect of high-temperature aging on physical strength of impregnated-paper insulation (solid-type)

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HERMAN HALPERIN is assistant equipment and research engineer, Commonwealth Edison Company, Chicago, Ill.

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Table I. Changes in Power Factors During Various Heating Periods for Experimental Oil-Filled Cable

Temperature Step—C	Weeks	Change in Power Factor							
		Room Temp (15 Kv)		Room Temp (95 Kv)		Elevated Temp (15 Kv)		Elevated Temp (95 Kv)	
		Minimum	Maximum	Minimum	Maximum	Minimum	Maximum	Minimum	Maximum
70	27	0.0001	0.0005	0	0.0013	−0.0007	0.0002	−0.0003	0.0014
80	24	−0.0001	0.0003	−0.0006	0.0005	−0.0004	0.0008	−0.0005	0.0005
90	115	−0.0009	0.0008	−0.0007	0.0005	−0.0014	0.0031	−0.0007	0.0013
100	34	−0.0007	0.0011	−0.0001	0.0003	−0.0032	0.0004	−0.0004	0.0006

degrees centigrade. The effect of aging upon the paper insulation was determined from measurements of

(*a*). The radial power factors of the tapes

(*b*). The tearing strength of the tapes.

The power-factor measurements were on circular areas of single tapes, one half inch in diameter and were made at 60 degrees centigrade and 50 volts per mil. For each sample of cable the power factor of the tapes was measured along a radial path from one conductor to sheath.

The measurements of tearing strength were made with an Elmendorf tearing tester, four to eight pieces of tape being placed together and torn longitudinally at the same time. The tapes were tested immediately after removal from the cable under normal room conditions of temperature and humidity. This seemed warranted since a preliminary investigation showed that variations in relative humidity between 20 and 50 per cent had showed that variations in relative humidity between 20 and 50 per cent had no appreciable effect on the test results so long as the tapes were tested soon after removal from the cable without removing the impregnating oil.

The changes in tearing strength of the paper tapes after various amounts of aging are shown by Figure 1. These data show that for most modern cables, subjection to a temperature of 100 degrees centigrade for 12 months produces negligible depreciation in physical strength of the paper tapes, the average decrease in tearing strength being only 4.6 per cent, as compared with a decrease of 40 per cent in 10 months for the tests reported in 1926–29 by the Massachusetts Institute of Technology.[2]

At 125 degrees centigrade, however, the average deterioration in tearing strength after five months' aging was 43 per cent. At this temperature the rate of decrease in tearing strength of recent paper appears to be of the same order as was found in the 1926 tests. The recent and other tests have shown that tapes which have decreased more than 50 per cent in tearing strength are usually so brittle that their useful life in cable may be considered ended.

The effects of aging on the average power factors of the tapes are shown by Figure 2. For some samples, heating at 100 degrees centigrade for 12 months or at 125 degrees centigrade for five months produced no appreciable change in average power factor. For the best samples, the radial power-factor curves also remained completely flat throughout the tests, with no indications of the common upturns adjacent to the conductor and sheath. For the worst sample, the power factor of all conductor tapes increased from 0.0025 to about 0.01 after 12 months' aging at 100 degrees centigrade; and after five months at 125 degrees centigrade, some of the inner tapes had power factors as high as 0.05. In general, such increases in power factor would not result in a cumulative heating condition for solid-type cable within the present general range of usage but might lower the carrying capacity of the higher-voltage cable by a few per cent.

These data indicate that from the standpoint of deterioration due to heat alone, the maximum safe temperature for modern impregnated-paper insulation is limited by depreciation in physical strength of the tapes rather than by changes in electrical properties. For low-voltage cables where the electrical stresses are too low to have any appreciable effect, therefore, it appears that the insulation could be safely operated almost continuously at conductor temperatures of 90 or 100 degrees centigrade without excessive deterioration; and temperatures as high as 125 or about 140 degrees centigrade during emergencies would be reasonable. The higher limits (100 and 140) would apply for wartime conditions. "Emergencies" or "emergency loading" in this paper means extra heavy loading that would occur on the average of one day per year.

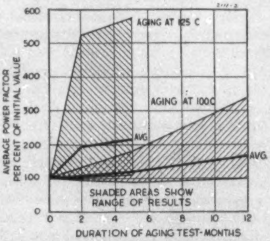

Figure 2. Effect of high-temperature aging on power factor of impregnated-paper insulation as determined from averages of radial power-factor curves at 60 C

Oil-Filled Cable

The 1939 paper included data on tests of three 1,000-foot installations of experimental single-conductor oil-filled cable that was being subjected to high temperatures. This cable was installed in conduit and subjected to 76 kv to ground by means of a tap to a 132-kv three-phase overhead line. After a net time of $8^1/_2$ years of testing, the cables were removed, and extensive investigations were made on samples from various locations.

The three cables are identified as *R*–0.386, *S*–0.400, and *T*–0.500, the number in each case being the insulation thickness in inches. Cable *R*–0.386 had oil feed through both a hollow core and flutes in the inside surface of the sheath, while the other two cables had oil feed through a hollow core only.

As indicated in the earlier paper, the conduit in which these cables were installed was in a very abnormal location and had unexpectedly large variations longitudinally in heating characteristics. As a result, the cables were subjected to widely different temperatures longitudinally. The temperatures were determined from direct resistance measurements of the average conductor temperatures at two- to three-week intervals and by measurements of longitudinal variations in duct temperature on two dates. As a result of this nonuniform temperature distribution, samples of cable were available that had been subjected to maximum conductor temperatures ranging from 95 to 140 degrees centigrade and to periods of heating to 100 degrees centigrade or higher ranging from 0 to 27,000 hours. The heating to which each of the three cables was subjected at the hottest and coolest locations along the conduit is summarized in Figure 3.

After $8^1/_2$ years of testing, cable *R*–0.386 failed because of overheating at the hottest part of the duct run. Figure 4 of the previous paper shows how the

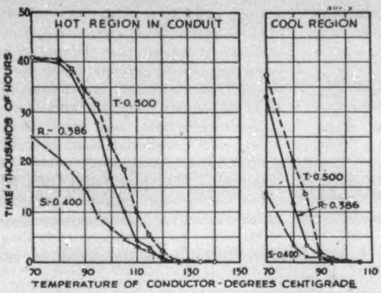

Figure 3. Time of heating at or above various temperatures for experimental oil-filled cables operated at 76 kv to ground

power factors of the cables varied during the tests. Cable *R*–0.386 was electrically stable up to the time of failure, and radial power factors measured on samples of this cable taken from the coolest and the hottest parts showed practically no changes due to all the testing.

On the other hand, cable *T*–0.500 had developed considerable increase in power factor. A sample from the cooler part of the conduit showed power factors at 60 degrees centigrade of the tapes of 0.03 near the conductor and near the sheath, with values dropping down to 0.005 in the middle of the insulation, while a sample from the hotter part of the length had a power factor of 0.025 or 0.03 radially through the insulation. The primary trouble with this cable was the development of an excessive amount of sheath expansion, which increased the internal sheath diameter by 0.06 to 0.26 inch (2$^1/_2$ to 12 per cent) with the greater expansions occurring at the hottest regions. This allowed core oil of high power factor to be drawn radially through the insulation. These excessive amounts of sheath expansion were due mainly to the very high sheath temperatures encountered in these tests and to the fact that the type of alloy used for the sheaths has abnormally high rates of creep, especially at the higher temperatures. Fracture is likely to occur after expansions of four or eight per cent in sheaths, except that the percentage is lower for calcium-type sheaths. This very unusual combination of very high power factors for the core oil and excessive sheath expansion is unique and, according to world-wide experience, is not at all to be expected in commercial installations.

An interesting phenomenon brought out by these tests, although not directly related to the effect of temperature on the insulation, was the fact that, at locations where the gap between the sheath and the insulation resulting from the sheath expansion exceeded about 0.15 inch, severe electric discharges occurred between the outer surface of the unshielded insulation and the inside surface of the sheath. These discharges burned numerous deep pits in the inside surface of the sheath and burned holes through the outer two to five tapes of the insulation. No visible evidence of damage was found below the sixth tape from the outside, and all indications were that the discharges had little if any effect on the power factor of the insulation below that tape. These cables were subjected to lightning and switching surges having crest voltages as high as 600 kv, and it is possible that these surges caused the initial breakdown of the oil gap. From other investigations, it seems possible also that gas was evolved from the paper during the very high temperatures.

Table II. Effect of Temperature Steps Upon Ionization Factor

Temperature Step—C	Percentage of Cables Tested Showing Increases in Ionization Factor Over Initial Value of: 0.0005 or less	0.0006 to 0.0010	0.0011 to 0.0050	0,0051 to 0.0100	0.0101 to 0.0200	0.0201 to 0.0300	Percentage of Cables That Failed
Nine Shielded Cables With 108 to 143 Mils of Insulation							
60	100						
80	56	33	11				
100	34	33	33				
115	56	22	22				
Five Shielded Cables With 90 to 105 Mils of Insulation							
60	80		20				
80	40	20	20	20			
100			60	20	20		
115			40	40	20		
Six Unused Belted 13-Kv Cables Made in 1937–39							
60	17	50	17	16			
80	17	17	17	16	33		
100			50		33	17	
115				17	50	33	
Five Used Belted 13-Kv Cables Made in 1924–28							
60			20	20	60		
80			20		80		
100				40	20		40
115				20		20	20

The results of measurements of the tearing strength of the paper insulating tapes from samples of cable that had been subjected to various temperatures are summarized in Figure 4. During preparation of this figure, the temperature treatment to which each sample had been subjected was reduced to the equivalent temperature for a period of 12 months, using the assumption that the rate of deterioration in physical strength of the insulation would double for each eight degrees centigrade increase in temperature. Since the tearing strength of the tapes when new had not been measured, the initial values for each cable were taken as the average results for all samples of tape that had been subjected to maximum temperatures of lower than 85 degrees centigrade.

A definite but rather gradual decrease in tearing strength with increasing temperature is shown by these data. The indications are that temperatures equivalent to at least 117 degrees centigrade for 12 months would be required to produce a reduction in tearing strength of 50 per cent (approximate allowable limit for useful life, especially in connection with removal and reinstallation).

The failure in cable *R*–0.386 occurred at a location where the conductor temperature had been at least 140 degrees centigrade for approximately 100 hours prior to the failure. The examination

Table III. Increase in Solid Losses During Aging Tests of Three-Conductor Cable

Cables	Percentage of Cables Tested Showing Maximum Increases in the Average of Radial Power Factors at 60 C of: 0.001 or Less	0.0011 to 0.0050	0.0051 to 0.010	0.0101 to 0.0272
Shielded*	23	54	23	
Belted	45	45		10

* Reduced insulation thicknesses.

indicated that the final failure was of the cumulative heating type, but the radial power factors of adjacent samples were low as in new cable. The exact mechanism of failure is not well understood, but it seems likely from other Chicago tests that the high temperature may have resulted in the evolution of some gas from the paper. The failure would then have been initiated by ionization of the gas with subsequent development of a cumulative heating condition.

Aging tests of six oil-filled experimental cables with a conductor size of 500,000 circular mils and 450 mils of insulation have been in progress since 1936 in an indoor laboratory. The cables have been subjected to various steps of heating up to 100 degrees centigrade as shown in Table I. Heating was applied partly continuously and partly in daily cycles. During some test periods a test voltage of 95 kv to ground was applied, and some test periods were without superimposed voltage. As Table I shows, the changes of the insulation, judging from the results of power-factor measurements, were small (0.003 or less).

These tests were continued at an average copper temperature of 110 degrees centigrade. After nine weeks at this temperature step without voltage, 95 kv was applied, and then a failure occurred in one joint due to the effect of overheating.

Experience in aging tests with overvoltage and heating currents on both solid and oil-filled types of cables for extra high voltages has shown in many cases that trouble caused by overheating may develop more readily in joints and potheads than in the cable. This may be due to the use of large amounts of insulating materials, which decrease the dissipation of heat, or due to loosening of metal parts, or other causes. This finding emphasizes the need for careful study of the limitations of the accessories in the determination of emergency ratings for extra high-voltage cables.

From a consideration of the results of these investigations together with other earlier data, the following conclusions may be drawn:

1. Oil-filled cables may be subjected continuously to conductor temperatures of the order of 100 degrees centigrade for at least 30,000 hours without producing any serious increases in power factor.

2. The maximum safe temperature for the *insulation* of oil-filled cable for long periods of time is probably limited by deterioration in the physical strength of the paper rather than by changes in electrical properties. From this standpoint the cable will withstand 115 degrees centigrade for one year or 100 degrees centigrade for at least 30,000 hours.

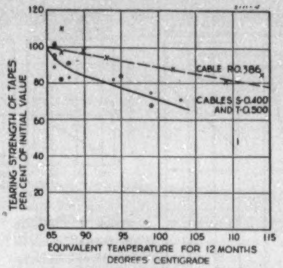

Figure 4. Effect of temperature on tearing strength of paper tapes from experimental oil-filled cables

(The preceding two conclusions are of considerable interest in connection with other oil-impregnated insulation, especially that normally used in transformers, but it must be noted that in oil-filled cable the amount of air present is relatively very small.)

3. At some temperature that is definitely well above 100 degrees centigrade and is probably between 120 and 140 degrees centigrade, there is danger of electrical failure of the cable occurring without previous changes in power factor. Joints and potheads may give trouble at lower temperatures.

4. If oil-filled cables are operated for long periods of time at sheath stresses or temperatures in excess of those for which they were designed, excessive sheath expansion may occur.

Maximum allowable temperatures for oil-filled cable may, therefore, be as high as follows: for usual conditions—85 and 105 degrees centigrade for normal and emergency loading, respectively; for wartime conditions—95 and 115 degrees centigrade for normal and emergency loading, respectively.

Aging Tests of Three-Conductor Solid-Type Cable

A great amount of information on the combined effect of voltage and temperature upon the stability of solid-type cables has been obtained in aging tests of three-conductor cables for operation at 9 or 12 kv, three-phase. As described in the previous paper, the tests consisted essentially in subjecting test lengths of 100 feet or more between pressure-tight terminals for four weeks to 24 kv, three-phase, continuously with 20 superimposed load cycles ranging in maximum temperatures from 60 to 115 degrees centigrade. The compact terminals were filled with heavy mineral oil, such as is used in solid cable.

Since the previous paper, 23 more cables have been tested. Since the inception of these series of tests in 1935, a total of 30 cables has been tested with pressure-tight potheads; five cables have been tested with open potheads, and four cables with old rosin compound made prior to 1920 have been subjected to similar but milder tests. Of the 30 cables tested with pressure-tight potheads, 17 were shielded cables and 13 were standard belted 13-kv cables with 141 mils of conductor insulation and 78 mils of belt insulation.

The net insulation thicknesses of the shielded cables ranged from 90 to 143 mils. Added to these walls of insulation were in most cases two or three special shielding tapes over the conductor and one special shielding tape over the insulation. The special shielding tapes were either metalized paper tapes or carbon black tapes. Based on the results of these aging tests, such as stability throughout tests with 125 mils of insulation, an insulation thickness of 141 mils including special shielding tapes has been adopted as standard for operation on the Chicago 12-kv system as compared with net insulation thickness of 172 mils recommended by the Association of Edison Illuminating Companies Cable Specifications.

Table II shows the effect of the standard aging tests with pressure-tight potheads upon the development of ionization in 25 representative cables. A great superiority in stability of the shielded over the belted cables is evident in spite of the reduced insulation thicknesses of the shielded cables, especially if the five cables with extremely thin insulation are eliminated from comparison. The nine shielded cables with insulation of 108 mils or more showed less increase in ionization than 0.005 throughout these aging tests. As was expected, more severe deterioration occurred in the cables with extra thin insulation. In many cases shielded cables showed only small increases in ionization or even decreases in the 115-degree-centigrade step after deterioration in the 80- and 100-degree-centigrade steps, possibly due to redistribution of compound at the extra high temperatures.

Belted cables showed, in general, progressing deterioration in successively higher temperature steps. Also, higher percentages of the cables tested reached greater degrees of deterioration. However, in contrast to shielded cables, they showed, in general, considerable recovery during days with voltage only and during cycles at only 60 degrees centigrade following the high-temperature cycles.

Failures occurred only in used belted cables and only in the 100- or 115-degree

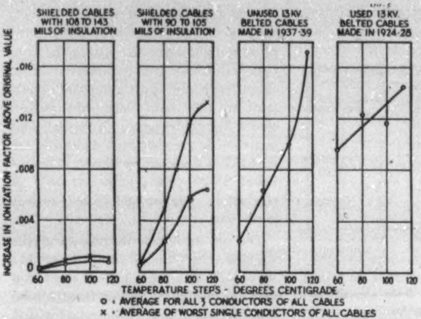

Figure 5. Increase in ionization factor at room temperature during aging tests

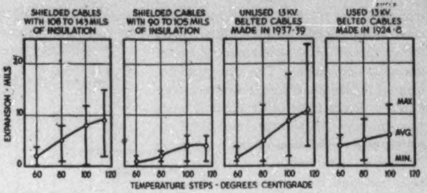

Figure 7. Increase in over-all diameter of three-conductor cables after various temperature steps in aging tests

step. The three failures that occurred were all due to ionization.

Figure 5 gives some indication of the rate of deterioration with increased temperatures, as it shows the average increase in ionization above the initial values for all cables in the various temperature steps. Since in many cases where deterioration occurred in shielded cables only one conductor was affected, both the increases for the worst conductors and the average increases for all three conductors are shown for the shielded cable. For the belted cables, only the average increases for all three conductors are given. The very small effects of the various temperature steps and especially of the 115-degree step are apparent for shielded cables with more than 108 mils of insulation. For unused belted cable the curve becomes progressively steeper with higher temperatures. The used belted cables tested showed greater changes but a flatter slope; the reasons for the shape of the curve are not completely clear.

Taking all data into consideration, and noting that the tests were made at twice operating voltage, one should not expect serious deterioration due to ionization on lines containing only cables made recently at emergency temperatures of 100 degrees centigrade or somewhat more; but some serious deterioration may occur at such temperatures on lines containing cables made, say, 14 or 18 years ago. The margin of safety is greater for the shielded cables. This conclusion agrees fairly well with the emergency temperature of 98 degrees centigrade recommended for 12-kv operation in the AEIC Simplified Practice Schedule.

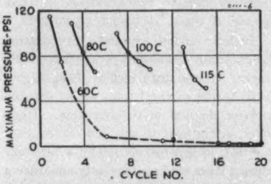

Figure 6. Maximum pressures in various temperature steps during aging tests of a typical sample of 500,000-circular-mil three-conductor shielded cable

As discussed in the previous paper, thermal failures (cumulative heating) due to steep dielectric loss-temperature curves may occur at even moderate temperatures in local spots of some old rosin cables, but such failures should not be expected in cables made later, even at temperatures as high as 120 degrees centigrade, except under abnormal conditions.

War conditions will necessitate increasing use of American wood pulp for insulating tapes instead of the Swedish wood pulp used heretofore. For this reason, comparative aging tests were made on samples insulated with Swedish wood pulp and similar samples insulated with American wood pulp. The samples were purposely made with very thin insulation in order to obtain definite deterioration. While, as expected, all samples showed serious deterioration, the test results as well as examinations and tests after completion of the aging tests showed no appreciable difference in stability of the two types of paper. Experimental lengths of various makes with reduced insulation thicknesses with American and Swedish wood-pulp paper insulation have been installed on heavily loaded 12-kv lines and will be removed after a few years of service for further comparisons.

Compared with the effect of these overload tests on ionization, the effect on the solid losses of the cables was small. As Table III shows, the great majority of the samples showed only small increases in solid losses, and only a few showed moderate or serious increases. It should be noted that the increases shown are the maximum changes; that is, the portions of the test lengths were selected that developed hot spots or showed otherwise the greatest degree of deterioration. The conclusion may be drawn that occasional overloads will not affect seriously the dielectric losses of the impregnated tapes themselves.

In accordance with the results of the electrical measurements, visual examination of aged cables gave evidence that deterioration was mainly due to ionization. Deteriorated samples showed dryness and wax, and in the case of more deteriorated belted cables carbon also.

Figure 6 shows typical pressure readings during the 20 heat cycles in the standard aging tests. The pressures dropped sharply in successive cycles of the same maximum temperatures. In general, the highest pressures occurred in the first week of 60- or 80-degree cycles rather than in subsequent cycles to higher temperatures. These data lead to the conclusion that severe overloads applied to cable that previously carried heavy loads will not produce excessive pressures, assuming that joints are not oil-filled and connected to reservoirs.

The expansion of the sheath produced by the heat cycles is summarized in Figure 7. As should be expected, a comparison of this figure with Figure 5 suggests a relation between sheath expansion and progress of ionization. These expansions are not sufficient to cause fracture in sound sheath. However, there were some cases in these aging tests where leaks occurred in the thin portions of eccentric sheath in old cables. None of these leaks occurred before the 100-degree step. These observations in the aging tests suggest that it must be expected that in service sudden overloads may open up cracks in thin or defective sheaths.

It is difficult to generalize as to allowable temperatures for three-conductor cable for operation at about 12 kv, mainly due to the variety of cables in service. It

Table IV. List of Cables on Which Movement Studies and Measurements Have Been Made in Chicago

	Kv Between Conductors		Conductor			Specified Thickness—Mils					
						Insulation					
No.	Rated	Operating	Size—Circular Mils	No.	Type of Cable and Conductor	On Conductor	Belt	Sheath	No. of Field Measurements Taken	A/W* Ratio	Outside Diameter in Inches
1	5	4	375.000	3	Belted, ordinary Belted, "compack"	94 94	63 63	109 109	269	0.144 0.148	1.98 2.05
2	5	4	375.000	4	Belted	94	63	125	—	0.148	2.35
3	13	4	350,000	3	Belted	141	78	125	11	0.119	2.27
4	13	12	500,000	3	Belted, ordinary Belted, "compack"	141 141	78 78	125 125	470	0.138 0.142	2.53 2.44
5	9	12	500,000	3	Shielded, "compack"	141	—	117	69	0.149	2.31
6	13	12	350,000	3	Belted	141	78	125	28	0.119	2.27
7	35	12	350,000	3	Belted	297	109	141	22	0.083	2.98
8	69	66	2,100,000	1	Solid, segmental conductor	688	—	156	30	0.117	3.48
9	69	66	2,100,000	1	Oil-filled, hollow core	315	—	141	82	0.145	2.82
10	69	66	1,000,000	1	Solid, standard stranding	688	—	141	17	0.087	2.87
11	69	66	750.000	1	Solid, standard stranding	750	—	141	146	0.0696	2.85
12	138	132	1,100,000	1	Oil-filled, hollow core	719	—	156	45	0.0772	3.30
13	132	132	600,000	1	Oil-filled, hollow core	719	—	100+90	26	0.0426	3.22
14	132	132	600,000	1	Oil-filled, hollow core	719	—	250**	7	0.0445	3.03
15	132	132	600,000	1	Oil-filled, hollow core	719	—	156	2	0.0533	2.89
16	138	132	250,000	1	Oil-filled, hollow core	560	—	133	31	0.0325	2.44

* A/W ratio is the ratio of the total copper area in the cross section of the cable expressed in circular inches to the weight per foot of cable expressed in pounds.

** Contains fluted oil channels 94 mils deep.

seems, however, from data in this paper that from the standpoint of the insulation (including radial expansion of the sheath), the normal and emergency temperature limits are, respectively, about as follows for limiting cable types: for modern shielded cable—85 and 110 degrees centigrade; for 1920–25 belted cable—75 and 90 degrees. For single-conductor cable, the limits may be the same as for shielded cable. For wartime conditions, the temperatures could be increased perhaps seven degrees for normal daily loading and ten degrees for emergency loading. For some old cables with relatively heavy insulation (usually containing relatively large amount of voids), allowable emergency temperatures during wartimes might well be 120 degrees or even higher, depending on cumulative heating characteristics and condition of sheath. With the very high cable temperatures, troubles may occur with the joints due to expansion of compound or movement against manhole wall, and so forth.

II. Limitations Due to the Sheath

The main objective in studying sheath life is to find out how to get a reasonably long life out of the sheath under any and all loadings up to the maximum that the insulation will stand. The loading and resulting temperature ranges determine the cable movement, which in turn determines the sheath life in the manholes.

The earlier paper presented some data on magnitudes of cable movement found in the field and theories relating thereto. In addition, some information was given regarding the relation between sheath life and cable movement as determined by the influence of cable size and construction, by sheath material, and by installation conditions in ducts and manholes. Further data and discussions on movement and sheath life are given herein.

As indicated in the previous paper, most underground circuits having material variations in daily temperatures—which is the usual case—are limited in rating for day-in-and-day-out (normal) loading more by the sheath than by the insulation. For example, the insulation might limit the normal daily temperature to 85 degrees centigrade for a given cable, while for the accompanying conduit temperatures and character of daily variation in load, the resistance of the sheath to cracking in the manhole may limit the copper temperature to 77 degrees. The problem of sheath cracking in manholes is affected by so many variables that it is hard to lay down any definite rules and positive numerical conclusions. Nevertheless, a great deal of data have been gathered which lead to more or less general conclusions that may serve well in guiding design and operating practices.

Laboratory Tests

Laboratory tests are mainly useful in determining the general engineering relations between sheath life and cable movement for different variables. The tests relating sheath life to magnitude of movement and to cable training in the manhole have all been made on pieces of cable 4 (Table IV) from one length which had copper-bearing lead sheath and compack conductors. The tests were made in the dummy manhole described in the previous paper with the cable fireproofed in the standard manner there described.

The relationship between sheath life and cable movement is shown in Figure 8. Where the movement at each duct mouth is 0.75 inch or less, the life appears to be inversely proportional to roughly the 1.5 power of the movement. For movements of over 0.75 inch the life is less than would be indicated by this relation; the limited data available indicate that in this range the life is inversely proportional to something more than the square of the movement.

In one set of tests in the dummy manhole, the one variable was the offset between the center line of the joint and the center line of the cable in the duct. The numbers of cycles to sheath cracking for offsets of $9^1/_2$, 19, and $28^1/_2$ inches were, respectively, 3,100, 6,800, and 10,000 cycles. Apparently, then, increasing the offset is very helpful in lengthening the life of the sheath. Of course, there is the attendant problem of finding enough room for the wider manholes thereby required. Also, as indicated in the previous paper, the magnitude of the daily movement at the duct mouth is affected by the restraint of the cable in the manhole; and with large offsets this restraint would be decreased somewhat to nullify part of the apparent advantage in longer offsets.

In another set of tests the one variable was the length of the manhole, the offset being 19 inches. For lengths of $7^1/_2$, 10, and 13 feet between duct mouths the corresponding number of cycles to sheath cracking was, respectively, 4,700, 6,800, and 6,900 cycles. Apparently, if the offset is kept constant, increasing the length of the manhole is of material value only

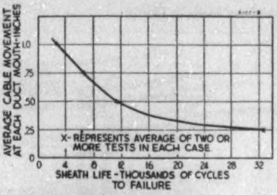

Figure 8. Relation of sheath life in dummy manhole to cable movement

up to a certain point. Beyond that point, apparently, the strain on the sheath is not much affected.

A number of tests have been made to determine the effect of fireproofing on the life of the sheath. The standard fireproofing compound in Chicago is a cement, sand, and asbestos mixture. The standard method is to apply this mixture over plain felted asbestos tape which is one-eighth inch thick and wrapped in a single butted layer over the sheath. With such fireproofing the life of the sheath in the dummy manhole does not seem to be much different, according to tests, from the life with no fireproofing at all. Omission of the asbestos tape, however, resulted in reductions of about one third in the life in the dummy manhole in a pair of tests at a movement of 0.75 inch and in a pair at a movement of 0.25 inch. Apparently, a thick solid fireproofing will shorten the life of the sheath, particularly if the fireproofing itself cracks in service.

In 1940 it was decided that the tests in the dummy manhole apparatus should be supplemented by tests on short pieces of cable and on small strips of sheath in which the magnitude of the cyclic deformation of the sheath could be more accurately controlled than is possible in the dummy manhole test. It was thought that the better reproducibility and relative cheapness of tests of this type would make them preferable for comparative tests of many variables in materials and conditions. In order that the results of such a test be applicable to service conditions, however, it is necessary that the type and the magnitude of the deformation be approximately the same as occur in service or in the dummy manhole test. Therefore, an investigation was started to determine the cyclic deformation of the sheath at various locations along the cable during tests in the dummy manhole apparatus. The investigation was later expanded to obtain deformation data for use in analyzing the effect upon the sheath life of variations in installation conditions and type of cable.

After trials of different schemes, it was found that the most satisfactory method of determining the cyclic sheath deformation was to measure the change in distance between the ends of small copper rivets soldered to the sheath at three-inch intervals along both the front and the back of the cable. A small conical hole was drilled in the end of each rivet, and the distance between the holes in adjacent rivets was measured with a dial gauge reading to the nearest 0.001 inch, thus giving an accuracy of ±0.03 per cent. The tests were made on fireproofed cables.

The early tests showed that the deformation at any given location along the cable might vary considerably during a given dummy manhole test. As a result, it was found necessary to measure the sheath deformations after various periods during the test and to take the average of all measurements at a given location as being the effective deformation at that location.

Figure 9 shows the variation in sheath deformation or strain along the cable for a typical test on cable 4 (Table IV). As shown by Table V, the maximum sheath strain for this cable under these conditions ranged from 0.35 to 0.42 per cent, and usually occurred in the three-inch section adjacent to the joint wipe. The results of tests on the same type of cable under different training conditions are also shown on Table V. The equivalent lives in the dummy manhole test and the maximum sheath strains were in all cases in the correct relative order; that is, higher deformations give shorter lives, but the number of measurements is insufficient to determine the relation between sheath strain and life.

When shielded cables were adopted as standard for use on the 12-kv system in Chicago in 1940, some tests in the dummy manhole were made to see whether the lives under bending were of the same order as for the belted cables which were previously used. As shown by Table V, the life of the shielded cable with standard training and 0.75-inch movement at each duct mouth was only about 4,300 cycles, or 63 per cent of the average life for all comparable tests of belted cable. With 0.50-inch movement at each duct mouth, the life of the shielded cable was about 5,400 cycles or one half of that for belted cable.

For both values of movement the measured sheath strains were slightly higher for the shielded than for the belted cables, but the differences were insufficient to account for the differences in equivalent lives in the dummy manhole test. The shorter lives of the shielded cables appear to be connected with the fact that the sheaths bulge and wrinkle under the repeated bending to a much greater extent than do those of the belted cables. Preliminary investigations suggest that this may be due to

(*a*). A tendency for the metal binder tape to concentrate the sheath deformation at certain spots.

Figure 9. Variations in sheath strain along cable for typical 13-kv belted cable in ten-foot manhole with 0.75-inch movement at each duct mouth

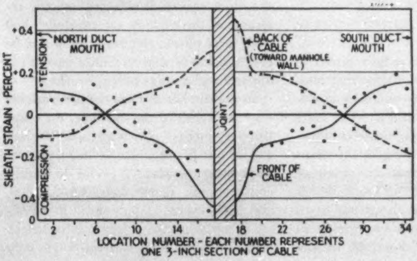

Figure 10 (right). Curves for estimation of average movement and distribution of measurements with respect to curves

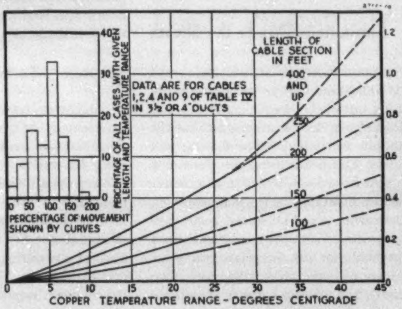

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Table V. Tests in the Dummy Manhole Apparatus of Three-Conductor Cables on Which Sheath Strains Were Determined

Type of Cable	Manhole Length—Feet	Cable Training	Movement at Duct Mouth—Inch	Maximum Sheath Strain—Per Cent	Cycles to Sheath Failure
Belted	10	Standard	0.75	0.42	7,080
Belted	10	Standard	0.75	0.35	7,520
Belted	7.5	Standard	0.75	0.60	5,620
Belted	7.5	Standard	0.75	0.70	3,900
Belted	10	Joint blocked to prevent lateral movement	0.75	0.33	8,400
Belted	10		0.75	0.33	8,400
Shielded	10	Standard	0.75	0.45	4,310
Shielded	10	Standard	0.75	0.60	About 4,000
Shielded	10	Standard	0.50	0.50	4,970
Shielded	10	Standard	0.50	0.33	5,740

(*b*). The failure of the usual metal binder to hold the conductors as firmly together as does the belt on a belted cable.

(*c*). The tendency for the slightly thinner sheaths used on the shielded cables (117 mils as compared to 125 mils on the belted cables) to wrinkle more easily.

The effects of these factors are being investigated further in an effort to determine what changes in construction of the shielded cables will be necessary to obtain longer sheath lives.

Field Data

The theory of cable movement, without which field data on movement are largely unintelligible and unusable, has been extended somewhat and brought into better agreement with the field data since the previous paper was published. The most interesting discovery is that long lengths of cable do not move any more than short lengths, within certain limitations. The graph in Figure 10 illustrates the present findings based on field measurements. For copper temperature ranges up to 25 degrees centigrade, all lengths in excess of 250 feet have only as much movement as a 250-foot length. This effect is probably due to snaking and anchoring of the cable in the duct or to duct friction.

The curves in Figure 10, which are almost entirely empirical, can be matched fairly well by using in formula 2 of the previous paper a value of D, the duct-friction coefficient, equal to about 1.6. This happens to be about the right value for the static coefficient of friction of cable in precast concrete duct, indicating that static friction, as well as complete anchoring, may be an important factor in limiting cable movement. When the coefficient of sliding friction, about 0.5, is used in the formula, the calculated movements are much higher than those found in service and exemplified in Figure 10 and, of course, would be still higher with sliding friction coefficients of 0.2 or 0.3, such as are sometimes reported. One case is known where the ducts are clogged with fine silt for several lengths on one part of a heavily loaded line, while on other parts of the line the ducts and cable are identical but free of silt. The daily range of movement at the duct mouth in this latter part of the line is about three times as great as in the silted ducts.

These points are strikingly illustrated by observations on a 500,000-circular-mil single-conductor 69-kv line having lengths up to about 1,000 feet in four-inch ducts. The data, for which the author is indebted to Cincinnati Gas and Electric Company, are given in Table VI. The lengths on which measurements were taken, given in column 1, range from 529 feet to 995 feet. In column 2 the measured total movement for both ends of each length for a copper temperature range of 38 degrees centigrade are given. It is especially interesting that the longer lengths had no more movement than the shorter lengths; in fact, the shortest length moved more than the longest length. The second interesting point is that, as shown by column 3, the observed movement was only about as much as would have been produced by 410-foot lengths, according to calculations using formulas for determining cable movement given in the previous paper. The remainder of each length was apparently immobilized by anchoring. The length of cable that actually moved, 410 feet, is somewhat longer than the corresponding value of 250 feet found for the cables studied in Chicago. One probable reason for this is that the Cincinnati cable was not fireproofed at the time of the field observations, whereas all Chicago cables are fireproofed. The absence of fireproofing increases the movement noticeably.

Data obtained early in 1942 for the Cincinnati line (with fireproofing on the cable in the manholes) showed for daily copper temperature ranges of about 18 degrees centigrade that the range of movement at the duct mouths was almost the same for 250- and 496-foot lengths as for 850- and 981- and 1,025-foot lengths. Also, it was found that slopes of six per cent for some of the conduit did not affect the daily movement.

Similar data on relatively small movement have been furnished by the Montreal Light Heat and Power Consolidated for its recent installation of 650,000-circular-mil 120-kv oil-filled single-conductor cable. Lengths of 682 to 960 feet had movements of $^1/_8$ to $^7/_{16}$ inch at a duct mouth for daily copper temperature ranges of 16 to 22 degrees centigrade. The largest movement was found for the 682-foot length and occurred with the 22-degree range.

The dashed portions of the curves in Figure 10 are for daily temperature ranges exceeding 25 degrees centigrade, which seldom occur in service. For this reason this portion of the curves is not well verified by field data. However, it can be said that the actual movements measured in Chicago have on the average been no greater than indicated by the curves. Any revision in this portion of the curves is expected to be toward smaller values of movement, especially for the longer lengths exceeding 250 feet.

The probability curve in the left-hand corner of Figure 10 gives the relation between the actual field data and the plotted curves. It may be seen that 46 per cent of all the field data are within 25 per cent above or below the values shown in the graph, while a few cases may be 100 per cent above or below the graph. Of course, it will be the lengths that have more than average movement that will tend to have relatively short lives before cracking of the sheath in the manhole. A rough analysis indicates that some lengths will start to crack after a period equal to one fourth or one third of the average life of the cable on the line, and after a period equal to one half the average life about one sixth of the original lengths will have cracked. If each length is replaced as it cracks, the indications are that the rate of cracking will increase up to a period equal to about two thirds the average cable life, after which it will tend to level off or even decrease somewhat. If the sheath limits the life of the line to about 20 years, for example, then the annual rate of sheath cracks may become 50 or 75 per 1,000 lengths.

As indicated by the title of Figure 10, no significant difference has been found in the movement in service of cables of the sizes indicated operating at 4, 12, and 66 kv. This is in accordance with expectations, since the ratio of conductor cross section to total cable weight is about the same for all these cables (see Table IV), and the movement should, therefore, be about the same.

Cable of a given size is found to have about as much movement in 3.5-inch duct as in four-inch duct, and probably also in three-inch and five-inch duct, although this latter is not well verified. Since lengths of cable in service are usually at least 250 feet long, limitation of the movement by anchoring is always a factor, and the negligible influence of duct size found to date indicates perhaps that anchoring occurs about as readily in large ducts as in small ones. Another way of looking at it is that the coefficient of either static or sliding friction is not much different for a cable in a large duct than in a small one, provided the cable can be installed in the smaller duct without actually jamming or tending to jam. The movement would therefore be expected to be about the same in both cases. Similarly, according to scanty data, the material of the duct does not seem to make much difference.

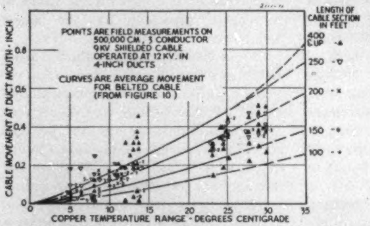


Figure 11. Comparison of movement of lengths of shielded cable with average movement of belted cable

It has been suggested that three-conductor cable constructed with a short lay should move substantially less than similar cable with a long lay. No significant difference has been detected based on field measurements on the usual run of belted cable and on some belted cable made some years ago with relatively short lay. These cables, of course, had the belt insulation and sheath tightly around the conductors. If, on the other hand, the three-conductor cable is of the shielded type, and the binder and sheath are not particularly tight, then the cable in ducts may move less with shortening of the lay. Even in this case there would still remain the thermal expansion of the lead sheath itself, which might produce substantial movement of the sheath at the duct mouth. Also, no significant difference has been found between cables having "compack" sector conductors and those having ordinary sector conductors.

It is not claimed that the factors discussed in the foregoing three paragraphs have no effect at all. What is meant is that their effects are not large enough to be detected individually in data on cable in service, because of the inherent variability of such data. For example, a single length of cable is often found to have considerable movement at one end and practically none at the other. The cause may be suspected to be variable friction along the duct, but it is not practicable to prove it. Similarly, the various other factors may be suspected, but no way has yet been found of isolating and proving them.

Considerable data have been obtained on cables not covered in the curves of Figure 10. Figure 11 shows data for 500,000-circular-mil three-conductor shielded cable operated at 12 kv (cable 5, Table IV). Such cable has about the same ratio of copper cross section to the total cable weight as the cables covered in Figure 10. However, for the larger temperature ranges the cable movement is somewhat less, and this characteristic tends to counterbalance the relatively smaller life to cracking in manholes for this cable as compared to belted type cable for a given movement at a duct mouth. Further field measurements are to be made. Other figures similar to Figure 11 could be plotted to show the data, for example, for field measurements on 350,000-circular-mil three-conductor 13-kv and 35-kv belted cables (cables 6 and 7, Table IV). These two cables have lower cross-section-to-weight ratios; and the field measurements at temperature ranges of about 10 or 20 or 30 or 42 degrees centigrade brought out that the movements were actually lower than on Figure 10. Similarly, cables 8, 10, and 11 of Table IV, that is, single-conductor 69-kv cables of the ordinary type, have lower cross-section-to-weight ratios than the cables of Figure 10, and their measured movement averages somewhat less. Figure 10 is used for predicting the probable movement of cables such as cables 6, 7, 8, 10 and 11 because of convenience and the assurance that the movement values will be conservatively high.

A direct relationship between cable movement and sheath life in service is not usually determinable except in special cases. As an example of the latter, the 69-kv cable in Chicago may be cited. There are over 4,000 lengths of 750,000- and 1,000,000-circular-mil single-conductor solid-type 69-kv cable on the system. Practically all of it has been in service about 14 years, and no sheath cracks due to movement have occurred. The daily temperature variation has been about 8 or 10 degrees centigrade throughout the life of the cables, and from Figure 10 the corresponding movement is found to be not more than about 0.15 inch at each duct. The manholes are all about $11^1/_2$ or 12 feet long, and the offset between the center of the cable at the duct mouth and center of joint is about 19 inches. On the basis of the usual distribution of measured cable movements for a given load, as shown on Figure 10, it has been computed that the average life of a cable line should be at least three times the period between installation and the occurrence of the first cracks. From this it may be concluded that the average life of the sheath on these cables will be at least 45 years. From this it is reasonable to conclude at once that any cable, excepting three-conductor shielded cable, having the same over-all diameter (2.8 inches—see Table IV) and ratio of copper cross section to total weight and installed in 12-foot manholes and subjected to not more than ten degrees centigrade daily temperature variation would have a life of at least 45 years.

If the expected daily temperature variations were to be larger, say 20 degrees centigrade, the probable life could be estimated as follows: From Figure 10 the daily movement would be about 0.37 inch. From Figure 8 the life under test conditions at 0.37-inch movement is roughly one third the life at 0.15 inch. From the estimates at the end of the previous paragraph, divided by three, it appears that the sheath life in service with a 20-degree range would be, perhaps, 15 years.

The big and outstanding point is that small increases in loading are reflected through the square law in sizable increases in temperature range; and these, in turn, cause large cable movements, because the cable movement increases faster than the first power of the temperature range. Furthermore, the sheath life decreases much faster than the first power of the increase in movement.

It has been found next to impossible to determine directly the numerical relation between cable movement and the actual sheath life of three-conductor cable in service. A rather unsatisfactory alternative which was perforce adopted, might be used by any utility in studying its own operating records for a given type of cable. This method results in data such as shown in Figures 13 and 14 of the previous paper. Such data may be extrapolated for higher loads, using a relation such as in Figure 8.

Referring to Figure 8, detailed studies of over 300 sheath cracks and history in service of the cables involved indicate that the number of daily cycles to cracking for cable in service is less than the number of cycles to cracking in the dummy manhole for a given movement. The ratio may be roughly 50 per cent. The duration of each dummy manhole cycle is only 70 seconds as compared with 24 hours for cable in service. The longer time for one thing results in relatively more plastic flow of the sheath. Some field results indicate that vibration or special conditions in the manhole may in some cases lower the life in service considerably below the usual value. Another factor tending to decrease the sheath life in manholes as compared to life in test is that the sheaths in manholes on well-loaded lines are at higher temperatures than sheaths in the laboratory; this influence would become larger with wartime loading of cables at increased temperatures.

Table VI. Data on Movement of Long Lengths of Single-Conductor Cable in Cincinnati

Actual Cable Length—Feet	Total Movement for 38 C Temperature Variation—Inches	Calculated Length of Cable to Produce Same Movement as Measured—Feet
750	2.0	320
840	2.50	420
995	2.375	400
529	2.625	470
598	1.875	290
812	2.25	360
874	3.375	640
		Average 410

In estimating the effects of the higher loads due to the present emergency, it should be kept in mind that the cable has been in service for some time, and possibly one third or more of the sheath life, for example, has been spent. This should mean, for example, that with new daily loading, which would for a new cable give say ten years' life, one may obtain only five or seven more years' life for the cable in service, and a relatively high rate of sheath cracking may occur within a year or two.

On the other hand, the effect of emergency loads that may be carried for a day or two once a year is practically negligible, assuming movements up to about two inches. Some of the tests in the dummy manhole disclose no material effect on the life of the sheath when the movement was changed from the regular test value of 0.75 inch to 2.00 inches for one cycle every 170 cycles. The calculations based on data such as in Figure 8 indicate that the equivalent movement during emergencies should have no large effect on the sheath life. Of course, incidental to such a movement at the duct mouth, there should be room for the cable to move freely in the manhole without damage to the accessories or buckling of the cable.

Mitigation of Sheath-Crack Troubles

One result is certain to come from the heavier loads to be carried by existing cables in these days: sheath cracks will be much more numerous than in the past. The use of cable for repairs is going to be accelerated, unless means can be found to

(*a*). Lengthen the life of present sheaths.

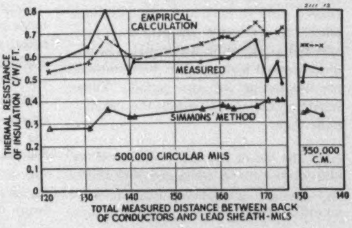
(*b*). Repair them without the use of replacement cable, or both.

A direct line of attack now being tried in Chicago is to stop the cable from moving at the duct mouth. Specially designed grips of the woven basket type were laced to the cable at each duct mouth and anchored there with an adjustable tensioning device. The tension was brought up till the movement had been reduced to a negligible value. Incidentally, prior to making this installation, the force required to restrain the daily movement of three-fourth inch at the duct mouth was actually measured by dynamometer at the location of the test and was found to be around 1,000 to 1,500 pounds. After one year's trial no damage to the sheath because of the grips has been detected. One trouble is that the cable cools during the winter, and the grip is loosened, with the result that the cable can move back and forth at the duct mouth during the cooler part of the year unless extra trips are made to adjust the tension. The trial is being continued.

Another possible line of attack is to reduce the maximum strain in the lead. The strains have been measured at a few locations on cables in service and agree pretty well with the strains found in laboratory tests. The maximum strain occurs adjacent to the joint wipes for normal cable training with the joints mounted to slide freely toward the manhole wall and back. If the joint is blocked so that it cannot slide, two things seem to happen. The cable movement is reduced because of the increased resistance to it in the manhole, and the strain is distributed more uniformly throughout the bends in the cable, thus reducing the maximum strain. A discussion of this latter effect is given in the previous paper. In two dummy manhole tests, the average sheath life with the joint blocked was about 20 per cent more than with normal installation. The movement was three-fourths inch at each duct in both cases. In some field trials the joints were blocked in several manholes, and the movement decreased about 30 per cent. This suggests that the two factors together would double the life of the sheath, and so blocking of the joints should certainly be investigated further. One point not yet solved is the means for successfully taking care of effects of seasonal variations of conduit and cable temperatures.

A different line of attack is to develop some satisfactory method of repairing cracked sheaths. Experience in Chicago has been that solder patches and tight sleeves are quite unsatisfactory, mainly because, by the time a sheath crack is found in service, the entire length of sheath in the manhole is nearly worn out. After being patched, it cracks somewhere else in a year or two. From experimental work originated by W. B. Elmer, formerly of Boston Edison Company, and taken up in Chicago, it has been found that if a short section of sheath in each training bend is removed and replaced by a flexible insert, such as a Sylphon bellows, the strain on the remainder of the sheath

Figure 12. Thermal resistance of insulation of three-conductor sector-shielded cables



becomes almost negligible. In one Chicago test a cable was run to failure with the joint blocked. The life was 8,417 cycles. At the end of the test, four six-inch Sylphons were installed in the bends near the joint and near the duct mouths, with the lead sheath removed under the Sylphons. The remaining sheath was clearly fatigued almost to failure. The cable was tested for an additional 16,805 cycles without any failure occurring. The deformations were measured and found to be concentrated in the Sylphons, being negligible in the remaining lead sheath. The question of mechanical problems and corrosion problems with the Sylphons, especially the latter problem, has not been completely solved so far as long life is concerned, but seems adequately solved for use at least during an emergency period of a couple of years.

One disadvantage of Sylphon repairs is that the line has to be taken out of service, the joint disassembled to install the Sylphons, and then rebuilt. The ideal repair from the standpoint of economy and speed would be to make a flexible insert out of some sort of hand-wrapped material in tape or strip form. Several of the most promising materials selected from the synthetic rubbers and plastics have been tried, with unsatisfactory results. The best results so far have been obtained with Glyptal 1,782 tape applied with Glyptal 1,276 cement. When tested on cable under water with pressure and vacuum cycles applied to the cable and moisture detectors under the hand-applied tape, lives of 28,000 cycles and over 43,000 cycles were obtained on two samples. The severity of manhole conditions, however, would substantially shorten the life of the material in actual service. Something better is desired, but it seems that Glyptal could probably be used if necessary with fairly satisfactory results.

Allowable Operating Temperatures

As a result of the studies discussed in parts I and II, the conditions for various types of cable in Chicago were reconsidered early in 1942, and new maximum allowable conductor temperatures were adopted as shown in Table VII. In addition special higher values were adopted for use during the war period. These wartime values are based on the assumptions that:

(*a*). The life of the cable system will be used up at a much higher rate than usual.

(*b*). The rate of troubles in cables in service will increase.

Table VII. Maximum Allowable Operating Temperatures for Impregnated-Paper-Insulated Lead-Covered Cables Adopted for Use in Chicago in 1942

Operating Voltage—Volts	Type of Cable	Maximum Allowable Conductor Temperature—Degrees Centigrade: Normal Operation	Emergency Operation
120–240	All	90 (100)	115 (135)
4,000	All	85 (95)	105 (120)
12,000	Belted made prior to 1931	78 (85)	90 (100)
12,000	Belted made in 1931 and later	78 (85)	95 (105)
12,000	Single-conductor and shielded three-conductor	78 (85)	105 (120)
33,000	Shielded, three-conductor cable with joints filled with "solid" compounds	71 (77)	84 (90)
66,000	Single-conductor, solid-type cable with joints filled with "solid" compounds	65 (68)	70 (75)
66,000	Oil-filled; single-conductor and three-conductor	78 (85)	105 (115)
132,000	Oil-filled; single-conductor	78 (85)	100 (110)

The values in parentheses are for use under wartime conditions only.

These temperatures for Chicago conditions are given only as an example of what one company is doing and are not considered to be necessarily applicable for other systems in which the installation and operating conditions are different. In general, the temperatures for normal operation of the higher-voltage cables were limited by the danger of excessive sheath cracking in manholes, while the temperatures for emergency operation of all cables and for normal operation of low-voltage cables were limited by danger of deterioration of the insulation or of excessive radial expansion of sheaths.

III. Heating Characteristics of Cables and Conduits

Three-Conductor Shielded Cable

Incidental to the accelerated aging tests on three-conductor shielded-type cable, mentioned in Part I, the thermal drop between the conductors and sheath was determined. The method was to average the results from at least four tests for each sample, the tests being made at the various temperatures from 60 to 115 degrees centigrade. The thermal drop was determined by the usual method of dividing the temperature difference between the conductor and sheath by the sum of the losses in the conductor and half the dielectric loss in the insulation.

Calculated values of thermal resistance were determined from the geometric dimensions of the cable using a thermal resistivity of 600 degrees centigrade per watt per centimeter cube. The values calculated by the D. M. Simmons[3] method are appreciably below the measured values, as indicated in Figure 12.

An empirical method of calculation was developed. It is based essentially on heat flow radially from only the backs of the sector conductors to the sheath. This method uses the usual formulas for one single-conductor cable with an empirical multiplier as follows:

$$R_i=\frac{\pi D_A}{\pi D_A-K}\,0.00522\rho\log_e\frac{D_s}{D_i} \qquad (1)$$

R_i = the thermal resistance of insulation in ohms per foot
ρ = the thermal resistivity of insulation
D_s = the inside diameter of the sheath
D_i = the diameter over the copper of the cabled conductors
D_A = the average of D_s and D_i

K equals six times the insulation thickness for conductors with corners of small radius, such as 500,000-circular-mil "compack" conductors. For conductors with more rounded corners, K has been found to be greater up to about nine times the insulation thickness.

As indicated in Figure 12, this method gives a fair agreement with the measured results. The general idea is that the thermal resistance of air gaps and of contacts between the various materials in series between the conductor and sheath all add to the thermal drop to more or less counterbalance the low resistivity of the metal shielding tapes and binder tapes. The results in the figure apply to cables having a net measured insulation ranging from 89 to 140 mils. The total measured distance between conductor and sheath, however, was some 20 to 40 mils larger.

Apparently for three-conductor shielded cables with relatively small insulation, the effect of the regular three-mil copper shielding tape on carrying heat out from the inner portion of the cable is small. This point was checked further by tests on one sample of cable with wires in the center filler space. The results of measurements of resistance of these wires generally indicate that the rise in temperature of the wires above the sheath temperature was about ten per cent less than the rise of the temperature of the conductors above sheath temperature.

For thicker insulations, K in the above formula would probably be less than indicated; that is, the copper shielding tape would be of relatively more benefit in keeping down the temperature rise.

Thermal Resistivity of the Sheath

During the aging tests of the three-conductor cables in the still air in the laboratory, sheath temperatures were determined. From these data the surface thermal resistivities were determined, and results are shown for four typical cables in Figure 13.

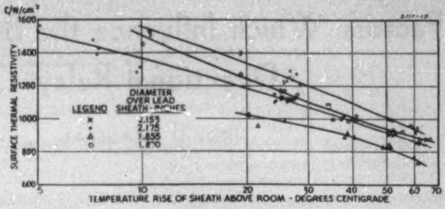


Figure 13. Surface thermal resistivity of lead cable sheaths (by tests in still air in laboratory)

1. One striking point is that for temperature rises of the sheath of 20 degrees or less, a cable of about two-inch diameter might have a surface thermal resistivity above the usual assumed value of 1,200.
2. There is considerable variation between the sheaths. The tests shown indicate a range of about 25 per cent for some cables of practically the same diameter.
3. The resistivity drops with temperature rise, as may be expected, the decrease between a 10-degree rise and a 40-degree rise being about 30 per cent.

Increasing Carrying Capacity of Conduits

In making a new design of conduit, it may be found from the standpoint of the desired loading of the cable that the originally proposed design of conduit would be inadequate. This situation may be remedied, for example, by spreading the ducts or by increasing the number of ducts and spreading out the cables by placing them in some of the outside ducts. Sometimes the problem arises as to whether for a given number of ducts the amount of heat that may be generated in them may be increased better

(*a*). By placing soil of low thermal resistance around the conduit instead of the soil found in the street.

(*b*). By simply having the conduit installed at a greater depth below the street surface.

This problem arises particularly where conduits are to be installed in soil consisting largely of slag or fine sand or miscellaneous backfill in the first two, four, or six feet of soil below the street level.

In the first method the special backfill material of low thermal resistivity is placed for a space of two feet on each side of the conduit and above the conduit for the distance from the street surface down to the bottom of the conduit. In such case the distance from the top of the conduit to the street surface may be the usual figure of about $2^{1}/_{2}$ feet. In the other method the cover over the conduit may become several feet, and the distance from the street surface to the bottom of the conduit may become, say, nine feet. There are two distinct technical advantages of this second method:

1. The conduit is brought closer to the ground water level.
2. The maximum temperature in the summer will be less for the base earth temperature than for conduit installed at the usual depth.

Sometimes soil conditions are such that even the replacement of so-called poor soil with a good soil will not solve the problem, because relatively little water is drawn into the so-called good soil to produce a low thermal resistance and good thermal dissipating characteristics for the conduit. The advantage of the lower value for maximum earth temperature for summer conditions is of particular interest, because the rating of the cable limits the loading more frequently in the summertime than in the wintertime.

One set of calculations on the economics involved showed that for installations of either a 6-duct conduit or a 12-duct conduit, the cost per kilovolt-amperes of allowable carrying capacity was appreciably less for the second scheme as compared with scheme (*a*). (Replacement of poor soil with soil of good thermal conductivity has been found very helpful in some cases for existing conduits.)

The method to be used for a new installation, however, depends to a large extent upon the conditions prevailing in the particular case. The gain obtained from increasing the depth of the conduit would be somewhat counterbalanced by the increase in required depth of the manholes and extra troubles that would accrue from water encountered in the manholes. Also, in some localities, rock is hit at relatively small depths.

Existing installations of other utilities may be of such a nature as to have a bearing on the method to be used. For installations in approaches to bridges, soil of poor conductivity may be very deep so that method (*b*) is not applicable. In this case method (*a*), with or without spreading out the cables by special duct formations, is effective. If further increase in current-carrying capacity of cables is desired for such locations, larger sizes of cables than are normally used along the remainder of the lines may be installed, thereby decreasing the losses in the conduit. For conduits under construction where poor soil is encountered in spots along a relatively small portion of the distance between manholes, replacement of soil has been found advisable.

Temperatures of Conduit

In the past usually most of the cables in a given conduit operated with daily maximum loads below the normal ratings. This phenomenon is recognized in establishing the ratings. If it were known that the several cables in a conduit were to operate part of each day throughout their lives at full normal rating, then the calculated normal ratings of each design of cable would be less than are usually established.

With wartime conditions, the maximum daily loading of cables usually increases. Also, the daily load factor usually increases. This means a substantial increase in the average amount of heat generated over each 24-hour period and a corresponding increase in conduit temperatures, even though *no* increase in normal ratings of the cables may have been established. The resulting copper temperatures may become much greater than occur in peacetimes, and even exceed the temperatures given in standards by several degrees or more.

In the previous paper an allowable limit of about 50 degrees centigrade was mentioned for the empty ducts of conduits in order to avoid

(*a*). Excessive drying out of the conduit and soil.

(*b*). An increase in heating constant.

(*c*). Resulting excessive conduit and cable temperatures.

Further field data have been studied. An analysis of heating characteristics of several conduits installed in soil of typical moisture content did not indicate an in-

Factors Which Influence the Behavior of Directional Relays

TROY D. GRAYBEAL
ASSOCIATE AIEE

Synopsis: Very little material dealing with the factors (such as network dissymmetries) which lead to incorrect operation of directional relays has appeared in the technical literature. Practical experience has been the guide as to the relative merits of the 12 possible connections which utilize either line-to-neutral or line-to-line voltages and line currents or the differences of line currents to actuate the relay elements. In this article a graphical method for analyzing directional-relay operation is developed, utilizing circle diagrams and symmetrical components, and the method is applied to general cases to ascertain the effects of network dissymmetries.

Since the relay volt-ampere expressions in terms of symmetrical components are too complicated to show the relative merits of the 12 connections, the expressions also are given in terms of the modified symmetrical components introduced by Edith Clarke and now widely used in network-analyzer studies of unbalanced faults. Circle diagrams based upon these modified components show conclusively that the ordinary 90-degree, 30-degree, and 60-degree connections are more free from disturbing influences than the other nine and therefore give more reliable operation in most practical applications of directional relays. The particular connection which will be most reliable at any given location can be determined by the methods given here when the constants of the power network are known.

THE demand for reliable and continuous service from modern power systems has made it essential that short circuits and other faults cause as little disturbance as possible to the system as a whole. Ideal protection would consist of relays which would locate accurately the faulted equipment and cause only such equipment to be disconnected from the system when short circuits occur. Since overcurrent and distance relay elements as used in loop and parallel feeder networks cannot differentiate between faults on the incoming and outgoing sides of a breaker, directional elements or relays must be used for this purpose. To prevent unnecessary loss of load and undamaged apparatus, and to prevent undue damage to the faulted equipment, the directional relays must be designed and adjusted to operate correctly for as many conditions of operation as possible on all types of faults, and in particular for those conditions which are of most frequent occurrence. A comprehensive knowledge of the behavior of directional relays of a system is necessary in order that this result may be achieved.

Of the many types of directional relays which are manufactured, this paper is limited to those which operate on the wattmeter principle. Other types in which voltage restraint coils are used are closely related, from the standpoint of operating principle, to distance relays. These have been treated in detail by J. H. Neher[1] who has developed excellent methods for analyzing the performance of different forms of the distance relay.

The behavior of a wattmeter-type directional relay depends upon the vector volt-amperes supplied to the relay element. The magnitude and angle of this quantity is obtained from the product, in the vector sense, of the current and the conjugate of the voltage at the relay. The torque developed by the relay element is proportional to $|VI| \cos(\theta+\phi)$. The angle between the relay voltage and current is θ, and ϕ is the "relay angle," which is the angle between the current and voltage applied to the relay for which given magnitudes of these quantities cause maximum torque to be developed. The relay angle is positive if the current leads the voltage for this condition.

The vector volt-ampere product may be expressed in terms of symmetrical components. The different terms of the expression then are evaluated separately and combined to indicate the direction of the relay torque. The coefficients of the several volt-ampere terms are given in Table I for both single-phase and poly-

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Troy D. Graybeal is instructor in electrical engineering, University of California, Berkeley, Calif.

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crease in heating constant for various temperatures up to 50 degrees.

A longitudinal duct temperature survey made in June 1941 along a conduit section showed temperatures along the portion of the length not affected by the presence of the manholes of 39 to 61 degrees centigrade. This conduit is located in miscellaneous fill consisting chiefly of cinders. This fill, being of a porous nature and well drained, normally holds very little moisture. The heating constant was found to be poor, but an analysis indicated no change from the previous year when the duct temperatures were much lower because of lighter loads on the cables. Other data, including experiences of over 20 years ago in Chicago, were also considered.

All of the data indicate that in many cases the allowable duct temperature limit of 50 degrees is too low, and at least for wartime conditions the maximum temperature for normal loading may be 55 or 60 degrees centigrade and five degrees higher for emergency loading.

Some factor of safety should be allowed for temperature variation between manholes, if duct temperatures are measured by the usual method at one spot along the conduit section. For the case where the idle duct temperatures were 39 to 61 degrees in the conduit section, the temperature 20 feet from the duct mouth was only 47 degrees, or 14 degrees lower than the maximum value. Such variation can be expected in conduit runs installed in soil of poor thermal conductivity when operating at temperatures of 50 degrees or more. Even in soil having good thermal characteristics, some variation, such as 15 or 25 per cent, in heating constant between two manholes may be expected, according to many field measurements. In this case, if the rise of the conduit temperature above base earth conditions is like a typical case in peacetime, that is, 16 degrees, then the variation is only four degrees; but, if the cable loading is up and the average conduit-temperature rise is 32 degrees, then the variation in duct temperatures may become about eight degrees—a large amount.

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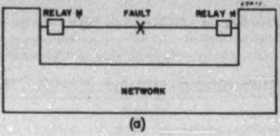
phase relays for all 12 possible connections which utilize either wye or delta voltages and either wye or delta currents to actuate the relay elements. "Line-to-neutral" voltages are referred to as *wye* voltages and "line-to-line" voltages as *delta* voltages. Likewise, "line" currents are *wye* currents and the "differences of line currents" are *delta* currents.

In order to study the effects of network dissymmetries, it is necessary to consider special network conditions beginning with the simpler cases. The general network representation illustrated in Figure 1 is used. In most cases, the impedance $\boldsymbol{Z_z}$ is considered zero, since it has no effect whatever upon the distribution of the total fault current between sides M and N. The effect of $\boldsymbol{Z_z}$ is to modify the voltage $\boldsymbol{V_M}$ which appears at the relay. Since the important consideration is the phase of $\boldsymbol{V_M}$ with respect to the relay current, $\boldsymbol{Z_z}$ has no effect unless its angle is different from that of the impedance $\boldsymbol{Z_M}$. In most actual power networks these angles will not differ greatly.

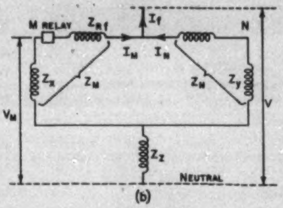
The simplest possible case is represented by $\boldsymbol{Z_z}=\boldsymbol{Z_{Rf}}=0$, $\boldsymbol{Z_1}=\boldsymbol{Z_2}=\boldsymbol{Z_0}$, and $\boldsymbol{Z_M}=\boldsymbol{Z_N}$ in all three sequence networks. When these conditions are substituted into the general relay volt-ampere expressions of Table I, four of the connections show inherent tendencies toward incorrect relay operation. If a particular connection will not give satisfactory performance in such a simplified system, it is of little use in a large complicated power network. Such connections, therefore, are not considered further and are omitted from Tables II and III.

Determination of Relay Behavior By Means of Circle Diagrams

Circle diagrams make possible visualization of the relations which must exist between the various sequence quantities as fault resistance or network constants vary. Thus, in Figure 3 drawn for the line-to-ground fault in terms of symmetrical components, the positive-sequence voltage vector is constrained to fall within the semicircle CE_{f0} for any value of fault or network resistance; with a purely reactive network the vector falls on the circumference. Similarly, the sum of the negative- and zero-sequence voltages must always fall within the semicircle OAC, and the current within the semicircle OI_fF. The ranges of possible variation of the sequence voltages and currents are shown by the diagram to be confined to definite areas which are determined by the network reactances. This situation would be very difficult to visualize from the symmetrical-component equations alone.



(a). Single-line drawing of an actual power network



(b). Equivalent circuit which represents either the positive-, negative-, or zero-sequence network of the actual system

Figure 1

The nature of the circle diagrams may be recognized from the circuits of Figure 2 in which the fault resistance is the variable parameter. These are series circuits in which $\boldsymbol{E_{f0}}$ (the positive-sequence voltage at the point of fault before the occurrence of the fault) is the driving voltage. In any circuit of this type, the current vector, as well as the vector voltage across any constant impedance portion of the circuit, describes a circle as the resistance varies.

For a line-to-ground fault, the voltage $\boldsymbol{E_{f0}}$ is laid off horizontally to scale and divided at point C (Figure 3) according to the ratio $CE_{f0}/OC=X_1/(X_2+X_0)$. If the magnitude of the fault current is known or assumed, the vector OI_f is constructed to scale so as to form a chord to the semicircular current locus. Then the lines OA perpendicular to $\boldsymbol{I_f}$, AE parallel to $\boldsymbol{I_f}$, and EE_{f0} perpendicular to $\boldsymbol{I_f}$ are drawn. The vector OB, which represents the voltage drop across both the negative- and zero-sequence networks, is constructed at an angle with $\boldsymbol{I_f}$ equal to the angle of the negative- and zero-sequence network impedances combined in series. The line DE_{f0} is drawn to intersect the line AE at the angle of the positive-sequence impedance to locate the end of the positive-sequence voltage vector $\boldsymbol{V_1}$. The vector BD represents the voltage drop $3I_fR_f$ consumed by the fault resistance. The two voltage vectors $-\boldsymbol{V_2}$ and $-\boldsymbol{V_0}$ lead the total fault current by the angles θ_2 and θ_0 respectively, and the lengths are such as to complete the parallelogram $O(-V_2)B(-V_0)$. The division of current in either sequence network is such that $\boldsymbol{I_M}$ and $\boldsymbol{I_N}$ lag the corresponding sequence voltage drop by the angles of the corresponding sequence impedances $\boldsymbol{Z_M}$ and $\boldsymbol{Z_N}$ respectively. The location of all the circular loci in terms of the network impedances is given in detail in Figure 4, and the mathematical basis for the diagram in appendix A.

The two semicircles OAC and CEE_{f0} (Figure 3) are the loci of the two voltages $(-\boldsymbol{V_2}-\boldsymbol{V_0})$ and $\boldsymbol{V_1}$ respectively, when the resistances of the sequence networks are neglected. When resistances are considered, the ends of the two corresponding voltage vectors must lie somewhere *inside* the two semicircles. It is impossible for either of the two voltages $-\boldsymbol{V_2}$, $-\boldsymbol{V_0}$ or their vector sum, when laid off from O, to fall above the semicircular arc OAC. Likewise, the positive-sequence voltage $\boldsymbol{V_1}$ cannot fall below the semicircular arc CEE_{f0} as long as the networks are inductive in nature.

The angles between the fault current $\boldsymbol{I_f}$ and the negative- and zero-sequence voltages are independent of the fault resistance. The angle between the positive-sequence voltage and the fault current varies with the fault resistance, but is restricted to the range zero to 90 degrees with the current lagging. If the impedance $\boldsymbol{Z_M}$ is more resistive than $\boldsymbol{Z_N}$, the current coming to the fault from side M will lead the total fault current, and the current coming from side N will lag the total fault current. The vector sum of these two components must, of course, make up the total. The current $\boldsymbol{I_{2-N}}$ coming from side N cannot lag the voltage $-\boldsymbol{V_2}$ by more than 90 degrees. It follows, therefore, that $\boldsymbol{I_{2-N}}$ can never lag $\boldsymbol{E_{f0}}$ by more than 90 degrees unless the zero-sequence network is more reactive than the negative-sequence network. In the same way, $\boldsymbol{I_{0-M}}$ can never lead the total fault current $\boldsymbol{I_f}$ by an angle greater than the angle of the zero-sequence network. As a limiting case, it may lag the voltage $-\boldsymbol{V_0}$ by as much as 90 degrees and thus lie in the third quadrant. If the currents $\boldsymbol{I_M}$ and $\boldsymbol{I_N}$ are equal in all three sequence networks, the maximum angle between them is given by the expression $(90°-\theta_2+\theta_0)$. These limitations as to the possible ranges of the sequence quantities are of value in the determination of the direction of relay torque from the relay volt-ampere expressions, particularly when the expressions are used to judge the relative merits of the various relay connections.

Impedance between the relay and point of fault affects principally the magnitude

Table I. Coefficients of All Terms in the Symmetrical-Component Expressions for Vector Volt-Ampere Input to a Relay

	Relay Connection	Relay	Current-Voltage Product	Volt-Ampere Terms*								
				(conj V_1)I_1	(conj V_1)I_2	(conj V_1)I_0	(conj V_2)I_1	(conj V_2)I_2	(conj V_2)I_0	(conj V_0)I_1	(conj V_0)I_2	(conj V_0)I_0
Wye-Voltage Wye-Current Connections	Zero-degree	*a*	$(\text{conj}\, V_a)I_a$	$1\angle 0$	$1\angle 0$	$1\angle 0$	$1\angle 0$	$1\angle 0$	$1\angle 0$	$1\angle 0$	$1\angle 0$	$1\angle 0$
		b	$(\text{conj}\, V_b)I_b$	$1\angle 0$	$1\angle 240$	$1\angle 120$	$1\angle 120$	$1\angle 0$	$1\angle 240$	$1\angle 240$	$1\angle 120$	$1\angle 0$
		c	$(\text{conj}\, V_c)I_c$	$1\angle 0$	$1\angle 120$	$1\angle 240$	$1\angle 240$	$1\angle 0$	$1\angle 120$	$1\angle 120$	$1\angle 240$	$1\angle 0$
		Polyphase		$3\angle 0$				$3\angle 0$				$3\angle 0$
	60-degree ordinary	*a*	$-(\text{conj}\, V_a)I_b$	$1\angle 60$	$1\angle 300$	$1\angle 180$	$1\angle 60$	$1\angle 300$	$1\angle 180$	$1\angle 60$	$1\angle 300$	$1\angle 180$
		b	$-(\text{conj}\, V_b)I_c$	$1\angle 60$	$1\angle 180$	$1\angle 300$	$1\angle 180$	$1\angle 300$	$1\angle 60$	$1\angle 300$	$1\angle 60$	$1\angle 180$
		c	$-(\text{conj}\, V_c)I_a$	$1\angle 60$	$1\angle 60$	$1\angle 60$	$1\angle 300$	$1\angle 300$	$1\angle 300$	$1\angle 180$	$1\angle 180$	$1\angle 180$
		Polyphase		$3\angle 60$				$3\angle 300$				$3\angle 180$
	60-degree alternate	*a*	$-(\text{conj}\, V_a)I_c$	$1\angle 300$	$1\angle 60$	$1\angle 180$	$1\angle 300$	$1\angle 60$	$1\angle 180$	$1\angle 300$	$1\angle 60$	$1\angle 180$
		b	$-(\text{conj}\, V_b)I_a$	$1\angle 300$	$1\angle 300$	$1\angle 300$	$1\angle 60$	$1\angle 60$	$1\angle 60$	$1\angle 180$	$1\angle 180$	$1\angle 180$
		c	$-(\text{conj}\, V_c)I_b$	$1\angle 300$	$1\angle 180$	$1\angle 60$	$1\angle 180$	$1\angle 60$	$1\angle 300$	$1\angle 60$	$1\angle 300$	$1\angle 180$
		Polyphase		$3\angle 300$				$3\angle 60$				$3\angle 180$
Delta-Voltage Wye-Current Connections	90-degree ordinary	*a*	$-(\text{conj}\, V_A)I_a$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 270$			
		b	$-(\text{conj}\, V_B)I_b$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 210$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 150$			
		c	$-(\text{conj}\, V_C)I_c$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 210$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 150$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 30$			
		Polyphase		$3\sqrt{3}\angle 90$				$3\sqrt{3}\angle 270$				
	30-degree ordinary	*a*	$(\text{conj}\, V_A)I_c$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 150$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 210$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 90$			
		b	$(\text{conj}\, V_B)I_a$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 330$			
		c	$(\text{conj}\, V_C)I_b$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 150$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 210$			
		Polyphase		$3\sqrt{3}\angle 30$				$3\sqrt{3}\angle 330$				
	30-degree alternate	*a*	$-(\text{conj}\, V_A)I_b$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 210$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 150$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 270$			
		b	$-(\text{conj}\, V_B)I_c$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 210$	$\sqrt{3}\angle 270$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 150$			
		c	$-(\text{conj}\, V_C)I_a$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 30$			
		Polyphase		$3\sqrt{3}\angle 330$				$3\sqrt{3}\angle 30$				
Wye-Voltage Delta-Current Connections	90-degree	*a*	$(\text{conj}\, V_a)I_A$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 270$		$\sqrt{3}\angle 90$	$\sqrt{3}\angle 270$		$\sqrt{3}\angle 90$	$\sqrt{3}\angle 270$	
		b	$(\text{conj}\, V_b)I_B$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 150$		$\sqrt{3}\angle 210$	$\sqrt{3}\angle 270$		$\sqrt{3}\angle 330$	$\sqrt{3}\angle 30$	
		c	$(\text{conj}\, V_c)I_C$	$\sqrt{3}\angle 90$	$\sqrt{3}\angle 30$		$\sqrt{3}\angle 330$	$\sqrt{3}\angle 270$		$\sqrt{3}\angle 210$	$\sqrt{3}\angle 150$	
		Polyphase		$3\sqrt{3}\angle 90$				$3\sqrt{3}\angle 270$				
	30-degree	*a*	$-(\text{conj}\, V_a)I_C$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 330$		$\sqrt{3}\angle 30$	$\sqrt{3}\angle 330$		$\sqrt{3}\angle 30$	$\sqrt{3}\angle 330$	
		b	$-(\text{conj}\, V_b)I_A$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 210$		$\sqrt{3}\angle 150$	$\sqrt{3}\angle 330$		$\sqrt{3}\angle 270$	$\sqrt{3}\angle 90$	
		c	$-(\text{conj}\, V_c)I_B$	$\sqrt{3}\angle 30$	$\sqrt{3}\angle 90$		$\sqrt{3}\angle 270$	$\sqrt{3}\angle 330$		$\sqrt{3}\angle 150$	$\sqrt{3}\angle 210$	
		Polyphase		$3\sqrt{3}\angle 30$				$3\sqrt{3}\angle 330$				
	30-degree alternate	*a*	$(\text{conj}\, V_a)I_B$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 30$		$\sqrt{3}\angle 330$	$\sqrt{3}\angle 30$		$\sqrt{3}\angle 330$	$\sqrt{3}\angle 30$	
		b	$(\text{conj}\, V_b)I_C$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 270$		$\sqrt{3}\angle 90$	$\sqrt{3}\angle 30$		$\sqrt{3}\angle 210$	$\sqrt{3}\angle 150$	
		c	$(\text{conj}\, V_c)I_A$	$\sqrt{3}\angle 330$	$\sqrt{3}\angle 150$		$\sqrt{3}\angle 210$	$\sqrt{3}\angle 30$		$\sqrt{3}\angle 90$	$\sqrt{3}\angle 270$	
		Polyphase		$3\sqrt{3}\angle 330$				$3\sqrt{3}\angle 30$				
Delta-Voltage Delta-Current Connections	Zero-degree	*a*	$(\text{conj}\, V_A)I_A$	$3\angle 0$	$3\angle 180$		$3\angle 180$	$3\angle 0$				
		b	$(\text{conj}\, V_B)I_B$	$3\angle 0$	$3\angle 60$		$3\angle 300$	$3\angle 0$				
		c	$(\text{conj}\, V_C)I_C$	$3\angle 0$	$3\angle 300$		$3\angle 60$	$3\angle 0$				
		Polyphase		$9\angle 0$				$9\angle 0$				
	60-degree	*a*	$-(\text{conj}\, V_A)I_B$	$3\angle 60$	$3\angle 120$		$3\angle 240$	$3\angle 300$				
		b	$-(\text{conj}\, V_B)I_C$	$3\angle 60$	$3\angle 0$		$3\angle 0$	$3\angle 300$				
		c	$-(\text{conj}\, V_C)I_A$	$3\angle 60$	$3\angle 240$		$3\angle 120$	$3\angle 300$				
		Polyphase		$9\angle 60$				$9\angle 300$				
	60-degree alternate	*a*	$-(\text{conj}\, V_A)I_C$	$3\angle 300$	$3\angle 240$		$3\angle 120$	$3\angle 60$				
		b	$-(\text{conj}\, V_B)I_A$	$3\angle 300$	$3\angle 120$		$3\angle 240$	$3\angle 60$				
		c	$-(\text{conj}\, V_C)I_B$	$3\angle 300$	$3\angle 0$		$3\angle 0$	$3\angle 60$				
		Polyphase		$9\angle 300$				$9\angle 60$				

* To find the symmetrical-component expression for the total vector volt-ampere input to any relay, take the sum of all the column headings, each multiplied by the corresponding coefficient taken from the row opposite the relay considered. The voltage and current components in the network at the point where the relay is located must be used in these expressions.

of the voltages at the relay. As the impedance $\boldsymbol{Z}_{Rf}$ becomes larger and larger as compared with $\boldsymbol{Z}_M$ and $\boldsymbol{Z}_N$, the negative- and zero-sequence voltages at M get smaller and smaller in magnitude. They are modified in phase only when the angle of $\boldsymbol{Z}_{Rf}$ is different from the angle of $\boldsymbol{Z}_M$. In general, these angles will not differ greatly, and the predominate effect of impedance between the relay and fault point is to decrease the negative- and zero-sequence voltages and to increase the positive-sequence voltage. In an extreme case where all the resistance of side M is between the relay and fault point, and all the reactance beyond the relay, the negative-sequence voltage $-\boldsymbol{V}_{2-M}$ would lie on a semicircle with $-\boldsymbol{V}_2$ as the diameter. The relay voltage $-\boldsymbol{V}_{2-M}$, therefore, may lie somewhat outside the semicircle OAC, but only in rare cases. The relative importance of $\boldsymbol{Z}_{Rf}$ is determined by how far back electrically from the fault point the relay is located. It becomes more important as the ratio $\boldsymbol{Z}_{Rf}/\boldsymbol{Z}_M$ increases. This ratio is larger for a line connected into a large power-system network than for a line of the same constants connected into a small power-system network.

The diagram of Figure 3, together with others drawn for different network conditions, shows it is impossible to design a directional-relay scheme which will operate correctly for all possible faults on a normal power system. Erratic operation may be expected, if the angles of the impedances measured opposite ways from the fault point are greatly different, or if the angle of the impedance between the relay and point of fault is considerably different from that of the total impedance Z_M. In general, the relay on the more reactive side of the fault is less liable to operate incorrectly.

When load currents are present, they must be added vectorially to the fault components in the positive-sequence network. Load currents increase one of the components I_{1-M} or I_{1-N} and decrease the other. In cases where the load current is comparable in magnitude with the fault current, the total positive-sequence current may be modified in phase sufficiently to cause incorrect relay operation. The relay most subject to this effect is the one at the receiving end of the line, since at this point the two components of positive-sequence current flow in opposite directions.

In order to obviate the effects of load current, some form of overcurrent device must be used in conjunction with the relay to prevent operation unless overcurrent or fault current exists in the system. The current balance scheme, which is commonly used for the protection of paralleled feeders, makes use of two current transformers, one in each feeder, with secondary windings connected so that the relay receives the difference of the two secondary currents. In this case, the relay current is given by the expression $I_f(1-f)$ which is independent of normal load current. I_f is the total fault current and f is the fractional length of the feeder from the relay to the point of fault. This scheme, however, is applicable only to feeders or other apparatus operating in parallel. In other cases where the load component of current may be comparable in magnitude with the fault components, the only workable scheme is the use of "ground relays" which operate on zero- or negative-sequence quantities only.

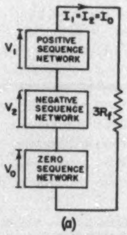
The ease with which directional-relay operation may be determined by use of the circle diagram is exemplified by Figure 5 which shows the behavior of an ordinary 90-degree-connected polyphase relay on a line-to-ground fault. The circle diagram is constructed from the network impedances in the manner outlined previously to give the magnitude and phase of the sequence voltages and currents at the relay. From Table I, the volt-ampere expression to be evaluated is $[(\text{conj } V_1)I_1\underline{|90}+(\text{conj } V_2)I_2\underline{|270}]$. The factor $3\sqrt{3}$ is omitted since the actual magnitude of the relay torque is unimportant. If the load current is small compared with the fault currents, I_1 is equal to I_2, and the expression may be written $(\text{conj } V_1-\text{conj } V_2)I_1\underline{|90}$. The relay torque then is proportional to the sum of the projections of the two voltages on the current vector I_1 rotated through the angle $(90°-\phi)$. Since both of these projections are positive for the conditions depicted in Figure 5, correct relay operation is indicated.

An alternative method which may be used to advantage when the volt-ampere expression consists of only two terms, makes use of the position of the current vector where the two voltage projections on the current are equal in magnitude but opposite in sign. A line in this position is designated an "equiprojection" line, and represents that phase position of the current for which the torque developed by an element having a relay angle of zero degrees is zero. If the relay angle ϕ is not zero, the zero-torque line is ϕ degrees leading the equiprojection line.

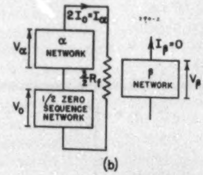
The equiprojection line is located by dropping a perpendicular from 0 to the line joining the ends of the two voltages $V_1\underline{|90}$ and $(-V_2\underline{|-90})$. Since the relay operates correctly if the current lags the zero-torque line by less than 180 degrees, correct operation is indicated in Figure 5, where the current is near the position for maximum torque for the two voltages concerned.

For the ordinary 90-degree-connected polyphase relay, the equiprojection line will coincide with E_{f0} if the positive- and negative-sequence networks are identical. Thus, disregarding the secondary effects of load currents, the direction of the relay torque is independent of the fault resistance for this connection.

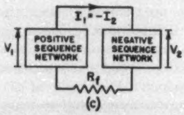
When the volt-ampere expressions consist of more than two terms, or when the two terms do not involve the same current, the first method described must be used. An example of this character is given in detail in appendix B.



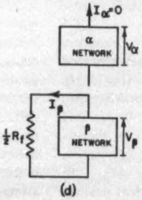
(a). Line-to-ground fault in terms of symmetrical components



(b). Line-to-ground fault in terms of modified symmetrical components



(c). Line-to-line fault in terms of symmetrical components



(d). Line-to-line fault in terms of modified symmetrical components

Figure 2 (left). Sequence network connections representing line-to-ground and line-to-line faults

Polyphase Directional Relays

A three-phase relay is less subject to erratic operation than are the three single-phase relays otherwise used on a three-phase system, since an incorrect operation of any one relay will result in an incorrect switching operation. That the three single-phase relays will behave dif-

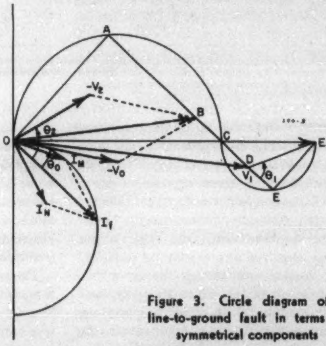


Figure 3. Circle diagram of a line-to-ground fault in terms of symmetrical components

Table II. Coefficients of All Terms in the Modified Symmetrical-Component Expressions for Vector Volt-Ampere Input to a Relay

	Relay Connection	Relay	Current-Voltage Product	Volt-Ampere Terms*								
				$(\mathrm{conj}\ V_\alpha)I_\alpha$	$(\mathrm{conj}\ V_\alpha)I_\beta$	$(\mathrm{conj}\ V_\alpha)I_0$	$(\mathrm{conj}\ V_\beta)I_\alpha$	$(\mathrm{conj}\ V_\beta)I_\beta$	$(\mathrm{conj}\ V_\beta)I_0$	$(\mathrm{conj}\ V_0)I_\alpha$	$(\mathrm{conj}\ V_0)I_\beta$	$(\mathrm{conj}\ V_0)I_0$
Wye-Voltage Wye-Current Connections	60-degree ordinary	a	$-(\mathrm{conj}\ V_a)I_b$	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	-1	0	0	0	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	-1
		b	$-(\mathrm{conj}\ V_b)I_c$	$-\frac{1}{4}$	$-\frac{\sqrt{3}}{4}$	$\frac{1}{2}$	$\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	-1
		c	$-(\mathrm{conj}\ V_c)I_a$	$\frac{1}{2}$	0	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	0	$\frac{\sqrt{3}}{2}$	-1	0	-1
		Polyphase		$\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$\frac{3}{4}$	0	0	0	-3
	60-degree alternate	a	$-(\mathrm{conj}\ V_a)I_c$	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	-1	0	0	0	$\frac{1}{2}$	$\frac{\sqrt{3}}{2}$	-1
		b	$-(\mathrm{conj}\ V_b)I_a$	$\frac{1}{2}$	0	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	0	$-\frac{\sqrt{3}}{2}$	-1	0	-1
		c	$-(\mathrm{conj}\ V_c)I_b$	$-\frac{1}{4}$	$\frac{\sqrt{3}}{4}$	$\frac{1}{2}$	$-\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$\frac{\sqrt{3}}{2}$	$\frac{1}{2}$	$-\frac{\sqrt{3}}{2}$	-1
		Polyphase		$\frac{3}{4}$	$\frac{3\sqrt{3}}{4}$	0	$-\frac{3\sqrt{3}}{4}$	$\frac{3}{4}$	0	0	0	-3
Delta-Voltage Wye-Current Connections	90-degree ordinary	a	$-(\mathrm{conj}\ V_A)I_a$	0	0	0	$\sqrt{3}$	0	$\sqrt{3}$			
		b	$-(\mathrm{conj}\ V_B)I_b$	$\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	$-\frac{3}{2}$	$\frac{\sqrt{3}}{4}$	$-\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$			
		c	$-(\mathrm{conj}\ V_C)I_c$	$-\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	$\frac{3}{2}$	$\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$			
		Polyphase		0	$-\frac{3\sqrt{3}}{2}$	0	$\frac{3\sqrt{3}}{2}$	0	0			
	30-degree ordinary	a	$(\mathrm{conj}\ V_A)I_c$	0	0	0	$\frac{\sqrt{3}}{2}$	$\frac{3}{2}$	$-\sqrt{3}$			
		b	$(\mathrm{conj}\ V_B)I_a$	$\frac{3}{2}$	0	$\frac{3}{2}$	$\frac{\sqrt{3}}{2}$	0	$\frac{\sqrt{3}}{2}$			
		c	$(\mathrm{conj}\ V_C)I_b$	$\frac{3}{4}$	$-\frac{3\sqrt{3}}{4}$	$-\frac{3}{2}$	$-\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$\frac{\sqrt{3}}{2}$			
		Polyphase		$\frac{9}{4}$	$-\frac{3\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$\frac{9}{4}$	0			
	30-degree alternate	a	$-(\mathrm{conj}\ V_A)I_b$	0	0	0	$-\frac{\sqrt{3}}{2}$	$\frac{3}{2}$	$\sqrt{3}$			
		b	$-(\mathrm{conj}\ V_B)I_c$	$\frac{3}{4}$	$\frac{3\sqrt{3}}{4}$	$-\frac{3}{2}$	$\frac{\sqrt{3}}{4}$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{2}$			
		c	$-(\mathrm{conj}\ V_C)I_a$	$\frac{3}{2}$	0	$\frac{3}{2}$	$-\frac{\sqrt{3}}{2}$	0	$-\frac{\sqrt{3}}{2}$			
		Polyphase		$\frac{9}{4}$	$\frac{3\sqrt{3}}{4}$	0	$-\frac{3\sqrt{3}}{4}$	$\frac{9}{4}$	0			
Wye-Voltage Delta-Current Connections	90-degree	a	$(\mathrm{conj}\ V_a)I_A$	0	$-\sqrt{3}$	0	0	0	0	0	$-\sqrt{3}$	
		b	$(\mathrm{conj}\ V_b)I_B$	$-\frac{3}{4}$	$-\frac{\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$\frac{3}{4}$	0	$\frac{3}{2}$	$\frac{\sqrt{3}}{2}$	0
		c	$(\mathrm{conj}\ V_c)I_C$	$\frac{3}{4}$	$-\frac{\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$-\frac{3}{4}$	0	$-\frac{3}{2}$	$\frac{\sqrt{3}}{2}$	0
		Polyphase		0	$-\frac{3\sqrt{3}}{2}$	0	$\frac{3\sqrt{3}}{2}$	0	0	0	0	0
Delta-Voltage Delta-Current Connections	60-degree	a	$-(\mathrm{conj}\ V_A)I_B$	0	0	0	$\frac{3\sqrt{3}}{2}$	$\frac{3}{2}$				
		b	$-(\mathrm{conj}\ V_B)I_C$	$\frac{9}{4}$	$-\frac{3\sqrt{3}}{4}$	0	$\frac{3\sqrt{3}}{4}$	$-\frac{3}{4}$				
		c	$-(\mathrm{conj}\ V_C)I_A$	0	$-\frac{3\sqrt{3}}{2}$	0	0	$\frac{3}{2}$				
		Polyphase		$\frac{9}{4}$	$-\frac{9\sqrt{3}}{4}$	0	$\frac{9\sqrt{3}}{4}$	$\frac{9}{4}$				
	60-degree alternate	a	$-(\mathrm{conj}\ V_A)I_C$	0	0	0	$-\frac{3\sqrt{3}}{2}$	$\frac{3}{2}$				
		b	$-(\mathrm{conj}\ V_B)I_A$	0	$\frac{3\sqrt{3}}{2}$	0	0	$\frac{3}{2}$				
		c	$-(\mathrm{conj}\ V_C)I_B$	$\frac{9}{4}$	$\frac{3\sqrt{3}}{4}$	0	$-\frac{3\sqrt{3}}{4}$	$-\frac{3}{4}$				
		Polyphase		$\frac{9}{4}$	$\frac{9\sqrt{3}}{4}$	0	$-\frac{9\sqrt{3}}{4}$	$\frac{9}{4}$				

* To find the modified symmetrical-component expression for the total vector volt-ampere input to any relay, take the sum of all the column headings, each multiplied by the corresponding coefficient taken from the row opposite the relay considered. The voltage and current components in the network at the point where the relay is located must be used in these expressions.

ferently is indicated by the expressions of Table I. The cross-product terms (such as $(\mathrm{conj}\ V_1)I_2$, $(\mathrm{conj}\ V_1)I_0$, and so forth) are identical in all three expressions for a particular connection except that they are rotated through different angles before being combined with the other terms. Thus, negative torque may be produced in one relay even though the other two operate correctly. Since the polyphase relay consists of three single-phase elements mounted on a common shaft, the cross-product terms in the volt-ampere expressions cancel. The relay is governed completely by the three terms $(\mathrm{conj}\ V_1)I_1$, $(\mathrm{conj}\ V_2)I_2$, and $(\mathrm{conj}\ V_0)I_0$. The latter term is zero in all cases except for ground faults, and then appears only in the expressions for the wye-voltage wye-current connections.

The wye-voltage delta-current connections give exactly the same relay operation as the corresponding delta-voltage wye-current connections. Therefore, neither set has any advantage over the other. Likewise, for faults not involving ground, the wye-voltage wye-current connections have no advantage over the delta-voltage delta-current connections. On ground faults the wye-voltage wye-current connections would give somewhat better performance. The angle of the $(\mathrm{conj}\ V_0)I_0$ term usually is such that it represents positive torque, and the relay is somewhat more free from the disturbing influence of normal load current.

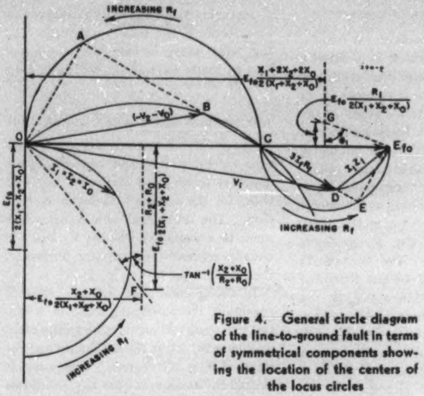

INCREASING R_f
A
B
$(-V_2-V_0)$
O
C
E_{fo}
G
θ_1
D
E
V_1
$I_1+I_2+I_0$
$3I_1R_f$
I_1Z_1
INCREASING R_f
$E_{fo}\frac{X_1+2X_2+2X_0}{2(X_1+X_2+X_0)}$
$E_{fo}\frac{R_1}{2(X_1+X_2+X_0)}$
$\frac{E_{fo}}{2(X_1+X_2+X_0)}$
$E_{fo}\frac{R_2+R_0}{2(X_1+X_2+X_0)}$
$\tan^{-1}\left(\frac{X_2+X_0}{R_2+R_0}\right)$
F
$E_{fo}\frac{X_2+X_0}{2(X_1+X_2+X_0)}$
INCREASING R_f

Figure 4. General circle diagram of the line-to-ground fault in terms of symmetrical components showing the location of the centers of the locus circles

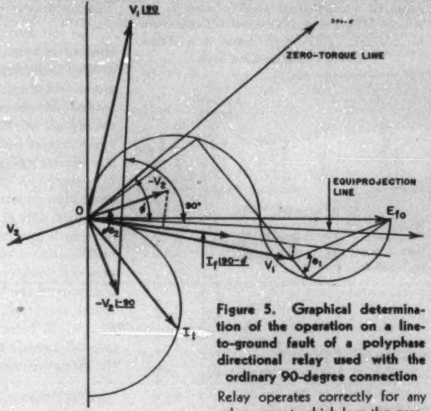


Figure 5. Graphical determination of the operation on a line-to-ground fault of a polyphase directional relay used with the ordinary 90-degree connection

Relay operates correctly for any relay current which lags the zero-torque line by less than 180 degrees

The relative merits of the 30-degree, 60-degree, and 90-degree connections may be studied by application of the circle diagram. The conditions existing during a line-to-ground fault are illustrated by the diagram of Figure 3. For the line-to-line fault, the diagram is similar, the essential difference being that point C is situated at or near the center of the line OE_{fo}, and $\boldsymbol{V}_0$ does not appear at all.

Since the positive-sequence voltage can never be less in magnitude than the negative-sequence voltage, the zero-sequence voltage, or the vector sum, the term (conj $\boldsymbol{V}_1$)$\boldsymbol{I}_1$ usually will govern the relay behavior. A possible exception exists with the wye-wye connections. For a ground fault on a circuit which is grounded at one end only, the zero-sequence current at the relay might be considerably greater than either of the other two components, in which case the (conj $\boldsymbol{V}_0$)$\boldsymbol{I}_0$ term would take precedence. For this special condition the relay would be less subject to incorrect operation than in more general networks.

For the 90-degree connections, the volt-ampere input to the relay is $k[(\text{conj } \boldsymbol{V}_1)\boldsymbol{I}_1\underline{|90}+(\text{conj } \boldsymbol{V}_2)\boldsymbol{I}_2\underline{|-90}]$. Neglecting load current, $\boldsymbol{I}_1$ is very nearly equal to $\boldsymbol{I}_2$ in any actual power network. The expression therefore may be written $k\boldsymbol{I}_1(\text{conj } \boldsymbol{V}_1-\text{conj } \boldsymbol{V}_2)\underline{|90}$. When only two terms are present in the volt-ampere expression, the relay torque may be obtained from the projections of the two voltages on the current vector rotated counterclockwise through the angle ϕ. For almost all conditions, $\boldsymbol{V}_1$ will lag $\boldsymbol{E}_{fo}$ by an angle of from 0 to 45 degrees, and $-\boldsymbol{V}_2$ will lie somewhere in the first quadrant as shown in Figure 3. Thus, the 90-degree connections will nearly always give correct operation if the relay angle ϕ is properly chosen.

The ordinary 30- and 60-degree connections give performance comparable with that of the 90-degree connections. The voltage $\boldsymbol{V}_1$ will, in general, lag the voltage $-\boldsymbol{V}_2$, so that rotation of $\boldsymbol{V}_1$ counterclockwise and $-\boldsymbol{V}_2$ clockwise tends to interchange their relative positions without appreciably affecting the torque produced. The relay angle ϕ should be zero or slightly positive for the 30-degree connection and positive for the 60- and 90-degree connections, perhaps 45 to 50 degrees for the latter, to insure correct relay operation for the greatest number of practical situations.

The 30- and 60-degree alternate connections are definitely inferior to the ordinary connections. To obtain the relay torque by means of the circle diagram, the two voltage vectors must be rotated in directions opposite to those for the ordinary connections. Thus, the voltages become widely separated in phase. One of the torque terms will usually be negative and the relay more subject to incorrect operation.

Modified Symmetrical Components and Relay Operation

In order that the relative merits of the several relay connections may be ascertained and compared when single-phase relay elements are considered, the system network impedances must be assigned different values, and the direction of the relay torque ascertained for each condition. The criterion as to the best connection is the range or allowable variation in both the magnitude and angle of the impedances that will not result in incorrect relay operation.

The direction of the relay torque is governed by the angle of the relay vector volt-ampere input for the particular relay and connection considered. The resultant torque is the algebraic sum of the separate components represented by the terms in the relay volt-ampere expression.

Values of network impedances which represent limiting cases from the standpoint of relay behavior are the only ones that need be considered critically when the effects of different forms of network dissymmetry are evident from the circle diagram. This is the case only when the relay volt-ampere expression contains very few terms. When symmetrical components are used for the determination of relay behavior in a generalized system network, the effects of dissymmetries are very obscure since many of the symmetrical component volt-ampere expressions contain as many as nine terms. Modified symmetrical components[3] not only provide a simpler circle diagram but also reduce considerably the number of terms in the relay volt-ampere expressions. For instance, the symmetrical-component expression for relay a of the ordinary 90-degree connection contains six terms as compared with two terms in the modified symmetrical-component expression. Thus, the use of modified symmetrical components makes possible a much more accurate evaluation of the relative merits of the several relay connections.

The circle diagram of the line-to-ground fault based upon the circuit of Figure 2b in terms of modified symmetrical components is illustrated in Figure 6. The

Table III. Coefficients of All Terms in the Modified Symmetrical-Component Expressions for Vector Volt-Ampere Input to a Relay During a Line-to-Line Fault

Relay Connection	Relay	Current-Voltage Product	Volt-Ampere* Terms		Common Factor
			$(\text{conj } V_\alpha)I_\beta$	$(\text{conj } V_\beta)I_\beta$	
60-degree ordinary Wye-voltage Wye-current	a	$-(\text{conj } V_a)I_b$	$-\frac{1}{\sqrt{3}}$	0	$\frac{3}{2}$
	b	$-(\text{conj } V_b)I_c$	$-\frac{1}{\sqrt{3}}$	1	$\frac{3}{4}$
	c	$-(\text{conj } V_c)I_a$	0	0	
	Polyphase		$-\sqrt{3}$	1	$\frac{3}{4}$
60-degree alternate Wye-voltage Wye-current	a	$-(\text{conj } V_a)I_c$	$\frac{1}{\sqrt{3}}$	0	$\frac{3}{2}$
	b	$-(\text{conj } V_b)I_a$	0	0	
	c	$-(\text{conj } V_c)I_b$	$\frac{1}{\sqrt{3}}$	1	$\frac{3}{4}$
	Polyphase		$\sqrt{3}$	1	$\frac{3}{4}$
90-degree ordinary Delta-voltage Wye-current	a	$-(\text{conj } V_A)I_a$	0	0	
	b	$-(\text{conj } V_B)I_b$	$-\sqrt{3}$	-1	$\frac{3}{4}$
	c	$-(\text{conj } V_C)I_c$	$-\sqrt{3}$	1	$\frac{3}{4}$
	Polyphase		$-\sqrt{3}$	0	$\frac{3}{2}$
30-degree ordinary Delta-voltage Wye-current	a	$(\text{conj } V_A(I_c$	0	1	$\frac{3}{2}$
	b	$(\text{conj } V_B)I_a$	0	0	
	c	$(\text{conj } V_C)I_b$	$-\sqrt{3}$	1	$\frac{3}{4}$
	Polyphase		$-\frac{1}{\sqrt{3}}$	1	$\frac{9}{4}$
30-degree alternate Delta-voltage Wye-current	a	$-(\text{conj } V_A)I_b$	0	1	$\frac{3}{2}$
	b	$-(\text{conj } V_B)I_c$	$\sqrt{3}$	1	$\frac{3}{4}$
	c	$-(\text{conj } V_C)I_a$	0	0	
	Polyphase		$\frac{1}{\sqrt{3}}$	1	$\frac{9}{4}$
90-degree Wye-voltage Delta-current	a	$(\text{conj } V_a)I_A$	$-\sqrt{3}$	0	1
	b	$(\text{conj } V_b)I_B$	$-\frac{1}{\sqrt{3}}$	1	$\frac{3}{4}$
	c	$(\text{conj } V_c)I_C$	$-\frac{1}{\sqrt{3}}$	-1	$\frac{3}{4}$
	Polyphase		$-\sqrt{3}$	0	$\frac{3}{2}$
60-degree Delta-voltage Delta-current	a	$-(\text{conj } V_A)I_B$	0	1	$\frac{3}{2}$
	b	$-(\text{conj } V_B)I_C$	$-\sqrt{3}$	-1	$\frac{3}{4}$
	c	$-(\text{conj } V_C)I_A$	$-\sqrt{3}$	1	$\frac{3}{2}$
	Polyphase		$-\sqrt{3}$	1	$\frac{9}{4}$
60-degree alternate Delta-voltage Delta-current	a	$-(\text{conj } V_A)I_C$	0	1	$\frac{3}{2}$
	b	$-(\text{conj } V_B)I_A$	$\sqrt{3}$	1	$\frac{3}{2}$
	c	$-(\text{conj } V_C)I_B$	$\sqrt{3}$	-1	$\frac{3}{4}$
	Polyphase		$\sqrt{3}$	1	$\frac{9}{4}$

* To find the modified symmetrical-component expression for the total vector volt-ampere input to any relay, take the sum of the two column headings, each multiplied by the common factor and the corresponding coefficient taken from the row opposite the relay considered. The voltage and current components in the network at the point where the relay is located must be used in these expressions.

point C is determined by the ratio $OC/CE_{\alpha-f_0}=(X_0/2)/X_1$. Since the β-network is open, the voltage V_β is constant and equal to $-j(V_1-V_2)=-jE_{f_0}$ for any value of fault resistance. For completeness, this vector is superposed on the diagram.

The Ordinary 60-Degree Connection

The volt-ampere expression for relay c, the one principally affected by a line-to-ground fault, does not lend itself readily to general treatment, since it consists of six terms. However, a detailed study of the conditions existing in a large number of specific cases indicates that the 60-degree connection will give correct relay operation except for extreme conditions of network dissymmetry. As long as the vector sum of I_0 and I_α is a vector in the third or fourth quadrants, the V_β products yield positive torque. The V_0 and V_α products yield positive torque for most positions of the currents I_0 and I_α in the fourth quadrant, particularly those near $E_{\alpha-f_0}$. In most cases the currents will not fall outside the fourth quadrant and the relay will operate correctly.

The 60-Degree Alternate Connection

This connection is inferior to the ordinary 60-degree connection. Relay b, the one subject to operation on a line-to-ground fault, has exactly the same volt-ampere expression as relay c for the ordinary 60-degree connection, except for the sign of the two terms involving V_β. Thus, regardless of the value chosen for the relay angle, some of the torque terms will be negative, and the relay subject to incorrect operation.

The Ordinary 90-Degree Connection

On a line-to-ground fault, relay a is the one principally affected. The volt-ampere expression consists of two terms which involve V_β, I_α, and I_0. Since V_β is constant, the variations of I_α and I_0 are all that need be considered. If the power network is symmetrical so that the relay components of current are inphase with the fault components, incorrect relay operation can never occur, if the relay angle ϕ is between zero and 90 degrees. It can be shown that the current I_M must always lie inside a semicardioid along the vertical axis (Figure 7), and inside a semicircle in the third quadrant. In most instances the relay current will lie in the fourth quadrant, but, because of network dissymmetries, may fall outside. If the α network is mostly reactive, and the zero network resistive as viewed one way from the point of fault and reactive as viewed the other way, the zero-sequence current on the reactive side will lie in the fourth quadrant

For the 90-degree connection the impedance between the relay and point of fault has no effect on the relay performance except as a secondary effect arising from modifications in V_β at the relay due to the flow of normal load current in the β network. Since the relay is affected by load currents, the total component I_α at the relay must include the component due to loads as well as the fault component.

The Ordinary 30-Degree Connection

The terms of the volt-ampere expression for relay b which contain V_β are identical (except for the factor $^1/_2$) with those for the ordinary 90-degree connection. The additional two terms which must be considered involve V_α and will usually represent the greater portion of the relay torque.

In the general case for any network condition, the voltage V_α must lie within the two semicardioidal regions illustrated in Figure 7. Thus the maximum angular range of V_α is 270 degrees. This range is shifted by 90 degrees from the 270-degree current range and would seem to indicate that the relay angle should be approximately equal to +90 degrees. For the usual practical network conditions however, the voltage V_α will lag slightly the voltage $E_{\alpha-f_0}$, and the current will lie in the fourth quadrant at an angle of perhaps 60 to 80 degrees lagging $E_{\alpha-f_0}$. Therefore, the relay angle should be only slightly positive or zero for most cases. The torque represented by the last two terms will, in general, be positive and add to that of the other two.

The Alternate 30-Degree Connection

The volt-ampere expression for the alternate 30-degree connection—relay c—is identical to that of the ordinary 30-degree connection—relay b—except for the sign of the two terms containing V_β. Thus, the torque represented by these terms will in most cases be of opposite sign from that represented by the other two. This connection is more subject to incorrect operation than the ordinary 30-degree connection.

The 90-Degree Wye-Voltage Delta-Current Connection

For this connection, relays b and c both operate on a line-to-ground fault, but in different ways as shown by the expressions of Table I. Except for the second term, one expression is the negative of the other. In many instances the second term would represent only a minor part of the total relay torque, and the two relays would function oppositely. This effect might occur even in a symmetrical network with the relay located near the fault point. This connection is definitely unreliable on ground faults. On other types of fault where the current I_β is present, the torque represented by the I_β

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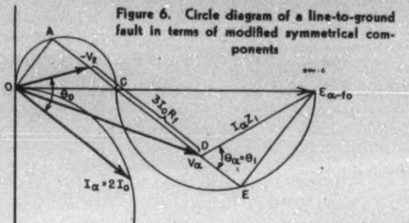
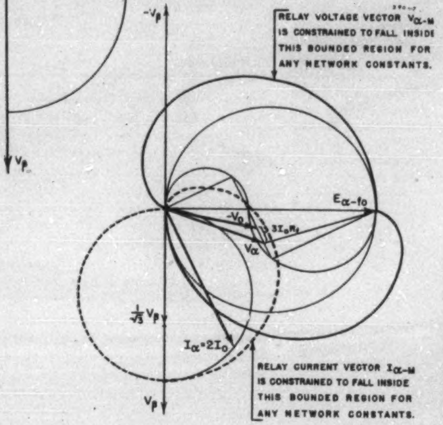

A
O
$-V_f$
G
θ_0
$E_{\alpha-fo}$
$3I_0R_f$
$I_\alpha Z_1$
D
V_α
$\theta_\alpha=\theta_1$
E
$I_\alpha=2I_0$
V_β

Figure 6. Circle diagram of a line-to-ground fault in terms of modified symmetrical components


$-V_\beta$
$E_{\alpha-fo}$
$-V_f$
$3I_0R_f$
V_α
$\frac{1}{\sqrt{3}}V_\beta$
$I_\alpha=2I_0$
V_β

RELAY VOLTAGE VECTOR $V_{\alpha-M}$ IS CONSTRAINED TO FALL INSIDE THIS BOUNDED REGION FOR ANY NETWORK CONSTANTS.

RELAY CURRENT VECTOR $I_{\alpha-M}$ IS CONSTRAINED TO FALL INSIDE THIS BOUNDED REGION FOR ANY NETWORK CONSTANTS.

Figure 7 (left). Circle diagram showing the limits of variation of the relay current and voltage for either a line-to-ground or line-to-line fault expressed in modified symmetrical components

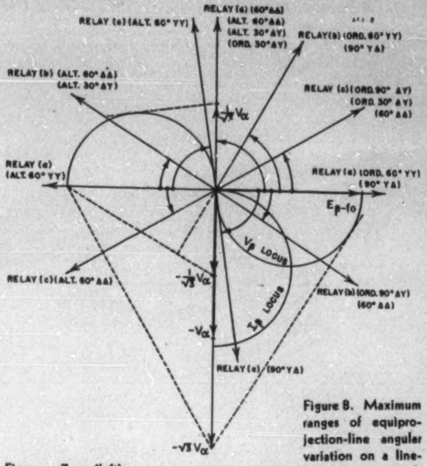

RELAY (a) (ALT. 60° YY)
RELAY (a) (60° ΔΔ)
(ALT. 60° ΔΔ)
(ALT. 30° ΔY)
(ORD. 30° ΔY)
RELAY (b) (ORD. 60° YY)
(90° YΔ)
RELAY (b) (ALT. 60° ΔΔ)
(ALT. 30° ΔY)
RELAY (c) (ORD. 90° ΔY)
(ORD. 30° ΔY)
(60° ΔΔ)
$\frac{1}{\sqrt{3}}V_\alpha$
RELAY (a)
(ALT. 60° YY)
RELAY (a) (ORD. 60° YY)
(90° YΔ)
$E_{\beta-fo}$
V_β LOCUS
RELAY (c) (ALT. 60° ΔΔ)
$-\frac{1}{\sqrt{3}}V_\alpha$
I_β LOCUS
RELAY (b) (ORD. 90° ΔY)
(60° ΔΔ)
$-V_\alpha$
RELAY (c) (90° YΔ)
$-\sqrt{3}V_\alpha$

Figure 8. Maximum ranges of equiprojection-line angular variation on a line-to-line fault as influenced by fault or network resistances

The arcs with arrowheads indicate the maximum angular range of the equiprojection lines of each relay of the connections listed in Table III. Limitations as to the allowable variation of the network constants are $Z_Z=0$ and angle of Z_{Rf} = angle of Z_1

terms in the general expressions would not be zero and, since these components of torque are of the same sign for both relays, better behavior of the two relays would be obtained.

THE 60-DEGREE DELTA-VOLTAGE DELTA-CURRENT CONNECTION

Relay *a* for the delta-voltage delta-current connection behaves in much the same way as relay *a* for the ordinary 90-degree connection, the chief difference being that for the former there is no zero-sequence torque term. In some ways this is an advantage, because the operation of the relay is independent of the zero-sequence network, except as a secondary effect arising because of modifications in the current I_α. On the other hand, this relay connection would give operation more subject to the influence of normal load current, because it operates on V_β and I_α only. Normal load current reduces to equal components of I_α and I_β in time quadrature with each other.

Relay *b* behaves in much the same way as relay *a*. In general I_α and V_α will both lie in the fourth quadrant, and the torque represented by the first term will be positive and add to that of the second. For both relays the relay angle ϕ should be positive, perhaps about 45 degrees for usual network conditions.

THE 60-DEGREE ALTERNATE DELTA-VOLTAGE DELTA-CURRENT CONNECTION

Comparing the volt-ampere expressions for this connection with those for the 60-degree delta-voltage delta-current connection, the signs are opposite for those terms involving the voltage V_β. Thus, the two relays tend to function oppositely on line-to-ground faults.

Relay *c* is actuated by two opposing torques, since the two terms will, in general, represent torque of opposite sign. The absolute values of the two vector volt-ampere components are of comparable magnitude, and incorrect operation of this relay is quite likely to occur. This connection is definitely inferior to the 60-degree delta-voltage delta-current connection.

Relay Operation on a Line-to-Line Fault

Circle diagrams for the line-to-line type of fault are quite similar to those for the line-to-ground fault, although somewhat simpler, because they do not involve the zero-sequence network. Since, in the equivalent representation of Figure 2*d*, the α network is open, the voltage V_α is constant. The voltage V_β follows the locus circle which has $E_{\beta-fo}$ as the diameter.

When the conditions for a line-to-line fault are substituted into the general volt-ampere expressions of Table II, each reduces to two terms. By factoring out the proper numerical factor, the coefficients of the terms involving V_β are reduced to unity, and the coefficients of the terms involving V_α to either $\sqrt{3}$ or $1/\sqrt{3}$. Since V_α is constant and equal to $E_{\alpha-fo}$ (except as influenced by normal load current), this procedure aids in the comparison of the various terms. The simplified expressions are given in Table III.

For the relay to operate correctly, the sum of the two torque components must be positive. Since the same current is involved in both components, it is necessary only that the algebraic sum of the projections of the two voltages on the current vector be positive to meet this condition.

To ascertain the effect of the possible current and voltage variations on the relay torque, the equiprojection-line method for the determination of relay behavior may be used to advantage. The equiprojection line represents that phase position of the current where the projections of the two voltages on the current vector are equal but of opposite sign. For any two given voltages, the equiprojection line is drawn from the origin perpendicular to the line which joins the ends of the two voltage vectors after one has been rotated through 180 degrees. Since the effect of the relay angle ϕ may be included by rotation of the equiprojection line through the angle ϕ, the maximum angular variations of the equiprojection lines may be studied independent of the relay angle. The proper value of the relay angle for any one of the different connections is such that the relay will yield positive torque for the range of currents most frequently encountered in actual power systems.

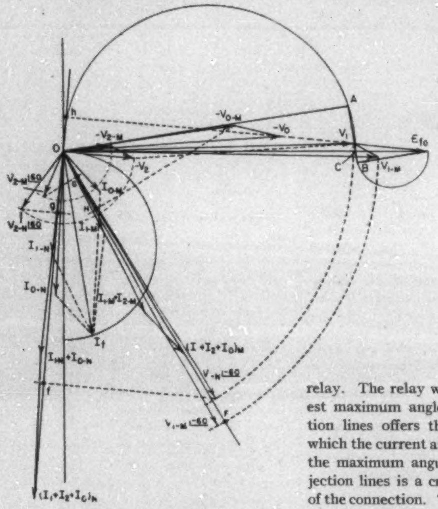


Figure 9. Illustrative example of the graphical determination of directional-relay behavior showing the operation of the ordinary 60-degree connection on a line-to-ground fault

For the case where the impedance $\boldsymbol{Z}_{\Sigma}$ is zero and the impedances $\boldsymbol{Z}_{Rf}$ and $\boldsymbol{Z}_1$ have the same angle, the maximum angular ranges of the equiprojection lines may be obtained from Figure 8. For this case, the current vector must lie either in the first or fourth quadrant, but usually in the fourth. The positions of the equiprojection lines indicated are the limiting positions, the angular ranges being indicated by arrows. The dotted construction lines show how the equiprojection lines were located. When just one voltage is involved in a relay volt-ampere expression, the equiprojection line is perpendicular to that voltage. The current angular range for correct relay operation is 180 degrees lagging the equiprojection line. Since the relay angle must be chosen such that the relay current will give positive torque, the angular range of the equiprojection line must be subtracted from 180 degrees to give the range of current variation for correct operation of the relay. The relay which yields the smallest maximum angle between equiprojection lines offers the largest range over which the current angle may vary. Thus, the maximum angular range of equiprojection lines is a criterion of the quality of the connection. The maximum angular ranges as determined from Figure 8 for the above-mentioned network conditions are given in the first column of Table A.

The figures of this table indicate that the three ordinary connections, the alternate 60-degree connection, and the 60-degree delta-voltage delta-current connection are the best. The latter connection gives very satisfactory operation on line-to-ground faults but is somewhat inferior on line-to-line faults. The converse is true for the alternate 60-degree connection.

When the restriction is removed that the impedances $\boldsymbol{Z}_{Rf}$ and $\boldsymbol{Z}_1$ have the same angle, the voltage $\boldsymbol{V}_\beta$ may lie anywhere within a semicircle above $\boldsymbol{E}_{\beta-f_0}$ and a semicardioid below $\boldsymbol{E}_{\beta-f_0}$. When the other restriction, that the impedance $\boldsymbol{Z}_{\Sigma}$ is zero, is removed, the maximum angular ranges are still further increased. The voltage $\boldsymbol{V}_\beta$ may then lie anywhere within a semicardioid below $\boldsymbol{E}_{\beta-f_0}$ in the fourth quadrant and a semicardioid above $\boldsymbol{E}_{\beta-f_0}$ in the first and second quadrants. The maximum angular ranges of the equiprojection lines are increased as shown in the table.

From the preceding considerations, the ordinary 90-degree connection is superior to any of the others. However, most of the connections are not so bad as they appear from the equiprojection-line angular variations, since these ranges are possible only for conditions of extreme dissymmetry which do not frequently occur in actual power networks.

Conclusions

Only three of the 12 relay connections which utilize wye or delta voltages and wye or delta currents seem to have definite merits for directional-relay applications. These three connections are the ordinary 60-degree (wye voltages and wye currents), the ordinary 90-degree (delta voltages and wye currents), and the ordinary 30-degree (delta voltages and wye currents). While it is not possible to state definitely which of these three connections is best for all possible faults, it would seem that the ordinary 90-degree connection is somewhat more free from disturbing influences than the other two. None of the twelve connections will give relay operation essentially independent of the type of fault as is the case with the 90-degree delta-voltage delta-current connection when applied to distance relaying.

For all the various connections, normal load current is the most disturbing influence with regard to correct relay operation. In many instances it may be impossible to obtain satisfactory results on ground faults without the use of "ground relays" which operate on negative- or zero-sequence quantities only. Some of the other factors besides the type of relay connection and normal load current in the system which influence directional relay operation are:

1. Type of fault.
2. Different angles in the positive-, negative-, and zero-sequence impedances as viewed from the fault point in the same direction from the fault.
3. Different angles in the impedances as viewed from the fault point in opposite directions in either sequence network.
4. Impedance between the relay and point of fault, particularly if the angle of this impedance is different from that of the total impedance as viewed from the point of fault.
5. Fault resistance.
6. Mutual impedance between the two parts of the system on opposite sides of the fault in networks of like sequence. Coupling of this nature is present in all except pure radial power networks.

Table A

Relay Connections	Maximum Angular Range of Equiprojection Lines (Degrees)		
	Network Conditions and Restrictions		
	$Z_s=0$ Angle of Z_{Rf} = Angle of Z_1	$Z_s=0$	None
Ordinary 60-degree	60	82	127
Alternate 60-degree	80	108	116
Ordinary 90-degree	60	66	74
Ordinary 30-degree	90	180	270
Alternate 30-degree	180	180	270
90-degree wye-voltage delta-current	120	130	130
60-degree delta-voltage delta-current	115	180	270
Alternate 60-degree delta-voltage delta-current	180	180	270

While most of the factors are not of sufficient importance to cause incorrect relay operation if only one influence were present, they are cumulative in the sense that several of these factors present at the same time may very easily result in incorrect directional relay operation.

Appendix A. Derivation of the Circle Diagram for the Line-to-Ground Fault in Terms of Symmetrical Components

From the conditions existing at the point of fault:

$$\boldsymbol{I}_f=\frac{\boldsymbol{E}_{f0}}{\boldsymbol{Z}_1+\boldsymbol{Z}_2+\boldsymbol{Z}_0+3R_f}=\frac{E_{f0}}{R_1+R_2+R_0+3R_f+j(X_1+X_2+X_0)} \quad (1)$$

Letting the subscripts x and y indicate components of $\boldsymbol{I}_f$ in the X and Y directions respectively, one obtains by cross-multiplying and equating the reals and imaginaries

$$I_x(R_1+R_2+R_0+3R_f)-I_y(X_1+X_2+X_0)=E_{f0} \quad (2)$$

$$I_x(X_1+X_2+X_0)+I_y(R_1+R_2+R_0+3R_f)=0 \quad (3)$$

Elimination of R_f from these two equations gives the current-locus equation:

$$I_x^2+\left[I_y+\frac{E_{f0}}{2(X_1+X_2+X_0)}\right]^2=\left[\frac{E_{f0}}{2(X_1+X_2+X_0)}\right]^2 \quad (4)$$

This equation defines a circle which passes through the origin. The center is on the Y axis at the point

$$\left[0,\frac{-E_{f0}}{2(X_1+X_2+X_0)}\right]$$

The positive-sequence voltage $\boldsymbol{V}_1$ is defined by the equation

$$\boldsymbol{V}_1=\boldsymbol{E}_{f0}-\boldsymbol{I}_1\boldsymbol{Z}_1 \quad (5)$$

or

$$(V_{1-x}+jV_{1-y})=E_{f0}-\frac{E_{f0}(R_1+jX_1)}{(R_1+R_2+R_0+3R_f)+j(X_1+X_2+X_0)} \quad (6)$$

Cross-multiplying, equating the reals and imaginaries, and eliminating R_f from the resultant two equations gives the locus equation of the positive-sequence voltage.

$$\left[V_{1-x}-E_{f0}\frac{X_1+2X_2+2X_0}{2(X_1+X_2+X_0)}\right]^2+\left[V_{1-y}-E_{f0}\frac{R_1}{2(X_1+X_2+X_0)}\right]^2=E_{f0}^2\left[\frac{R_1^2+X_1^2}{4(X_1+X_2+X_0)^2}\right]=\left[E_{f0}\frac{Z_1}{2(X_1+X_2+X_0)}\right]^2 \quad (7)$$

This equation defines a circle with center at the point

$$\left[\frac{E_{f0}(X_1+2X_2+2X_0)}{2(X_1+X_2+X_0)},\frac{E_{f0}R_1}{2(X_1+X_2+X_0)}\right]$$

and of radius

$$\frac{E_{f0}Z_1}{2(X_1+X_2+X_0)}$$

It intersects the X axis at the two points

$[E_{f0}, 0]$

and

$$\left[\frac{E_{f0}(X_2+X_0)}{X_1+X_2+X_0},0\right]$$

These two intercepts, being independent of R_1, R_2, and R_0, depend only upon the *reactances* of the network.

It is more convenient to treat the vector sum of the negative- and zero-sequence voltages as a single voltage. The separate components may then be obtained easily after the diagram is thus constructed. For convenience, let

$$(-\boldsymbol{V}_2-\boldsymbol{V}_0)=\boldsymbol{V}=\boldsymbol{I}_f(\boldsymbol{Z}_2+\boldsymbol{Z}_0) \quad (8)$$

or

$$(V_x+jV_y)=\frac{E_{f0}[R_2+R_0+j(X_2+X_0)]}{R_1+R_2+R_0+3R_f+j(X_1+X_2+X_0)} \quad (9)$$

Cross-multiplying, equating the reals and imaginaries, and eliminating R_f from the resultant two equations gives the locus equation of the negative of the vector sum of the negative- and zero-sequence voltages.

$$\left[V_x-E_{f0}\frac{X_2+X_0}{2(X_1+X_2+X_0)}\right]^2+\left[V_y+E_{f0}\frac{R_2+R_0}{2(X_1+X_2+X_0)}\right]^2=E_{f0}^2\left[\frac{(R_2+R_0)^2+(X_2+X_0)^2}{4(X_1+X_2+X_0)^2}\right]=\left[E_{f0}\frac{|\boldsymbol{Z}_2+\boldsymbol{Z}_0|}{2(X_1+X_2+X_0)}\right]^2 \quad (10)$$

This equation defines a circle with center at the point

$$\left[\frac{E_{f0}(X_2+X_0)}{2(X_1+X_2+X_0)},\frac{-E_{f0}(R_2+R_0)}{2(X_1+X_2+X_0)}\right]$$

and of radius $\dfrac{E_{f0}|\boldsymbol{Z}_2+\boldsymbol{Z}_0|}{2(X_1+X_2+X_0)}$

It intersects the X axis at the two points

[0, 0]

and

$$\left[\frac{E_{f0}(X_2+X_0)}{2(X_1+X_2+X_0)},0\right]$$

which, being independent of R_1, R_2, and R_0, depend only upon the *reactances* of the network.

Simultaneous solution of the two voltage locus equations—the equation of $\boldsymbol{V}_1$ and the equation of $(-\boldsymbol{V}_2-\boldsymbol{V}_0)$—gives the points of intersection of the two circles

$$\left[\frac{E_{f0}[(X_1+X_2+X_0)(X_2+X_0)+(R_1+R_2+R_0)(R_2+R_0)]}{(R_1+R_2+R_0)^2+(X_1+X_2+X_0)^2},\frac{E_{f0}[(R_1+R_2+R_0)(X_2+X_0)-(X_1+X_2+X_0)(R_2+R_0)]}{(R_1+R_2+R_0)^2+(X_1+X_2+X_0)^2}\right]$$

and

$$\left[\frac{E_{f0}(X_2+X_0)}{X_1+X_2+X_0},0\right]$$

The second point is the point of intersection of the two voltage locus equations with the X axis. Since this point is determined only by the reactances of the three sequence networks, it is independent of the resistances of the networks, as well as the fault resistance. As the resistances of the sequence networks approach zero, the two points of intersection approach each other, and the two locus circles approach tangency. The second point is always on the X axis, while the first may be either above or below the X axis depending upon the relative values of the sequence-network resistances and reactances. The first point represents conditions on the loci when the fault resistance is zero.

The derivation of the circle diagram for the line-to-line fault in terms of symmetrical components, and the derivation of circle diagrams for both types of faults in terms of modified symmetrical components follow closely the method used for the derivation of the circle diagram of the line-to-ground fault. All are based upon the circuit diagrams illustrated in Figure 2.

Appendix B. Illustrative Example of the Graphical Determination of Directional-Relay Behavior

To illustrate the use of circle diagrams and symmetrical components for the determination of directional-relay behavior, the following example is given in which the impedances of the equivalent-network representation of Figure 1 are per-unit values taken from an actual power network.

$\boldsymbol{Z}_{1-z} = \boldsymbol{Z}_{2-z} = 0.020\underline{|90.0}$ $\quad\boldsymbol{Z}_{0-z} = 0.240\underline{|90.0}$

$\boldsymbol{Z}_{1-X} = \boldsymbol{Z}_{2-X} = 0.128\underline{|70.5}$ $\quad\boldsymbol{Z}_{0-X} = 0.214\underline{|52.6}$

$\boldsymbol{Z}_{1-Rf} = \boldsymbol{Z}_{2-Rf} = 0.097\underline{|34.0}$ $\quad\boldsymbol{Z}_{0-Rf} = 0.238\underline{|32.0}$

$\boldsymbol{Z}_{1-M} = \boldsymbol{Z}_{2-M} = 0.214\underline{|54.9}$ $\quad\boldsymbol{Z}_{0-M} = 0.489\underline{|40.8}$

$\boldsymbol{Z}_{1-N} = \boldsymbol{Z}_{2-N} = 0.188\underline{|88.5}$ $\quad\boldsymbol{Z}_{0-N} = 0.188\underline{|88.5}$

$\boldsymbol{Z}_{1-y} = \boldsymbol{Z}_{2-y} = 0.188\underline{|88.5}$ $\quad\boldsymbol{Z}_{0-y} = 0.188\underline{|88.5}$

$\boldsymbol{Z}_{1} = \boldsymbol{Z}_{2} = 0.124\underline{|75.5}$ $\quad\boldsymbol{Z}_{0} = 0.383\underline{|84.6}$

$\boldsymbol{Z}_1+\boldsymbol{Z}_2+\boldsymbol{Z}_0 = 0.629\underline{|81.0} = 0.098+j0.621$

$\boldsymbol{Z}_2+\boldsymbol{Z}_0 = 0.505\underline{|82.4} = 0.067+j0.501$

Type of fault: Line-to-ground
Relay connection: Ordinary 60-degree
Relay angle ϕ: 0 degrees

Construction of the Circle Diagram

The purpose of the circle diagram is to obtain the relative magnitude and phase of each of the sequence voltages and currents at the relay in the form of a vector diagram. The direction of the relay torque then is found by graphical evaluation of the torque components represented by the terms of the volt-ampere expressions of Table I.

In Figure 9, the vector $\boldsymbol{E}_{f0}$ is laid off horizontally to scale, and the point C located from the ratio $OC/OE_{f0} = (X_2+X_0)/(X_1+X_2+X_0) = 0.501/0.621 = 0.806$. The center of the fault current-locus circle is on the vertical axis below O by the amount $E_{f0}/[2\times(X_1+X_2+X_0)] = 0.805$. This point may be taken at any convenient distance below O, and the actual current scale left undetermined unless the current magnitude is required for other purposes.

The fault current vector $\boldsymbol{I}_f$ is drawn lagging $\boldsymbol{E}_{f0}$ by 81.0 degrees, the angle of the impedance of the three sequence networks in series. The lines OA and BE_{f0} are perpendicular to the fault current, and the line ACB is parallel to the fault current. The line $E_{f0}V_1$ is drawn at 75.5 degrees with AB to locate the end of the positive-sequence voltage vector $\boldsymbol{V}_1$. Since the fault resistance is zero, this point also represents the terminus of the vector sum of the negative- and zero-sequence voltages $-\boldsymbol{V}_2$ and $-\boldsymbol{V}_0$.

The voltages $-\boldsymbol{V}_2$ and $-\boldsymbol{V}_0$ are drawn leading the fault current by 75.5, and 84.6 degrees respectively. The length of these vectors are such that they add vectorially to give the vector $\boldsymbol{V}_1$.

The current components $\boldsymbol{I}_{2-M}$, and $\boldsymbol{I}_{2-N}$ (equal to $\boldsymbol{I}_{1-M}$ and $\boldsymbol{I}_{1-N}$ respectively) are obtained most easily from the magnitude ratios $I_{2-M} = (0.188/0.385)I_f = 0.488I_f$ and $I_{2-N} = (0.214/0.385)I_f = 0.556I_f$ from which the corresponding current parallelogram is constructed. Likewise, the zero-sequence current components are obtained from the magnitude ratios $I_{0-M} = (0.188/0.632)I_f = 0.298I_f$ and $I_{0-N} = (0.489/0.632)I_f = 0.772I_f$.

The sequence voltages at M are found by adding the corresponding impedance drops to the sequence voltages at the point of fault. The positive-sequence voltage drop is $|\boldsymbol{I}_{1-M}|(0.097)$. This drop is added to $\boldsymbol{V}_1$ at the angle 34.0 degrees leading the current $\boldsymbol{I}_{1-M}$ to give the voltage $\boldsymbol{V}_{1-M}$. Since $\boldsymbol{I}_{1-M}$ is equal to $\boldsymbol{I}_{2-M}$, the same voltage drop in both magnitude and phase is subtracted from $-\boldsymbol{V}_2$ to give the voltage $-\boldsymbol{V}_{2-M}$. The zero-sequence voltage drop is $|\boldsymbol{I}_{0-M}|(0.238)$. This drop is subtracted from $-\boldsymbol{V}_0$ to give the voltage $-\boldsymbol{V}_{0-M}$.

Graphical Determination of the Direction of Relay Torque

The method for the graphical determination of the direction of relay torque is applicable to any case whether the sequence voltages and currents are obtained from the circle diagram, by calculation, or by use of the network analyzer. Once the vector diagram of the sequence quantities is constructed to scale, the direction of relay torque is obtained by graphical evaluation of the torque represented by the terms of the volt-ampere expressions of Table I for the relay and type of connection considered.

For the ordinary 60-degree connection, the relay most affected by a line-to-ground fault is relay *c*. The volt-ampere expression for this relay (see Table I) may be written (conj $\boldsymbol{V}_1$) $\underline{|60}(\boldsymbol{I}_1+\boldsymbol{I}_2+\boldsymbol{I}_0)-$(conj $\boldsymbol{V}_2$) $\underline{|120}(\boldsymbol{I}_1+\boldsymbol{I}_2+\boldsymbol{I}_0)-$(conj$\boldsymbol{V}_0$)$\underline{|0}$ $(\boldsymbol{I}_1+\boldsymbol{I}_2+\boldsymbol{I}_0)$. Thus, the three sequence currents at the relay may be combined and the resultant treated as a common reference current for the determination of the behavior of the relay. With reference to Figure 9, the voltage $\boldsymbol{V}_{1-M}$ is rotated clockwise through 60 degrees and then projected on the current vector $(\boldsymbol{I}_1+\boldsymbol{I}_2+\boldsymbol{I}_0)_M$. Likewise, the voltage $-\boldsymbol{V}_{2-M}$ rotated clockwise through 120 degrees, and the voltage $-\boldsymbol{V}_{0-M}$ are projected on the same current vector. This process gives the three line segments *OF*, *OG*, and *OH* which are proportional in length to the components of relay torque represented respectively by the $\boldsymbol{V}_1$, $\boldsymbol{V}_2$, and $\boldsymbol{V}_0$ terms of the volt-ampere expression. Since the three line segments extend downward from O, each represents positive torque. The sum of the three is, therefore, positive, and correct relay operation is indicated.

The behavior of the relay at N is obtained by a similar process in which the current vector $(\boldsymbol{I}_1+\boldsymbol{I}_2+\boldsymbol{I}_0)_N$ and the voltages $\boldsymbol{V}_1$, $\boldsymbol{V}_2$, and $\boldsymbol{V}_0$ are used. The three components of torque are represented by the line segments *Of*, *Og*, and *Oh*. In this case, *Oh* represents negative torque. The resultant of all three however is positive, so that correct operation of the relay at N also is indicated.

Notation

$\boldsymbol{E}_{f0}$—Positive-sequence voltage at the point of fault before occurrence of the fault

$\boldsymbol{E}_{\alpha-f0}$, $\boldsymbol{E}_{\beta-f0}$—$\alpha$ and β components of the voltage at the point of fault before the occurrence of the fault

$\boldsymbol{I}_f$—Total fault component of current at the fault

$\boldsymbol{I}_1$, $\boldsymbol{I}_2$, $\boldsymbol{I}_0$—Positive-, negative-, and zero-sequence components of current at the relay, or in the fault if so specified

$\boldsymbol{I}_\alpha$, $\boldsymbol{I}_\beta$, $\boldsymbol{I}_0$—α, β, and zero components of current at the relay, or in the fault if so specified

R_1, R_2, R_0—Resistance components of $\boldsymbol{Z}_1$, $\boldsymbol{Z}_2$, and $\boldsymbol{Z}_0$

$\boldsymbol{V}_1$, $\boldsymbol{V}_2$, $\boldsymbol{V}_0$—Positive-, negative-, and zero-sequence components of voltage at the point of fault, or at the relay if so specified

conj $\boldsymbol{V}$—Conjugate of the voltage vector $\boldsymbol{V}$

X_1, X_2, X_0—Reactance components of $\boldsymbol{Z}_1$, $\boldsymbol{Z}_2$, and $\boldsymbol{Z}_0$

$\boldsymbol{Z}_1$, $\boldsymbol{Z}_2$, $\boldsymbol{Z}_0$—Total equivalent impedance of the positive-, negative-, and zero-sequence networks as measured at the point of fault

θ_1, θ_2, θ_0—Angles of the impedances $\boldsymbol{Z}_1$, $\boldsymbol{Z}_2$, and $\boldsymbol{Z}_0$

ϕ—The "relay angle"; that is, the angle between the current and voltage applied to the relay for which given magnitudes of these quantities cause maximum torque to be developed. The angle is positive if the current leads the voltage for this condition

When necessary to distinguish between components of current or voltage at different points in the network, subscripts based upon Figure 1b are used. Thus, $\boldsymbol{I}_{1-M}$ is the positive-sequence component of current at point M and $\boldsymbol{V}_{1-M}$ is the corresponding component of voltage.

Impedances of the different parts of the network are designated by the subscripts indicated in Figure 1b. Thus, $\boldsymbol{Z}_{1-Rf}$ is the impedance between the relay and point of fault in the positive-sequence network.

WYE AND DELTA CURRENTS AND VOLTAGES

$\boldsymbol{I}_A$, $\boldsymbol{I}_B$, $\boldsymbol{I}_C$—*Delta* currents

$\boldsymbol{I}_a$, $\boldsymbol{I}_b$, $\boldsymbol{I}_c$—Line or *wye* currents

$\boldsymbol{V}_A$, $\boldsymbol{V}_B$, $\boldsymbol{V}_C$—Line-to-line or *delta* voltages

$\boldsymbol{V}_a$, $\boldsymbol{V}_b$, $\boldsymbol{V}_c$—Line-to-neutral or *wye* voltages

$\boldsymbol{V}_A = \boldsymbol{V}_c - \boldsymbol{V}_b \qquad \boldsymbol{I}_A = \boldsymbol{I}_c - \boldsymbol{I}_b$

$\boldsymbol{V}_B = \boldsymbol{V}_a - \boldsymbol{V}_c \qquad \boldsymbol{I}_B = \boldsymbol{I}_a - \boldsymbol{I}_c$

$\boldsymbol{V}_C = \boldsymbol{V}_b - \boldsymbol{V}_a \qquad \boldsymbol{I}_C = \boldsymbol{I}_b - \boldsymbol{I}_a$

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The Combination of Probability Curves in Engineering

ROGER I. WILKINSON
ASSOCIATE AIEE

1. Introduction

ENGINEERS are quite familiar with simple probability distributions showing the relative likelihoods of occurrence of the different values which a varying physical or electrical quantity may assume. As the complexities of machines and circuits in modern design increase, it becomes highly desirable to be able to estimate the probability distribution resulting from the joint presence of two or more sources of variation. This paper will undertake to present something of the theory of making such combinations, and its application to a number of practical examples.

2. Simple Probability Distributions

If a variable quantity x may take only values which are separated by finite intervals, it is known as a *discrete* variate. If there is no such restriction on the variable it is a *continuous* variate. We shall let p_x be the probability that the discrete variate takes the value x, and $\theta(r)dr$ be the probability that the continuous variate lies between r and $r+dr$.

To summarize in short compass the salient characteristics of any variate, discrete or continuous, it is convenient and customary to calculate the moments of its probability distribution. The ith moments taken about the zero of the variates are usually designated by μ_i', and are given by:

Discrete

$$\mu_i' = \sum_{x=\text{all values}} x^i p_x$$

Continuous

$$\mu_i' = \int_{r=\text{all values}} r^i\theta(r)dr \qquad (1)$$

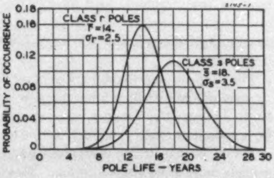

Figure 1. Two "mutually exclusive" universes

Likewise the ith moments taken about the average value ($\bar{x}$ or $\bar{r}$) of the variates are designated by μ_i, and are given by:

$$\left.\begin{aligned}\mu_i &= \sum_{x=\text{all values}} (x-\bar{x})^i p_x\\ \mu_i &= \int_{r=\text{all values}} (r-\bar{r})^i\theta(r)dr\end{aligned}\right\} \qquad (2)$$

Certain transfer formulas are available relating the μ_i''s and the μ_i's of the first four moments; they are often of value since it may be more convenient first to calculate from data or theory the moments about one of the two reference points (0 or the mean), and then obtain the other if desired. These formulas are:

$$\left.\begin{aligned}\mu_0 &= \mu_0' = 1\\ \mu_1 &= 0\\ \mu_2 &= \mu_2' - (\mu_1')^2\\ \mu_3 &= \mu_3' - 3\mu_2'\mu_1' + 2(\mu_1')^3\\ \mu_4 &= \mu_4' - 4\mu_3'\mu_1' + 6\mu_2'(\mu_1')^2 - 3(\mu_1')^4\end{aligned}\right\} \qquad (3)$$

A closely related set of descriptive functions which we shall want to use are known as semi-invariants. If the ith semi-invariant be designated λ_i, then

$$\left.\begin{aligned}\lambda_0 &= \mu_0' = \mu_0 = 1\\ \lambda_1 &= \mu_1'\\ \lambda_2 &= \mu_2\\ \lambda_3 &= \mu_3\\ \lambda_4 &= \mu_4 - 3\mu_2^2\end{aligned}\right\} \qquad (4)$$

The 0th moment as seen by equations 1 is merely the sum of all values of the probability function and by definition must equal unity.

The first moment about zero, μ_1', of a variate is its *arithmetic mean* or average value $\bar{x}$ or $\bar{r}$; it thus indicates the general position of the variate along the scale of measurement.

The second moment μ_2 of a variate about its mean is a measure of the dispersion of the distribution. μ_2 is known as the *variance*, $\sigma=\sqrt{\mu_2}$ is called the *standard deviation.*

The third moment about the mean is a measure of the asymmetry of a variate's distribution; the asymmetry or *skewness* is defined as

$$k = \frac{\mu_3}{\mu_2^{3/2}} = \frac{\mu_3}{\sigma^3} \qquad (5)$$

The fourth moment about the mean is a measure of the length of the tails and the peakedness of the mode of a distribution. This gives rise to the parameter known as the *excess* or *kurtosis* of a distribution; it is defined as

$$\beta_2 = \frac{\mu_4}{\mu_2^2} = \frac{\mu_4}{\sigma^4} \qquad (6)$$

If the first few moments (or their associated parameters, mean, standard deviation, and so forth) serve to describe a probability distribution with considerable accuracy, then two distributions with a number of identical corresponding moments should closely resemble one another. To fit (or smooth) an observed probability distribution by an appropriate analytical curve form with unspecified constants, we first determine the lower moments of the analytical curve in terms of these constants. Then equating corresponding theoretical and observed moments gives relationships from which the constants of the fitting curve can be calculated. As many moments must be equated as there are unknown constants to be determined. For example, the value of the first moment or arithmetic mean a in the Poisson fitting distribution, $a^x e^{-a}/x!$ would be set equal to the observed mean of a set of data which this type of curve was appropriate to fit.

A variate's true distribution, if we could but determine it, would, except for rare instances, have a smooth unimodal form. Hence it will usually be desirable to smooth by the method of moments or in some other fashion a variate's rough observed distribution before entering upon its combination with other variates.

3. Laws of Combining Probabilities

Two or more events may occur independently, they may be mutually exclusive, or they may be correlated. If two events are independent, and the probability in favor of the first event is p_1, and in favor of the second is p_2, then the probability they will both occur is the *product* of their individual probabilities, p_1p_2. If either event a, or event b, or event c, and so forth, can happen, and the occurrence of one precludes the happening of another, they are known as mutually exclusive events. If events $a, b, c, d\ldots$ are mutually exclusive, the probability that *either a or b*, for instance, will occur is the *sum* of their individual probabilities,

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ROGER I. WILKINSON is a member of the technical staff, probability research department, Bell Telephone Laboratories, Inc., New York, N. Y.

$p_a + p_b$. Correlated events lie between the limiting cases of independent and mutually exclusive events, and the laws of combining their probabilities are determined by the amount and kind of dependence between them.

4. Combining Variates With Mutually Exclusive Probabilities

When two or more mutually exclusive variates are combined, the result is a "mingled" universe made up of variate r (with probability distribution p_r or $\theta(r)dr$) h proportion of the time, and variate s (with probability distribution q_s or $\phi(s)ds$) j proportion of the time.... The distribution functions of the resultant z of such mutually exclusive variates are:

Discrete Variates

$$F(z)=hp_z+jq_z+\ldots$$

Continuous Variates

$$F(z)dz=h\theta(z)dz+j\phi(z)dz+\ldots \qquad (7)$$

The moments of the resultant z taken about zero are readily expressed in terms of the moments of the component variates, as

$$\left.\begin{array}{l} \mu_{1z}'=h\mu_{1r}'+j\mu_{1s}'+\ldots \\ \mu_{2z}'=h\mu_{2r}'+j\mu_{2s}'+\ldots \\ \ldots\ldots\ldots\ldots\ldots\ldots \\ \mu_{tz}'=h\mu_{tr}'+j\mu_{ts}'+\ldots \end{array}\right\} \qquad (8)$$

These equations permit us to express the parameters of z in terms of the parameters of its components as follows:

$$\left.\begin{array}{l} \bar{z}=h\bar{r}+j\bar{s}+\ldots \\ \sigma_z=[h(\sigma_r^2+\bar{r}^2)+j(\sigma_s^2+\bar{s}^2)+\ldots \\ \qquad -\bar{z}^2]^{1/2} \\ k_z=\dfrac{1}{\sigma_z^3}\,[h(k_r\sigma_r^3+3\sigma_r^2\bar{r}+\bar{r}^3)+ \\ \quad j(k_s\sigma_s^3+3\sigma_s^2\bar{s}+\bar{s}^3)+\ldots- \\ \qquad (3\sigma_z^2\bar{z}+\bar{z}^3)] \\ \beta_{2z}=\dfrac{1}{\sigma_z^4}\,[h(\beta_{2r}\sigma_r^4+4k_r\sigma_r^3\bar{r}+6\sigma_r^2\bar{r}^2+ \\ \bar{r}^4)+j(\beta_{2s}\sigma_s^4+4k_s\sigma_s^3\bar{s}+6\sigma_s^2\bar{s}^2+\bar{s}^4)+ \\ \qquad \ldots-(4k_z\sigma_z^3\bar{z}+6\sigma_z^2\bar{z}^2+\bar{z}^4)] \end{array}\right\} \qquad (9)$$

Example—Length of Life of Outside Plant

A public utility serving a given area uses two kinds of poles in its outside plant. One kind, r, has an average life of 14 years with a standard deviation of 2.5 years, and the other, s, has an average life of 18 years with a standard deviation of 3.5 years. As shown in Figure 1 variations about the average life are substantially according to the normal law

$$f(x)=\frac{1}{\sigma_x\sqrt{2\pi}}\,e^{-\frac{(x-\bar{x})^2}{2\sigma_x^2}}$$

If approximately 20 per cent of the poles in the plant are of the r type, what is the distribution of life lengths on which the accounting department may base its pole depreciation rate?

Solution. The distribution of the overall life length will be, according to equation 7,

$$F(z)=0.20\,\frac{1}{\sigma_r\sqrt{2\pi}}\,e^{-\frac{(z-\bar{r})^2}{2\sigma_r^2}}+ \quad 0.80\,\frac{1}{\sigma_s\sqrt{2\pi}}\,e^{-\frac{(z-\bar{s})^2}{2\sigma_s^2}}$$

$$=\frac{0.20}{2.5\sqrt{2\pi}}\,e^{-\frac{(z-14)^2}{2(2.5)^2}}+ \quad \frac{0.80}{3.5\sqrt{2\pi}}\,e^{-\frac{(z-18)^2}{2(3.5)^2}}$$

This distribution is shown in Figure 2 and is, of course, merely the weighted sum of the ordinates of the two mutually exclusive components. The parameters of this composite distribution are readily found from equations 9. For example, the average length of life of the whole pole plant is

$$\bar{z}=0.20(14)+0.80(18)=17.2 \text{ years}$$

and the standard deviation of the universe of individual pole lives is

$$\sigma_z=[0.20(2.5^2+14^2)+0.80(3.5^2+18^2)-17.2^2]^{1/2}=3.69 \text{ years}$$

5. Combining Independent Variates

In order to determine the resultant of any chosen values of two or more independent variates, the process by which they combine must be specified. This is expressed analytically by the *equation of relation;* it is written

$$z=\xi(r,\ s,\ t\ \ldots) \qquad (10)$$

For a particular choice of r, s, t ... (denoted by subscripts) we have $z_1=\xi(r_1, s_1, t_1 \ldots)$. The probability of their joint occurrence is then, by section 3, the product of their individual probabilities of occurrence:

Discrete Variates

$$F(z_1)=p_rp_sp_t\ldots$$

Continuous Variates

$$F(z_1)dz=\theta(r_1)dr\phi(s_1)ds\psi(t_1)dt\ldots \qquad (11)$$

Equations 11 give the probability for obtaining z by just one particular selection from usually a number of mutually exclusive combinations of r, s, t ... which when substituted in equations 11 will produce the given value of $z=z_1$. Hence by the law of combining mutually exclusive events, the total probabilities of obtaining z_1 are

Discrete Variates

$$F(z_1)=\mathfrak{S}p_rp_sp_t\ldots$$

Continuous Variates

$$F(z_1)dz=\mathfrak{S}\theta(r)dr\phi(s)ds\psi(t)dt\ldots \qquad (12)$$

where the symbol $\mathfrak{S}$ indicates the summation of all eligible cases. Since z_1 is any value of z, equations 12 are the desired expressions for the probability distribution of z.

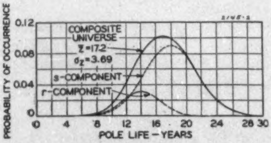

Figure 2. Composite universe formed from two "mutually exclusive" universes

5.1 Step-by-Step Combination of Independent Variates

It is clearly possible to obtain a distribution of the resultant $F(z)$, by writing out in tabular form all the ways of forming each value of z, then taking the products of the individual probabilities of occurrence for each way as in equations 11, and finally summing the probabilities according to equations 12. Such a procedure does not give the result in a general or analytical form for use in subsequent applications, but it does have the immediate advantage of providing the solution to a particular problem.

When more than two elemental variates are to be combined, it is nearly always possible first to combine two, then combine their resultant with the third elemental variate, and so on. The combination of any two variates is then done by means of a two-way or "square" table displaying the values of one variate (say s) with their corresponding probabilities of occurrence in the horizontal direction as column headings, and the values and probabilities of the other variate (say r) in the vertical direction, as line headings. An outline of this form is shown in Table I. There is no theoretical need that the range of the variate r represented by r_1 should equal the next range r_2, and the subsequent r ranges, or that the r ranges should be the same as the s ranges. Practically, however, such a selection is usually made.

For discrete variates the unit difference between successive r's and s's will generally be just one unit on the natural scale of measurement, such as one call, one object, one occurrence; otherwise the exactness of the solution will have been lost.

The step-by-step combination of continuous variates will seldom be wholly exact, because the variates' ranges must be broken down into a number of discrete intervals, each of which is represented by a single more or less typical value. However by taking these intervals smaller and

Table I

			s—Variate				
			s_1 $\phi(s_1)$	s_2 $\phi(s_2)$	s_3 $\phi(s_3)$	—	s_n $\phi(s_n)$
r—variate	r_1	$\theta(r_1)$	$\xi(r_1, s_1)=z_{11}$ $F(z_{11})=\theta(r_1)\cdot\phi(s_1)$	$\xi(r_1, s_2)=z_{12}$ $F(z_{12})=\theta(r_1)\cdot\phi(s_2)$	$\xi(r_1, s_3)=z_{13}$ $F(z_{13})=\theta(r_1)\cdot\phi(s_3)$	.	$\xi(r_1 s_n)=z_{1n}$ $F(z_{1n})=\theta(r_1)\cdot\phi(s_n)$
	r_2	$\theta(r_2)$	$\xi(r_2, s_1)=z_{12}$ $F(z_{21})=\theta(r_2)\cdot\phi(s_1)$	etc.	etc.	. . .	
	r_3	$\theta(r_3)$	$\xi(r_3, s_1)=z_{31}$ $F(z_{31})=\theta(r_3)\cdot\phi(s_1)$			. . .	
	.	.	.	.	.	. . .	.
	.	.	.	.	.	.	.
	.	.	.	.	.	.	.
	r_m	$\theta(r_m)$	$\xi(r_m, s_1)=z_{m1}$ $F(z_m)=\theta(r_m)\cdot\phi(s_1)$			. . .	$\xi(r_m, s_n)=z_{mn}$ $F(z_{mn})=\theta(r_m)\cdot\phi(s_n)$

smaller, the errors in the resultant may be reduced to any required amount.

In the small areas opposite each pair of r_i, s_j values in Table I are shown two entries. In the upper left corner is the value of z_{ij} which results from substituting r_i and s_j in the equation of relation $z_{ij}=\xi(r_i, s_j)$; it is, of course, the value of z which would occur if these particular values of r and s were present. In the lower right corner is entered the product of the two probabilities shown at the column and line headings, that is $\theta(r_i)\times\phi(s_j)$. Thus upon the completion of the table, all values of r have been combined according to the equation of relation with all values of s. If any restrictions are to be observed on the association of certain ranges of r with those of s, the proper areas are readily omitted from the table. If m classes of r and n classes of s are made, then we shall have mn z values each with its associated probability of occurrence.

The distribution $F(z)$ may now be formed in a manner identical with that used in grouping observed data, that is, class intervals are chosen, the proper values of z are assigned to each, and the probability of occurrence in each interval obtained by adding the probabilities for all of the z's falling therein. The cumulative distribution and the moments and parameters of z may then be calculated in the usual way if desired.

If the simple sum or difference of the r and s variates is to be found, dividing each into intervals of the same width as suggested above will expedite the working. In Table I all the values of a particular $z_w=r+s$ for the sum case (or $r-s$ for the difference case) will lie along one diagonal. The values of z_{w-1} will lie on the next diagonal above; the values of z_{w+1} on the next diagonal below and so on. Hence

1. There is no need to write in the z values in the body of the table.

2. The sum of the probabilities shown in any z diagonal is the total probability of occurrence of that value of z.

Each such sum may then be written conveniently at the edge of the square table opposite the end of its diagonal; this is the desired probability distribution.

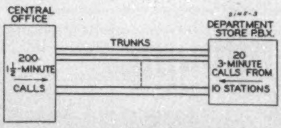

Figure 3. Trunking arrangement between PBX and central office

EXAMPLE—PBX TRUNK ENGINEERING

A group of trunks is to be provided for two-way use between a department store PBX (private branch exchange) as in Figure 3. There will be ten "outgoing" telephones at the store, originating on the average two three-minute trunk calls each in the busy hour. In addition, a sufficient number of stations will be provided at the store for incoming service only, to handle the 200 $1^1/_2$ minute customers' calls which are made in the average busy hour. How many trunks should be installed so that the customers may receive $P=0.01$ service?

Solution. Each of the 10 outgoing telephones will have a probability p of $\frac{2(3)}{60}=0.10$ of being in use at any random instant. The probability that exactly s will be so engaged is the binomial term

$$p_s=\phi(s)=\frac{n!}{s!(n-s)!}p^s(1-p)^{n-s}$$

where $n=10$ and $p=0.10$. Since a large number of sources will originate the incoming calls their distribution will be well represented by the Poisson expression

$$q_r=\theta(r)=\frac{a^r e^{-a}}{r!}$$

where

$a=$average simultaneous calls

$$=\frac{200(1^1/_2)}{60}=5.0$$

We now construct the square Table II, first calculating the various values of $\theta(r)$ and $\phi(s)$ from the above formulas to insert as column and line headings respectively. The s values have been reversed from the usual ascending tabular order so that the constant z diagonals will end at the right-hand edge of the table. The product of each pair of probabilities $\theta(r_i)\phi(s_j)$ is recorded in the body of the table and the diagonals summed as shown to give the desired $F(z)$ distribution. The cumulation of $F(z)$, designated $P(\geqq z)$, is also given.

From this last column we read the answer desired: To insure that the customers will not find the lines to the store busy oftener than 0.01 of the time, we must provide 13 trunks $[P(\geqq 13)=0.0083]$.

5.2 The Exact Analytical Combination of Independent Variates

If the probability distributions represented by the symbols p_r, p_s... and $\theta(r)dr$, $\phi(s)ds$ ..., in equations 12 are expressed as mathematic functions, it may well be that in numerous cases the summation operation indicated by $\mathfrak{S}$ can be performed analytically, thus giving the resultant $F(z)$ in similar analytical form. We must remember that $\mathfrak{S}$ represents a very special summation in which the relationships expressed by the equation of relation 10 are maintained. We may insure this by solving for one of the variates, say r, in equation 10, obtaining

$$\left.\begin{aligned} &r=f(z, s, t\ldots) \\ &\text{and} \\ &dr=\frac{df(z, s, t\ldots)}{dz}dz \end{aligned}\right\} \quad (13)$$

and substituting these in equations 12

Then the remaining independent variates may be summed over all ranges consistent with the value of s selected. Equations 12 then assume the completely definitized forms:

Discrete Variates

$$F(z)=\sum_{s}\sum_{t}\ldots\sum p_{f(z,\,s,\,t)}\ldots p_s p_t\ldots$$

Continuous Variates

$$\left.\begin{aligned}&F(z)dz=\\&dz\int_s\int_t\ldots\int\theta[f(z,s,t\ldots)]\times\\&\frac{d[f(z,s,t\ldots)]}{dz}\phi(s)ds\psi(t)dt\ldots\end{aligned}\right\}\quad(14)$$

5.21 Analytical Addition of Discrete Variates

The basic equation 14 for discrete variates can of course be applied whatever the equation of relation, and often its straightforward use is the simplest procedure. However for the addition, which is by far the commonest case, of discrete variates a method involving *frequency arrays* is found to be very powerful.

For our purpose, a frequency array is an expression containing the index A, which when *expanded in powers of* A, will give as the coefficient of A^r, the probability of occurrence of exactly r successes or events. Thus for the Poisson distribution the frequency array is $e^{m(A-1)}$, since, when expanded in powers of A, it yields the individual Poisson terms

$$e^{m(A-1)}=e^{-m}\left(1+mA+\frac{m^2}{2!}A^2+\frac{m^3}{3!}A^3+\ldots+\frac{m^r}{r!}A^r+\ldots\right)$$

We note that the probability of finding exactly r events occurring with an average occurrence of m is the coefficient of A^r, that is $m^re^{-m}/r!$

Secondly by substituting e^α for A, we obtain the "moment array" of the variate, which possesses the property that when it is expanded in *powers of* α, the successive moments of the distribution taken about zero are given as the coefficients of $\alpha, \frac{\alpha^2}{2!}, \frac{\alpha^3}{3!}\ldots.$ Moreover the moment array *with respect to the mean* instead of the origin of the variate can be written by adding the factor $e^{-\alpha(\text{mean})}$. Thus the Poisson moment array with respect to the mean would be $e^{-\alpha m}\times e^{m(e^\alpha-1)}$. Expanding we find

$$e^{m(e^\alpha-1-\alpha)}=1+m\frac{\alpha^2}{2!}+m\frac{\alpha^3}{3!}+(m+3m^2)\frac{\alpha^4}{4!}+\ldots$$

Table II. Step-by-Step Addition of Binomial and Poisson Variates

Number of Calls in Progress by Customers (Average = 5.0)		Number of Calls in Progress From Store Binomial: $\phi(s)=\{{n \atop s}\}p^s(1-p)^{n-s}$; n = 10, p = 0.1 (Average = 1.0)											Total Calls in Progress From Both Sources		
r	$\theta(r)=\frac{a^re^{-a}}{r!}$ $\theta(r)$	s = 10 $\phi(s)=$ —	s = 9 $10^{-9}\cdot9.000$	s = 8 $10^{-7}\cdot3.64501$	s = 7 $10^{-6}\cdot8.73803$	s = 6 0.000138	s = 5 0.001488	s = 4 0.011160	s = 3 0.057396	s = 2 0.193710	s = 1 0.387420	s = 0 0.348678	z = r + s	F(z)	$P(\geqq z)$
0	0.006738					0.000001	0.000010	0.000075	0.000387	0.001305	0.002610	0.002349	0	0.002349	0.999992
1	0.033690					0.000005	0.000050	0.000376	0.001934	0.006526	0.013052	0.011747	1	0.014357	0.997643
2	0.084224				0.000001	0.000012	0.000125	0.000940	0.004834	0.016315	0.032630	0.029367	2	0.043724	0.983286
3	0.140374				0.000001	0.000019	0.000209	0.001567	0.008057	0.027192	0.054384	0.048945	3	0.088488	0.939562
4	0.175467				0.000002	0.000024	0.000261	0.001958	0.010071	0.033990	0.067979	0.061182	4	0.133890	0.851074
5	0.175467				0.000002	0.000024	0.000261	0.001958	0.010071	0.033990	0.067979	0.061182	5	0.161573	0.717184
6	0.146223				0.000001	0.000020	0.000218	0.001632	0.008392	0.028325	0.056650	0.050985	6	0.162002	0.555611
7	0.104445				0.000001	0.000014	0.000155	0.001166	0.005995	0.020232	0.040464	0.036418	7	0.138826	0.393609
8	0.065278				0.000001	0.000009	0.000097	0.000728	0.003747	0.012645	0.025290	0.022761	8	0.103800	0.254783
9	0.036266					0.000005	0.000054	0.000405	0.002082	0.007025	0.014050	0.012645	9	0.068798	0.150983
10	0.018133					0.000002	0.000027	0.000202	0.001041	0.003512	0.007025	0.006322	10	0.040930	0.082185
11	0.008242					0.000001	0.000012	0.000092	0.000473	0.001596	0.003193	0.002874	11	0.022081	0.041255
12	0.003434						0.000005	0.000038	0.000197	0.000665	0.001330	0.001197	12	0.010889	0.019174
13	0.001321						0.000002	0.000015	0.000076	0.000256	0.000512	0.000461	13	0.004945	0.008285
14	0.000472						0.000001	0.000005	0.000027	0.000091	0.000183	0.000164	14	0.002080	0.003340
15	0.000157							0.000002	0.000009	0.000030	0.000061	0.000055	15	0.000816	0.001260
16	0.000049							0.000001	0.000003	0.000009	0.000019	0.000017	16	0.000297	0.000444
17	0.000014								0.000001	0.000003	0.000005	0.000005	17	0.000102	0.000147
18	0.000004									0.000001	0.000002	0.000001	18	0.000031	0.000045
19	0.000001									0.000000	0.000000	0.000000	19	0.000011	0.000014
													20	0.000003	0.000003

Hence the first, second, third, fourth ... moments of the Poisson variate about its mean are 0, m, m, $m+3m^2$ ... respectively. In Table III are given for four discrete variates common in engineering, the expressions for the frequency and moment arrays and the corresponding parameters.

The important characteristic of frequency arrays which we can make use of in adding discrete variates is that the array of their sum is the *product* of the individual arrays. Thus to add two Poisson variates r and s with distributions p_r and p_s, we multiply their frequency arrays giving

Array of $z=r+s$ is $[e^{a_r(A-1)}][e^{a_s(A-1)}]$

This when expanded in powers of A gives

$$e^{-(a_r+a_s)}e^{A(a_r+a_s)}=e^{-(a_r+a_s)}\times\left(1+(a_r+a_s)A+\frac{(a_r+a_s)^2}{2!}A^2+\frac{(a_r+a_s)^3}{3!}A^3+\dots\right)$$

But by definition the coefficient of A^z is the probability of exactly z occurrences. Hence

$$F(z)=\frac{(a_r+a_s)^z e^{-(a_r+a_s)}}{z!}$$

which demonstrates the truth of the familiar statement that the sum of two Poisson variates is a third Poisson variate whose average equals the sum of the components' averages. This same result could have been found by direct application of equation 14.

The sum distribution of the binomial and Poisson variates found step-by-step in Table II can readily be checked by the frequency array method. In a similar fashion the analytical expression for the sum and the moments of the sum of any two or more discrete variates can be obtained. The individual variates, whose frequency arrays are given in Table III will be found to simulate quite a number of the simple discrete distributions found in practice. The table could, of course, be extended indefinitely.

5.22 Analytical Combination of Continuous Variates

The application of equation 14 to the three most common equations of relation between two continuous variates yields

Linear Combination, $z=r\pm s$

$$F(z)dz=dz\int_s \theta(z\mp s)\phi(s)ds \qquad (15)$$

Product Combination, $z=rs$

$$F(z)dz=dz\int_s \theta\left(\frac{z}{s}\right)\frac{1}{s}\phi(s)ds \qquad (16)$$

Quotient Combination, $z=\frac{r}{s}$

$$F(z)dz=dz\int_s \theta(zs)s\phi(s)ds \qquad (17)$$

The combinations of a great variety of continuous variates even in just the three type groups above can be imagined. We shall take up a few cases which illustrate the method of working.

5.22.11 Linear Combination of Distributions Limited in Both Directions—Square-Topped

If we have two square-topped variates r and s with distributions

$$\theta(r)dr=\frac{1}{b-a}dr$$

$$\phi(s)ds=\frac{1}{h-g}ds$$

what is the distribution of z, when $z=r+s$? Consider first the simple case of $a>h$ and $h-g>b-a$ (or $h+a>b+g$), as shown in Figure 4. From equation 15 we write immediately,

$$F(z)dz=\frac{dz}{b-a}\frac{1}{h-g}\int_{s=\text{all values}} ds$$

We need now to select carefully the limits of integration for s which include all the admissible values of s but no others. There will be three cases.

Case 1. When z lies between $a+g$ and $b+g$

$$F_1(z)=\frac{1}{b-a}\frac{1}{h-g}\int_{s=g}^{z-a} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(z-a-g)$$

Case 2. When z lies between $b+g$ and $a+h$

$$F_2(z)=\frac{1}{b-a}\frac{1}{h-g}\int_{s=z-b}^{z-a} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(b-a)=\frac{1}{h-g}$$

Case 3. When z lies between $a+h$ and $b+h$

$$F_3(z)=\frac{1}{b-a}\frac{1}{h-g}\int_{s=z-b}^{h} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(b+h-z)$$

The distribution of the resultant z is shown in Figure 4, and, as one might have expected, shows discontinuities at the points $b+g$ and $a+h$. Clearly the restriction of $a>h$ would make no change if it were lifted. We insure (although not absolutely) our selection of the proper limits for s by checking that the area under $F(z)$ is unity, that is

$$\text{Area}=\int_{a+g}^{b+g}F_1(z)dz+\int_{b+g}^{a+h}F_2(z)dz+\int_{a+h}^{b+h}F_3(z)dz=1$$

If instead of $h+a>b+g$ we had taken $h+a<b+g$, the equations for $F_1(z)$ and $F_3(z)$ would remain unchanged for the end intervals, while $F_2(z)$ for the central interval of z would become

$$F_2'(z)dz=\frac{1}{b-a}\frac{1}{h-g}\int_{s=g}^{h} ds$$

$$=\frac{1}{b-a}\frac{1}{h-g}(h-g)=\frac{1}{b-a}$$

Finally it may be observed that when $a+h=b+g$, that is the two variates r and s have the same range, the central interval of z disappears entirely, leaving simply an isosceles triangular distribution whose average is $\bar{z}=\bar{r}+\bar{s}$, and whose range is the sum of the corresponding ranges of the r and s variates.

The distribution of the *difference* between two square-topped variates is obtained in a manner very similar to that used in determining their sum. Likewise, the distribution form resulting is very similar to the sum distribution; an example is shown as the lower diagram of Figure 4.

EXAMPLE—MINIMIZING NOISE IN TELEPHONE CIRCUITS

Series capacitors are used in each side of the talking circuit at several points in local crossbar central office practice to isolate

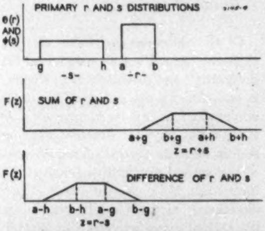

Figure 4. Linear combination of "square-topped" distributions

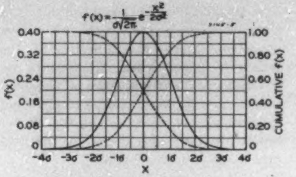

Figure 5. Individual and cumulative normal distribution

various d-c circuit elements. In order to insure satisfactory longitudinal unbalance characteristics (to mimimize noise) the two capacitors comprising a pair need to have very nearly identical capacitances. Suppose stock capacitors available for this purpose vary in capacitance with equal likelihood over the range 2.2 microfarads ±2.5 per cent. What will be the expected (or average) unbalance in the two sides of the circuit due to this cause? What is the probability the unbalance will exceed 0.08 microfarad?

Solution. Since the two capacitor capacitance distributions are identical, the $F(z)$ distribution will be an isosceles triangle, ranging about the mean $\bar{z}=0$, from $-2(0.025)(2.2)=-0.11$ microfarad to $+0.11$ microfarad. The expected unbalance will be found by determining the average of the absolute deviations from $\bar{z}=0$. In the notation used above and seen on Figure 4, this is

$$\overline{|z|}=2\int_0^{b-g}\frac{z}{(b-a)(h-g)}(b-g-z)dz$$

$$=0.0367 \text{ microfarad}$$

when $b=h=2.2+0.055=2.255$, and $a=g=2.2-0.055=2.145$. Similarly the probability that the unbalance z will exceed 0.08 microfarad is given by

$$P(>0.08)=\int_{0.08}^{0.11}F(|z|)dz=\frac{2}{(b-a)(h-g)}\times$$

$$\int_{0.08}^{0.11}(b-g-z)dz=0.0744$$

5.22.12 Linear Combination of Normal Variates

Of all continuous probability distributions the normal law shown plotted "individually" and cumulatively in Figure 5 is undoubtedly the one most frequently encountered in engineering work. Although the physical situation may but seldom meet the theoretical requirements of the normal distribution (such as that the variate has unlimited extent in both directions), nevertheless in many cases the normal curve provides a satisfactory fit over the major portion of the variate's range.

Let the variates r and s have the normal distributions

$$\theta(r)=\frac{1}{\sigma_r\sqrt{2\pi}}e^{-\frac{(r-\bar{r})^2}{2\sigma_r^2}} \qquad (18)$$

and

$$\phi(s)=\frac{1}{\sigma_s\sqrt{2\pi}}e^{-\frac{(s-\bar{s})^2}{2\sigma_s^2}} \qquad (19)$$

We desire the distribution of $z=r+s$. Substituting directly in equation 15 gives

$$F(z)dz=dz\int_{s=-\infty}^{+\infty}\theta(z-s)\phi(s)ds$$

$$=dz\int_{-\infty}^{\infty}\frac{1}{\sigma_r\sqrt{2\pi}}e^{-\frac{(z-s-\bar{r})^2}{2\sigma_r^2}}\times$$

$$\frac{1}{\sigma_s\sqrt{2\pi}}e^{-\frac{(s-\bar{s})^2}{2\sigma_s^2}}ds$$

$$=\frac{1}{\sqrt{\sigma_r^2+\sigma_s^2}\sqrt{2\pi}}e^{-\frac{[z-(\bar{r}+\bar{s})]^2}{2(\sigma_r^2+\sigma_s^2)}}dz \qquad (20)$$

Quite obviously this resultant distribution is of the same normal form as were the two components, since if we set

$$\bar{z}=\bar{r}+\bar{s} \qquad (21)$$

$$\sigma_z=\sqrt{\sigma_r^2+\sigma_s^2} \qquad (22)$$

we obtain

$$F(z)dz=\frac{1}{\sigma_z\sqrt{2\pi}}e^{-\frac{(z-\bar{z})^2}{2\sigma_z^2}} \qquad (23)$$

By an exactly similar analysis we should have found that the distribution of the *difference* $z=r-s$ of two normal variates has exactly the same distribution as given in equation 23, if instead of equation 21 we write

$$\bar{z}=\bar{r}-\bar{s} \qquad (24)$$

It is thus quite obvious that *the sum (or difference) of two normal variates has a normal distribution whose mean equals the sum (or difference) of the means of the primary distributions, and whose standard deviation is the square root of the sum of the squares of the component standard deviations.*

Example—the Tractor Plant

In the assembly plant of a tractor company, one operation consists in bolting two connecting parts A and B end to end as in Figure 6. It is found by sampling the bins containing the two parts that eight per cent of the A parts exceed a variation of ±0.10 inch from the nominal or design value, and 14 per cent of the B parts exceed a variation of ±0.05 inch from their nominal value. What proportion of the bolted assemblies will exceed an error of 0.15 inch from the designed value?

Solution. Suppose past studies have shown that both parts A and B have manufacturing variations closely following the normal law. Then reading on Figure 5, we see that if eight per cent are to exceed a deviation from the mean of ±0.10 inch (that is four per cent exceed a +0.10-inch deviation and four per cent exceed a −0.10-inch deviation), the +0.10 deviation must correspond to +1.75 σ_A standard deviations, whence $\sigma_A=\frac{0.10}{1.75}=0.05714$ inch. Likewise with part B if we find that seven per cent of the samples exceed a deviation of +0.05 then 0.05 equals 1.48 standard deviations, or $\sigma_B=\frac{0.05}{1.48}=0.03378$ inch.

Now from equation 22 we calculate the standard deviation of the sum of the lengths of A and B, as

$$\sigma_z=\sqrt{(0.05714)^2+(0.03378)^2}=0.06638$$

We know from equation 23 that z is normal, so we have merely to find the probability of exceeding a deviation from the average of the specified 0.15 inch in a normal curve with $\sigma_z=0.06638$. This deviation when expressed in terms of σ_z is 2.26 standard deviations. Referring once more to Figure 5 we find the probability is approximately $2(0.012)=0.024$ that a random assembled unit will exceed the specified range. Or, conversely, we should expect $(1-0.024)100=97.6$ per cent of the assemblies to fall within the specified range.

5.22.2 Product Combinations of Variates

A single pair of variates will be chosen to illustrate the procedure followed to obtain the distribution of their product. Suppose we have a generalized exponential distribution (often designated as Pearson's type III) in r and a square-topped distribution in s, given by

$$\theta(r)=\frac{c^{n+1}}{\Gamma(n+1)}r^ne^{-cr}$$

$$\phi(s)=\frac{1}{h-g}$$

to find $F(z)$, when $z=rs$.

Substituting in equation 16 gives

$$F(z)dz=dz\int\theta\left(\frac{z}{s}\right)\frac{1}{s}\phi(s)ds$$

$$=dz\frac{c^{n+1}}{\Gamma(n+1)}\frac{1}{h-g}\int_{s=g}^{h}\left(\frac{z}{s}\right)^ne^{-c\frac{z}{s}}\frac{1}{s}ds$$

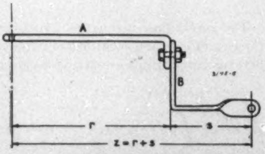

Figure 6. Assembly of tractor parts A and B

Table III. The Frequency and Moment Arrays, and Parameters of Several Common Discrete Distributions

		Moment Arrays		Parameters of the Universe			
Universe	Frequency Array	About Zero*	About Mean**	mean, $\bar{x}$	Standard Deviation, σ	Skewness, k(= $\sqrt{\beta_1}$)	Kurtosis, β_2
Binomial:							
$f(x)=\binom{n}{x}p^x(1-p)^{n-x}$	$(q+pA)^n$	$(q+pe^{\alpha})^n$	$e^{-np\alpha}(q+pe^{\alpha})^n$	np	$\sqrt{npq}$	$\frac{1-2p}{\sqrt{npq}}$	$3\frac{n-2}{n}+\frac{1}{npq}$
Poisson:							
$f(x)=\frac{m^x e^{-m}}{x!}$	$e^{m(A-1)}$	$e^{m(e^{\alpha}-1)}$	$e^{-m\alpha}\cdot e^{m(e^{\alpha}-1)}$	m	$\sqrt{m}$	$\frac{1}{\sqrt{m}}$	$3+\frac{1}{m}$
Simple exponential:							
$f(x)=(1-e^{-c})e^{-cx}$	$\frac{1-e^{-c}}{1-Ae^{-c}}$	$\frac{1-e^{-c}}{1-e^{\alpha-c}}$	$e^{\frac{\alpha e^{-c}}{1-e^{-c}}}\frac{(1-e^{-c})}{(1-e^{\alpha-c})}$	$\frac{e^{-c}}{1-e^{-c}}$	$\frac{e^{-\frac{c}{2}}}{1-e^{-c}}$	$e^{\frac{c}{2}}+e^{-\frac{c}{2}}$	$e^{c}+e^{-c}+7$
Generalized exponential:							
$f(x)=\frac{(1-e^{-c})^2}{e^{-c}}xe^{-cx}$	$A\frac{(1-e^{-c})^2}{(1-Ae^{-c})}$	$e^{\alpha}\frac{(1-e^{-c})^2}{(1-e^{\alpha-c})}$	$e^{\frac{2\alpha}{1-e^{-c}}}\frac{(1-e^{-c})^2}{(1-e^{\alpha-c})}$	$\frac{1+e^{-c}}{1-e^{-c}}$	$\frac{\sqrt{2}e^{-\frac{c}{2}}}{1-e^{-c}}$	$\frac{e^{\frac{c}{2}}+e^{-\frac{c}{2}}}{\sqrt{2}}$	$\frac{e^{c}+e^{-c}+10}{2}$

* The moment array about zero is obtained by substituting $e\alpha$ for A in the frequency array.

** The moment array about the mean is obtained by multiplying the moment array about zero by $e^{-\alpha(\text{mean})}$.

Let $t=\frac{1}{s}$, so that $s=\frac{1}{t}$, $ds=-\frac{1}{t^2}dt$. This results in

$$F(z)dz=\frac{c^{n+1}}{\Gamma(n+1)}\frac{z^n dz}{h-g}\times \int_{1/h}^{1/g} t^{n-1}e^{-czt}dt \qquad (25)$$

The integration of this formula for n unspecified cannot be performed exactly. For low integral values of n, however, the expression can readily be written out by making a succession of n integrations, since

$$\int t^{n-1}e^{-ct}dt=\frac{t^{n-1}e^{-ct}}{-c}+\frac{n-1}{c}\int t^{n-2}e^{-ct}dt$$

until the last integral is of the form

$$\int e^{-ct}dt=-\frac{1}{c}e^{-ct}$$

Thus when $n=1$, equation 25 becomes simply

$$F(z)dz=\frac{c}{\Gamma(2)}\frac{dz}{h-g}\left[e^{-\frac{cz}{h}}-e^{-\frac{cz}{g}}\right]$$

If n is any positive integer, it can be shown that

$$F(z)dz=\frac{c}{n}\frac{dz}{h-g}\left[P\left(n,\frac{cz}{g}\right)-P\left(n,\frac{cz}{h}\right)\right]$$

where $P(n, m)=\sum_{x=n}^{\infty}\frac{m^x e^{-m}}{x!}$, the Poisson summation which is tabled for wide ranges.

5.22.3 Quotient Combination of Variates

To illustrate the exact analytical determination of the distribution of the quotient of two independent variates, we choose the case of $z=r/s$ in which r and s are of the generalized exponential form. Their distributions are

$$\theta(r)=\frac{c^{n+1}}{\Gamma(n+1)}r^n e^{-cr}$$

$$\phi(s)=\frac{q^{m+1}}{\Gamma(m+1)}s^m e^{-qs}$$

From equation 17

$$F(z)dz=dz\int\theta(zs)s\phi(s)ds$$

$$=dz\frac{c^{n+1}}{\Gamma(n+1)}\frac{q^{m+1}}{\Gamma(m+1)}\times\int_{s=0}^{\infty}(zs)^n e^{-czs}s^{m+1}e^{-qs}ds$$

$$=\frac{c^{n+1}}{\Gamma(n+1)}\frac{q^{m+1}}{\Gamma(m+1)}z^n dz\times\int_{s=0}^{\infty}s^{m+n+1}e^{-(cz+q)s}ds \qquad (26)$$

Apparently equation 26 is of the same form as equation 25 just considered under "Products," except that here the limits of s are 0 and ∞. This last condition produces the complete gamma function, since

$$\int_0^{\infty}x^n e^{-ax}dx=\frac{\Gamma(n+1)}{a^{n+1}}$$

in which $\Gamma(n+1)=n!$ for n integral. Hence equation 26 reduces to

$$F(z)dz=c^{n+1}q^{m+1}\frac{\Gamma(m+n+2)}{\Gamma(n+1)\Gamma(m+1)}\times\frac{z^n dz}{(cz+q)^{m+n+2}} \qquad (26\text{-a})$$

Tables of logarithms of the Γ function are available as well as factorials of numbers which may be used in their place when m and n are integers.

Since the rth moment of z in equation 26 is

$$\mu_r'=\frac{q^r}{c^r}\frac{\Gamma(n+r+1)}{\Gamma(n+1)}\frac{\Gamma(m-r+1)}{\Gamma(m+1)}$$

we readily find

$$\bar{z}=\frac{q}{c}\frac{n+1}{m}$$

and

$$\sigma_z=\frac{q}{cm}\sqrt{\frac{(m+n+1)(n+1)}{m-1}}$$

It is to be particularly noted that $\bar{z}$ does *not* equal the ratio of $\bar{r}$ and $\bar{s}$, which here would give $\frac{q}{c}\frac{n+1}{m+1}$ instead of $\frac{q}{c}\frac{n+1}{m}$.

5.3 Approximate Analytical Combinations of Independent Variates

Very often it becomes highly desirable to find an estimate of the resultant of two or more independent variates when it is impossible or inexpedient to combine them either by the step-by-step or the completely analytical method. Sometimes, for instance, raw data depicting the behavior of one of the elements are widely scattered or are not easily fitted by the usual methods; a fitting curve may have too complex an equation to permit analytical manipulation; complete information on one or more primary variates may be lacking; or again only estimates of the resultant parameters may be needed for one's purpose.

In this event satisfactory resort may usually be made to various schemes for combining the parameters of the distribution functions to obtain the corresponding parameters of the resultant curve. From these, in turn, a frequency distribution may be constructed if desired, using one of the systems of generalized frequency curves (such as the Pearson types or the Gram-Charlier series). The more powerful of these combination methods will now be considered for the three common operations, those having linear, product, and quotient equations of relation.

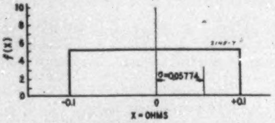

Figure 7. Resistance variation in coils of wire

5.31 Approximate Linear Combinations

The parameters which lend themselves best to the approximate linear combination of variates are the semi-invariants defined by equations 4. Thiele has demonstrated the following relationship for every linear function of uncorrelated observations, the extreme simplicity of which he says "renders the semi-invariants unrivalled as the most suitable symmetrical functions and the most powerful instrument of the theory of observations."

If the equation of relation is $z=a+bx+cy+\ldots+dt$ then the semi-invariants L_r of the resultant z distribution are

$$\left.\begin{array}{l} L_1=a+b\,{}_x\lambda_1+c\,{}_y\lambda_1+\ldots+d\,{}_t\lambda_1 \\ L_2=b^2\,{}_x\lambda_2+c^2\,{}_y\lambda_2+\ldots+d^2\,{}_t\lambda_2 \\ \cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots \\ \text{and, generally} \\ L_r=b^r\,{}_x\lambda_r+c^r\,{}_y\lambda_r+\ldots+d^r\,{}_t\lambda_r \end{array}\right\} \qquad (27)$$

for $r\geqq 2$.

Having found the semi-invariants of the combined distributions, we may if we like, retransfer them to moments by equations 4, and then determine the mean standard deviation, skewness, and kurtosis of the resultant. Following through these operations we can express these latter parameters in terms of those of the component distributions:

$$\bar{z}=a+b\bar{x}+c\bar{y}+\ldots+d\bar{t} \qquad (28)$$

$$\sigma_z=\sqrt{b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2} \qquad (29)$$

$$k_z=\frac{b^3\sigma_x^3k_x+c^3\sigma_y^3k_y+\ldots+d^3\sigma_t^3k_t}{[b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2]^{3/2}} \qquad (30)$$

$$\beta_{2z}=\frac{b^4\sigma_x^4(\beta_{2x}-3)+c^4\sigma_y^4(\beta_{2y}-3)+\ldots+d^4\sigma_t^4(\beta_{2t}-3)}{[b^2\sigma_x^2+c^2\sigma_y^2+\ldots+d^2\sigma_t^2]^2}+3 \qquad (31)$$

Equations 28 and 29 of course include the familiar relations:

1. The mean of the sum (or difference) of independent variates is the sum (or difference) of the means of the individual variates.
2. The standard deviation of the sum (or difference) of independent variates is the square root of the sum of the squares of the individual standard deviations.

It is perhaps not universally appreciated that these relations are true whatever the distribution forms of the component variates.

When n identical variates are to be added, which is a common case, equations 28 to 31 reduce to

$$\left.\begin{array}{l} \bar{z}=n\bar{x} \\ \sigma_z=\sqrt{n}\,\sigma_x \\ k=\dfrac{1}{\sqrt{n}}k_x \\ \beta_2=\dfrac{1}{n}\,[\beta_{2_x}-3]+3 \end{array}\right\} \qquad (32)$$

Thus the resultant's average is just n times an individual average, the standard deviation has increased at the rate of $\sqrt{n}$, while the skewness has diminished as $1/\sqrt{n}$, and the kurtosis is approaching 3 as a limit at a rate dependent on $1/n$. It will be seen that these last two limits, $k=0$ and $\beta_2=3$ are characteristic of the normal law. Likewise it can be shown that the same limits are approached for any linear equation of relation as n increases; moreover the primary variates need not all be alike. Hence we conclude that, if a large number of variates are combined linearly, their resultant will tend to approach the normal distribution with mean and standard deviation calculated from equations 28 and 29. As a practical matter, if no one distribution has a standard deviation substantially larger than the others to be combined linearly, the resultant of as few as six or eight components will nearly always be so closely normal that it may be considered normal for most engineering purposes.

Example—Resistance Unbalance in Wires

Two copper wires of 50 miles length are to be built from 120-pound coils (173 pounds to the mile, approximately), and the electrical resistances per coil vary with an equal likelihood over all values ±0.1 ohm from the average as shown in Figure 7. What is the probability that the completed wires will differ in resistance more than 1.5 ohms?

Solution. The number of coils n in each wire will be $n=\dfrac{50(173)}{120}\approx 72$. Hence the probability distribution for single wires will be closely normal about a mean value $\bar{r}$ equal to the average resistance per coil times the number of coils in the 50-mile length. The standard deviation of a single coil's resistance, by application of equation 2, is

$$\sigma_x^2=\mu_2=\frac{K\displaystyle\int_{-0.1}^{+0.1}x^2dx}{K\displaystyle\int_{-0.1}^{+0.1}dx}=\frac{\dfrac{2(0.001)}{3}}{0.2}=0.0033333$$

and $\sigma_x=0.05774$ ohm. Similarly we find $k_x=0$, and $\beta_{2x}=1.80$. Then by equation 32 the standard deviation for a single wire of 72 coils is $\sigma_{72}\doteq\sqrt{72}\,\sigma_x=0.4899$ ohm. Likewise $k_{72}=\dfrac{1}{\sqrt{72}}0=0$, and $\beta_{2n}=\dfrac{1}{27}\,[1.80-3.0]+3=2.9833$. The single-wire resistance distribution will then be closely normal, with the equation

$$\theta(r)=\frac{1}{0.4899\sqrt{2\pi}}e^{-\frac{(r-\bar{r})^2}{2(0.4899)^2}}$$

The distribution of the difference $z=r_1-r_2$ in two such wires will likewise be normal (from section 5.22.12) and will have the parameters, from equations 24 and 22

$$\bar{z}=\bar{r}_1-\bar{r}_2=0$$

$$\sigma_z=\sqrt{\sigma_{r1}^2+\sigma_{r2}^2}=\sqrt{2(0.4899)^2}$$
$$=0.6918\text{ ohm}$$

This distribution of z is shown in Figure 8. The probability that the two wires will differ in resistance more than 1.5 ohms, that is in excess of $2.168\sigma_z$, is read from a normal probability table or from Figure 5 to be 0.0302. The corresponding areas are indicated in Figure 8.

5.31.1 Distributions of Means

The reliability of the observed mean of a sample of n items is often desired.

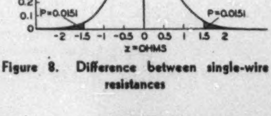

Figure 8. Difference between single-wire resistances

The true answer involves an application of *a posteriori* probability theory. However if n is large, say 100 or more, it is usually satisfactory to substitute the following *a priori* problem: If n items are drawn at random from a hypothetical universe with assumed parameters, what is the probability that a sample differing from this universe as far as that actually drawn could have resulted? This probability is then taken as a satisfactory approximation to the probability that the unknown true universe differs from the observed sample as far as did the hypothetical universe. The best estimate of the standard deviation of the unknown universe is the observed sample's standard deviation.

The distribution of the *means* of samples of n items is identical with that of the *totals* of n items when the latter's variate scale is reduced by $1/n$th. It is easy to see that the parameters of the means of successive samples of n items are

$$\bar{\bar{x}} = \frac{1}{n}[\text{mean of the totals distribution}] = \bar{x} \tag{33}$$

$$\sigma_{\bar{x}} = \frac{1}{n}[\text{standard deviation of the totals distribution}] = \frac{\sqrt{n}\sigma}{n} = \frac{\sigma}{\sqrt{n}} \tag{34}$$

$$k_{\bar{x}} = k_{\text{totals}} \tag{35}$$

$$\beta_{2_{\bar{x}}} = \beta_{2\text{totals}} \tag{36}$$

Hence as n increases, the distribution of the means of successive samples of size n rapidly approaches the normal law, while the expected average is the universe average, and the standard deviation (or standard error) of the means decreases inversely as the square root of the number of observations n. This last, of course, agrees with the common sense conclusion that the larger the sample the more reliable its mean should be.

Example—Sampling of Manufactured Product

A sample of 225 rough castings is taken at random from a shipment of about 10,000 and weighed individually. The average weight is found to be 11.72 pounds with a standard deviation of 0.804 pound. What is the probability that the average weight for the whole shipment is less than 11.60 pounds?

Solution. We solve the allied *a priori* sampling problem: If the true average is 11.60 pounds with standard deviation of 0.804 pound, what is the probability that a sample of the size drawn could have a mean of 11.72 *or higher?* The standard deviation of averages of samples with 225 items is estimated from equation 34 as $\sigma_{\text{avg}} = \frac{0.804}{\sqrt{225}} = 0.0536$. The discrepancy between the hypothetical universe mean and the observed sample mean is $11.72 - 11.60 = 0.12$, which in terms of σ_{avg} is $\frac{0.12}{0.0536} = 2.24$ standard deviations. From the cumulative curve of Figure 5 the probability of exceeding a deviation of 2.24 standard deviations is about 0.0125. We take this as the probability that the unknown true average in the shipment could lie below 11.60 pounds.

5.32 Approximate Product Combinations

In the formation of products the characterizing *moments* rather than the semi-invariants (which are so powerful in linear combinations) are found to be particularly useful. From equations 14 we can immediately write the moments of the product distribution (for discrete variates) as

$$\begin{aligned}\mu_i' &= \Sigma\Sigma \ldots \Sigma z^i p_r p_s \ldots p_t \\ &= \Sigma\Sigma \ldots \Sigma(rs \ldots t)^i p_r p_s \ldots p_t\end{aligned}$$

Since we are dealing with independent variates, this last expression may be rewritten as

$$\mu_i' = \Sigma r^i p_r \Sigma s^i p_s \ldots \Sigma t^i p_t$$

which in moment notation is

$$\mu_i' = \mu_{i_r}' \cdot \mu_{i_s}' \ldots \mu_{i_t}' \tag{37}$$

The same relationship can as readily be developed using continuous variates with integral signs replacing the summation signs. We conclude that *the ith moment about zero of the product is equal to the product of the individual ith moments about zero of the components.*

From equation 37 expressions showing the relationship between the parameters of the resultant and of the components are easily written. We find

$$\bar{z} = \mu_1' = \bar{r}\bar{s} \ldots \bar{t} \tag{38}$$

$$\sigma_z^2 = [(\sigma_r^2 + \bar{r}^2)(\sigma_s^2 + \bar{s}^2) \ldots (\sigma^2 + \bar{t}^2)] - (\bar{r}\bar{s} \ldots \bar{t})^2 \tag{39}$$

The values of the "higher" parameters can more readily be found in any specific case by numerical substitution in the basic equation 37, except perhaps in the case of just two variates.

Just as it was found that a linear combination of n independent variates tended toward the normal form as n increases, it may be shown by an analogous reasoning that the *product* of n independent variates tends toward the log-normal form. The log-normal distribution is a positively skewed unimodal curve whose lower limit is zero and upper limit is infinite and whose logarithms form a normal distribution. The chief restriction to this general conclusion on the trend of product distributions is that no one of the components should have the ratio of its standard deviation to its mean value very much larger than all of the other components' standard deviation-to-mean ratios.

5.33 Approximate Quotient Combinations

The problem of determining the parameters of a quotient distribution, given the corresponding parameters of the components, is complicated by the fact that values of the divisor variate must not ordinarily be permitted to occur at zero with any except a negligible likelihood. Approximate solutions must usually then be guarded by various restrictions regarding the distribution s in the quotient equation of relation $z = \frac{r}{s}$. If, however, we have a quotient distribution of the form $z = \frac{r + \bar{r}}{s + \bar{s}}$ in which $\bar{s}$ is several times the standard deviation of s, a satisfactory parameter solution can usually be found.

The most obvious, and oftentimes the easiest, procedure is to transform the equation of relation from a quotient type to the product type by merely changing the distribution of the variable in the denominator to its reciprocal's distribution. That is, if the equation of relation is given as $z = \frac{r}{s}$, then by setting $s = \frac{1}{w}$ we obtain $z = rw$ which is now a product of two independent variates. The subsequent analysis can then be handled as described in section 5.32 by taking products of the corresponding moments of r and w. Thus

$$\mu_{i_{z(=r/s)}}' = \mu_{i_r}' \cdot \mu_{i_{w(=1/s)}}'$$

This procedure will work in all cases to give the true moments of the final quotient distribution $F(z)$, except where the form of the w distribution is such that the moments cannot readily be determined. Even then a practical solution can sometimes be found, as suggested by Karl Pearson, by tabulating the reciprocals of the observations if the information on s is given in this form, and then calculating the moments of the reciprocal distribution. Likewise if the analytical expression for the probability distribution of the s variate is given, and this defies handling, the area under the probability curve may be divided into an appropriate number of class intervals, the area (observations) in each determined, and the procedure just outlined then carried through.

Fieller has given an exact expression for the distribution of $F(z)$ when the two variates r and s are normal. It is quite complex and for most engineering problems would be highly laborious to calculate. If the normal divisor variate s has a mean such that $\bar{s} \geqq 3\sigma_s$, then Geary has found that although z itself is not normal, a second variable dependent on z which we will call t is nearly normal. The variable t corresponding to any selected z is found from

$$t=\frac{\bar{s}z-\bar{r}}{\sqrt{\sigma_r^2+\sigma_s^2z^2}}$$

Then

$$F(z_1<z<z_2)\approx\frac{1}{\sqrt{2\pi}}\int_{t_1}^{t_2}e^{-\frac{t^2}{2}}dt$$

in which the value of the definite integral is readily read from a normal probability table or from the cumulative normal curve on Figure 5.

Craig and others have studied the general quotient problem and have established relationships between the final distribution's semi-invariants and those of the r and s components. If r and s are substantially normal and uncorrelated, and the s values occur with negligible likelihood at $s=0$, that is $\bar{s}>hs$ where h is say 5 or more, the quotient semi-invariants are given approximately by

$$\left.\begin{aligned}
L_1&=\mu_{1z}'=\bar{z}\approx\frac{\bar{r}}{\bar{s}}\left[1+\left(\frac{\sigma_s}{\bar{s}}\right)^2+\right.\\
&\qquad\left.3\left(\frac{\sigma_s}{\bar{s}}\right)^4+15\left(\frac{\sigma_s}{\bar{s}}\right)^6\right]\\
L_2&=\mu_{2z}=\sigma_z^2\approx\left(\frac{\bar{r}}{\bar{s}}\right)^2\left[\left(\frac{\sigma_s}{\bar{s}}\right)^2+\right.\\
&\qquad\left.8\left(\frac{\sigma_s}{\bar{s}}\right)^4+69\left(\frac{\sigma_s}{\bar{s}}\right)^6\right]+\left[\left(\frac{\sigma_r}{\bar{s}}\right)^2+\right.\\
&\qquad\left.3\left(\frac{\sigma_s}{\bar{s}}\right)^2\left(\frac{\sigma_r}{\bar{s}}\right)^2+15\left(\frac{\sigma_s}{\bar{s}}\right)^4\left(\frac{\sigma_r}{\bar{s}}\right)^2\right]\\
L_3&=\mu_{3z}\approx\left(\frac{\bar{r}}{\bar{s}}\right)^3\left[6\left(\frac{\sigma_s}{\bar{s}}\right)^4+\right.\\
&\qquad\left.116\left(\frac{\sigma_s}{\bar{s}}\right)^6\right]+3\left(\frac{\bar{r}}{\bar{s}}\right)\left[2\left(\frac{\sigma_s}{\bar{s}}\right)^2\left(\frac{\sigma_r}{\bar{r}}\right)^2+\right.\\
&\qquad\left.24\left(\frac{\sigma_s}{\bar{s}}\right)^4\left(\frac{\sigma_r}{\bar{s}}\right)^2\right]\\
L_4&=\mu_{4z}-3\mu_{2z}^2\approx72\left(\frac{\bar{r}}{\bar{s}}\right)^4\left(\frac{\sigma_s}{\bar{s}}\right)^6+\\
&\qquad84\left(\frac{\bar{r}}{\bar{s}}\right)^2\left(\frac{\sigma_s}{\bar{s}}\right)^4\left(\frac{\sigma_r}{\bar{s}}\right)^2+12\left(\frac{\sigma_s}{\bar{s}}\right)^2\left(\frac{\sigma_r}{\bar{s}}\right)^4
\end{aligned}\right\}\tag{40}$$

Naturally, the further either of the two component distributions deviates from the conditions stated above under which these equations have been derived, the more approximate they become.

Summary of Fundamentals

Probability or Frequency Curve Description of Variates

1. The majority of the essential information contained in a batch of data taken on a variate x can be summarized in a small number of statistical constants or parameters called the mean $\bar{x}$, standard deviation σ_x, skewness k_x, and kurtosis β_{2x}. These parameters are usually found by calculating the moments (or the semi-invariants) either of the observations or of the theoretical distribution describing the variate x.

Laws of Combining Simple Probabilities

2. When two events are mutually exclusive, the probability of one or the other occurring is the sum of their individual probabilities of occurrence.

3. When two events are independent the probability of their joint occurrence is the product of their individual probabilities of occurrence.

Combining Variates With Mutually Exclusive Probabilities

4. The parameters of the distribution z composed h proportion of the time by variate r, j proportion of the time by variate s, . . . , are given by

$$\bar{z}=h\bar{r}+j\bar{s}+\ \ldots$$

$$\sigma_z=[h(\sigma_r^2+\bar{r}^2)+j(\sigma_s^2+\bar{s}^2)+\ \ldots\ -\bar{z}^2]^{1/2}$$

$$\begin{aligned}k_z=\frac{1}{\sigma_z^3}[&h(k_r\sigma_r^3+3\sigma_r^2\bar{r}+\bar{r}^3)+\\&j(k_s\sigma_s^3+3\sigma_s^2\bar{s}+\bar{s}^3)+\ldots-(3\sigma_z^2\bar{z}+\bar{z}^3)]\end{aligned}$$

$$\begin{aligned}\beta_{2z}=\frac{1}{\sigma_z^4}[&h(\beta_{2r}\sigma_r^4+4k_r\sigma_r^3\bar{r}+6\sigma_r^2\bar{r}^2+\bar{r}^4)+\\&j(\beta_{2s}\sigma_s^4+4k_s^3\bar{s}+6\sigma_s^2\bar{s}^2+\bar{s}^4)+\\&\ldots-(4k_z\sigma_z^3\bar{z}+6\sigma_z^2\bar{z}^2+\bar{z}^4)]\end{aligned}$$

Combining Independent Variates According to the Equation of Relation, $z=\xi(r, s, t\ldots)$

5. The total probability of $F(z)$ is found from $F(z)=\mathfrak{S}p_rp_sp_t\ldots$ or $F(z)dz=\mathfrak{S}\theta(r)dr\times\phi(s)ds\psi(t)dt\ \ldots$ in which $\mathfrak{S}$ means the summation of the probability products for all possible ways of forming z according to the equation of relation.

6. $F(z)$ may sometimes be evaluated most conveniently for particular problems by a step-by-step method of combining probabilities.

7. If the probability distributions for the variates are known analytically, it is often possible to perform the $F(z)$ summations, obtaining an exact analytical expression for $F(z)$. Results typical of this method are:

(*a*). The sum of two Poisson variates is a third Poisson variate whose average equals the sum of its components' averages.

(*b*). The sum (or difference) of two normal variates has likewise a normal distribution whose mean equals the sum (or difference) of the means of the components, and whose standard deviation equals the square root of the sum of the squares of the components' standard deviations.

(*c*). The product of a square-topped and a simple type III Pearson variate yields the form

$$F(z)=\frac{c}{h-g}\left[e^{-\frac{cz}{h}}-e^{-\frac{cz}{g}}\right]$$

(*d*). The quotient of two generalized exponential (Pearson type III) variates has the form

$$F(z)=c^{n+1}q^{m+1}\ \frac{\Gamma(m+n+2)}{\Gamma(n+1)\Gamma(m+1)}\times\frac{z^n}{(cz+q)^{m+n+2}}$$

8. Approximate distributions of the resultant variate can be constructed if its parameters can be found. These can be expressed in terms of the components' parameters as follows:

(*a*). *Linear Equations of Relation.* $z=a+bx+cy+\ \ldots\ +dt$

$$\bar{z}=a+b\bar{x}+c\bar{y}+\ \ldots\ +d\bar{t}$$

$$\sigma_z=\sqrt{b^2\sigma_x^2+c^2\sigma_y^2+\ \ldots\ +d^2\sigma_t^2}$$

Expressions for k_z and β_{2z} are given by equations 30 and 31. From these we conclude that the sum of a large number of independent variates, no one of which has a predominant variation, tends rapidly toward the normal form with the parameters $\bar{z}$ and σ_z just given. Likewise, the standard deviation of the means of successive samples of size n is $1/\sqrt{n}$ times the standard deviation of the universe.

(*b*). *Product Combinations.* The mean and standard deviation of the product of variates r, s, . . . t, is given by

$$\bar{z}=\bar{r}\bar{s}\ \ldots\ \bar{t}$$

$$\sigma_z^2=[(\sigma_r^2+\bar{r}^2)(\sigma_s^2+\bar{s}^2)\ \ldots\ (\sigma_t^2+\bar{t}^2)]-(\bar{r}\bar{s}\ \ldots\ \bar{t})^2$$

As the number of variates combined according to a product equation of relation increases, the resultant tends toward the log-normal form.

(*c*). *Quotient Distributions.* No rigorous method exists for obtaining the parameters of the quotient distribution in terms of the components' parameters. A number of estimates involving certain restrictions are given in section 5.33.

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Series Capacitors for Transmission Circuits

E. C. STARR
FELLOW AIEE

R. D. EVANS
FELLOW AIEE

Synopsis: This paper presents the results of an analytical and experimental investigation of the use of series capacitors to increase the permissible loadings of long high-voltage a-c transmission lines. Because of limitations imposed by synchronous stability and reactive kilovolt-ampere requirements, conventional lines, when extended to the higher voltages and greater lengths, cannot be loaded to values of power sufficiently high to develop maximum over-all economy. It is shown that, when suitably applied, series capacitors will make possible the desired optimum loadings and will very materially reduce transmission costs.

The theoretical possibilities of using series capacitors to compensate the excessive inductive reactance of long lines have received consideration for many years. Practical series capacitors require protective equipment, but shunting of the capacitors in the earlier schemes resulted in a decrease instead of an increase in the transient-stability limits.

The authors propose series capacitors of the limited-voltage type in combination with auxiliary equipment which not only protects the insulation but quickly restores the capacitors to the circuit after the faulted conductors are isolated, thus preventing a decrease in system power limit at the time of need. The application requirements of necessary apparatus are also briefly outlined.

Results of analytical studies and miniature-system tests in connection with a typical application of series capacitors to one of the major lines of a typical transmission system are included. This work deals with relative transmission costs, transient stability, spontaneous hunting, and the subsynchronous operation in the induction starting of machines. The conclusion is reached that the proposed series-capacitor scheme appears practicable for long transmission lines.

THE necessity for long-distance transmission of large blocks of power has grown rapidly during the last few years. Some of the large-scale hydroelectric developments in the western states are located in relatively remote districts and have given rise to transmission problems involving distances of the order of 250 miles and blocks of power of the order of 300,000 kw. Proposed developments in other locations involve similar or longer transmission distances and equal or greater blocks of power. The three most important considerations in each of these major power developments are:

1. The conservation and most effective utilization of important natural resources.
2. The delivery of large blocks of power to natural load centers at minimum annual costs. With all factors given due consideration, these costs should not exceed those of other available power sources.
3. The delivery of large blocks of power to natural load centers with a high degree of reliability.

Due to basic circuit considerations, as transmission distances are increased, the voltages indicated become progressively higher, and the line costs increase at an accelerated rate. For the longer distances, power-limit or stability considerations, together with load and line reactive-kilovolt-ampere requirements, cause the unit power costs to rise rapidly. A basic analysis of the fundamental circuit properties involved indicates that, although a number of methods can be employed to improve the operating characteristics of long transmission circuits, the most promising expedient at the present time is line-reactance compensation by means of series capacitors. In the following sections, the theory and application of these devices as they relate to the costs and performance of major transmission circuits will be discussed in their essential details.

Modified Basic Analysis of Transmission Problem

Reduced to its fundamental elements, an a-c transmission line and its associated transformers constitute a connecting link of finite impedance between a generating station and its load, or between two electric systems. It has been shown that the power delivered over a circuit containing lumped impedance Z of angle θ, with voltages E_s and E_r maintained at the sending and receiving ends respectively, when these voltages are separated in phase position by the angle δ, is expressed by the following equation:

$$P_r=\frac{E_sE_r}{Z}[\cos(\delta-\theta)]-\frac{E_r}{E_s}\cos\theta] \qquad (1)$$

Resistance effects are ordinarily small in practical transmission circuits, and a close approach to the actual performance, when a reasonable stability margin is allowed, can be obtained by neglecting resistance in the preceding equation. The result is the simple expression as follows:

$$P_r=\frac{E_sE_r}{X}\sin\delta \qquad (2)$$

The power transfer is then a function of the product of the sending and receiving voltages, is a function of the angular displacement between these voltages, and is an inverse function of the circuit equivalent reactance interposed between the above voltages.

Control of Circuit Impedance

The most convenient and economic voltage of generation is approximately

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E. C. Starr is professor of electrical engineering, Oregon State College, Corvallis, Oreg., and consulting engineer, Bonneville Power Administration, Portland, Oreg. R. D. Evans is consulting transmission engineer, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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13.8 kv. Similarly, the best voltage for primary distribution in moderately heavy load areas and also for large individual loads is approximately 13.8 kv. If a transmission circuit must be employed between the generating station and load center, the above two voltages referred to a common base and maintained by regulating devices, are the quantities appearing as E_s and E_r in the preceding equations. The equivalent reactance X of the transmission circuit between the generating and receiving busses then determines in a large measure the amount of power than can be transmitted over this circuit.

If overhead transmission circuits are employed, the reactance of the conductors themselves will approach 0.8 ohm per mile at 60 cycles for any practical conductor and for all types of construction. It is evident that, as the transmission distance increases, the circuit reactance increases rapidly, and the power transmitting ability may thereby be reduced below the required value. Resistance effects, as controlled by conductor size, can be reduced from values giving transmission losses of the order of 15 per cent to practically zero without materially altering the power limit, although they do have a definite influence on the economic problem.

An approximate expression for maximum power, assuming 13.8 kv for sending and receiving voltages, and assuming 35 degrees between these voltages to allow for the reactance of terminal equipment and margin in stability, is as follows:

$$P_{rm} = \frac{110}{X} 10^3 \text{kw} \quad (3)$$

The maximum power capacity is thus an inverse function of the equivalent reactance between the 13.8-kv generating and load busses of the transmission circuit.

If overhead line construction is to be retained, four different alternatives are available for reducing this equivalent reactance to the required value. These are:

1. Line-impedance conversion.
2. Multiple-circuit operation.
3. Reduction of system frequency.
4. Line-reactance compensation.

These items will be discussed in the indicated order in the following paragraphs.

High-Voltage Operation

For the reduction of equivalent series impedance, the first and most commonly employed expedient is impedance conversion which is accomplished by transformation to higher voltage. The well-known relationship between the high-side and the low-side equivalent impedance of a transformer, namely, that the equivalent low-side impedance of the high-side circuit is equal to the actual impedance of the high-side circuit divided by turn ratio squared, makes it possible to reduce the equivalent impedance between the low-voltage busses to a marked degree.

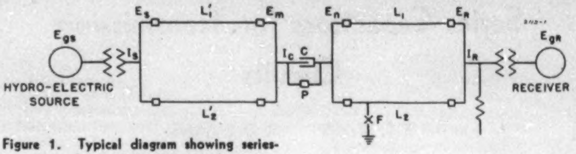


Figure 1. Typical diagram showing series-capacitor scheme of the limited-voltage type for use on long multiple-circuit transmission lines

C—Series capacitor
P—Protective equipment
L_1, L_2, L_1', L_2'—Sections of transmission line
E_{gs}, E_{gr}—Internal voltages at sending and receiving ends
E_s, E_m, E_n, E_r—Voltages on transmission line
I_s, L_r, I_c—Line currents

As an example of the effectiveness of a high-voltage transformation in reducing the equivalent impedance of a circuit, it can be observed that the reactance of an overhead line 100 miles long is approximately 80 ohms at 60 cycles. If line resistance were totally absent, if infinite busses were available at each end of the line, and if voltages of 13.8 kv were maintained at the terminals, the maximum synchronous capacity of the circuit would be approximately only 2,390 kw. A ten-to-one transformation on each end of the line, resulting in a line voltage of 138-kv, neglecting the reactance introduced by the transformers, would reduce the circuit reactance as it affects the low-voltage busses to 0.01 × 80 ohms or 0.80 ohm. This change, neglecting transformer impedances and line resistance and assuming infinite terminating busses, would result in a synchronous power limit 100 times that obtained originally, or 239,000 kw.

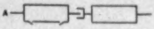
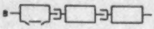
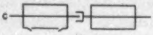
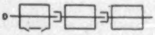
This particular expedient, namely, raising the line voltage by transformation to reduce the equivalent impedance between generator and load busses, has been followed to the practical exclusion of all other methods available for reducing this impedance. Voltages as high as 287.5 kv have been employed in gaining this end, and it will be observed that the equivalent impedance of a line of that voltage referred to a 13.8-kv bus is approximately 0.0023 times the actual line impedance.

It is evident that the only essential purpose of going to high transmission voltages is to reduce the equivalent impedance between the low-voltage busses at the terminals of the circuit. This one benefit, together with reduced equivalent surge impedance, which has been shown to be essential to the transfer of large blocks of power at high efficiencies, can be gained through high-voltage operation only at considerable expense. When voltages are increased, transmission structures become larger, conductor diameters, separations, and clearances become necessarily greater, and line insulation becomes more costly. Transformer and other high-voltage substation equipment costs increase much more rapidly than the voltages in the higher ranges, and transformer reactances, which add directly to the reactances of other elements of the circuit, increase more rapidly than the voltage rating.

Conductor diameters and desirable cross-sectionable areas are thrown so far out of balance at the higher voltages by corona loss, permissible line efficiencies, and mechanical considerations that, considering the necessarily great investment in the high-voltage system, electrical loadings approaching the values required to develop maximum transmission economies cannot be attained in the longer high-voltage lines. Most of the existing long, high-voltage circuits, because of stability limitations and reactive-kilovolt-amperes requirements, cannot be loaded to values exceeding approximately 50 to 75 per cent of the magnitude required to develop minimum annual costs per kilowatt transmitted. As an example of this limitation, it can be observed that the ratio of line loading for maximum economy to line loading for maximum power established by stability is approximately 2.5 in the case of a typical two-circuit 230-kv line somewhat less than 250 miles long and equipped with a mid-point sectionalizing station and normal low-reactance terminals. Even greater discrepancies exist in other more costly circuits of comparable length.

From the preceding discussion it is apparent that the expedient of ever

Table I. Relative Power Limits for Various Circuit Arrangements and Amounts of Capacitance Compensation

Case	Schematic Diagram	Compensation*	Relative Power Limits**
A		0	1.00
		20	1.15
		40	1.36
		50	1.50
		67	1.80
B		0	1.13
		20	1.33
		40	1.62
		50	1.80
		60	2.06
C		0	1.80
		20	2.14
		40	2.65
		50	3.00
		60	3.47
D		0	1.93
		20	2.32
		40	2.94
		50	3.38
		60	3.97

* Ratio of capacitive reactance to total reactance with all lines in service (per cent).

** Ratio of limits with single-line section out, using as reference case *A* without capacitive compensation. Resistance effect neglected.

higher transmission voltages, which are employed only for the purpose of reducing the equivalent system reactances, has reached and perhaps passed its justifiable limit until other steps are taken to strike a better balance among all factors concerned. The line-charging kilovolt-amperes of long high-voltage circuits is useful in most cases but detrimental in some. In those cases for which it is of benefit, it is obtained at relatively high cost.

It seems evident then that serious consideration should be given to other available means for decreasing the equivalent reactance between generator and load busses where long-distance transmission is necessary, and for increasing the electrical loading of these circuits to their points of maximum economy.

Multiple Circuits

The use of two or more transmission circuits in multiple results in a proportional decrease in the system transfer impedance, and when terminal ratings are increased in proportion, results in a similar increase in power capacity. Considerable improvement in operating characteristics and transient-stability limits can be obtained when such multiple circuits are sectionalized at one or more points proportionally distributed throughout their length. Individual line sections on which faults have occurred can then be isolated in only a few cycles after the initiation of the fault with a minimum of increase in total circuit reactance. The costs of multiple circuits with sectionalizing facilities go up in approximate proportion to the power capacities and consequently little is gained toward improving the economic loading status of long high-voltage circuits.

Reduction of System Frequency

Since inductive reactance is a direct function of frequency, the loading of simple systems with given transformation ratios should, over the narrow practical range available, attain synchronous limits in approximate inverse proportion to the frequency employed. This expedient as a supplement to high-voltage transformation, or impedance conversion, has been employed in a number of foreign countries. However, because of the relatively small over-all benefit to be obtained, due to incandescent-lamp flicker at the lower frequencies, increased size and cost of induction equipment, and the necessity for numerous frequency changers, this method of gaining low reactance is not advisable and is being looked upon with disfavor in some localities where it is now practiced. The stability problem may not be avoided unless a considerable part of the load can be utilized at the transmission frequency.

D-C Operation

If the frequency is reduced to zero, that is, if direct current at high voltage is employed, power transfer is accomplished by difference of magnitude between terminal voltages instead of by difference between angular positions. It can be shown that the basic gain for direct current, when the same maximum insulation stresses are allowed, is produced by the 41 per cent higher effective voltage. This results in twice the power delivered over a given circuit having the same conductors and insulation and permitting the same transmission efficiency. Further advantages, however, are indicated. No synchronous stability limitations exist, and, if adequate terminal equipment can be made available, lines can be loaded to their economic limits. Moderately long a-c lines have synchronous load limits approximating their surge-impedance values. The economic limit employing direct current with the same conductors and insulation, in the average case, is approximately three times the a-c limit. If lines as long as 500 miles are required, this ratio would become larger than four. If it becomes necessary to transmit large blocks of power to points as remote as 500 miles, the d-c system will be particularly attractive. In that case the large saving in line costs over a-c requirements would make available very considerable sums for the necessary d-c terminal equipment. If this equipment can be made available at a figure that will increase the total terminal cost to a value approximately two times present a-c terminal cost, 500-mile transmission of large blocks of power can be made as economically feasible as some of the present systems of much shorter lengths. D-c transmission is not suited for supplying power to the intervening area, as there is no d-c equivalent for the a-c transformer. The major problem hinges upon the successful development of adequate terminal equipment.

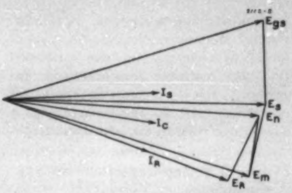


Figure 2. Vector diagram for series-capacitor transmission system shown in Figure 1

Line-Reactance Compensation

The most important alternative or supplement to high-voltage transformation for reducing the equivalent impedance between generator and load low-voltage busses is the use of line-reactance compensation. Such compensation can best be obtained through the use of static capacitors operating in series with the

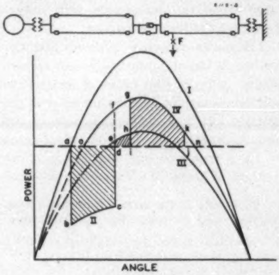


Figure 3. Power-angle diagram for sending end of series-capacitor transmission system connected to infinite receiver

ab—Fault on one line section, series capacitor shunted
ce—Faulted line section switched out
hg—Series capacitor restored to the circuit
k—Point of maximum angular swing
For stability,
area $(abcd) \leq [\text{area } (edih) + \text{area } (gijk)]$

Curve	Fault Condition	Capacitor	Line Sections Out
I	Before	In	None
II	During	Out	None
III	After	Out	One
IV	After	In	One

transmission-line conductors. After considering all these methods in the light of recent developments, it was concluded that line-reactance compensation deserved a special study, the results of which have led to the series-capacitor scheme described in the next section.

Proposed Series-Capacitor Scheme

The general arrangement proposed for using series capacitors for the compensation of the inductive series reactance of transmission circuits is shown schematically in Figure 1. This figure shows a protected or limited-voltage type of series capacitor located in a common-bus connection in a two-circuit line. The proposed arrangement would increase the permissible transmission-circuit loading, which is normally based on the system riding through a double line-to-ground fault and the subsequent circuit-isolating operation. Such an arrangement with series capacitors can be used to increase by 50 per cent or more the rating of the circuit, assuming that generating and receiving equipment are increased in proportion. Previous series-capacitor schemes actually reduce transient-stability limits,[1] because they lack the quick restoration feature of the present proposal. Vector diagrams for a particular load condition on the system of Figure 1 are given in Figure 2 which shows the large negative phase-angle shift introduced by the series capacitor.

The series-capacitor system, just described, is based on an analysis of factors which include: the type of capacitor, whether of nonlimited- or limited-voltage design; location of the capacitor in the circuit, whether in the individual lines or in a common-bus connection; the method of using the capacitor, whether

1. Normally in the circuit, shunted during the fault and restored after fault isolation.
2. Normally not in the circuit but switched into the circuit after the fault is isolated.

The factors also include the application problems of single-circuit and multiple-circuit lines.

TYPES OF CAPACITORS

When systems with series capacitors are subjected to faults, high-voltage drops across the capacitors are produced by the increased line currents, and measures against overvoltage must be provided. Two types of capacitors may be used:

1. The nonlimited-voltage type in which sufficient insulation is provided in the capacitor to meet the fault condition.
2. The limited-voltage type which has insulation sufficient for all normal-circuit conditions and protective equipment to limit the excess voltages.

The nonlimited-voltage type of series capacitor has the desirable feature of always being available to provide reactive compensation, even during system faults. Unfortunately, the cost of the nonlimited-voltage type of capacitor is prohibitive, at least in comparison with the limited-voltage type. This results from the fact that, when high-speed fault clearing is employed, the capacitor cost is determined principally by the maximum-voltage condition and the capacitive reactance desired at 60 cycles.

The limited-voltage scheme uses

1. Capacitors that are much smaller and less expensive than those of the nonlimited type.
2. Protective equipment that not only limits overvoltages by providing a shunting path of negligible or low impedance but also opens this path promptly upon fault removal, incidentally reducing fault currents to the noncompensated value during the shunting operation.

Protective apparatus and the basis for selecting capacitor voltage ratings cannot be discussed here because of space limitations. Tests, however, have shown that suitable protective apparatus can be built. In the studies of system performance to determine the application requirements of series capacitors for transmission circuits, it is assumed that the protective equipment

1. Limits voltage on the first half-cycle of overvoltage.
2. Restores series capacitor to the circuit within two cycles after the fault is isolated.

LOCATION IN THE CIRCUIT

Series capacitors can be located at any point in a single-circuit line, but preferably near the reactance center of the system, as this limits the duty on the capacitor protective equipment, reduces the voltages to ground, and minimizes the problems of exciting-inrush currents. For multiple-circuit lines the series capacitor can be located in the individual line sections or in the common-bus connection, as shown in Figure 1. Multiple-circuit lines are normally laid out so that the circuit load can be carried with one line section out of service. For such conditions there is a decided advantage in using the series capacitor in a common-bus connection not only because the capacitor kilovolt-amperes is not reduced by the switching out of a line section, but also because fewer units are required. Several series capacitors may be used, but there is little advantage in using more than the number of intermediate switching stations.

SWITCHING ARRANGEMENTS FOR SERIES CAPACITORS

The condition of highest circuit reactance on a multiple-circuit line occurs as a result of switching operations that isolate a line section. Series capacitance may be required for the condition of highest circuit reactance but may not be needed for the normal condition with all lines in service. Hence, series capacitance can be used in two ways:

1. Series capacitor normally in the circuit, shunted for the fault condition, and quickly restored to the circuit after fault isolation.
2. Series capacitor not normally in the circuit, but switched into the circuit after a line section is switched out of service.

A combination of these arrangements so as to vary the amount of capacitance compensation according to the load or circuit arrangement is also possible. Both of these arrangements can be used with the layout of Figure 1. However, in the second arrangement the control equipment should be similar to that for the first arrangement in order to meet the condition for a second fault in the remaining section of the two parallel lines. The choice between these arrangements depends upon the relative advantages from the stability standpoint in starting from a condition of lower or higher system angle, as discussed in the section on stability.

AMOUNT OF COMPENSATION

Stability and economic conditions control the amount of capacitive reactance to be used in any particular case. Assuming that generating and receiving systems are adjusted in proportion to the load transmitted, and that equal margins of stability are to be obtained, the relative stability limits can be estimated approximately from the inverse ratio of the 60-cycle transient reactances of lines including the series capacitor. Table I lists for several circuit arrangements and amounts of capacitance compensation the relative power limits calculated for the lines including capacitors but not terminal equipment. The amount of compensation is the ratio in per cent of the capacitive reactance to the total reactance with all line sections in service. The tabulated power limits are for one line section out of the circuit and are compared to that of a two-circuit line with one intermediate station but without series capacitors. Table I shows that, neglecting resistance, 67 per cent capacitive-reactance compensation is required with a two-line system and one intermediate station to give the same power limit as a system with three lines and one intermediate station and no series capacitors, assuming in both cases a transient disturbance requiring the isolation of a faulted line section. Similarly, considering the same two-circuit line carrying a given load, Table I shows for the same system and for the same stability margin that 50 per cent capacitance compensation is required for a 50 per cent increase in power limit.

Power-System Stability With Series Capacitors

Series capacitors increase the stability limits of systems by reducing the series or transfer reactance between internal voltages of machines, and thereby increasing the synchronizing power. Thus, series capacitors increase both the steady-state and transient stability of systems. Usually the transient-stability limits are well below the steady-state limits, so that a considerable increase in the former is possible before the latter condition becomes controlling. Nevertheless, it is desirable when considering series capacitors to investigate steady-state as well as transient limits.

The stability problem with series-capacitor systems can be viewed in the same manner as that of other systems by considering only the 60-cycle reactances of the circuit. It is, of course, necessary to take into account the changes in circuit reactance produced by the inclusion of the series capacitors in the circuit, and for the shunting of the capacitors during the fault and possibly for a brief interval thereafter.

A physical picture of the system phenomena during a fault and the resulting transient can be obtained from Figure 3. The system is assumed to consist of a hydroelectric generator feeding through a series-capacitor transmission system to an infinite bus, subjected to a fault at the point *F* which is cleared by the isolation of the faulted line section. The circuit is assumed to be without loss so that the power-angle diagrams are simple sine curves. Parts of four power-angle curves are plotted for the four conditions that arise as a result of the application of a fault and the subsequent circuit-isolating operation. These include:

1. The initial condition with all lines and series capacitors in service, curve I.
2. Condition 1 plus the application of the fault and the shunting of the series capacitors, curve II.
3. Normal condition but with one line section and series capacitor out of service, curve III.
4. The system normal with series capacitors in circuit but with one line section out of service, curve IV.

Consider the system of Figure 3 to be operating at the load and angle corresponding to the point *a* on curve I. Upon the application of the fault, the output of the generator drops to the point *b* on curve II. The difference in power *ab* accelerates the generator and increases the angle by which it leads the receiver. At the point *c* the fault is to be isolated by the action of high-spread breakers and relays, thus changing the operating point to *e* on curve III. Power corresponding to *de* decelerates the generator, but because of the accumulated velocity it swings forward along curve III. At the point *h* the series capacitor is restored to the circuit by the opening of the shunting path in the protective equipment, and operation proceeds from the point *g* on curve IV. Because the energy stored during the accelerating period has not been completely absorbed, the system continues to swing forward ultimately reaching point *k*, such that the decelerating area [area (*edih*) + area (*gijk*)] equals the accelerating area (*abcd*). The system is stable for the conditions shown and will oscillate about the point *o* and ultimately come to rest at that point. The system would be unstable for the conditions shown unless the series capacitors were restored to the circuit promptly after the isolation of the faulted line section.

If series capacitors of the nonlimited-voltage type are used, the curves of Figure 3 still apply; but upon the isolation of the fault at point *c*, the operating point changes instantly to the point *f* on curve IV. In this case the total area under the power-angle curve available for deceleration is increased by the area (*efgh*). There is, therefore, some advantage in reducing the interval required for the restoration of the series capacitor to the circuit after the fault has been removed.

Examination of Figure 3 shows the great importance of using high-speed breakers and relays for circuit isolation when series capacitors are used. The duration of the fault is of greater importance than the duration of the period in which the series capacitor is shunted by protective equipment. Fortunately, high-speed breakers and relays operating in four to six cycles are available. When these are used, the energy of acceleration is greatly reduced and is easily offset by relatively small increases in the power-angle curve above the transmitted load.

The power-angle curve II of Figure 3 is based on faults of the low-resistance type. If the system faults are of the high-resistance type, such as may occur on lines without ground wires, the resultant load on the generating station may be greater than the normal output. Under such conditions radically different transient conditions obtain, as illustrated in Figure 4a. For this case, upon the application of the fault the output of the generator immediately increases from a' to b' on curve II*a*. The power corresponding to $a'b'$ decelerates the generator and reduces the angle by which it leads the receiver. At the point c' the faulted line section is cleared, and, assuming self-clearing protective equipment, the operation point changes at once to point f' on curve IV corresponding to the final circuit condition, with the series capacitor in the circuit, but with one line section out of service. Under this condition the power corresponding to f' produces a large accelerating action. However, the system, because of the accumulated velocity, continues to swing back and ultimately reaches the point

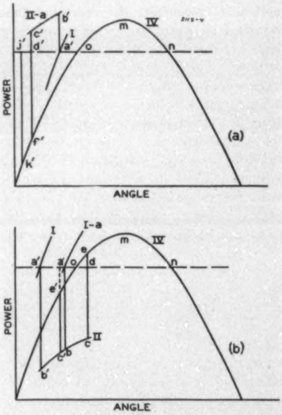


Figure 4. Power-angle diagram illustrating the effects of

(a). High-resistance fault

(b). Different initial angles

Curve I—Initial condition—all lines and capacitors in circuit

Ia—Initial condition—all lines but no capacitors in circuit

II—Fault condition—low resistance

IIa—Fault condition—high resistance

IV—Final condition—capacitors in circuit—one line section out of service

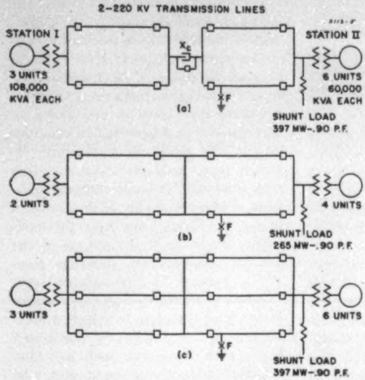


Figure 5. Schematic diagram of typical transmission line

Principal features of layouts (a), (b), and (c)

k' such that the accelerating area($d'f'k'j'$) equals the decelerating area ($a'b'c'd'$). The power system now swings forward with increasing velocity corresponding to the accelerating area ($k'oj'$). For the power-angle relations shown in Figure 4a, the decelerating area (omn) is insufficient to absorb the resultant transient and instability results.

In connection with Figure 4a, it is pertinent to consider the condition that results if all or part of the series capacitor were not introduced into the circuit until after the faulted line section is cleared. This would result in a higher circuit reactance for the initial condition and a larger initial angle at the beginning of the transient disturbance. A little consideration will show that starting with a larger initial angle, say at the point o, the system will swing back through an angle slightly greater than before, but the maximum point reached on the backward swing will occur at an angle larger than that corresponding to the point k'. Consequently, the system will swing through a small angle, and for the conditions shown in the figure stable operation will obtain. Thus for some system conditions there is a gain from the stability standpoint in starting from a relatively large initial angle and switching the capacitors into the circuit only when they are needed because of the switching out of a line section.

For a different case shown in Figure 4b, the presence of a large series capacitor in the circuit initially is disadvantageous. The fault is assumed to be of very short duration, and the capacitance compensation to be quite large for the initial condition. Assuming again that the protective equipment is of the self-clearing type, the transition is made at once from the fault condition of curve II to the final circuit condition of curve IV. For the case without capacitors initially in the circuit and the transient starting from the point a, the accelerating area is $abcd$, and the area available for deceleration is $demn$. For the case with capacitance normally in the circuit and the transient starting from a smaller initial angle at a', the accelerating area is $a'b'c'e'o$, and the area available for deceleration is omn. A comparison of these two cases shows that the system is more stable when starting from the larger initial angle; that is, from the circuit condition with the higher circuit reactance or smaller capacitance compensation.

The conditions illustrated in Figures 4a and 4b have been introduced to show the various factors involved in the problem. However, if appreciable angular swing occurs during the fault so that the transition to the final circuit condition occurs in the vicinity of the point o, then there is usually an advantage in having the series capacitor in the circuit for the initial as well as the final circuit condition. This result will vary with the number of tie points in the system and the amount of change in circuit reactance. For the usual case, however, there is sufficient movement of the machines on the system to justify the inclusion of the series capacitor normally in the circuit.

Reclosing Breakers

The advantage of reclosing breakers for improving system stability can be increased by the use of series capacitors in combination. This results from the fact that the power limit for the normal circuit conditions is large in comparison with the power limit with a line section out of service. This is illustrated by the large decelerating area under curve I in Figure 3, as compared with curve IV.

Single-Circuit Lines

Series capacitors can be used on single-circuit lines to increase the power limits, substantially in inverse proportion to the change in 60-cycle reactances. Such power systems are normally laid out to withstand disturbances that result from the loss of any transmission-line section, such as may result from the dropping of a line conductor. Accordingly, such systems are normally designed for operation close to the steady-state stability limit with the knowledge that faults on the circuit will normally result in circuit outage. Series capacitors can be used on such single-circuit lines to increase the stability limit, both for steady-state conditions and for transient disturbances resulting from faults on other line sections.

Single-pole switching with quick reclosing[2] is being used to improve the performance of single-circuit lines, particularly tie lines, from the standpoint of single line-to-ground faults. If series capacitors are added to such systems they will increase the power limit for the initial and final circuit conditions and on sound phases during fault condition. Such systems with series capacitors are, of course, subject to the limitations of other single-pole switching systems. Thus the length of line section must be limited or other measures used for insuring suppression of the arc to the faulted conductor when isolated at both ends. The system layout must be such that the time permissible from the stability standpoint is long enough to permit the reclosing cycle.

Special Application Problems

Actual transmission systems differ in a number of respects from the simplified conventional systems which have been considered hitherto in the discussion of the stability problem. For example, the preceding discussion has been based on

Table II. Characteristics of Motor-Generator Sets

Rating Damper Winding Reactance (Per Unit)	Motor Generator1 100 Kva— Copper	Motor Generator2 100 Kva— None
X_d'	0.260	0.260
X_q	0.577	0.577
X_2	0.151	0.418
T_{do}'	1.20	1.20
Inertia Constant H Kw-Sec per Kva	8.60	2.68

resistanceless circuits supplied by hydrogenerators feeding into an infinite bus. The actual system includes loss and the receiver includes a shunt load with local generators also feeding into it. For the investigation of stability and similar problems, such as spontaneous hunting and subsynchronous operation in the induction starting of machines, it is advantageous to consider a typical transmission system suitable for series-capacitor application.

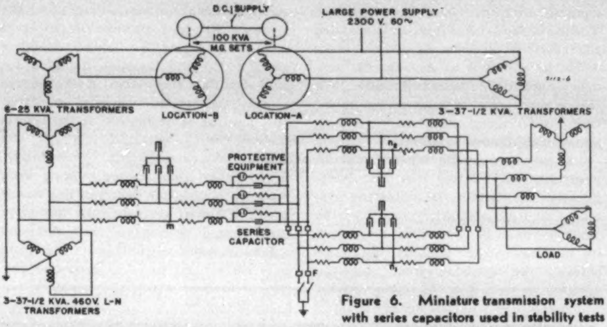


Figure 6. Miniature transmission system with series capacitors used in stability tests—simplified schematic diagram

Typical Transmission System for Series-Capacitor Application

In order to develop further the characteristics of series-capacitor systems, a typical transmission circuit in a system undergoing considerable expansion was selected for theoretical analysis and subsequently for miniature-system tests. For these purposes simplified layouts approximating certain stages of the transmission development were selected. The principal features of these are shown schematically in Figure 5. In these layouts the output of two or three Station I generators is assumed to be transmitted over two or three 220-kv circuits for a distance of 234 miles. Three particular layouts selected for study are:

Figure 5a—Two 220-kv circuits.
Three 108 megavolt-ampere generators.
With series capacitors.

Figure 5b—Two 220-kv circuits.
Two 108 megavolt-ampere generators.
No series capacitors.

Figure 5c—Three 220-kv circuits.
Three 108 megavolt-ampere generators.
No series capacitors.

Miniature-System Tests

To complement the analytical work, miniature-system tests were made in the Westinghouse stability laboratory in order to examine further the operating characteristics of a system with series capacitors. The miniature-system tests included:

1. Stability.
2. Spontaneous hunting.
3. Subsynchronous effects in the induction starting of induction motors or synchronous machines.

The layout shown schematically in Figure 6 was used for the miniature-system stability tests. Two motor-generator sets, designated as M-G 1 and M-G 2, were used in set locations *A* and *B* respectively. These sets are identical, except for inertia and damper windings, and the characteristics affected by these features are given in Table II. Dampers are of the connected type in open slots.

The miniature-transmission system was adjusted to have circuit constants corresponding to the layout of Figure 5a, choosing for the generator the direct-axis transient reactance, X_d'. These constants and the miniature-system voltages were chosen so as to load the generator to the same proportion of its rating as the generators of the actual transmission system. The excitation for the generator was controlled by an exciter-rheostatic voltage regulator maintaining constant voltage on the transmission line. The receiver system was represented by transformers and impedance to the power supply corresponding to the transient reactance of the Station II generators and transformers, and by a shunt-impedance load.

The series capacitors were connected between the two busses at the intermediate sectionalizing station. Tests were made principally with capacitive-reactance values of 61 per cent and 42 per cent of the total inductive reactance for two lines for both sections, although in a few hunting tests still other values were used. Each capacitor was provided with shunt protective gaps which included a low resistance in series to limit the discharge current through the protectors. A special self-clearing type of protective gap was used.

The layout to simulate the system of Figure 5b was obtained by correspondingly increasing the impedance of the generators, transformers, and loads in the ratio of 3 to 2. The layout corresponding to Figure 5c was obtained by reducing in the ratio of 3 to 2 the impedances of the series branches of the

Table III. Stability Limits of Various Systems for Double Line-to-Ground Fault

Layout	Series Capacitor	Station I Generator (Megavolt-Amperes)	220-Kv Lines	Miniature-System Stability Limit (Kw)
Figure 5a	61 per cent	324	2	104
Figure 5a	42 per cent	324	2	94
Figure 5b	None	216	2	61
Figure 5c	None	324	3	113

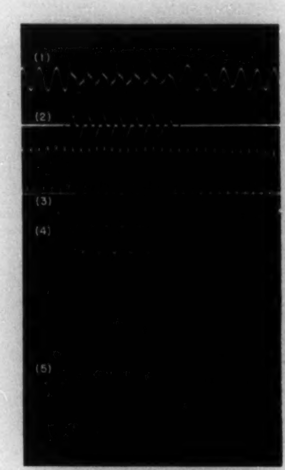


Figure 7. Typical oscillogram from miniature-system tests showing special protective gaps operating during double line-to-ground fault and self-clearing at the end of fault to restore capacitors to the circuit

1, 5—Capacitor voltages—phases *B* and *C*
2, 3—Gap currents—phases *B* and *C*
4—Voltage to neutral on phase *C*

two-lines section between Station I and the intermediate station and halving the series impedance of the unfaulted line-section between the intermediate station and the receiver.

The stability tests were run by adjusting the system to deliver the desired amount of power under specified voltage conditions. A fault was applied to one transmission line section by means of the fault breaker. Faults were of the zero-impedance, double line-to-ground type. The results of the stability tests are given in Table III.

Figure 7 shows an oscillogram for a stable condition corresponding to Figure 5a. The oscillogram shows a double line-to-ground fault of about seven cycles duration. Both protective gaps functioned during the fault condition and at the end of the fault were self-clearing. It will be noted that the voltages across the capacitors were limited by protective-gap breakdown during each half-cycle.

Observations with the aid of a stroboscopic device on the relative phase relation of the rotor of the set in the *B* location to the voltage of the power supply at receiver showed that the stability limits occurred for the three layouts of Figures 5a, 5b, and 5c at about the same phase displacements. The three systems, however, delivered different amounts of power for the same phase angle, the amounts of power being approximately in inverse proportion to the total 60-cycle transfer reactance between internal voltages.

Results of the tests given in Table III correspond to faults applied only at the intermediate station in the section between it and the receiver. This location required operation of protective gaps on the series capacitor, a circumstance unfavorable to the series-capacitor system in comparison with the layouts without series capacitors. The two sections of transmission circuit were interchanged, and the faults were applied at the sending end of the first section. The results of stability tests for this condition gave approximately the same ratio of stability limit for the layouts corresponding to Figures 5a and 5b as for faults at *F*.

Hunting Tests

Spontaneous hunting of synchronous machines is an infrequently encountered phenomenon on operating power systems.[3,4] Such hunting occurs only on systems having a high ratio of resistance to reactance, and on lightly-loaded machines and on machines which are not equipped with suitable damper windings. The use of series capacitors, by decreasing the effective 60-cycle circuit reactance would be expected to increase the tendency toward spontaneous hunting. For this reason tests were made on the miniature system using machines both with and without damper windings.

Figure 8. Oscillogram from hunting tests showing the oscillation-damping action of motor-generator set at location *A* of the miniature-transmission system of Figure 6

The first step in the experimental investigation of hunting was the determination of whether the miniature system of Figure 6 adequately simulated the electromechanical conditions in regard to hunting. The obvious difference between the actual system and the miniature system is in the representation of the prime mover. In the miniature system the prime mover is represented by a motor supplied by another motor-generator set obtaining power from an infinite bus. Tests under conditions for simulating hunting showed that motor-generator set *A* tended to follow the oscillation of motor-generator set *B*, introducing a damping action on it. Accordingly, a relatively high resistance was introduced in the supply to the d-c motor of set *B*. Under this condition, the ratio of power oscillations in output of generator *B* to motor input was over 50 to 1. This is shown in the oscillogram of Figure 8 by the single-phase watt element recording the output of the generator without dampers and the fluctuation in the direct current to the motor. It will be noted that the power oscillations in the two sets are substantially in quadrature. In order to increase the power output of set *B*, the voltage of the motor circuit was increased by adding a 250-volt shop supply in series.

A physical explanation for spontaneous hunting has been given by C. F. Wagner.[3] He showed that the tendency toward hunting depends upon the operating point on the power-circle diagram of the system, and that the oscillations have positive damping for loads beyond a given value and negative damping for loads less than this value. As stated previously, the tendency toward spontaneous hunting is increased as additional resistance and capacitive reactance are introduced into a circuit. Accordingly, the tests for spontaneous hunting were made with different values of line resistance introduced symmetrically into each equivalent *T* section and with different values of series capacitance. The tests were carried out so as to determine the critical load at which the tendency toward spontaneous hunting would be encountered. This was done by setting up the system for the desired operating condition and introducing an oscillation produced by a switching operation. The conditions were carefully examined to determine whether the oscillations corresponded to positive or negative damping. By observing the switching oscillations at several different loads it was possible to determine the critical point. Tests were carried out with both the sets, thus giving tests on

1. A generator without dampers.
2. A generator with copper dampers.

The results of the tests on spontaneous hunting are summarized in the curves of Figures 9 and 10. In Figure 9 the conditions in regard to line resistance and amount of series capacitive reactance are plotted in per cent of the normal line reactance for two circuits and two sections, not considering the reduction due to the series capacitor. In these curves the critical load is expressed as a per cent of the transient stability limit for the corresponding case as listed in Table III using the values corresponding to normal line resistance for all cases. Figure 10 gives the same data as Figure 9 but is plotted in terms of the total resistance and total transient reactance at 60 cycles between internal voltages, taking into

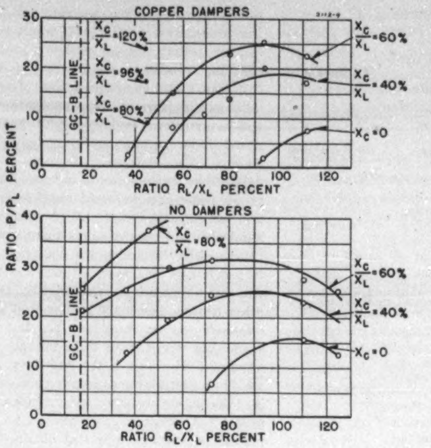


Figure 9. Critical conditions for spontaneous hunting from miniature - system tests—no hunting for points above curves

R_L, X_L—Resistance, reactance of two-circuit lines

X_C—Series-capacitor reactance

P/P_L—Ratio of transmitted power to stability limit

account the reactance of the series capacitor. The miniature system using the generator with dampers when laid out to represent the layout of the typical transmission system was stable for normal values of line resistance, even when series capacitors were used of higher reactance than contemplated for the amount of power to be transmitted. The miniature system using machine without dampers was stable when transmitting a reasonable amount of power in comparison to the stability limit. Hunting under light load is not considered important as the capacitors can be switched out of the circuit because they are not needed under this condition for stability.

A special hunting test was made to show whether series capacitors affected the hunting conditions except by changing the 60-cycle reactance of circuit. For this purpose two circuits of equal resistance and equal 60-cycle reactance were compared using

1. A circuit with only inductive branches.
2. A circuit with additional inductive reactance offset by equal capacitive reactance.

Using the generator without damper windings, no hunting was found with the inductive circuit but there was hunting with the series capacitors, although the critical load was only $2^1/_2$ per cent of the transient stability limit.

Since the spontaneous hunting has received but relatively little attention in recent years, there are little data to show how the damping on a miniature system compared with that of actual power systems. Accordingly, tests were made on the miniature-system setup for the layout of Figure 5a with a 60 per cent series capacitor, but with the system carrying half load. A line section was switched out of service and then restored, the latter transient being recorded on oscillograms, both for the machine without damper windings and for the machine with copper dampers. For the machine without dampers the natural frequency was 43 cycles, while for the machine with copper dampers the natural frequency was 80 cycles. The oscillations for these cases were reduced by about 7 and 30 per cent respectively per cycle of electromechanical oscillation. It is proposed that tests be made on the actual power system, and the results compared with those obtained from similar tests on the miniature system.

Induction Starting of Machines

An interesting test was made to simulate the induction starting of induction motors or synchronous machines. This test was made with the layout of Figure 5a with a 60 per cent capacitor but with the load and receiver system disconnected at the end of the second transmission section of Figure 6. A 10 horsepower squirrel-cage induction motor was connected at the receiving end of the transmission system. Under normal voltage conditions the motor started without any difficulty. The generator voltage was reduced to about 20 per cent of normal in order to simulate the starting of an induction motor with a heavy inertia load, and under this condition, the motor operated at a subsynchronous speed. This phenomenon of subsynchronous operation[5] is caused by resonance at a frequency below 60 cycles. At normal voltages on the system the induction motor has sufficient torque to overrun the subsynchronous speed before the torque caused by low-frequency resonance can build up. At low system voltages, the motor acceleration is slower and the torque caused by low-frequency resonance builds up to a higher value. In spite of this the motor accelerated beyond the subsynchronous speed but was unable to reach normal speed and dropped back to the subsynchronous speed. As was expected, a load of high power factor, about 95 per cent, drawing as much current as the accelerating current of the motor was sufficient to avoid operation at a subsynchronous speed and to permit acceleration to normal speed. This peculiar condition is considered of little importance from a practical standpoint, since no normal operating condition is visualized in which the difficulty would be encountered. The nearest approach is the condition of starting up a system with the series capacitor in the circuit followed by the induction starting of a large synchronous condenser at the receiver. Under these conditions diffi-

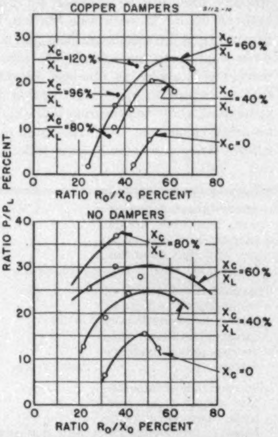


Figure 10. Critical conditions for spontaneous hunting from miniature-system tests—no hunting for points above curves

R_L, X_L—Resistance, reactance of two-circuit lines

R_O, X_O—Total circuit resistance and reactance

X_C—Series-capacitor reactance

P/P_L—Ratio transmitted power to the stability limit culty may be encountered in accelerating the condenser to normal speed. The difficulty can, of course, be overcome by switching the capacitor out of service until the condenser is in synchronism. If considerable generating capacity or considerable shunt load is connected in parallel with the condenser, no difficulty in starting is to be expected.

Economic Advantages of Series Capacitors

An example of the substantial economies that can be effected by the judicious use of series compensation in appropriate circuits will now be given. In this example the two-circuit, 230-kv line between the Station I and the Station II developments is assumed to be compensated to bring it more nearly in line with the load capacity required to develop maximum transmission economy. An additional generator at Station I and a corresponding increase in generator and load capacity at the Station II end are assumed. Series capacitors giving $66^{2}/_{3}$ per cent compensation are to be located at the mid-point of the transmission line, as in Figure 5a. This series capacitor will give with three Station I generators and two lines a greater margin of stability than will be obtained for the case of Figure 5b with two Station I generators, two transmission lines, and no series capacitors, and closely the same margin of stability as obtained in the case of Figure 5c with three Station I generators, three transmission lines, and no series capacitors. The calculated results of the economic study are as follows:

Transient stability limit of uncompensated two-circuit line with normal margin (load reduced 20 per cent for same machines)	175,000 kw
Transient stability limit of compensated two-circuit line with proportionally increased terminals with corresponding stability margin	290,000 kw
Line-reactance compensation required on complete two-circuit line base	66.7 per cent
Series capacitor required	108,000 kva total
Series capacitor installed cost at \$11.00 per kilovolt-ampere	\$1,190,000
Cost of one additional circuit with sectionalizing facilities (based on minimum construction cost)	\$4,500,000
Saving in capital outlay through use of series compensation, or 73.5 per cent of cost of third circuit	\$3,310,000
Transmission efficiency:	
Three-circuit line, full-load	95.0 per cent
Two-circuit line, compensated, full-load	92.4 per cent
Increased losses, two-circuit line as compared to three-circuit line, all sections operating	8,560 kw at max. load
Incremental loss chargeable to compensated two-circuit line at 85 per cent loss factor (equals approximately 82 per cent load factor)	6,180 kw
Cost of generating incremental loss chargeable to compensated two-circuit line at \$125.00 per kilowatt generation cost	\$772,000
Incremental short-time synchronous-condenser capacity required by the two-circuit compensated line at full load with one section out over that required by three-circuit line at full load and one section out	42,800 kvar
Incremental cost of synchronous-condenser capacity at \$4.00 per reactive kilovolt-ampere (over-load rating)	\$171,500
Total cost of compensation, or 47.4 per cent of the cost of the equivalent third circuit	\$2,133,500

The above conservative example illustrates clearly the very considerable economy that can be effected by the proper use of series compensation. If high emergency-load capacity is the primary consideration, the aggregate increase in losses is negligible, and the comparison is even more favorable.

In the case of heavy impedance loads, or of lines materially shorter than the one chosen in the above example, the loadings of maximum economy may be attainable without compensation and without giving rise to any synchronous stability problem. In such cases, excessive voltage regulation instead of instability will be the major controlling factor. The circuit loadings will be considerably in excess of the normal surge-impedance values, and series-reactance compensation can be employed to improve the voltage regulation and the power factor at the generator end of the line.

A further study of the particular application, however, may in certain cases disclose that the same kilovolt-ampere rating in shunt capacitors applied to the low-voltage bus at the receiving-end of the transmission circuit would result in slightly better voltage regulation and efficiency conditions than when that capacitor is used as a series device. This difference in favor of shunt application will be particularly evident when the load power factor is near unity or slightly lagging. The application of shunt capacitors in this case results in improved power factor and decreased transformer and line currents with decreased voltage regulation and reactive kilovolt-amperes generated in the line and in the transformer leakage reactance. If such a circuit were operating independently of other systems, and if the load were of a relatively steady character, the shunt application might prove to be the more economic. However, if the load is supplied by a multiple transmission system, or is of fluctuating character, series compensation can result in considerably improved performance over that obtained when the necessary capacitive kilovolt-amperes are used in shunt on the load bus. In such cases the problems require individual analysis to determine the most economic expedient to obtain the desired end, both as regards economy of operation and system performance.

When synchronous stability limitations prevent the most economic loadings, series compensation, up to a certain degree, will in general be found to give better economies than other alternatives. If increased equivalent surge-impedance load ratings are required, the line capacitive susceptance can in effect be increased by shunt capacitors on the low-voltage receiving bus by the same factor that the line equivalent reactance is decreased by series compensation.

Requirements of Series Capacitors and Protective Equipment

Results of the analytical studies and miniature-system tests lead to the following conclusions as to the requirements of series capacitors and their protective equipment.

Series capacitors should be of the limited-voltage type and provided with protective equipment which

1. Operates on the first cycle of overvoltage to limit the voltage stress on the insulation.
2. Restores the capacitor to the circuit in the shortest practical time after the faulted line section is cleared, preferably within not more than two cycles.

Provision will be desired in some cases to switch the capacitor into the circuit only after a line section is switched out of service. Additional protective features should be provided to prevent sustained high currents which would produce important heating effects. The protective equipment should include means for switching the capacitor into the circuit and out of the circuit as may be desired locally for inspection or repair, or by remote control as may be found advantageous from the standpoint of system operations. A small impedance in the shunting circuit is advantageous in limiting condenser discharge current; in addition, resistance tends to damp transient currents and improve stability.

Conclusions

Analytical studies, including the economic problem, have shown that long high-voltage transmission circuits can be operated at their optimum loadings only when their equivalent impedances, or effective lengths, are reduced by a sizable factor. This reduction in transfer impedance can be accomplished most economically through the use of series compensation. Such applications on appropriate systems would result in considerable over-all economic gains. Although in the shorter lines of lower voltage, independent economic gains may not be possible through the application of series compensation, these circuits can be increased in emergency capacity and electrical stiffness to a marked degree by the application of appropriate reactance compensation.

The analytical work and the miniature-system tests have shown that the stability of transmission systems with series capacitors can be estimated from consideration of the 60-cycle constants of the circuit in the conventional manner. Such estimates or calculations of stability should take into account the decreased circuit impedances that result when series capacitors are in the circuit and also the transient changes that result from the operation of protective equipment.

Transmission systems with series capacitors are not subject to spontaneous hunting, providing machines are equipped with suitable damper windings, or provided the system is not operated at too low a percentage of its normal rating. By observing practical operating restrictions at light load, it is possible to avoid problems of hunting and subsynchronous operation in the induction starting of machines.

The proposed scheme of using series capacitors of the limited-voltage type, in combination with protective equipment that quickly restores the capacitor to the circuit after fault isolation, is viewed as providing attractive possibilities for increasing the permissible and economic loading of long transmission circuits. Series-capacitor schemes have inherent advantages for increasing the permissible short-time loads or for increasing the emergency loads of existing systems. The scheme is viewed as sufficiently promising as to justify a large-scale experimental installation.

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Lightning Investigation on 132-Kv Transmission System of the American Gas and Electric Company

Discussion and authors' closure of paper 42-18 by I. W. Gross and G. D. Lippert, presented at the AIEE winter convention, New York, N. Y., January 26–30, 1942, and published in AIEE TRANSACTIONS, 1942, April section, pages 178–85; previous discussions published in AIEE TRANSACTIONS, 1942, pages 450–3.

R. H. Golde (British Electric and Allied Industries Research Association, London, England): The maximum values of lightning currents which may flow in the various parts of a transmission system are of great interest to all engineers concerned with the protection of overhead lines, and the authors' continued collection and latest presentation of statistical data are a most valuable addition to our knowledge of this subject. In their discussion, however, the authors draw attention to two features in their results which they suggest are in conflict with the present knowledge of surge phenomena and, in particular, that relating to the effective surge impedance of line conductors. The probable explanation of these results is as follows:

Table A gives the maximum voltage values which would be obtained if the maximum line currents plotted in Figure 5 of the authors' paper were multiplied by a surge impedance of 400 ohms. The resulting products, if regarded as voltage surges on the line conductors, are rightly described as fantastic. I should therefore like to suggest that the relatively few high current values given in Figure 5 were actually measured in cases in which a direct stroke occurred to a line conductor somewhere in mid-span with immediate flashover to earth at the first tower or towers encountered. In this case the short length of line wire between the point struck and the first tower carries practically the whole lightning current, but as its length is small compared with the equivalent length of the front of the average lightning current, it is misleading to attribute to it any surge-impedance value. The discharge phenomenon is simply that of a charged condenser—the lightning channel—discharging to earth through a short path with its inherent capacitance and inductance values, the conditions being very different from those obtaining for a long line conductor. The resulting surge voltages which are propagated along the line would then be given by the line currents measured beyond the region where discharge to earth takes place multiplied by the correct surge impedance of the line.

The second point which, according to the authors, "requires some further study or interpretation" concerns the line surge impedance values derived in Table II. These values are much too small to be explained by any known effect, including that of corona. Now it appears that the current and voltage values quoted in Table II were measured when discharge of the arresters protecting the stations in question took place. Under these conditions reflection phenomena invalidate the calculation of the line surge impedance as the quotient of the bus voltage and line current. An accurate calculation could be carried out only if the discharge characteristic of the arrester, the shape of the incident surge, and the distance between the recording points and the discharging arrester were known. If, in the absence of these data, it be assumed, as a first approximation, that the measured line currents and bus voltages reach their crest values at the same instant and that the incident surge has a very short front and long tail, then

$$E=e\frac{2R}{Z+R}$$

and

$$I=i\frac{2Z}{Z+R}$$

where

E = busbar voltage
e = voltage of incident surge
I = resulting line current during operation of arrester
i = current of incident surge
Z = line surge impedance
R = equivalent discharge resistance of arrester

Since e/i is equal to Z, it follows that the quotient E/I is independent of the line surge impedance and is in fact equal to the equivalent discharge resistance of the arrester at the instant when both bus voltage and line current reach their maximum. The resistance values quoted in Table II are quite reasonable if thus explained, and there is no need to invoke any change in the accepted value for the line surge impedance.

There are several items on which further comments might be made but only the most important may be mentioned. Returning to Figure 5, it has been indicated that the highest current values recorded are probably not line currents in the sense of surges which are propagated along the line. Assuming that currents of the order of 3,000 amperes are the highest which can travel along the line conductors without causing flashover, it is seen from the two curves in Figure 5 that some 75 per cent and 90 per cent respectively of the currents recorded fall within that range. These currents may therefore be due to cases of back flashover, though it would also be interesting to get the authors' information as to the maximum possible earth fault currents on these systems, since it may be feasible to explain at least some results by normal frequency earth fault currents following a flashover at one or more towers. It follows that Figure 5 consists of two parts, that is, effects of direct strokes to line wires and cases of back flashover, which are not strictly comparable.

The beneficial effect of bonding the tower legs below the ground surface is convincingly shown by the currents discharged through these connections. The authors regard this effect to be due to "an even, uniform, and quick distribution of current to the various tower structural members." It is difficult to see why the lowest cross members of lattice towers should not be equally effective, and I should suggest that the effect described is primarily due to these additional connections acting as earth electrodes.

The good result to be obtained by the use of long earthing tubes, particularly in soil with a top layer of high resistivity, has long been recognized by practical experience. The authors' current measurements provide, therefore, interesting direct confirmation. On the other hand, the meager effect of a ten-foot tube (Table III, case 2), installed as shown in Figure 2, is hardly surprising, since this tube which is placed between tower legs of equal length to itself cannot be expected to contribute materially to the combined earthing resistance of the tower. It would be interesting if the authors could install some earthing tubes at the ends of the 40-foot counterpoise wires and determine the current distribution for that case.

I. W. Gross and G. D. Lippert: The discussion of the paper by Mr. Golde is most welcome, and we are gratified to note that the data are helpful in furthering the study of lightning phenomenon. Some of the items discussed by Mr. Golde have been previously discussed by Mr. Hagenguth and others and have been commented upon by the authors. Further comment on these items by the authors at this time does not appear necessary.

The surge crest ammeter links on the line conductors were so adjusted and calibrated that the maximum possible 60-cycle fault currents (in the order of 2,500 amperes) would not materially affect the results.

The beneficial effect of bonding the four tower legs together below ground is apparently not obtained by the diagonal bracing of the tower above ground. Lightning-current measurements (not reported in the paper) made on four leg towers above the point of connection of the first lattice braces indicated the lightning current was concentrated in the two legs directly connected to the counterpoise where the underground bonding was not in use.

The 10-foot ground rods reported in the paper were not installed with any expectancy of obtaining an appreciable decrease in the over-all tower-footing resistance. These short rods were installed for the sole purpose of studying their performance in combination with longer rods. It was believed that the effect of these short rods would be largely obscured by the longer rods and the 250-foot counterpoise connections, and the observed results to date have indicated that this is precisely what happens.

Rectifier Terminology and Circuit Analysis

Discussion and authors' closure of paper 42-83 by C. H. Willis and C. C. Herskind, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 496–9.

I. K. Dortort (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): A uniform system of terminology for the circuit elements of rectifier systems is highly desirable, and any attempt to bring some order out of the present confusion deserves everyone's co-operation. To that end, it is suggested that we retain certain terms of the present terminology.

The greatest disagreement probably exists concerning the terms half-wave, full-wave and single-phase rectifiers. The true single-phase rectifier is recognized as a half-wave rectifier by all. It is nothing more than a simple check valve seldom used in power circuits. If we dispose of it by giving it some such name as "simple half-wave" or "valve," the problem of naming the other types of circuits is very much simplified. We could then go back to the terms half-wave and full-wave rectifiers, terms which are generally acceptable and understood by the majority of people using rectifiers. The terms would apply strictly to the conductively connected transformer windings or circuit elements.

The terms suggested by the authors are similar to the German "one-way" and "full-way" connections. Hermann in *Elektrotechnische Zeitschrift*, December 1938, does not indicate that the use of these terms has improved the situation in Europe over our own.

It is believed that it would be better to remove the full-wave or "double-way" rectifier from under the heading of "simple rectifier." The full-wave rectifier partakes of the properties of both the "multiple" and "cascade" rectifiers in that the d-c voltages are added, but the ripple voltages do not coincide, producing a ripple of higher order. This applies quite generally to any cascaded half-wave or full-wave rectifiers whose phases do not coincide.

More thought should be given to the problem of properly naming rectifying devices or valves which may be used for purposes other than rectification. They may perform the function of inversion, frequency changing, commutation for "commutatorless motors." The function of inversion and rectification may be embodied in one device. In all of the functions listed here the rectifiers act as commutators. Some years ago Brown Boveri adopted the generic term of "mutator."

An easily evaluated factor providing a common denominator for a large number of rectifier circuits will certainly prove useful. To a very limited extent, the percentage reactance of the transformer has been used that way in the past. The reactance factor given in the paper can be readily modified to make it universally applicable at least to the commonly known and used power rectifier circuits.

The commutating reactance is the total lumped reactance between two anodes which undergo commutation, divided by two. In other words, it is the reactance attributed to each anode in a balanced system. This quantity is called x_c in the paper. In the same way we can call the commutating voltage, E_c, one-half the voltage appearing between the two anodes. This is, of course, the anode to neutral voltage, $E_s \times \sin(\pi/p)$. The peak value of the current which would flow in a short circuit between these two anodes is, $(\sqrt{2}E_c)/X_c$. Let us call this value I_c'. The shape of the anode current during commutation is a segment of a sine wave of this amplitude.

The ratio $(I_cX_c)/(\sqrt{2}E_c)$, where I_c is the total direct current to be commutated by an anode, is akin to the so-called percentage reactance of the transformer and might be called the per unit reactance drop. Actually it is equal to I_c/I_c' and could also be called the per unit load based on the peak short-circuit current.

If one uses $(I_cX_c)/(\sqrt{2}E_c)$ as the reactance factor and calls it F_x for convenience, the equation for the overlap becomes $\cos(u + oc) = \cos\alpha - F_x$. This relationship holds for any number of phases.

Another interesting property of this modified reactance factor can be demonstrated very easily. The theoretical no-load voltage of the rectifier is $\dfrac{\left(\sqrt{2}E_s \sin\dfrac{\pi}{p}\right)}{\dfrac{\pi}{p}}$. This can be converted to or derived directly[1] from Goodhue's relationship to $\dfrac{\sqrt{2}E_c}{\dfrac{\pi}{p}}$. The drop in voltage on the d-c side due to reactance is $\dfrac{I_cX_d}{\dfrac{2\pi}{p}}$ as shown in the paper. The per unit d-c voltage drop is therefore $^1/_2[(I_cX_c)/(\sqrt{2}E_c)]$ or $^1/_2F_x$, regardless of the number of phases or the types of windings! And this ratio holds true whether it is a half-wave or full-wave rectifier! Should the need ever arise, this relationship can be applied to unsymmetrical circuits by readily means of Goodhue's rectifier calculus.

The curves given for the a-c current harmonics and d-c voltage ripple harmonics apply only to a restricted number of systems. Since F_x is a common parameter for more systems than $(I_cX_c)/E_s$, curves plotted against F_x should have more general application.

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C. H. Willis and **C. C. Herskind:** The modification of the reactance factor as suggested by Mr. Dortort so as to make it universally applicable to the different types of rectifier circuits has been considered by the authors. Such modification would be desirable if it provides the simplest and most convenient procedure for describing and calculating rectifier characteristics. However, a careful consideration of the number and form of the characteristic curves required to describe the more commonly used rectifier circuits and a study of the procedures involved in the use of these curves by the rectifier designer, indicate that the reactance factor as defined in the paper has a number of advantages over the other definitions which are possible.

If the reactance factor is defined by the quantity I_cX_c/E_s, a complete set of curve sheets will be required for each type of circuit, classifying the circuits in accordance with the value of P. While the angle of overlap can be shown on a single curve sheet for all types of circuits, if the reactance factor is defined by the full third term of the overlap equation, most of the other characteristics such as overlap factor, power factor, harmonic voltages and currents, interphase transformer voltage, and so forth, have values which are functions of both the angle of overlap and the number of phases P. Therefore, these characteristics require different curve sheets for the different values of P, regardless of the manner in which the reactance factor is defined. Furthermore, the reactance factor I_cX_c/E_s represents the simplest form in which the essential factors may be combined in a single quantity and should be easiest to use.

Thermal Co-ordination of Motors, Control, and Their Branch Circuits on Power Supplies of 600 Volts and Less

Discussion of paper 42-79 by B. W. Jones, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 483–7.

Jerome J. Taylor (The Detroit Edison Company, Detroit, Mich.): The approach to the subject of generalized protection of motors and lines is interesting and direct; the subject is a live one now, because many people do not realize how explosive a low-voltage fault can be when source capacity is large. In the latter respect ordinary cartridge fuses, properly constructed and installed, are remarkable both as to current-interrupting and current-limiting capabilities, with the result that in many cases they will clear the circuit with no disturbance whatever, regardless of prospective short-circuit current. This is somewhat more than can be said for most breakers in spite of the comparatively high cost of other types. The combination of such fuses with overload relays of the type described in the paper, appears to give complete protection to the equipment involved, and to the flexible in its application to individual requirements.

Selenium Rectifiers and Their Design

Discussion and author's closure of paper 42-86 by J. E. Yarmack, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 488–95.

C. G. Howard (Fansteel Metallurgical Corporation, North Chicago, Ill.): This paper is timely and one I consider highly beneficial to the members of AIEE engaged in the application or use of rectifiers.

The subject has been very well handled by Mr. Yarmack, who has been associated with the manufacture and application of the selenium rectifier since its introduction in the United States about four years ago.

I have closely followed the application and growth of the selenium rectifier in the electrical industry since its development in Europe about 1932. Having been associated with the manufacture of rectifiers since 1924 for various branches of the railway, electrical, and radio industries, I have been highly interested in basic developments of rectifiers of the electrolytic and dry disc types.

Our company's first application of the selenium rectifier was in the railway signal field, where it has been accepted by a large number of the railroads as a highly reliable means of charging signal batteries, and supplying d-c power for various signal and communication purposes.

Having studied and tested the selenium rectifier for about ten years, I am well acquainted with its characteristics and limits. To my knowledge, the selenium rectifier has been highly satisfactory in field performance wherever properly applied, and service conditions taken into account.

Selenium-rectifier equipment manufactured by our company, incorporating hundreds of thousands of selenium plates, has established field service records which substantiate the performance and life claims made for this rectifier.

I did not attend the meeting at which the paper of Mr. Yarmack was presented, so I am not familiar with any comments which may have been made from the floor regarding the selenium rectifier. I can only say that those who are interested in investigating the true properties and characteristics of the selenium rectifier, will find it to be a very useful development.

E. A. Harty (nonmember; General Electric Company, West Lynn, Mass.): The method used by the author to obtain the constants of the rectifier circuit, as well as those of the rectifier unit, appears very elaborate and less direct than the following procedure:

Inasmuch as the voltage impressed per element is quite definite and cannot be exceeded without danger of breaking down the blocking layer, there is a definite maximum average d-c voltage that can be obtained for each type of circuit at the maximum rated current.

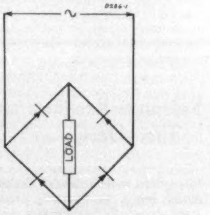


Figure 1. Full-wave single-phase bridge-type circuit

For example, for a single-phase full-wave bridge circuit such as shown in Figure 1 of this discussion, the d-c voltage at full load is 12 volts. Therefore, for every 12 volts required, one cell per leg would be needed.

A family of curves as shown on Figure 2 of this discussion permits estimating the a-c voltage required for each cell per leg, used in the proposed rectifier circuit. Values are shown at different current densities. All that is required is to multiply the readings obtained by the number of series cells used.

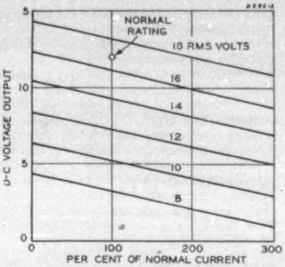


Figure 2. A-c–d-c voltage of unit bridge selenium full-wave rectifier (resistance load)

The author frankly admits that the selenium rectifier ages and that both the output and efficiency decreases with time. However, according to my records based on ten years' experience, the author's "dv" term increases more than 50 per cent. Aging data accumulated from foreign and domestic tests, by the writer, seem to indicate that all selenium rectifiers which were manufactured in Europe previous to 1938 aged very badly if operated at full voltage and that their resistance more than doubled. Typical aging curves are shown in Figure 3. Aging data on units manufactured since 1938 appear to show promise of better aging. Domestic cells are relatively new, and aging data do not exceed two years' duration, and only time will tell how they will age.

Inasmuch as not more than 18 volts rms can be safely applied to selenium elements a family of curves should be given similar to Figure 2, except using cells that have aged fully. To prevent overloading cells, this latter curve should be used for design purposes and not the curves for new elements, as recommended by the author.

Aging in rectifier circuits where the rectifier resistance changes with time can be minimized by making the rectifier resistance a small percentage of the total circuit resistance. Figure 4 shows this graphically. The use of fins, wider spacing, and fans results in making the rectifier resistance a larger portion of the circuit resistance. Assuming that the author's "dv" term, which is a function of the rectifier resistance, changes only 50 per cent by reference to Figure 4, the amount of aging can be anywhere from 5 to 22 per cent depending on whether the rectifier resistance is 10 or 50 per cent of the circuit resistance.

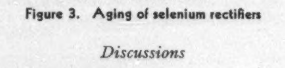


Figure 3. Aging of selenium rectifiers

Hence, overrating of selenium rectifiers should be watched carefully. D-c voltages should always be derated, to prevent increasing the a-c voltage beyond the safe limits as specified by the manufacturers, which may result in rectifier failures.

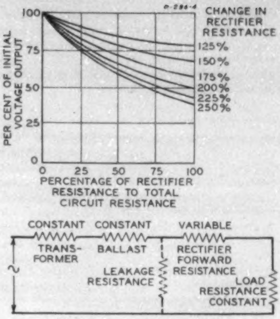


Figure 4

John E. Yarmack: Although Mr. Harty's suggested simplified method of design of selenium rectifiers would be useful in some cases, it would not, in the author's opinion, serve the purpose when one deals with a large variety of plates of different assemblies and sizes, and for all possible circuits, kinds of loading and types of service.

As to the aging of selenium rectifiers, the discusser illustrates a marked progress made in the technique of manufacture of selenium rectifiers. The illustrations and comments, however, lack information as to the extent of these tests and the conditions under which they were performed. It is worth reiterating, however, that not voltage per plate, but the current and the ultimate temperature of the plates created by current flowing, is the real factor causing higher or lesser degree of aging of selenium rectifiers.

Reactance and Skin Effect of Concentric Tubular Conductors

Discussion and author's closure of paper 42-78 by H. B. Dwight, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section pages 513–18.

H. W. Bousman (General Electric Company, Schenectady, N. Y.): Those of us who use concentric conductors will welcome this latest contribution to the design of such conductors. Some years ago Professor Dwight taught us to express the resistance ratio of an isolated tube in terms of the square root of the ratio of frequency to resistance per unit length. This parameter has proved to be a great timesaver for the designer who may have to deal with a variety of conductor materials. Even when copper is used, the parameter is convenient and useful, because it minimizes the computation required to show the effect of temperature on the resistance ratio. Would it be possible to put the resistance ratio of an outer return conductor in terms of this parameter?

Incidentally, there is a relation between the resistance ratio of an outer tube and that of the same tube used as an isolated conductor. The symbols are those of the paper.

$$\left(\frac{R_{ac}}{R_{dc}}\right)_{\substack{\text{as outer}\\ \text{conductor}}} - 1 = \left[\left(\frac{R_{ac}}{R_{dc}}\right)_{\substack{\text{isolated}\\ \text{tube}}} - 1\right]\frac{r}{q}$$

This is a simple slide-rule procedure for design use in determining the resistance of outer tubular conductors from the author's previously published curves for isolated conductors.

H. B. Dwight: In reference to Mr. Bousman's discussion, the curves of Figure 2 may be plotted on mt where

$$m = \sqrt{\left(\frac{8\pi^2 f}{\rho}\right)}$$

as shown in equation 11. The quantity m is a constant, for Figure 2. One may write

$$mt = t\sqrt{\left\{\frac{8\pi f}{R(r^2-q^2)}\right\}} \qquad (1)$$

where R, which equals

$$\frac{\rho}{\pi(r^2-q^2)}$$

is in abohms per centimeter. By multiplying the horizontal readings for each curve in Figure 2 by $\sqrt{r^2-q^2}/t$ times a constant, one can plot the curves on $\sqrt{(f/R)}$, as was done in AIEE TRANSACTIONS, volume 41, 1922, page 189. If desired, curves plotted on mt may be accompanied by the equation

$$mt = 0.0277\sqrt{\left\{\frac{tf}{R(d-t)}\right\}} \qquad (2)$$

as was done in the *General Electric Review*, April 1930, page 250, R being in ohms per 1,000 feet. Where the tubular conductors are stranded, it is undoubtedly preferable to use R, the resistance of the conductor for some assigned length, rather than ρ the resistivity.

All of the curve sheets based on R require a slide-rule operation before reading from the curves, but Figure 2 requires no slide-rule work, for solid copper tubes, unless one is using a different frequency or resistivity.

For a frequency f, multiply the tube thickness by $\sqrt{f/60}$ before reading from the curves, as stated after equation 29 in the paper.

For resistivity ρ_1, multiply the tube thickness by $\sqrt{\rho/\rho_1}$ before reading from the curves, where ρ is the resistivity of standard annealed copper at 75 degrees centigrade, in the same units as ρ_1. In this way, changes in resistivity caused by the metal used or the temperature, may be allowed for.

The stranding of tubular conductors has often been a source of misunderstanding. Such conductors are really a mixture of metal and air, and ρ_1 for a stranded conductor should be obtained by dividing R, the resistance of the conductor per unit length (one centimeter or one inch), by $\pi(r^2-q^2)$, the gross area of cross section (square centimeters or square inches).

The equation given in Mr. Bousman's discussion should be useful at low frequencies, for the range of Figure 2, that is, for the resistance ratio of the outer tube less than about 3. It can be derived by omitting the later terms of the low-frequency solution, and it can be checked by readings from Figure 2. For instance, for $t = 0.9$ inch and $t/d = 0.20$,

$$\frac{R_{ac}-R_{dc}}{R_{dc}}\text{ (outer)} \div \frac{R_{ac}-R_{dc}}{R_{dc}}\text{ (isolated)}$$

$$= \frac{1.80}{1.10} = 1.64\text{, from Figure 2}$$

or

$$= \frac{r}{q} = \frac{1}{0.6} = 1.67$$

by Mr. Bousman's equation.

For frequencies high enough for the later terms of equations 36 and 37 to be discarded, a different result is obtained, namely,

$$\frac{R_{ac}\text{(outer)}}{R_{ac}\text{(isolated)}} = \frac{r}{q}$$

However, this has no apparent application, for the natural procedure would be to use the high-frequency formulas directly.

Temperature-Aging Tests on Class-A-Insulated Fractional-Horsepower Motor Stators

Discussion of paper 42-82 by J. A. Scott and B. H. Thompson, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 499–501.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): The paper is a useful addition to the knowledge of class A materials, which (latter) are a large proportion of all insulation now in use. With wartime conditions exerting pressure toward increased loading of equipment, such knowledge is of very great value. Nevertheless, it is to be hoped that eventually a low-cost insulating material can be developed that will operate indefinitely at high temperatures without deteriorating. The temperature-sensitivity of most present insulations, compared to copper and steel, has for years past forced many designs and ratings to be based on this one factor alone. There is now reason to think that the severity of the restriction will soon be eased. When this is accomplished, ratings can be based on other factors such as efficiency, voltage drop, torque, and so forth, which have a more logical relation to performance, and the field will then be open to new and interesting designs.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors are to be congratulated on the practical method of making tests on small motors. As pointed out in the paper, their findings of degrees' increase to half the life is in fair agreement with what has been found for class A insulation used in transformers. If it were practical to make similar tests on transformers, we might find that the results would not check the 8-degree rule, which I have used for several years, as well as Scott and Thompson found, namely, a 10.5-degree, 11-degree rule and, in one case, a 15-degree rule. The reason for wide variations found in the length of life is given in the following statement which appeared in a paper entitled "Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperatures" presented at the summer convention:[1]

> "The estimated life of a transformer is at best only a rough approximation. The dielectric strength of insulation does not deteriorate until it has become embrittled and cracked. It is possible therefore for a transformer to continue to operate long after the mechanical life of its insulation is well used up unless subjected to excessive mechanical stresses (short circuit, handling, or other mechanical shocks), since its normal stresses are quite low. It does not follow therefore that a transformer will fail when its insulation is embrittled; on the contrary, if severely strained it might fail before the mechanical strength is completely used up."

While no one can claim to predict the exact life of apparatus by any rule, we are making progress. I feel that the authors of this paper have made a very worth-while contribution to this complex problem.

REFERENCE

1. EMERGENCY OVERLOADING OF AIR-COOLED OIL-IMMERSED POWER TRANSFORMERS BY HOT-SPOT TEMPERATURES, V. M. Montsinger, P. M. Ketchum. AIEE TRANSACTIONS, volume 61, 1942, pages 907–16.

High-Voltage Fusing of Transformer Banks

Discussion and authors' closure of paper 42-80 by H. H. Marsh, Jr., and G. B. Dodds, presented at the AIEE North Eastern District meeting, Schenectady, N. Y., April 29–May 1, 1942, and published in AIEE TRANSACTIONS, 1942, July section, pages 533–5.

Bruce O. Watkins (nonmember, Rural Electrification Administration, St. Louis, Mo.): It is gratifying to see that the authors give consideration to the setting and the type of the protective device on the load side of the transformer bank before deciding on a fuse for the supply side. On Rural Electrification Administration systems it usually is necessary to install as high a rating of device as possible, consistent with short-circuit protection of the transformers on the load side, in order to obtain a sufficient number of co-ordinated overcurrent devices in series for sectionalizing the rather lengthy lines. Often severe limitations in sectionalizing the system have been imposed by the supplying utilities' requirements for the supply side fuse.

Some operators also do not seem to realize that the time-current characteristics of fuse links of the same rating, but made by different manufacturers, are totally different. It is entirely insufficient to specify the fuse rating, and probably the better method is to define the required time-current characteristic.

It is to be hoped that other utilities will follow this lead, and that the practice of fusing for a specified time full-load transformer rating will soon become obsolete.

H. L. Rawlins (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is a very welcome one in that it recites the experience and practices with high-voltage fuses. There has been a dearth of information dealing with high-voltage fuses from the operators' viewpoint, and it is hoped that this paper will open the field for further discussions.

The direct comparison between fused stations and unfused stations is very enlightening, and vividly reveals the ability of high-voltage fuses to rapidly clear a fault before serious damage can occur or the fault spread to adjacent apparatus.

The authors have stressed the use of 34.5-kv fuses on a 22-kv system. This practice may have been desirable at one time and no doubt was adopted before the advent of laboratory tested fuses of high interrupting ability. The requirement of the company with which I am associated is that high-voltage fuses must satisfactorily interrupt all values of current from the minimum current that will melt the link to currents in excess of the interrupting rating, with rated voltage across one unit, on circuits having a rapid rise of transient recovery voltage. Many years of satisfactory operating experience with high-voltage fuses meeting this requirement indicate that overvoltage fusing is not justified. Furthermore, certain types of fuses, when of higher-voltage rating than required, may, during interruption, produce voltage surges in excess of the insulation strength of the system.

Of particular interest are the authors' remarks concerning proper maintenance of high-voltage fuses. Such fuses, protecting transformer banks, when properly applied with respect to secondary protective devices, will never blow unless there is a fault within or immediately adjacent to the transformer. It is conceivable that such a fuse could be in service 20 years or more before being called upon to interrupt a fault. It is apparent, therefore, that a definite program for the inspection and maintenance of high-voltage fuses is required, and it would be interesting to know the requirements set up by the Duquesne Light Company.

H. H. Marsh, Jr., and G. B. Dodds: In Mr. Rawlins' comments of this paper he has brought up two points that make further explanation by the authors desirable. The first of these is his question as to our reasons for using $34\frac{1}{2}$-kv fuses on a 22-kv system. As Mr. Rawlins surmised, this practice was adopted in the early days when original spacings and construction of fuses and mountings were found to be inadequate. As pointed out in the paper it was found that the higher-voltage rating fuses cured some of the early difficulties.

Since that time the manufacturers have developed and tested their fuses to the point where reliable and satisfactory operation of fuses at their nominal rated voltage is satisfactory. In the meantime, however, we had installed a considerable number of the fuses of a higher-voltage rating, and we have continued that practice in the Schweitzer and Conrad type *D* and *C* fuses, in order to simplify the replacement and the storeroom stock of fuses. In the use of fuses on other voltages and in the use of fuses of other types, where the usage commenced after satisfactory fuses of nominal system voltage rating were available, we have not continued the practice of using overvoltage fuses.

In the second point brought up by Mr. Rawlins in which he called attention to the fact that fuses may be in service for long periods of time and not be called upon to interrupt a fault, he recognizes that it is difficult to test the condition of such fuses and invites information as to what practice is followed on the Duquesne Light Company system.

As the Schweitzer and Conrad liquid-type fuse is the only fuse we have had in use for a sufficiently long period of time to obtain worth-while experience, our comments will be restricted to this type of fuse. Defects that have been located by observations are low liquid, cracks in the glass tube, and discoloration of the liquid. In the event of the first two of these items, the fuse should obviously be replaced. In connection with the discoloration of the liquid, experience must indicate whether this is a normal or abnormal discoloration. Normal discoloration consists of the liquid assuming a slightly brownish tinge but remaining clear, while abnormal discoloration may cause a milky appearance or a precipitate. In some cases of improper sealing, water may have gained access to the liquid.

In regard to other types of fuses, our experience has been limited either to too few a number or too short a period to be of any value in determining whether the fuses are in good operating condition or not. We would welcome advice from the manufacturers of the Schweitzer and Conrad type *SM* and the Westinghouse *BA* fuses as to how we could determine whether such fuses are in satisfactory operating condition.

Sleet Problems on Electrified Railroads

Discussion and author's closure of paper 42-121 by H. F. Brown, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 589–93.

F. E. Snell (Cleveland Railway Company, Cleveland, Ohio): The problem of sleet prevention in the light traction industry came into existence simultaneously with installation of the first trolley wires strung in localities subjected to freezing weather. Because of geographical location, some cities are naturally more liable to sleet storm damage than others which are situated in the more temperate zones. For this reason the methods used in combating sleet may differ greatly in different sections of the country.

During the last 50 years many devices have been used with varying degrees of success for effective removal of sleet from trolley wires. Some of these devices were soon discarded, because the trolley wire was greatly damaged by their use.

The trolley wheel, used extensively on street cars, has an average life of approximately 12,000 miles. In spite of this average life, a single bad sleet storm may wear out many wheels which are almost new. Carbon shoes, used on modern street cars and trackless trolley coaches as current collectors, are critically affected by sleet, and much trouble is experienced under such circumstances.

In addition to trolley wheel and shoe damage, trolley wires may also be damaged to such an extent that several wire failures occur during a prolonged severe storm. Sleet, accompanied by high winds, may cause considerable damage to overhead plant as well as costly service interruptions.

Without doubt, preparedness is the most important ally of a traction company in combating sleet. Some sections of the country are very susceptible to sudden drops in temperature. Should this occur during a rain storm and at a temperature slightly above the freezing point, formation of ice on trolley wires is a logical result. Any operating difficulties which may develop, however, depend not only upon the severity of the storm but also upon the preparedness of a company promptly to remove ice as it forms. Experience proves that street-car or trackless-trolley overhead can be kept operative as long as vehicles can move in the street.

Contrary to popular belief, neither expensive equipment nor an abnormal organization are necessary to keep overhead plant relatively free from ice during a freezing storm. The one important fact is to know what is coming and be prepared for it. When weather changes may be expected, a short-wave radio tuned to pick up aviation weather reports at stated intervals of time is of great assistance. This, accompanied by a check with the local weather bureau, will permit an operator to foresee the possibility of sleet by several hours. If it is raining when the temperature is slightly above 32 degrees, frequent thermometer readings will indicate any change toward the freezing point.

Co-ordination of all available weather information is of great importance. This work is often one of the duties of a trouble dispatcher, or it may be assigned to some other authorized employee who is capable of determining the probable condition of the weather. When ice threatens to damage property and disrupt service, it is his responsibility to notify the proper officials as far in advance as possible.

At such a time service on overhead plant should be increased as much as possible, because the frequent passage of trolley wheels or shoes will prevent minor ice formation. If conditions become worse, carbon shoes should be removed from the vehicle and replaced with old steel shoes which have grooves worn into their faces. The relatively soft carbon has very little cutting effect and is conducive to severe arcing, whereas the action of the steel shoes is much more pronounced. Frequent service on all wire, accompanied by the use of steel shoes on those vehicles normally equipped for shoe operation, will permit satisfactory operation during average storms.

In extremely severe cases "sleet cutters" should be used. Such a cutter may be attached to the standard trolley wheel for street-car operation, but in the case of trolley coaches the sleet shoes should be substituted for steel shoes. A minimum number of coaches should be provided with sleet shoes at first and the number increased, if necessary, dependent upon the severity of the storm.

A severe storm can also be combated by the use of a line truck. The truck should be equipped with two old trolley bases and two trolley poles having rigid harps and sleet-cutting wheels should be available for instant use. When in service the base spring tension should be set to approximately 40 pounds. This excessive force of the wheels against the wires will effectively remove a heavy coating of ice. As soon as a storm subsides or ice ceases to form, all cutting shoes or wheels should be removed immediately, because they are liable to score the wire if used excessively.

Necessary investment in special equipment is surprisingly low and does not require an increased maintenance personnel.

In closing, two facts are of prime importance and they must never be forgotten or overlooked. Know what is coming and be prepared when it arrives. Experience has often proved that it is easier and cheaper to *keep* out of trouble than it is to *get* out of trouble.

D. R. MacLeod (General Electric Company, Erie, Pa.): Sleet storms can cause serious interruptions in service on electrified railroads. Fortunately, the type of sleet storm which causes interference with electric operation is not of frequent occurrence, but, since it is the aim of the railroads and rapid transit systems to maintain their schedules under all conditions, the operators make provision for this type of emergency also.

One of the difficult problems is that of cars and locomotives lying in yards over night with their pantographs down. Ice may form to such a thickness as to prevent the raising of the pantographs. It would be interesting to know what methods are used to insure getting them in service promptly during sleet storms. When the pantograph has been raised, electric methods of heating provide the best-known means of keeping the pantographs free from ice. The equipment required on an a-c locomotive or car is relatively inexpensive and can be installed in the space available at the base of the pantograph. The equipment must be designed to withstand normal and transient voltages on the trolley and should be protected against faults to ground at the pantograph.

Various methods of using load currents to keep trolley wires free from ice are available. Usually these methods are at the expense of regulation, so that low voltages must be tolerated during the emergency. In this paper mention is made of three schemes one of which has been used for some years. The water-box scheme is an emergency method which can be justified on the basis that it requires an inexpensive equipment. The scheme shown in Figure 3 (scheme 1) of the paper is subject to the disadvantage that care must be taken to prevent disastrous overvoltages in case a ground occurs on one of the trolleys to which the 1,000-volt secondary winding of the heating transformer is connected. A ground on the trolleys is equivalent to a short circuit on the heating transformer. A circuit breaker should be provided on the 1,000-volt side and interlocked with the 11,000-volt breaker so that the latter can not open before the 1,000-volt side is cleared. The 1,000-volt winding must be designed to stand 11,000 volts. Another disadvantage of scheme 1 is that it maintains 1,000 volts between trolleys which could not be permitted where there are crossovers between the tracks.

W. J. Clardy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is mentioned in Mr. Brown's paper that the "dancing" of conductors has been observed when no sleet or wind are present. In some instances, such movement occurs during rapid intensity changes in the magnetic fields surrounding the conductors. These sudden alterations set up repulsions and attractions between cables which cause the oscillations. Any quick current build-up or fade-out produced by circuit interruptions incident to sleet on the contact wire or due to other rapid variations in power demand may lead to the phenomena.

The suggested scheme of using 11,000-volt energy passing through a trolley or feeder circuit and a water rheostat to ground, for heating the wire, appears wasteful of power. A current flow of 800 amperes is 8,800 kw at 11,000 volts and unity power factor. This is 8,800 kilowatt-hours per hour. Thus, at one cent per kilowatt-hour, the operating expense is 88 dollars per hour for each trolley or feeder circuit and some 95 per cent of this amount is wasted in resistor losses.

No mention is made of the measures which may be undertaken to avoid difficulties because of sleet or third rails. This is a problem which requires attention on suburban electrifications and urban rapid transit systems. The Chicago, Aurora and Elgin Railroad has a unique device for freeing the third rail of sleet. It consists of a special six-inch shoe which is moved down in contact with the third rail in advance of the collector shoe. Actuation is by means of a four-inch cylinder supplied with air at 100 pounds pressure. The shoe has seven blunt nose teeth which act as cutters. The teeth are set at a 45-degree angle so as to be self-freeing.

Dwight L. Smith (Chicago Rapid Transit Company, Chicago, Ill.): Mr. Brown's excellent paper deals with the prevention and removal of sleet on overhead trolley only and does not cover the subject so far as lines operating on third rail. The Chicago Rapid Transit Company operates almost entirely on top-contact unprotected third rail and faces a potentially serious sleet problem because of this type of construction. It would be desirable to either convert to under contact rail or to protect the present top contact rail, but both structural and equipment clearances are such that the cost of either of these plans would be prohibitive. Most of the third rail on the elevated lines is 80 pounds with a conductivity equivalent to approximately 1,000,000 centimeters of copper and the third rail to be used in the subway is 144 pounds, special section low carbon with a conductivity equivalent to over 2,000,000 centimeters. With such conductivities, it is manifestly impossible to handle the sleet problem by resistance heating as may be done with overhead systems.

The Chicago Rapid Transit Company has solved the problem by the use of mechanical sleet crushers and scrapers on motor cars. These devices are installed on the beam supporting the trolley shoe collector and are normally held from contact with third rail by cams. When their use is necessary, they are lowered to make contact with the top surface of the rail and are forced down by spring pressure of about 150 pounds. The crusher is a cylinder approximately three inches in diameter with helical ridges which rolls and breaks up and crushes the sleet coating. The scraper consists of two hardened steel blades arranged at an angle of about 30 degrees with the line of the trolley rail, which follow the crushers and scrape off the crushed ice. Formerly these devices were used only for sleet removal and the ordinary shoe collector relied on for current collection; later the scraper was connected in parallel with the collecting shoe and thus itself acts as a current collector.

Naturally, considerable arcing occurs because of imperfect contact, but this has been prevented from jumping to grounded portions of the truck by the use of flashboards and cup washers on the bolt ends. When the type of construction makes it possible, the wooden shoe beam is constructed with its top face at about a 45-degree angle in order to prevent sleet and moisture from collecting on a horizontal surface.

With this device it is possible to remove sleet which has accumulated on the third rail, and the operation of a fairly frequent service prevents further accumulation.

At certain critical locations such as on inclines where the sleet tends to flow downgrade and results in a very heavy coating near the bottom of the grade and in yards where cars have been stored during a sleet storm without any movement on the track, special means are used to remove and prevent further accumulation of sleet. The company has developed a sleet paste which consists of a mixture of powdered graphite, an antifreeze vehicle and a small amount of light oil mixed to the consistency of a thin paste. This paste is applied by hand by means of a long-handled brush and disintegrates heavy sleet coatings rapidly and prevents further sleet from adhering to the rail for an appreciable time. No successful means have been found for applying it other than by hand, and the cost of the paste itself and its application has resulted in confining its use only to such critical locations.

By the two means described above, delays due to sleet accumulation on the third rail have been almost entirely eliminated.

H. F. Brown: Mr. Smith's comments relative to sleet removal from third rail are interesting and timely, in connection with this general subject. This particular phase of the sleet problem was not touched upon in my paper as being outside its immediate scope. The New Haven Railroad does not operate or maintain any third rail, although their locomotives operate into the Grand Central Station over the New York Central tracks, from under running third rail.

Mr. MacLeod touches upon some design difficulties of scheme 1, outlined in the paper. These were recognized but not detailed in the paper, as this scheme was not used.

Mr. Clardy points out the economic disadvantage of the water-box scheme. Such costs as he mentions must be weighed against the possibility of operation by other forms of motive power and against the cost of delays to important passenger trains. His comments regarding conductor movements (dancing) due to sudden alterations in their surrounding field are important in connection with this subject, since this phenomenon has been often noticed on the feeders mentioned in this paper.

Electric Control for Steam Boilers on Diesel-Electric and Straight Electric Locomotives

Discussion and author's closure of paper 42-127 by E. H. Burgess, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942 and published in AIEE TRANSACTIONS, 1942, August section, pages 604–06.

Arthur Bessey Smith (Associated Electric Laboratories, Inc., Chicago, Ill.): This paper is a welcome addition to the literature of electric control, whether for boilers, machines, or other devices.

The necessary brevity of the paper left numerous points open to question, some of which I believe should be added to complete the picture.

1. It seems to be assumed that the heat generated by the burner varies with the speed of the burner motor.

2. As the boiler pressure rises, it seems that the pressure switch *opens* the contacts in the sequence 5-4-3-2-1.

3. It is not clear how the coils of the pilot relay and the solenoid valve are connected to the heavy duty contacts. If wired exactly as shown, when high pressure has opened all of the contacts, current will be cut off both pilot relay and solenoid; thus both will release.

4. Does the pilot-relay contact close at the operation or at the release of the pilot relay? This is involved in the pilot relay interlock during the off period, when the time-delay relay is said to be kept energized, page 604, column 3 of the paper.

5. How is the water level in the drum or equivalent maintained?

6. What is the function of the coil marked "pressure switch?"

7. On page 604, column 2, "boiler is ready to run" —does this mean "ready to raise steam from cold?"

E. H. Burgess: 1. Heat generated by the burner varies with the speed of the burner motor inasmuch as the amount of fuel forced through the burner is proportionate to the burner motor speed.

2. When the pressure switch coil is energized, the contact fingers close in sequence 1-2-3-4-5, which actually provides step starting for the motor. When the steam pressure rises to a predetermined point number 5 finger opens, thereby inserting a definite amount of resistance in series with the motor through the number 5 load relay. As the pressure continues to rise, the balance of contacts open in sequence 4-3-2-1, and after number 1 opens, the motor stops entirely.

3–4. The pilot relay coil and solenoid valve coil are connected to the armature of the number 1 and number 2 load relays and are only energized when either of these load relay contacts are closed, when high pressure causes all the heavy duty relay contacts to open, then both the solenoid valve and pilot relay became de-energized. The contacts of the pilot relay make only when the coil is de-energized, which, as can be seen from the circuit, complete the circuit to the coil of the outfire relay.

5. During the normal operation of the boiler an excessive amount of water is pumped into the coils, which does not turn into steam. This excess water is removed from the steam line in the separator, and the height of water in the separator is controlled by a ball float. The excess water then returns to the storage tank through a heat exchanger.

6. The multifinger pressure switch is of the normally open contact type which, when the coil is energized, slowly closes the contacts in sequence 1-2-3-4-5. Steam pressure exerts pressure against the contacts only and in no way interferes with the operating coil.

7. "When boiler is ready to run," means that all operating and safety devices are set in normal position, such as main line switch closed, all safety switches closed, and ignition switch in the "on" position. The preceding conditions must be met before the boiler will generate steam, regardless of whether it is from a cold start or from a hot boiler that has just been shut down.

Electric Facilities and Operating Plan for the First Chicago Subway

Discussion of paper 42-137 by C. E. De Leuw, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 780–7.

W. J. Clardy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. De Leuw's interesting paper gives a comprehensive outline of progress in the betterment of urban travel facilities in Chicago. Rapid transit is essential to the prosperity and growth of every large city. Subway and elevated lines serve as means of low-cost and convenient access to shopping and delivery districts. The territory available for residential purposes is increased, and the concentration of workers in homes near offices is avoided.

The expansion of New York, Chicago, Philadelphia, Boston, and Cleveland demonstrates this dependence on quick travel between various sections of cities. During recent years many additions have been made to the subway lines which serve New York. An interesting phase of the development is the replacement of elevated lines in a number of locations. After new subways were completed and operation initiated, the old elevated routes were abandoned and the structures demolished. Thus, street appearance is improved, and property values bettered. The New York system has introduced some novel types of subway streamlined rolling stock. A multisection car, composed of five bodies on six trucks, is in operation which seats 198 passengers and has a total capacity of 712. The weight of this unit is 182,600 pounds. Another articulated car used consists of three bodies on four trucks. There are 84 seats, the total capacity is 318, and the weight 75,540.

Rapid transit in Chicago dates back to 1892. The present elevated lines are functioning remarkably well within their limitations, but the city requires additional facilities to provide for growth and expansion. The new subway will contribute materially in meeting these needs. War conditions are preventing the immediate purchase of new rolling stock for the subway, but reasonably satisfactory operation can be expected from existing steel cars of the Chicago Rapid Transit Company. The delay in the procurement will prove beneficial because of the increase in production capacity of lightweight materials suitable for car construction after the war.

Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits

Discussion and authors' closure of paper 42-129 by G. F. Lincks and C. R. Craig, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 813–21.

E. L. Bayles (Northwestern Public Service Company, Huron, S. D.): The Huron–Iroquois line of the Northwestern Public Service Company is a three-phase 11-kv line approximately $48^1/_2$ miles long with solidly grounded neutral, using number 6 hard-drawn conductor. Thirty-foot poles are used with triangular construction having a $4^1/_2$-foot crossarm, a forged steel pole-top pin, and 28-inch strap braces. Steel pins are used in the arms, and insulators are nominally 15 kv. A one-quarter-inch Siemens–Martin steel neutral wire is carried the entire length of the circuit on the poles at approximately one foot below the lower attachment point of the braces.

The neutral wire is grounded at each

transformer installation, at each overhead guy and each pole. Grounding conditions are poor to fair. Driven ground rods on transformer installations and down guys are connected to the neutral test from eight ohms on up, while pole grounds run from a minimum of 100 ohms on up to an unreadable value.

The terrain is quite flat with maximum exposure of line to lightning, wind, and sleet. There are practically no trees or other objects which might shield the line, and by the same token faults are not caused by proximity to trees.

Transformers fed from the line are protected by expulsion gap arresters or by standard valve-type arresters. There are, however, a number of older types of transformers which have a very low impulse strength and which flash over internally in some instances at a lower voltage than the settings of the arresters of either type. These transformers are being weeded out as time goes on but are the probable source of some few kickouts on this line apart from the trouble experienced on the line itself. Inasmuch as the transformer fusing is interposed between the line and transformer troubles, this is largely a matter of co-ordination which in most cases was such as to relieve the line by blowing the transformer fuse.

This line normally serves five small towns and a good-sized packing house load which taps off at a point a few miles from the source. A 15-kv reclosing oil circuit breaker with time-over current relays protects the entire line while *three* F.P. 119 oil reclosers are in the line to the small towns at the point just beyond the packing-plant tap and thus protect this important load from trouble on the 40 odd miles of line. The oil circuit breaker is set at 48 amperes with a time of one second at 300 per cent of trip current. The F.P. 119 oil reclosers are the 25-ampere size.

The oil reclosers were installed May 15, 1941, and replaced a three-shot reclosing fuse installation. For the 12-month period ending May 15, 1942, the number of operations recorded by the reclosers were as follows:

Phase *A*	149
Phase *B*	36
Phase *C*	172

Total number of lockouts	4
Permanent line faults	2
Lockouts on repeated transient faults	2
Faults cleared successfully	over 168

In explanation of the unusual number of operations it should be said that the period included a rather severe sleet or glaze storm with high winds which resulted in dancing conductors and numerous transient faults. On a similar installation near Watertown under sleet storm conditions we have experienced 27 recloser operations over night without a lockout.

It could not be said in fairness that the installation of these reclosers saved the replacing of over 350 fuses, as no doubt the time during which the fuse was out would include a number of potential cases of trouble. Unfortunately, we do not have an accurate record of fuse replacements prior to the installation of reclosers. We do know, however, that during one 24-hour period in a sleet storm we replaced 100 links in a three-shot fuse at the same point.

From the economic viewpoint we feel that time, material, car mileage, and outages saved by this installation more than justify the installation of this set of oil reclosers.

Bruce O. Watkins (nonmember; Rural Electrification Administration, St. Louis, Mo.): In the paper Mr. Lincks and Mr. Craig assume that there is a substation attendant. Usually on Rural Electrification Administration systems there is no such attendant. As is pointed out, this will result in greater relative benefits for automatic resetting reclosers. Also, it is assumed that in all cases there is a substation breaker. In very few cases are there such breakers on REA systems. A great many resetting reclosers can be purchased for the cost of one substation breaker, and the tendency in REA systems has been to place such reclosers on the line, rather than investing in a substation breaker. Also, since most REA substations are of relatively low capacity, the interrupting rating required of the substation devices has not usually necessitated such a breaker.

The experience with resetting reclosers on REA systems has been very favorable. Managers who have used them report greatly reduced outage time and maintenance costs over the originally fused system. Unfortunately, the exact reduction in such costs is not available. We are now attempting to compare maintenance costs on recloser-equipped systems with fused systems similar in size, consumer density, revenue, and so forth. Such figures may assist in sectionalizing comparisons.

Table I shows a three months' summary of a report on single-pole 15-kv resetting reclosers on REA systems which reported the same number of reclosers for three consecutive months (July, August, and September) in 1941.

There are features of the presently available resetting reclosers which could be improved. The ideal recloser would possess characteristics such that:

1. Internal distribution transformer fuses beyond the recloser would blow before recloser lockout on transformer failure.

2. A larger number could be co-ordinated in series than with present reclosers.

3. Opening time would be sufficiently fast to prevent excess conductor or gap burning.

4. Opening time would be short enough to protect adequately small substation transformers with the larger sizes of reclosers.

5. The installation and co-ordination of less expensive devices beyond the recloser could be made.

Of course some of these objectives conflict, and some compromise will be necessary. We feel, however, that the automatic resetting recloser offers great possibilities in rural distribution sectionalizing and that, as developments continue, they will offer even greater benefits.

Table I. Quarterly Summary

Number of systems reporting	39
Number of reclosers	450
Miles behind reclosers	11,441
Miles per recloser	25.4
Total operations	8,094
Operations per recloser	18
Lockouts	208
Lockouts per recloser	0.462
Per cent lockouts per operation	2.57
Operations per mile	0.706

R. F. Quinn (General Electric Company, Schenectady,. N. Y.): The authors have presented a means of placing the problem of whether to sectionalize and with what to sectionalize on a sound economic basis. This study and the conclusions should be of real value to distribution engineers in studying their particular systems with a view to getting the maximum in continuity of service for every dollar invested.

The paper covers all combinations of sectionalizing and branch-protection schemes now available or in common use. However, manual sectionalizing is covered only in general terms. Actual data showing the effect on restoration time and automotive mileage for a line equipped with manual sectionalizing are not included in the curves.

Manual sectionalizing, as commonly employed, splits the main line up into several sections. In locating a fault the trouble crew travels at a high rate of speed to the mid-sectionalizing point, opens it, and then has the station operator close the circuit. If the line remains energized, the fault is obviously beyond the section point and vice versa. They then split the faulted half of the line into smaller sections until they locate the one on which the trouble is located. After locating the faulted section, the crew travels at a slower rate of speed to actually find and repair the fault.

An additional study has been made to arrive at comparable data on manual sectionalizing of a line without branches as in Figure 2 of the paper. Three locations of disconnecting cutouts having solid blades permitted dividing the line into four equal sections. The same general assumptions were used as in the study described in the paper, and it was assumed that the trouble crew:

(*a*). Traveled 30 miles per hour average speed to mid-point, opened cutout, phoned, and had breaker closed, and then either traveled back to the sectionalizing cutout closer to the substation if service was *not* restored, or to the cutout beyond the midpoint if service *was* restored (average speed 30 miles per hour). Climbed pole, opened cutout, and checked to see if service was restored. Then searched faulted section at 15 miles per hour

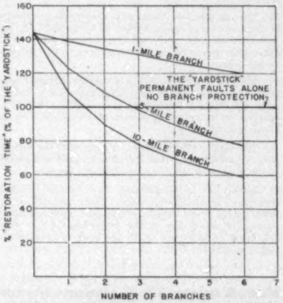


Figure 1. Restoration time for 1-, 5-, and 10-mile branches using manual feeder sectionalizing

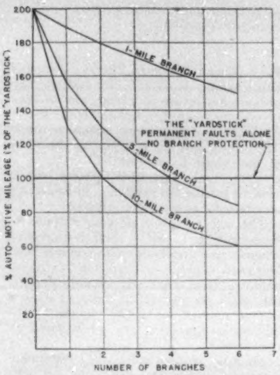


Figure 2. Automotive mileage for 1-, 5-, and 10-mile branches using manual feeder sectionalizing

traveling to mid-point. After repairing faults, traveled back 30 miles per hour to close sectionalizing cutout.

(*b*). With branches added to the feeder studied as in (*a*), search for the fault was confined to one-fourth the total mileage in all the branches. The crew traveled at 15 miles per hour.

(*c*). Five minutes were allowed for climbing the pole and 30 minutes for repairing permanent faults, which are the same values used in the study made by the authors.

The data obtained are plotted in Figures 1 and 2 of this discussion. It should be noted that the values are the same for all percentages of temporary faults. Thus, to make a comparison with the data presented by the authors, the curves on manual sectionalizing Figures 1 and 2 can be plotted directly on the respective curves Figures 5 to 12 of the authors' paper.

These data support the authors' conclusions on manual sectionalizing. They indicate that:

1. Manual sectionalizing of a feeder having no branches increases "restoration time" and automotive mileage in providing an improvement in service continuity over that for an unsectionalized line.

2. The restoration time and automotive mileage is the same for all percentages of temporary faults, and thus manual sectionalizing is most effective at the higher percentages of temporary faults (above 75 per cent).

3. Manually sectionalizing a feeder without any branches is much less beneficial than on a feeder with branches. Traveling out and back on a number of branches in search of the fault can be eliminated thereby.

4. The longer the branches, the greater the reduction of "restoration time" and automotive mileage effected by manually sectionalizing a feeder.

5. Manually sectionalizing a feeder having branches generally provides less reduction of "restoration time" and automotive mileage than automatic protection of the branches except with a number of branches at 85 per cent of temporary faults or higher.

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper is primarily an economical consideration of the fusing problems on distribution circuits and as such makes a logical engineering presentation of the problem. In the conclusions much emphasis is placed on the value of "overlapping the substation reclosing protection with all line protective devices," but this is not always possible, because, in many cases where long feeders are involved, a far end fault may produce less current than the full load current on the feeder at the substation. Also, if such a scheme is to be used, it would seem desirable to have the breaker clearing time below the damaging time for the various line fuses.

The conclusion that "single-element fusing of branches five miles and longer" is economically justified appears reasonable for the general case. However, severe tree exposures often make fusing practical on much shorter branches and possibly the criteria for branch fusing might be based on the number of faults occurring per year rather than length based on an average number of faults per year.

G. F. Lincks and **C. R. Craig**: The contributions made by those discussing the paper are greatly appreciated by the authors.

E. M. Bayles gives some interesting data on a line where the percentage of temporary faults are much higher than the 85 per cent maximum of the study and there are more faults. (4+ instead of 1 per mile.) These faults apparently occur largely during periods of severe lightning and sleet storms with a majority resulting from a very few storms. All of these special conditions would enhance the economic justification for using automatic resetting reclosers of the type mentioned.

In calling attention to the inability to always set substation breaker relays so as to clear ground faults at the end of the line, W. C. Westrate places emphasis on a limitation to the more widespread use of relaying so the substation reclosing protection overlaps all line protective devices. This limitation is more pronounced on ungrounded circuits than on grounded circuits where the ground relay can be set below the load current of the phase wires. Recognizing this condition, the study presented in the paper pointed out that where such overlapping protection reaches only part way out on the line, the decrease in "restoration time" and automotive mileage over a nonreclosing or reclosing breaker is only approximately one-quarter of the decrease provided by overlapping the whole line. A very fine presentation of the problems involved in this type of overlapping protection will be found in the engineering report 47 Edison Electric Institute publication *J*-1, "Positive Disconnection of Distribution Circuits During Faults to Ground."

In concluding that "single-element fusing of branches five miles and longer is the most effective in saving sufficient restoration expense to liquidate the initial cost," it was not intended to imply that this would never be true on shorter branches. As Mr. Westrate points out, conditions on specific circuits or branches of a circuit may require a different criterion than the length of the branch. In some instances a large number of faults per year or a higher percentage of temporary faults on a very short branch might justify the initial expense of single-element fuses or even reclosers by the savings in restoration expense as well as the improvement in service continuity thus provided.

The emphasis placed on the use of relays and substation breakers which overlap the line protective equipment may have misled Mr. Watkins into the interpretation that the study presented was based primarily on the use of breakers at the substation. Actually, with an attended substation, it makes no difference in the conclusions reached in the paper whether a relay and breaker, a recloser, or a fuse is employed. With an unattended station a reclosing device that reset automatically very likely would be justified by the saving in restoration expense. As Mr. Watkins indicates, the decision as to whether this device is a recloser or a relay and breaker depends on the economies of each circuit.

The operating records with reclosers which REA are compiling should prove of value to the industry. It has been suggested that the value of such records would be enhanced greatly if there were some standards set up for compiling the records. For example, in the three months operating data presented by Mr. Watkins, there is no segregation as to whether valve-type arresters, expulsion protector tubes, or plain rod gaps were employed. If the expulsion protector tubes are not properly co-ordinated with the reclosers, they may cause it to operate unnecessarily. Plain rod gaps always require the recloser to interrupt the short circuit caused when they flash over. With both types, service would be restored on the first reclosure, thus showing a higher percentage of operations without lockout than where valve-type arresters are used. However, most of these momentary circuit interruptions would be avoided with the valve-type arresters. It was suggested that another point of dissimilarity in records is the "yardstick" used for comparison of the improvement provided. Possibly, in lieu of a standard, comparison of actual operating results with the results of the mathematical studies might provide the needed uniformity.

The actual data and curves for manual sectionalizing presented by R. F. Quinn are a valuable addition to the studies presented in the paper. The curves in his Figures 1 and 2 should prove useful, as they can be plotted directly on the curves in the paper.

Modern Cathode-Ray Oscillograph for Testing Lightning Arresters

Discussion and authors' closure of paper 42-108 by E. J. Wade, T. J. Carpenter, and D. D. MacCarthy, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 549–53

O. Ackermann (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors of this paper have by necessity concentrated on explaining the details which make their equipment operate so successfully. This gives an opportunity to discuss the significance of their work in the field of cathode-ray oscillography in general.

As indicated in the bibliography to their paper, Dufour of France is the father of the high-voltage cold-cathode oscillograph. He used a mechanical sequence switch to apply high voltage to the cathode for a short time and to initiate the electrical phenomenon he wanted to study during this period. In this form, the instrument was introduced in the United States. The system soon proved too clumsy for the graphic recording of surges, to the study of which the instrument was applied. Harrington and Opsahl developed purely electrical high-speed switching by means of three-electrode gaps for the initiation of discharges in circuits serving cathode-ray oscillograph and surge generator. This resulted in a precision and rapidity of timing hitherto unknown. The circuits were published in the *Electric Journal* of August 1927 and have laid the foundation to a large number of systems of timing circuits interconnected by tripping spark gaps. The cathode-ray oscillograph with "impulse beam" was thus successfully established in the United States.

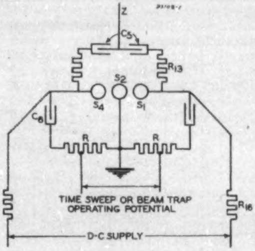


Figure 1. Completely balanced oscillograph trip circuit responding to impulses of either polarity at Z

Notations correspond to those of Figure 6 of the paper

In the meantime, many workers in Europe had concentrated on the design of oscillographs with continuously energized cathode and with systems of "beam traps" which kept the beam off the photographic film while it was not needed for recording. One such system, that of Doctor Norinder, was introduced in the United States by the Westinghouse Electric and Manufacturing Company in 1928 and was developed by the writer into a system of three or four cross-connected and balanced pairs of plates which, while "untrapping" or releasing the beam, do not produce a deflection of the latter in the recording part of the instrument. This has been disclosed in a paper of the writer in AIEE TRANSACTIONS, volume 49, 1930, page 467.[1] It should not have been necessary for the authors to suggest by their reference to a 1932 British paper that the method had originated outside of this country.

Interest was now focused almost exclusively on the oscillographs designed for continuous beam output. They attained considerable importance in the industry, and as is quite natural with an instrument finding wide application, they had to meet ever rising demands on their performance, particularly on writing speed. Increased writing speed is most directly obtained by raising the cathode voltage and the beam current which, in instruments designed for continuous output, meant complications such as artificial cooling of the discharge tube, higher insulation strength, and larger dimensions.

At this point, the system of the impulse beam, abandoned by most workers, but quietly fostered by the authors and their associates, bears new fruit, as it is, of course, entirely feasible to force a much higher discharge energy through a tube for a short time than the latter could handle continuously. This, however, is not the only thing which accounts for the extremely sharp and clean-cut pictures of 100,000,000-cycle transients obtained by the authors with their new "impulse-beam" oscillograph. In order to achieve this, the timing and beam release circuits must be very carefully laid out physically, shielded, and balanced. In this the authors have done a very good job, as every worker with oscillographs will acknowledge.

Regarding one feature of their circuit, namely, the four-electrode gap, it appears to the writer that a three-electrode gap should function in exactly the same manner. For the four-electrode gap of the authors to operate, *S*-3 must spark to *S*-4 first, and, once this spark is established, *S*-3 and *S*-4 act as one body, as they would if they were connected permanently.

We do not hesitate to give credit to the authors for the direct grounding of the center sphere *S*-2 but believe that the circuit could be simplified and modified as indicated in Figure 1 of this discussion, so as to respond even to surges of either polarity. A portion of the tripping impulse as divided by the resistors *R* and *R*-13 is impressed on spheres *S*-1 and *S*-4, adding to the potential to ground of one and subtracting from that of the other. Whichever potential exceeds the breakdown of the gap to *S*-2 will cause that particular half to flash over. Practically the full potential of *C*-5 now is applied across the other half of the gap which will then break down immediately. This, incidentally, also is the major function of *C*-5 in the authors' original circuit, although this is not mentioned in their paper.

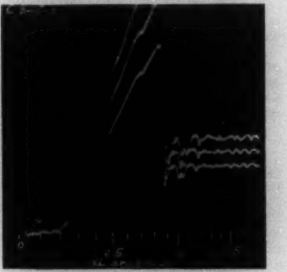


Figure 2. Typical high speed record obtained with continuously excited 50-kv oscillograph

A feature on which more information would be desirable is that of the cathode voltage regulator. One point in particular should have been mentioned in order to make the operation of the whole system clear, namely, that the left-hand terminal of the regulator must become grounded if a voltage of 80 kv is to be obtained at *G*-1.

That there are distinct possibilities in the method of impulse excitation is quite evident, as it has also been demonstrated by the work of Doctor Slack and his associates with impulse-excited high-voltage X Ray tubes. However, the continuously excited oscillograph is simpler in its operation and timing. It is, therefore, more desirable for general work. That it is entirely adequate for all practical purposes, which means to fractions of one microsecond, is demonstrated by hundreds of records such as that shown on Figure 2 of this discussion.

REFERENCE

1. A CATHODE-RAY OSCILLOGRAPH WITH NORINDER RELAY, ITS DESIGN AND APPLICATION, **O. Ackermann.** AIEE TRANSACTIONS, volume 49, 1930, pages 467–75.

J. M. Bryant and **M. Newman** (University of Minnesota, Minneapolis, Minn.): The authors are to be congratulated on making improvements in the cold-cathode cathode-ray oscillograph described in their paper. This type of oscillograph offers a better opportunity over the hot-cathode type for obtaining strong traces at ultrahigh frequencies and sweeps. A similar development is shown in technical paper 27, "Developments in High-Speed Oscillography," by J. M. Bryant and M. Newman, published March 1942 by the engineering experiment station of the University of Minnesota. In that paper new design features are given in sufficient detail to allow application to older slower-speed cathode-ray oscillographs. It is interesting that so many design features were solved in a similar manner and at about the same time but through independent work. The trace shown in Figure 8 of the paper under discussion compares in speed of sweep and writing speed with one published by ourselves in "Measurement of Very Short Time Lags," AIEE TRANSACTIONS, volume 59, 1940,[1] which we believe has usefully served as a goal to approach. Attention is called to the fact that "writing speed" is not necessarily limited by the velocity of light.

REFERENCE

1. MEASUREMENT OF VERY SHORT TIME LAGS, **J. M. Bryant, M. Newman.** AIEE TRANSACTIONS, volume 59, 1940, pages 812–16.

E. J. Wade, T. J. Carpenter, and **D. D. MacCarthy:** The authors are pleased to have received Mr. Ackermann's thorough discussion of our paper. We are familiar with the cathode-beam relay developed by Norinder and improved by Ackermann. We referred to the work of Burch and Whelpton since we preferred their type of beam trap, because it seemed simpler to build than the Norinder relay, and with it the beam is released by removal of the voltage from the plates. The plates of the Norinder relay must be perfectly balanced in order to avoid a deflection proportional to the voltage that is applied to release the cathode beam.

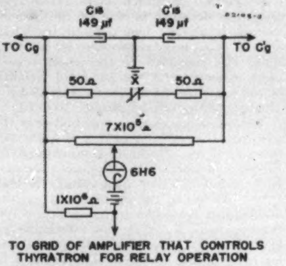


Figure 3. Schematic circuit diagram for cathode voltage regulator

In Figure 1 of his discussion Mr. Ackermann gives a circuit for a three-electrode gap to replace the four-electrode gap shown in Figure 6 of our paper. In our opinion the four-electrode gap is advantageous, since it permits the sweep circuits to be started by an initiating impulse, lower in energy than would be necessary with the three-electrode gap. In order to initiate the complete breakdown of the three-electrode gap, the initiating impulse must change the voltage on one of the end gaps (see Figure 1 in Mr. Ackermann's discussion). This change in potential will cause current to flow through R_{14} to other portions of the high-voltage circuit and through the relatively low-impedance path through C_6 and R. The charges corresponding to these currents must be supplied from the cathode generator and tends to reduce the cathode voltage which should be constant. To start the operation of four-electrode gap, the initiating impulse has only to change the potential of the electrode S_3 which has a low capacity to other parts of the circuit. The time and charge necessary to do this is relatively small. Since the four-electrode gap is always initiated by an impulse from the cathode supply that is negative, there is no advantage in using a gap that will respond to surges of both polarities.

Mr. Ackermann has requested more information on the cathode voltage regulator. A schematic diagram of the regulator is shown in Figure 3 of this discussion. When the cathode generator is put on charge, relay X opens, and one of the capacitors, C_{15} and C_{15}', charges in series with each of the balanced banks C_g and C_g' of the Marx circuit shown in Figure 6 of the paper. The voltage across C_{15} and C_{15}' is proportional to the sum of the voltages on C_g and C_g'. The rectifier tube conducts when the drop on the potentiometer exceeds the bias voltage. This results in the operation of relays that initiate the sequence of operations described in the paper. When the capacitors C_g and C_g' are charged to 20 kv, the voltage on each of the capacitors C_{15} and C_{15}' is 67 volts. During the discharge of the cathode generator, the ground terminal of the capacitor C_g is 67 volts negative with respect to ground which is entirely negligible in comparison with the 80 kv supplied by the generator.

It is of interest to learn that Bryant and Newman have applied many of the same design features to a cold-cathode oscillograph that they have built.

Modern Impulse Generators for Testing Lightning Arresters

Discussion and author's closure of paper 42-96 by Theodore Brownlee, presented at the AIEE summer convention, Chicago, Ill., June 22-26, 1942, and published at AIEE TRANSACTIONS, 1942, August section, pages 539-44.

A. M. Opsahl (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. In an impulse generator for lightning-arrester testing, the primary requirement is that it deliver an impulse test current of specified shape and magnitude through the test piece. The variation in voltage, electrostatic capacity, inductance, resistance, and discharge voltage characteristic of the test piece is so great as to make detailed calculations on each test circuit too laborious. For a number of years the curves in Figure 1 of this discussion have been found very helpful in determining the test circuit to deliver a specified test current.

2. As shown in the sketch, the capacitor discharges through the inductance L and load resistance R. The capacitor discharge formulas were used to obtain general curves applicable to any impulse current generator. The curves show the per cent of capacitor voltage that appears across load resistors. The load resistors are multiples of the impedance characteristic of the circuit $\sqrt{L/C}$. The time scale is plotted in multiples of the time characteristic of the circuit $\sqrt{LC}$.

3. The shape of the desired current wave determines the per cent of capacitor voltage available across the load resistor. The required current then determines the load-resistor magnitude. From this, a numerical value is obtained for the factor $\sqrt{L/C}$. From the required time to crest current in microseconds, a numerical value is obtained for the time factor $\sqrt{LC}$ in microhenries and microfarads. From a solution of these equations, the required capacity and inductance can be obtained.

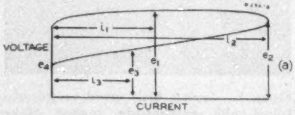
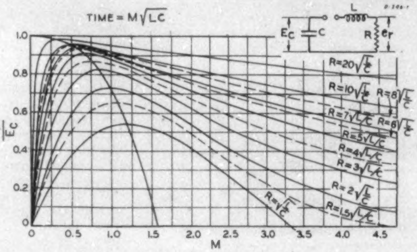


Figure 2

4. As stated by Mr. Brownlee, an arrester valve element is not a linear resistor, so the simple calculation above is not adequate unless the arrester is only a small part of the load resistor. As a matter of economy, to obtain the maximum test from the available equipment, the arrester is as large a part of the load resistance as possible. Mr. Brownlee's Figure 1B shows the arrester substituted for part of the load resistor. In Figure 2 of this discussion is a sketch of a typical volt-ampere curve of a valve arrester. On the rise in current, e_1 is substantially constant, and a better figure is obtained for the required circuit inductance by assuming the arrester is a constant voltage instead of a resistance element. The time to crest current does not vary rapidly with changes in circuit resistance. In the region from e_2 to e_4 there is substantially a straight line. For calculation of crest current, the arrester element can be regarded as a resistance $(e_2-e_4)/i_2$ plus a voltage e_4 which is subtracted from the capacitor voltage.

Figure 1



5. Equal impulse generator elements in series proportionately increase the voltage rating of the arrester element that can be tested, but do not change magnitude or shape of current wave. Equal impulse generator elements in parallel increase the available current without changing the current wave shape.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): Mr. Brownlee has shown in his paper that the wave shape of the output currents of impulse generators can be calculated with very good accuracy, even though part of the discharge circuit contains an impedance which varies with the current. While experience certainly helps, a thorough understanding of the problem on the theoretical basis as shown in the paper certainly is required. For many years the tests on lightning arresters were further unnecessarily complicated by requirements of obtaining a definite voltage wave shape before arrester gap breakdown, followed by a definite current wave shape, so that impulse testing of arresters was really one of the most difficult of the impulse tests. It is therefore very gratifying that the author makes public the results of his experiences.

Of particular interest is the arrangement of generator 1 in a steel housing. Such a housing should go a long way toward eliminating many of the spurious oscillations which are liable to occur with the open type of generator. Here the stray capacitance formed between capacitors and ground in connection with the circuit inductance produce current flow through the ground circuit, thereby upsetting the balance between testpiece ground and oscillograph ground. The oscillograms presented show that this aim of confining stray capacitance currents has been completely accomplished.

The paper shows the growing importance of impulse tests in the design and research of lightning protective equipment. It is perhaps of interest to note that in the Pittsfield works of the General Electric Company alone, a total of 12 impulse generators is in more or less constant use for the determination of impulse properties of parts as well as of the completed products.

Tests on completed transformers are at times equally complex, not only on account of the complexity of the impulse discharge circuit because of the inductance to ground of the winding tested, but also because of the presence of the other windings on the same core. The voltages at the bushings of the other windings under conditions of test tend to rise up to and above the bushing sparkover, unless suitable impedance to ground is connected to them. While calculations can be made to analyze these voltages for a given transformer, the labor involved would be prohibitive. The electric transient analyzer is used for this purpose. Frequently such an impedance is obtained with Thyrite Since the transient analyzer operates at relatively low voltage (order of 1,000 volts), the amount of Thyrite, when checking with the analyzer, must be of necessity very much smaller than for the actual high-voltage test. It has been possible to match the Thyrite for the two tests effectively, so that for any voltage applied to the transformer, the wave shapes match perfectly. Such a match is of course important to obtain the necessary assurance that failure in the transformer winding did not occur. If the same amount of Thyrite is used for the 70 per cent check wave as for the 100 per cent full wave, the wave tail of the applied wave is liable to be considerably shorter on the 100 per cent wave. Thus, a failure in the winding producing a similar change in wave shape may be obscured.

Calculations such as shown in the paper are always desirable and necessary to obtain checks on the performance of equipment under impulse conditions imposing high stresses on the test specimen.

J. M. Bryant (University of Minnesota, Minneapolis, Minn.): The requirements for tests of all types of lightning arresters to meet standard specifications necessitate an impulse generator (or generators) providing a wide range of currents and voltages. We agree with Mr. Brownlee that under certain conditions a rapid way of determining the resulting current test waves would be from previously established curves for given conditions for a similar type arrester. As an example Figure 3 shows a group of current discharge waves through an arrester, all of which have practically the same specification. In addition to the impulse generator, it is necessary to have an accurate linear shunt for current, an accurate voltage divider for potentials, an oscillograph rapid enough to be capable of recording the events, such as described by Wade, Carpenter, and MacCarthy, and connections from the shunt and voltage divider which eliminate harmonics due to stray magnetic and electrostatic fields without the use of series resistances or other makeshifts which alter the actual values for the current or voltage curves. These specifications make it necessary to use infinite pains in making accurate tests. The author is to be congratulated in the clearness with which he has presented in so short a paper the results of what must have taken years of research and planning.

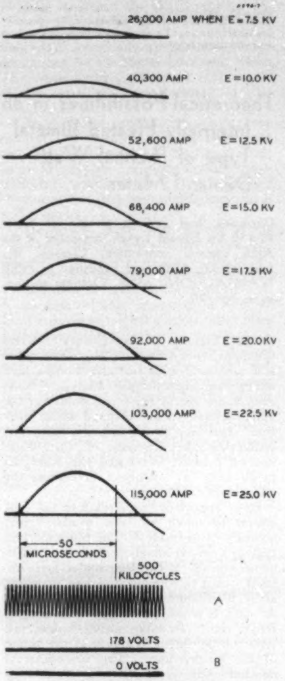


Figure 3. A group of current discharge waves through an arrester

A. Time scale calibrating frequency 500 kilocycles per second two microseconds per cycle

B. Current scale calibration. Shunt resistance 0.00278-ohm. Deflection equals 64,000 amperes

Theodore Brownlee: The discussions by Bryant, Hagenguth, and Opsahl agree well with the text of the paper. Mr. Opsahl's Figure 1 is equivalent to part of Figure 2 in the paper, and, over the range it covers, it permits wave calculations with about the same facility. His simplifications of the volt-ampere characteristic of a lightning arrester shown in Figure 2 are reasonable. Similar results will be obtained using his method or the curves in Figure 5 of the paper.

Regulated Rectifiers in Telephone Offices

Discussion and author's closure of paper 42-95 by D. E. Trucksess, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 613–17.

Clarence E. Lomax (Associated Electric Laboratories, Inc., Chicago, Ill.): This paper by Mr. Trucksess is very timely and is full of interest for all who have to do with the central-office power plant. It suggests to me a number of questions which I believe will be of interest to others.

1. Is supervision provided to indicate burnt-out tubes? If supervision is provided, is it equally effective on full load, light load, and no load?

2. Is there a means provided for extinguishing flashing when flashing occurs, and is it equally effective on full load, light load, and no load?

3. Does a charger which is rated at 30 amperes capacitance hold a constant voltage up to a 30-ampere load, or does the voltage start dropping before the full load is delivered?

4. When two chargers are required to operate in parallel, must any special action be taken to divide the load equally?

5. Is it necessary, when two chargers are used in parallel, to use chargers of equal capacity, equal age, and equal service usage?

Frederick L. Kahn (Automatic Electric Company, Chicago, Ill.): There have been a number of telephone-type regulated rectifiers on the same market which we are in the habit of referring to as "constant voltage rectifiers." These rectifiers use dry-plate rectifying elements (copper oxide or selenium) so that we have almost come to think of regulated chargers being synonymous with nontube-type chargers. We are very much interested to hear that equally close regulation of batteries in telephone exchanges can be obtained by 3-, 10-, and 30-ampere tube chargers, especially as the price of dry-plate units has made their use in chargers of higher capacitance almost prohibitive. The design of the tube-type regulated chargers at this time justifies the assumption that they have a lower first cost than constant voltage charging equipment of various types now available.

The following questions concern only the 48-volt rectifiers of 3-, 10-, and 30-ampere capacity:

1. As these chargers appear to operate continuously when used in regulated charging circuits, how often do tubes have to be replaced?

2. What is the electrical efficiency of these chargers? At what per cent of full load do they have maximum efficiency?

3. Are the 30-ampere chargers cheaper in first cost and operating cost than 25-ampere diverter pole motor generators?

4. What is lowest possible output of 10- and 30-ampere chargers when used in self-regulating service? (trickle charge rate)

5. What overload protection is provided if chargers are connected across a battery discharged during power failure?

6. How are variations of a-c voltage compensated for?

7. Do these rectifiers need any special adjustment for equalizing charge, or does the cutting in of additional countercells automatically raise the output voltage of the chargers?

D. E. Trucksess: The questions of C. E. Lomax and F. L. Kahn pertain to details in the design of the regulated rectifiers listed in Table I. These details and many more were not included in the original paper in order to keep it within the prescribed length.

In answer to Mr. Lomax:

1. In all cases some means are provided to indicate when a rectifier tube has failed. In most cases a voltage relay continually measures the regulated voltage, and, as soon as it goes out of the floating limits, it rings an alarm. In a few cases, a current transformer and relay are used with each individual bulb. The disadvantage of this type of alarm is that the alarm circuits cannot distinguish a tube failure from the condition of zero current produced by the regulating circuit as a result of no office load. Therefore, with regulated tube rectifiers the best alarm is a float voltage alarm.

2. In regard to flashover of the rectifier tubes, the thyratron tube, when used in 24-, 48-, or 130-volt rectifiers, does not flash over. The two-element-tube rectifier tubes flash over with voltage surges from the power line, and flashover relays are provided. These relays automatically disconnect the rectifier from the a-c line and battery during flashover and automatically reclose after the flashover is completed.

3. The 30-ampere regulated rectifiers hold a constant voltage up to full load, and at full load the circuit automatically transfers from a constant voltage-regulating circuit to a constant current-regulating circuit, holding the load at 30 amperes during any further charges in line or increase in load.

4, 5. When two or more regulated rectifiers are operated in parallel with regulation of better than two per cent, it is not practical for rectifiers to be operated under voltage control as the load will not divide equally, and it is impractical to attempt to automatically balance the load between them. When rectifiers, listed in Table I, are required to operate in parallel, means are provided for the constant voltage regulation to be transferred to constant current regulation. Therefore, rectifiers of unequal capacity, age, or service usage may be operated in parallel.

In answer to Mr. Kahn's questions:

1. In general, the tubes used in the regulated rectifiers shown in Table I will operate approximately one year at full load and from $1^{1}/_{2}$ to 2 years with the typical telephone type load of full load during the day and light load at night.

2. The electrical efficiency of regulated tube rectifiers is affected by the d-c battery voltage and the rectifier size; therefore, one figure will not cover the wide range of rectifiers listed in Table I. The maximum efficiency occurs at full load, which varies from 60 to 70 per cent for 50-volt rectifiers and 40 to 50 per cent for 24-volt rectifiers.

3. The 30 rectifiers listed in Table I have a cheaper first cost and operating cost than a corresponding size motor-generator set.

4. The 3-, 10-, and 30-ampere regulated tube rectifiers will operate and maintain their regulated voltage down to the trickle charge of the battery, which in most cases is one to five per cent of the full load rating of the rectifier.

5. Overload protection for regulated rectifiers is required in case the rectifier comes back after a power failure and attempts to float a discharged battery. In all cases the rectifiers shown in Table I have automatic overload protection. In the small rectifiers a ballast lamp causes the voltage to droop with overloads. In larger sizes the control is changed automatically from voltage to current regulation at full load.

6. The normal voltage regulation of the phase shift and booster types of control is sufficiently accurate to compensate for variations in the a-c line voltage. With the magnitude type of control, the normal regulations would not be sufficient, and it is supplemented by the line compensating circuit described as part of the combined rectifier and inverter.

7. In regard to equalizing, charging, or overcharging, the larger rectifiers have built into the control circuit a separate voltage control to increase the floating voltage from 2.15 to 2.3 volts per cell. With the smaller rectifiers, the regulating leads are connected to the load side of the countercells so that for overcharging the countercell switch is opened, and the rectifier automatically increases the battery voltage by an amount equal to the drop over the countercells.

Theoretical Possibilities in an Internally Heated Bimetal Type of Thermal Watt-Demand Meter

Discussion and author's closure of paper 42-110 by Edward Lynch, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 764–70.

P. M. Lincoln (Therm-Electric Meters Company, Inc., Ithaca, N. Y.): This paper is a very interesting one and shows a high ability for mathematical analysis. However, there are at least two points in the author's final conclusion that I would question. One has to do with his statement concerning the accuracy of the block-interval demand meter and the other has to do with his conclusion concerning the thermal demand meter.

First, regarding the block-interval type where, to quote his own words, "great accuracy is desirable." The block-interval type of demand meter measures the arithmetic average of the load under measurement. Any arithmetic average is necessarily a discontinuous function of time. As such, it is subject to a possible error of 50 per cent. To make matters worse, this error can be caused to appear at the option of the service user. The block-interval demand meter is perfectly accurate when we consider the particular time interval which it happens to select for measurement, but this time interval is only one out of an infinite number that might be selected. If Mr. Lynch will reread his reference 3, he will note that a series of tests made in Ithaca on block-interval demand meters extending over a period of 18 months showed a maximum error of 27.3 per cent and an average error of 10.1 per cent. In only ten per cent of the cases under measurement in Ithaca was there found no apparent error, and even in these cases there might have been errors if some intermediate time intervals had been chosen. Mr. Lynch's reference 3[1] also points out that maximum demand can be accurately measured only by a "continuous function of time." The logarithmic average is a continuous function of time while the arithmetic average is not and cannot be made a "continuous function of time." The block-interval demand meter is not only not accurate—Mr. Lynch to the contrary notwithstanding—but also cannot be made to be accurate.

Now, coming to Mr. Lynch's analysis of the thermal demand wattmeter, I question whether he carried his analysis far enough to arrive at an accurate result. The thermal wattmeter depends for its indication on the difference in temperature arrived at between two masses of matter which are heated by electric currents. This difference in temperature depends, in turn, not only in the difference in rate of heat application to the two masses of matter but also on the *rate of heat escape*. Final indication of the thermal demand meter is attained only when these two rates have become equal. If the rate of heat escape were always directly proportional to temperature elevation, Mr. Lynch's analysis would be perfectly correct and constitutes a beautiful piece of mathematical analysis. However, as pointed out in my paper, "An Improved Electrothermic Instrument,"[2] the rate of heat escape when such heat escape takes place by any method other than thermal conduction does not follow a first power law against temperature elevation. In the thermal wattmeter, where the actuating element is of bimetal strip as described by Mr. Lynch, most of the heat escapes by thermal convection. As proved by Chester W. Rice in his AIEE paper entitled "Free Convection of Heat in Gases and Liquids—II"[3], thermal convection varies as the 1.25 power of the temperature elevation—*not* the first power. Mr. Lynch does not take account of this in his analysis. I would, therefore, question the accuracy of the conclusions he arrives at in his paper.

I fully agree with Mr. Lynch that the "simple exponential is ideal." Due to the time taken for heat diffusion, the simple exponential cannot be entirely realized, but it can be closely approached. Why does not Mr. Lynch go still further and point out that the use of the thermal demand meter is the only known way in which maximum demand can be accurately measured?

REFERENCES

1. MEASUREMENT OF MAXIMUM DEMAND, **P. M. Lincoln.** AIEE TRANSACTIONS, volume 61, 1942, February section, pages 57–62.

2. AN IMPROVED ELECTROTHERMIC INSTRUMENT, **Paul M. Lincoln.** AIEE TRANSACTIONS, volume 54, 1935, May section, pages 474–81.

3. FREE CONVECTION OF HEAT IN GASES AND LIQUIDS—II, **Chester W. Rice.** AIEE TRANSACTIONS, volume 43, 1924, pages 131–43.

Edward Lynch: It was with much interest that I read P. M. Lincoln's remarks and studied his discussion. His first point can, perhaps, be more clearly understood without an opportunity for misunderstanding if we say, as he has, that "the block-interval demand meter is perfectly accurate" in indicating continual arithmetic average demand and add, as I have, that the thermal demand meter indicates a slightly different quantity which has been designated as continuous logarithmic average demand. The accuracy and precision of both types of meters as measuring devices is of such a high order that each can be built to measure the quantity for which it is intended and with accuracies well beyond requirements of normal use.

The second point in question illustrates an important "design principle." Heat escape from the actuating elements of thermal meters should be by controlled conduction rather than by radiation or convection. In the mathematical analysis this means that heat escape is proportional to the first power of temperature rise.

Doctor Lincoln stated that in the thermal wattmeter where the actuating element is a bimetal strip most of the heat escape is by thermal convection. This was not the case in the internally heated bimetal meter designed to check the analysis made in the main paper. In fact, the internally heated bimetal meter inherently fosters heat escape by conduction. This is easily realized when one recognizes the close correlation between electrical and thermal conductivity. Since we must have good electrical conductivity into and out of and between the actuating elements we readily have available good thermal conductivity as well. This was taken advantage of in the design of the meter on which tests were made, and the results of these tests substantiated the analytical conclusions reached.

A New Jewel for Indicating Instruments

Discussion and authors' closure of paper 42-134 by F. K. McCune and J. H. Goss, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 673–6.

John H. Miller (Weston Electrical Instrument Corporation, Newark, N. J.): The description of the test methods used on the new glass jewel for indicating instruments by McCune and Goss and the tabulation of the results are currently of importance to those using electric indicating instruments, because it indicates the dynamic character of the art and is assurance that functional quality is being maintained in such instruments, even in spite of important material difficulties.

The Weston Electrical Instrument Corporation is manufacturing glass instrument jewels in large quantities, and these jewels are being used with eminently satisfactory results in many fields. Aircraft instruments, incorporating electric mechanisms, are being almost wholly supplied with this type of bearing.

About ten years ago the Weston Electrical Instrument Corporation started an investigation of substitutes for sapphire jewels to obtain a more perfect contour and surface than was possible in sapphire, as was required for highly sensitive instruments. Glass jewels were known and used at that time; in fact, a patent was issued as far back as 1899, covering glass *V* jewels, but none was found of satisfactory quality for use in Weston instruments.

Preliminary work was therefore done on the development of a satisfactory glass jewel in the Weston laboratories, and by 1934 and 1935 extensive field tests on a new form of glass jewel showed it to be equal to and in many respects superior to sapphire, particularly where vibration was encountered.

In view of the evident usefulness of this type of bearing, as well as a matter of protection against the possibility of cessation of sapphire-bearing imports, a small pilot plant was installed about 1938 to establish a satisfactory production technique. When the war broke out in Europe, and shipments of sapphire bearings were cut off, a very large number of these glass jewels were supplied to Sangamo–Weston, Ltd., England, whose tests confirmed their usefulness in aircraft instruments. It is understood that today practically all electrical instruments used by the Royal Air Force, which were manufactured in England, carry jewels of this type, now manufactured in that country.

This extensive manufacturing experience enabled the Weston Company to expand the pilot plant to a full-fledged production basis as sapphire bearings became more difficult to obtain in the United States and that expansion has been continuous and is still going on.

The Weston glass jewel, known as the Weston *Y* bearing, has been developed with perhaps greater stress on the matter of surface finish and resistance to vibration than to the factor of shock, although shock tests of the order of $250G$ appear to be required in any direction before pitting takes place with a moving system having less than one gram weight. As is indicated by McCune and Goss, vibration tests have been surprisingly satisfactory, and it seems apparent from such tests conducted in the Weston laboratories, at the Bureau of Standards, and elsewhere, that actual jewel wear is materially less under conditions of severe vibration with the *Y* bearing as compared to the sapphire. The perfection of the surface finish is probably responsible, as well as the fact that the bearing more nearly approaches the hardness of the pivot, thus reducing the tendency of the pivot to wear.

While it is admittedly difficult to cut a perfect pit in a sapphire, it is hardly believed that variation in lineal cutting speed from center to periphery, as referred to by McCune and Goss, is responsible for the difficulty, since the cutting tool traverses any point in the sapphire once every revolution, and will take off as great a thickness of material near the center as near the edge. Wear on the cutting tool is perhaps a function of cutting speed, but it is believed that cutting speed is not a factor with regard to the relative depth of material removed per revolution.

It is interesting to note that the coefficient of friction varies so little between materials, and it seems evident that the criterion in the bearing is not so much the friction coefficient, but rather its ability to maintain its original shape and surface under conditions of severe vibration, shock, and use. It seems quite probable that these new special glass bearings, as developed in the several laboratories, will find fields of usefulness in future years in small instruments with light elements, where high sensitivity is required, even though sapphire bearings again become as plentiful as in years gone by.

F. K. McCune and **J. H. Goss:** Mr. Miller has presented an interesting history of the Weston Electric Instrument Corporation's experience with glass jewels. His findings are in general agreement with ours and are especially interesting on vibration. Work not complete at the time of writing the paper has shown the following curve to be typical of instrument performance after severe vibration.

Mr. Miller has stated "shock tests of the order of $250G$ appear to be required in any direction before pitting takes place" for a moving system of one gram weight.

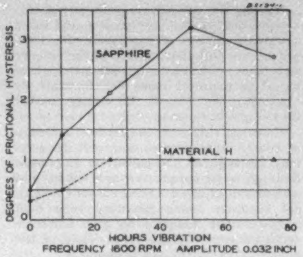


Figure 1

Our experience has indicated that such a value may depend materially on the speed with which the acceleration reaches its maximum and varies with the rigidity of the moving system in question. Work done on a device in which the motion was of the form of a damped sine wave with a time of 1.5×10^{-3} seconds from initiation to completion of a half-cycle gave very different values from those obtained with a slow rate of loading the jewel surface. However, the value at which the pivot was mushroomed in the case of the sapphire was approximately that at which a blemish would be produced in the jewels of material *H*.

In conclusion, the authors are both continuing work on the *V* jewels and progressing the development of ring jewels from the same material. Many hundreds of thousands of the *V* jewels have already been produced with characteristics as represented in this paper.

A New Moving-Magnet Instrument for Direct Current

Discussion and authors' closure of paper 42-123 by H. T. Faus and J. R. Macintyre, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 586–8.

John H. Miller (Weston Electrical Instrument Corporation, Newark, N. J.): A comparison of the energy for full-scale deflection taken by the new moving magnet instrument mechanism of Faus and Macintyre, with that required for conventional mechanisms may be of interest.

It is stated in the paper that with a moving element weight of 0.205 gram and an effective torque for a full scale of 0.35 millimeter-gram, giving a torque-to-weight ratio of 1.7, the coil energy was 0.05 watt for full scale.

Conventional small a-c iron-vane instruments, with somewhat heavier moving elements, on the basis of springs to give the same torque-to-weight ratio, would require 0.5 watt. In other words, the new instrument discussed has an energy sensitivity something like ten times that of conventional soft-iron instruments, which have been used for many years.

Making the comparison with permanent-magnet moving-coil instruments, and using a moving system of practically the same weight, we find that for one of the simpler structures with moderate damping, there would be required about 0.00036 watt, or something like $^1/_{140}$ of the energy needed for the new instrument.

However, this is on the basis of a design of nearly three decades ago, and more recent instruments compared on the same basis of equal torque-to-weight ratio, and with about the same moving element weight, are regularly being produced with high coercive force magnets to give full-scale deflection on 0.000025 watt, $^1/_{2,000}$ of the energy taken by the new instrument.

It is believed that this comparison with equal torque-to-weight ratios is quite valid, since, obviously, both old and new types of mechanisms can be supplied with bearings of any one of the several types currently used, and, with similar bearings and similar moving system weights, similar torque-to-weight ratio should be the determining criteria.

While it is admitted that iron-vane instruments of conventional design have a considerable d-c error, this, of course, can be reduced, if important, by the use of suitable material in the iron vanes.

In the relatively wide energy span separating the conventional iron-vane instruments and the best of the permanent-magnet moving-coil instruments, a span from 500,000 down to 25 microwatts, or a factor of 20,000 to 1, this new design, on a comparable torque-to-weight ratio basis, has only progressed one decimal place from the conventional iron-vane type.

Since the only advantage of the new mechanism is low cost, one wonders if it is sufficiently less costly to justify the relatively high energy taken from a circuit which effectively limits its broad applications.

J. R. Macintyre and **H. T. Faus:** A comparison of the moving magnet instrument with the iron-vane a-c instrument is not relevant, since reasonably accurate and well-damped instruments of this type are as expensive as moving coil instruments. Our comparison was with the lower-cost polarized vane instrument which has the additional disadvantages of poor scale distribution, short scale, and poor damping.

The comparison of the microwatt sensitivity of the moving magnet instrument with that of the moving-coil instrument does not give an accurate picture of the relative merits of the two designs. For example, Mr. Miller gives a figure of 0.000025 watt for full-scale deflection on a sensitive moving-coil instrument. Using this figure, one not familiar with instruments might assume that a ten-ampere ammeter could be built with a resistance of $\frac{0.000025}{100} = 0.00000025$ ohm. This is not the case, because springs of the required low torque cannot be built to carry ten amperes, much less to have a resistance as low as the above value. The general practice in a moving-coil instrument of this rating is to use a shunt with a 50-millivolt drop and to connect the instrument winding, in series with a suitable resistor, across this shunt. The net result is an instrument with an over-all loss of 0.5 watt. An instrument of the same rating in the moving magnet design would have a winding consisting of a single turn of heavy copper wire occupying the entire winding space. The total watts loss of the instrument would be about 0.05 watt. The addition of a shunt to give ambient temperature compensation equivalent to that of the moving-coil instrument would not increase this to more than 0.1 watt, or one-fifth the power required by the complete moving-coil instrument.

The general practice is to build voltmeters with a full-scale current of ten milliamperes except for particular applications where low current consumption is required. Using a full-scale current of ten milliamperes, a moving magnet voltmeter with jeweled bearings can be built in full-scale ranges from ten volts up with a torque-to-weight ratio adequate to insure a sustained accuracy of two per cent of full-scale reading. This is equivalent to that usually guaranteed for minature moving-coil instruments. It refers to the exact construction shown in the paper and by no means represents the ultimate sensitivity obtainable in moving magnet instruments. Since the over-all power consumed by both types of instruments is the same, the moving magnet design is at no disadvantage because of the greater power consumed by its element.

From the foregoing it is apparent that there is a wide range of instrument ratings in which no use can be made of the maximum sensitivity of the moving-coil instrument and in which the moving magnet instrument is at no disadvantage due to its lower sensitivity.

The authors felt that the implied advantages of the instrument would be apparent to those familiar with instruments. However, in view of Mr. Miller's comments, "Since the only advantage of the new mechanism is low cost," it may be well to bring some of these advantages into greater prominence.

The maximum change in reading of the moving magnet instrument in a ten-gauss field is one half of one per cent of full-scale reading. The corresponding change for a moving-coil instrument is of the order of three per cent. The stray field produced by the permanent magnet of a moving-coil instrument will cause a considerable error in the reading of another moving-coil instrument when mounted close to it and is sufficient to cause an appreciable error in a magnetic compass at a distance of several feet. Mounting on a steel panel will change its calibration because of the shunting effect of the panel on the instrument magnet. Effective shielding to overcome these deficiencies is difficult, because a shield close to the magnet will shunt a large part of its flux, and one far from it will increase the outside dimension of the instrument. In either case the calibration difficulties will be increased, as the instrument must be calibrated with the shield in place, and the shield interferes with the stabilizing a-c knockdown treatment which should be given to the instrument magnet. None of these difficulties are experienced with the moving magnet instrument. These instrument elements may be mounted as closely together as their physical dimensions permit. Since their dimensions are so much smaller than those of moving-coil instruments, it is possible to mount more of these instruments in a given space. This is a very valuable feature in many applications.

Another valuable feature of the moving-magnet construction is the readiness with which it may be adapted for measuring current ratios by substitution of another fixed coil for the control magnets. The instrument then becomes a ratio meter and the scale distribution may be predicted by the same vectorial construction shown in Figure 4.

Emergency Overloads for Oil-Insulated Transformers

Discussion and authors' closure of paper 42-101 by F. J. Vogel and T. K. Sloat, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 669–73.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): This paper adds some further data on the aging of insulation and gives some proposals on how to use the data. I cannot agree with some of the claims set forth in the paper, for example, the statement that at 120 degrees centigrade the tensile strength drops to approximately 50 per cent and levels off. Further, it states that it would appear that this 50 per cent strength would be maintained for long periods of time. This is an important point, because, if true, it would permit long-time operation at 120 degrees centigrade without any further sacrifice in the life of a transformer. As a matter of fact, the data in Figure 5 on Fuller board aged in oil protected by nitrogen show a continued decrease in tensile strength.

I agree that the way in which the curves are plotted in Figure 1 might lead one to assume that the tensile strength had leveled off. All aging data that I have ever obtained fell on a straight line when plotted on semilogarithmic paper, with time on the logarithmic scale and temperature on the uniform scale and temperature on the uniform scale. In fact, all of the aging data shown in Mr. Clark's paper fall on a straight line indicating in some cases and actually showing in other cases that the tensile strength continues to deteriorate at the same semilogarithmic rate until zero life is reached. This is one nature's laws discovered many years ago by Sir Isaac Newton and known as the "die away curve."

I have taken the liberty of replotting the data shown in Figure 1 of the authors' paper on semilogarithmic paper. Figure 1 indicates that the tensile strength does not level off but apparently continues to deteriorate at the same rate. When replotted on semilogarithmic paper, and extrapolation of the data shown in their Figures 5 and 11 indicates the 135 degrees centigrade life is longer than the 120 degrees centigrade life. Furthermore, there appears to be no consistent rule in degrees increase to double the rate of aging as shown by these curves.

In a discussion of Figure 5 it is stated that the markedly lower strengths obtained with pressboard and varnished cambric, as compared to Manila paper alone, are due to higher acidity. However, these tests were made with oxygen-free oil, protected by a nitrogen atmosphere. These results are therefore inconsistent with the claim that the panacea is to provide an inert gas cushion above the oil.

The assumption that the hot-spot rise over top oil varies as the loss 0.8 can be used for all classes of transformers, particularly small distribution-type coils, does not agree with our findings. For instance, we have found that under the condition where the 0.8 power holds for horizontal coils, for vertical coils the exponential power ranges from approximately 0.9 to 1.0, depending on the thickness of the winding. For thick windings having high internal gradients and small surface drop, the power is close to unity as would be expected. In general, for thin single-layer vertical coils, the power is approximately 0.1 higher than for horizontal coils. This is, of course, due to the greater effect of the viscosity of the oil for horizontal than for vertical coils at the lower temperatures, whereas for the higher temperatures the difference in the effect of viscosity tends to disappear. This effect on the temperature rise of the horizontal and vertical coils is very well illustrated in Figures 3 and 4 of reference 1.

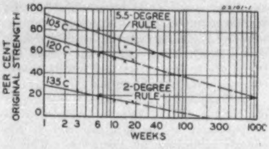


Figure 1. Tensile strength of Manila paper in oxygen-free oil protected by a nitrogen atmosphere from Figure 1 of paper by Vogel and Sloat

REFERENCE

1. HOT-SPOT WINDING TEMPERATURES IN SELF-COOLED OIL-INSULATED TRANSFORMERS, F. J. Vogel, Paul Narbutovskih. AIEE TRANSACTIONS, volume 61, 1942, March section, pages 133–6.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): The paper is very fine in showing effects of thermochemical factors on the mechanical strength of transformer insulation. This is a valuable but somewhat more specialized study than indicated by its title. From an operating standpoint, a correlation between mechanical strength and electrical failure still has to be made; this subject is barely touched. Destructive testing of small motor stators has been recently reported.[1] It appears that similar tests of (small) transformers are practical and that the results would add interpretative value to the more difficult studies already accomplished.

REFERENCE

1. TEMPERATURE-AGING TESTS ON CLASS-A-INSULATED FRACTIONAL-HORSEPOWER MOTOR STATORS, J. A. Scott and B. H. Thompson. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 499–501.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The authors have shown that transformers of the usual design can probably be considerably overloaded without serious effect on their life, and there appears to be unanimity of opinion on this point throughout the industry. The writer would like to call attention to the fact that overloading transformers also proportionally increases the transformer regulation. Furthermore, much of the load growth which is requiring the overloading of transformers is industrial load, usually of lower power factor than the system average. This may result in a considerable increase in the system requirements of reactive power, which would also increase the regulation of the transformers. Superimposed on this is the increase of reactive losses in the transmission system caused by the increased line loadings at lower power factors. If sufficient reactive capacity is not available near the load centers, the system voltages may sag considerably, thereby further adding to the reactive power demands.

It therefore follows that, unless the system is unusually well supplied with reactive capacitance, considerable attention should be paid to this matter of adequate voltage, as otherwise it is possible that the overload ability of transformers cannot be fully realized because of distribution voltage limitations, and no substitute for volts at the customer's service has yet been found.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The results shown in Figure 1 of the paper are very surprising in that they indicate practically no deterioration in the aging tests between 6 and 18 weeks. According to these data, insulation kept indefinitely at 105 degrees centigrade would remain at 70 per cent of its original strength, and 120 degrees could not produce more than 50 per cent deterioration at any time. In the oven-aging tests in Chicago on sealed oxygen-free samples of various makes of impregnated-paper insulated cables, which were described in my paper on "Load Rating of Cable—II,"[1] a sharp decrease in tearing strength occurred in tests at 125 degrees centigrade between two and five months. This decrease was found for each of the several samples tested. The studies made years ago at the Massachusetts Institute of Technology, on the effect of heat on paper insulation, corroborate the continuously decreasing trend of mechanical strength with time of aging, found in the recent Chicago tests. Possibly, tests on a large number of samples for durations beyond 18 weeks would modify the slope of the curves in Figure 1 of the paper.

Table I

Test Temperature Degrees Centigrade	Mechanical Strength After 100 Days—Per Cent of Original Value		
	Chicago Tests		Tests by Vogel and Sloat Figure 3
	Average	Minimum	
100	99	91	78
125	67	54	32

Also, the slopes of the curves in Figure 3 of the paper, which are obtained by cross-plotting from Figure 1, are subject to question. The 20, 40, and 50 per cent curves are based on only one measured point, and the 60, 65, and 70 per cent curves are based on two points. The uncertainty of the slope of the curves is illustrated by the fact that, according to Figure 1, the strength remains at 54 per cent in the 120-degree test between 8 and 18 weeks while, according to Figure 3, the strength decreases at 120 degrees from 54 per cent after eight weeks to 49 per cent after 14 weeks.

Table I of this discussion is a comparison of the results obtained in the oven-aging tests in Chicago, Figure 1 of my paper on "Load Rating of Cable—II," with values taken from Figure 3 of the paper by Vogel and Sloat.

Do the authors have an explanation for these large discrepancies?

REFERENCE

1. LOAD RATING OF CABLE—II, Herman Halperin. AIEE TRANSACTIONS, volume 61, pages 930–42.

F. M. Clark (General Electric Company, Pittsfield, Mass.): My interest in this paper concerns primarily the following statement which is made in the general summary:

> "Acids are formed in service by contact between oxygen and transformer oil. Excluding this contact by blanketing the transformer with inert gas results in a high mechanical strength of the insulation at a given temperature and time or conversely permits a higher operating temperature."

On the basis of the data submitted in the paper this statement can be true only within definite limits. It cannot be true at 135 degrees centigrade, for, if one compares the data for Manila paper of Figure 1 and Figure 11, one finds no real advantage from the use of the nitrogen gas. I have reproduced in Figure 2 of this discussion the pertinent data presented by the authors. A relation such as this indicates that at 135 degrees centigrade heat alone causes paper deterioration at a rate which is not dependent on the oxidation effects referred to by the authors.

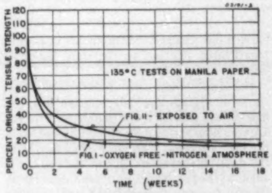


Figure 2

Although the authors do not illustrate the behavior of oil-treated Fuller board aged in the presence of oxygen the remarkable stability of the Fuller board in the absence of oxygen (Figure 9 of the paper) might possibly be taken as support for the author's contention that oxidized oil is responsible for the mechanical deterioration of the cellulose. The Fuller board of Figure 9 is characterized by substantially no decrease in mechanical strength during a period of 18 weeks at 120 degrees centigrade. This apparent mechanical stability is contrary to the data accumulated in our own laboratory and does not agree with the authors' data of Figure 1 for Manila paper, where a loss of 50 per cent in tensile strength is shown for the same oxygen-free test conditions. Neither do the data agree with the previously published work of C. F. Hill to which the authors refer. Mr. Hill states that cellulose is "subject to both temperature and oxidation effects, the temperature effect of course taking place above 105 degrees centigrade."

In Figure 3 of this discussion I have compared the data on Fuller board presented by Mr. Hill with that presented by the authors, converting Mr. Hill's data to percentage of original tensile strength in order to make the comparison clear. Although both investigators are in fair agreement at 135–140 degrees centigrade aging, there is a marked divergence in the data presented for the temperature range from 105–120 degrees centigrade. In our own laboratory we obtain, as does Mr. Hill, a decrease in strength as the aging progresses, but, unlike Mr. Hill, we find no stabilization in mechanical strength after about 50 per cent of the initial strength has been lost. The mechanical deterioration in the studies which we have carried out proceeds continuously in a semilogarithmic relation to the time factor of heating.

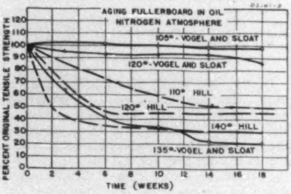


Figure 3

Our own explanation of the fundamental cause of mechanical deterioration in cellulose differs greatly from that offered by the authors who attribute it to acid formation in the oil. I am not especially concerned with the data presented which test the combined effect of Fuller board and varnished cloth. Obviously acids derived from sources foreign to the cellulose or the oil may or may not affect the mechanical strength of the cellulose, depending upon their chemical natures Their presence, as described by the author. in Figure 6, is not dependent on the presence of oxygen. No data are presented by the authors correlating the behavior of such combinations of materials with the thesis advanced. Furthermore, the effect of acids derived from varnished films appears to be of but academic interest, for, as the authors state, acid formation from this source has been practically eliminated by the use of synthetic materials. I am, however, concerned with the suggestion that acid formation in the oil through oxidation is responsible for the mechanical deterioration. I call attention to the fact that the 50 per cent deterioration in the mechanical strength of Manila paper in Figure 1 was obtained with substantially no change in the oil acidity.

Elimination of acid formation as the result of oxidation in mineral oil can be brought about by a variety of methods. Only one of these methods, the elimination of oxygen, produces any substantial effect on the mechanical aging of cellulose. I suggest that the presence of oxygen affects the mechanical life of oil-immersed cellulose because of its direct oxidation action on the cellulose. As such, as suggested by Mr. Hill the effect of oxygen is most important for the lower temperatures applied. As the temperature is raised, the mechanical deterioration because of heat alone becomes of increasing importance. This type of deterioration involves the formation of acids derived from the cellulose and occurs whether or not oxygen is present. The use of nitrogen to eliminate oil acidity is not of fundamental importance. Although the elimination of oxygen may be of academic value in promoting the mechanical stability of cellulose insulation if a transformer is operated at temperatures of 75–95 degrees centigrade, it must be remembered that the oxidation reaction itself proceeds so slowly at these temperatures that it is demonstrated only with great difficulty. Higher temperatures of operation in the range of 100–120 degrees centigrade, or higher in the case of overload conditions, bring into play the thermal type of cellulose deterioration. This type of deterioration becomes of greater importance as the temperature and the duration of its application are increased. In a general sense, we have found that the insulation will age at a rate not materially affected by the presence or absence of mineral oil. Acids derived from mineral transformer oil of the American type have not been found to be a dominating factor in determining the mechanical life of the insulation.

T. K. Sloat and F. J. Vogel: We desire to acknowledge Mr. Von Voigtlander's discussion, and it is true that regulation must be considered in any application.

With reference to Mr. Taylor's discussion, tests on small transformers have been made and indicate that they are still operative, even after thousands of overloads and high temperature cycles. The results of these tests and others will undoubtedly furnish the material for papers which will be presented at a later time.

We are rather surprised at the small amount of deterioration shown in Mr. Halperin's discussion. Neither Mr. Clark's nor our tests indicate such high values after 100 days at 100 or 125 degrees centigrade. Mr. Halperin has criticized the construction of Figure 3. For purposes of analysis, data in the form of Figure 3 were more valuable than Figure 1, which was the original data. When curves are drawn with different parameters, unless the data are extremely accurate, some variations are to be expected. In this particular case, the very nature of the materials led to variations which could not be eliminated except by an extremely large number of tests and samples.

Mr. Montsinger, in his discussion, clearly points out his difference of opinion as to the characteristics of insulation deterioration. He is not, however, entirely consistent in his criticisms. He refers to Figure 5 as indicating that insulation shows a continuous decrease in tensile strength, even when protected by nitrogen. In that case, the introduction of varnish-treated material is shown to be the cause, and even an inert atmosphere will not provide protection when such materials are used. Mr. Clark, in his discussion, clearly took this point into consideration.

Mr. Montsinger has seen fit to replot the data of Figure 1 on semilogarithmic paper. He, however, first assumes that the data should be represented by straight lines on semilogarithmic paper. If that assumption were not made, curves could equally well be shown as flattening out in the period between six to ten weeks and results nearly constant after 10 to 15 weeks.

In considering Mr. Clark's discussion, one might think that we recommended temperatures in the neighborhood of 135 degrees centigrade for operation. This is not true. Within definite limits, excluding oxygen does result in a higher mechanical strength of insulation for a given time and temperature. It is generally well recognized that thermal decomposition of cellulose takes place at a temperature above 125 degrees centigrade, and the advantage of an inert atmosphere would be partially lost if operative temperatures in this range were continuously employed.

Table II

Temperature Limits	Oxidation	Thermal Decomposition
Clark	75–100	Above 100
Sloat-Vogel	75–120	Above 120

With respect to the discussion regarding the effectiveness of acids, additional tests made at the same time as those represented show that Fuller board is affected by acids. It might be pointed out in all cases the acids referred to contain some peroxides. The peroxides are the detrimental agents, as they cause cellulose deterioration. However, it is quite well accepted that the acids are a measurement of the peroxides present.

With respect to Mr. Clark's discussion regarding the mechanical stability of cellulose insulation as affected by oxygen, one might be led to believe that the rate of insulation deterioration at the lower temperatures is so slight that it would be demonstrated with great difficulty, and that there is less and less difference as the temperatures become higher. One of the best evidences to the coutrary is the comparison between Figure 1 and Figure 11 of the paper. In Figure 1 at 105 degrees centigrade, the insulation strength seems to be about 70 per cent of its original value after eighteen weeks at 105 degrees centigrade, whereas, with air present it was down to 35 per cent and decreasing rapidly. There is, however, some agreement with Mr. Clark, after all, in the results of our tests. We agree on the types of cellulose deterioration, but not in the degrees of temperature at which the types of deterioration take place as shown in Table II of this discussion.

It should be noted, that there is no sharp line of demarcation. Both effects overlap to some extent. Temperature limits in Table II should be considered as those limits within which either oxidation or thermal decomposition are predominate factors.

Factors Affecting the Mechanical Deterioration of Cellulose Insulation

Discussion and author's closure of paper 42-98 by F. M. Clark, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 742–9.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): F. M. Clark's paper adds a definite milestone in determining the effect of temperature and moisture on the aging of insulation. For several years we have been in need of more exact data on how the higher temperatures affect the rate of aging of cellulose insulation. While I have used the eight-degree rule in deriving short-time overloads where the temperatures were considerably in excess of 110 to 120 degrees centigrade, I had no definite proof that it held for the higher temperatures. Consequently, I am considerably relieved to learn that the eight-degree rule holds up to 200 degrees centigrade.

Of course, it should not be overlooked that Mr. Clark finds that for temperatures in the order of 90 degrees centigrade to 105 degrees centigrade, a rule less than eight degrees holds (in the order of three to six degrees), and for lower temperatures a still lower rule, as low as two degrees, holds.

Based on Mr. Clark's data, the eight-degree rule should be used in estimating short-time overloads where the hottest spot temperature is in excess of approximately 110 degrees to 115 degrees centigrade, and approximately a five-degree rule when estimating the effect of temperatures in the neighborhood of 90 degrees to 100 degrees centigrade on the life of transformers. There is one very important way in which the five-degree rule can be used, namely, to see whether it is economical to increase the temperature rise of transformers above 55 degrees centigrade. For example: by the five-degree rule, the life of a 60 degrees centigrade rise transformer is half that of a 55 degrees centigrade rise transformer. The extra capacity gained when increasing the average rise from 55 degrees centigrade to 60 degrees centigrade, is in the order of six to seven per cent, and the gain in cost is in the order of three to four per cent. Consequently, by this process of reasoning, we save an initial investment of three to four per cent for a transformer having one-half the life. The initial saving in cost does not sound like a good economical proposition.

There is one very important point that has been settled by these tests, namely, that the effects of several overloads on the life of a transformer, are additive. This has generally been assumed in the past, but we had no definite proof that these assumptions were correct.

Mr. Clark's discovery of the important role that water content plays in the rate of aging also explains why my early tests showed that the insulation aged faster in mineral oil than in air—oven aged. Since the importance of keeping the insulations perfectly dry was not realized, the samples no doubt picked up considerable water between the drying period and the starting of the aging tests. The moisture that was picked up previous to the oven- or air-aged samples naturally was driven off immediately, while the oil-immersed samples retained their moisture to a certain extent.

To quote from my 1930 AIEE paper on "Loading Transformers by Temperature":

> "No completely satisfactory explanation has ever been given for this shorter life of insulation (aged) in oil than in air, though it has been suggested that it may be due either to the oil softening the fibers, or the acids in the oil attacking the fibers. This is a good subject for a physicist to study."

While this paper does not show comparative aging in air and in oil, such tests have been made under closely controlled moisture conditions, and no material difference was found in the rate of aging. Apparently this question has been cleared up, and we can no longer claim that the life of cellulose is longer in air than in properly protected oil, provided, of course, that the insulation is dry.

REFERENCE

1. LOADING TRANSFORMERS BY TEMPERATURE, V. **M. Montsinger.** AIEE TRANSACTIONS, volume 49, 1930, pages 776–92.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): I have followed with interest Mr. Clark's series of papers, which always contain a large amount of useful information. Figure 6 is of special interest, but it seems that some caution should be used in applying these data. The figure indicates that toward the end of the life in the range of 75 to 100 degrees centigrade, an increase in temperature of 2 or 2.4 degrees cuts the remaining life in half, and the conclusion may be drawn that a slight temperature increase is a serious matter. It should be noted, however, that this conclusion is based on values extrapolated from about 100 weeks to 2,000,000 or 5,000,000 weeks and from about 25 per cent reduction in strength to 100 per cent reduction in strength. It is quite possible that actually the slope of the 75-degree curves in Figures 3 and 4 might be quite different for long durations or, in other words, that the total life is quite different from the extrapolated values of 2,000,000 or 5,000,000 weeks. Such a change would, of course, affect the relation between rate of deterioration and temperature increase.

From a practical standpoint the upper portion of Figure 6 is relatively most important, that is, down to 50 per cent of the initial strength. The average for the first 50 per cent of the mechanical life consumption of the temperature increase to double the rate of deterioration ranges from about six to ten degrees centigrade which, for practical purposes, agrees quite well with Montsinger's eight-degree rule.

Figure 4 of Mr. Clark's paper shows much greater deterioration than indicated in oven-aging tests in Chicago of sealed cable samples, which are described in my AIEE paper on "Load Rating of Cable—II."[1] As in both Mr. Clark's and the Chicago tests, the insulation was vacuum dried, and oxygen was excluded, the tests should be expected to be comparable. Figure 4 of Mr. Clark's paper shows for one year at 100 degrees centigrade a reduction in strength of about 35 per cent while the same test period in Chicago produced for a large number of samples an average reduction in strength of four per cent and a maximum reduction of 19 per cent. To obtain a 50 per cent reduction in mechanical strength in one year, a temperature of about 102 degrees is required, according to Mr. Clark's tests, while the tests on short cable samples and on large trial field cable installations in Chicago indicate that about 120 degrees is required. Possibly, the difference in cable oils and transformer oils had some effect on the large resultant difference of 18 degrees centigrade, but that explanation does not seem adequate.

The information on the effect of moisture and of volatile acidic products upon the rate of deterioration is of considerable interest. The possible importance of the effect of moisture and volatile products upon oil-impregnated insulation was early recognized in the research project on insulating oils and cable saturants at the Massachusetts Institute of Technology, and for this reason these studies were made in a closed system which allowed retention of moisture and volatiles if desired. However, no concrete data, especially on paper strength such as given by Mr. Clark, were available. From a practical standpoint, I am glad to see additional evidence emphasizing the importance of keeping the content of moisture and acids in impregnated insulation to a minimum.

Mr. Clark's discussion of the effect of oxygen may create the impression that, above 120 degrees centigrade, oxidation becomes less pronounced while, if I understand correctly, oxidation above 120 degrees becomes less important only in comparison with the rapidly increasing pyrochemical decomposition.

REFERENCE

1. LOAD RATING OF CABLE—II, **Herman Halperin.** AIEE TRANSACTIONS, volume 61, 1942, **pages** 930–42.

J. G. Ford (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): I am very much interested in the paper of F. M. Clark on the subject of "Factors Affecting the Mechanical Deterioration of Cellulose Insulation." The paper indicates that he has done considerable detail work on this study of a problem which is very important to the electrical industry. There are several points which come to mind which I would like to discuss briefly.

In the method used by Mr. Clark as described in his paper the initial samples of 0.003 manila paper were conditioned at 65 per cent relative humidity at 70 degrees Fahrenheit. I note that the oil impregnated papers are tested immediately after being taken from the oil sample. This brings up the question as to the dryness of samples and the effect of the oil on the tensile strength on which aging tests have been made. It would appear that it would be desirable to test the samples all at the same water content so that the values obtained would be representative of the actual change in tensile strength. This, of course, would involve extracting the oil from the paper and subsequently humidifying under the same condition.

I note that practically all of the work that has been done on the subject of aging of cellulose insulation as used in transformers, involves tensile-strength measurements on the insulation. Personally I do not believe that deterioration in tensile strength tells the complete story, and I would suggest that compression strength as well as shear strength would be of even greater importance, particularly, since the short-circuit stresses on transformers subject the insulation to both compression and shear rather than tension. Based on actual tensile strength on old transformers we have examined, it would appear that such transformers would not stand any short-circuit tests. However, such transformers have been tested and found to stand up, again indicating that the insulation is more liable to fail by compression rather than tension. Of course, it is understood that lower tensile strength would also indicate lower compression strength of insulation, but this may not be a direct relationship. Mr. Clark has made a considerable number of tests to demonstrate the effects of decomposition products of cellulose on the deterioration of the cellulose. I think we all agree that decomposition products are detrimental and are a factor in the life of a transformer. However, I believe that the type of test Mr. Clark has made exaggerates the effect of decomposition products inasmuch as the cellulose is heated in a sealed tube in which all the components are at the same temperature, whereas in a transformer the winding temperature is always higher than the temperature of the oil and surrounding temperatures, and generally in any transformer there is a cool zone which will allow for the condensation of these products, thus reducing the effects. We have recognized the effects of reaction products from varnish and other materials as being also in the same category as decomposition products of the cellulose itself and have attempted to eliminate such products in the present design of the transformer. In many cases we have substituted varnish-treated materials with plain paper, and, where varnishes are used, we have adopted synthetic, low-acid-type varnishes, so as to keep the acidity at a minimum value.

I believe the study of insulation deterioration in transformers is very important and merits further study, but I am convinced that we should have compression and shearing strengths as well as tensile strength, and also that such laboratory information should be backed up by actual tests on aged transformers.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): Mr. Clark's extensive data concerning deterioration of cellulose insulation at high temperature and his analysis of these data mark a great advancement in this important field. His quantitative information on the effect on deterioration at various temperatures of moisture and oxygen, singly or together, go far beyond anything previously available.

Over the past few years various manufacturers have been studying the deterioration of varnished cloth insulation at various temperatures. The very great increase of deterioration rate produced by the presence of small quantities of water has been found there also. Furthermore it has been found that this greater mechanical deterioration has been accompanied by a large increase in dielectric power factor. These results apply to sealed samples. In samples open to the air the moisture is driven off at the aging temperatures, and its effect becomes unimportant.

F. M. Clark: The study of the aging of any organic material is extremely difficult, because the products of the chemical changes involved are frequently extremely effective in determining the rate of subsequent deterioration. This has led to the generally accepted idea that the best way to study the deterioration of an organic product whether it be an automobile tire or mineral oil is to reproduce as nearly as possible the conditions of actual commercial use. This is true of cellulose insulation. Attempts to reproduce the conditions of transformer service, however, have resulted in a variety of test data which have been frequently in conflict and have been of doubtful value because of the difficulty of controlling the conditions of the test run. In general, therefore, the laboratory tests on the deterioration have been found to be most acceptable when some sacrifice has been made in the desire to reproduce commercial conditions in favor of the necessity of obtaining accurate test control. This has been done in the various studies both in this country and abroad in the testing of mineral oil and cellulose. Test results obtained in such a way have invariably been found to represent more severe deterioration than would be expected from service data but with proper consideration to the basic principles involved, such studies have promoted progress toward the goal of greater chemical and mechanical stability.

Mr. Halperin finds that the laboratory data which I have presented indicate a more severe deterioration than he has observed in cable studies. The test set-up which I have used was intended to produce data applicable to transformer use. Other studies which we have made indicate that the degree of deterioration observed under a definite set of test conditions is affected by the mechanics of the test set-up. This factor must not be ignored when one considers the commercial data presented by Mr. Halperin. I have not observed, however, that the differences in test set-up will alter the fundamental behavior involved, although they may affect the rate of deterioration. Thus despite wide differences in test set-up, the presence of moisture has always resulted in an acceleration of the mechanical deterioration in cellulose insulation.

It is true as stated by Mr. Halperin that the effects of oxidation do not disappear at temperatures above 120 degrees centigrade and I have tried to avoid any such impression. What I have tried to demonstrate is that, because of the importance of the purely thermal type of cellulose deterioration, chemical agents and mechanical means which might be effective in promoting the mechanical stability of cellulose at low temperatures where the oxidation effect is of importance must not be assumed to possess equal value in the stabilization of the cellulose at the higher temperatures obtained under overload conditions.

As indicated by Mr. Atkinson, the effect of moisture on insulation is of importance even when varnished materials are used. The relation between the chemical and mechanical changes in paper and the resulting power factor is a problem of commercial and technical importance.

I have followed the work of V. M. Montsinger in this field for many years. It is a pleasure to present data corroborating suggestions which he has made in the past and which have many times formed the basis for transformer design and operation.

I agree with Mr. Ford that other properties than tensile strength may be of value in the final analysis of those factors which affect the aging of cellulose. I find, however, that in a study of this type, limited objectives frequently result in faster progress than can be otherwise obtained. I hope that Mr. Ford or myself may sometime be able to present data covering the response of the compression and shearing properties of cellulose insulation to those factors which are discussed in this paper. With specific reference to the water content of the insulation when tested for tensile strength after aging, it has been our experience that the variation directly traceable to the difference in water content of the paper is of small magnitude in comparison to the total change described in the tests of this article. Attempts to follow a procedure such as suggested by Mr. Ford (extraction of oil and later humidification of the paper) have produced no worth-while results and frequently have raised questions directly related to the oil extraction effects. The work of Mr. Ford and his associates in this field has been of interest and value, and I hope that by the continued study of this problem in both of our laboratories we may contribute toward its ultimate evaluation and solution.

Application of Apparatus and Conductors Under Various Ambient-Temperature Conditions

Discussion and author's closure of paper 42-93 by R. E. Hellmund and P. H. McAuley, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 553–8.

V. M. Montsinger (General Electric Company): This paper gives some very valuable information on ambient temperatures throughout the United States and how they should be used. I am particularly interested in the author's derivation of the equivalent ambient obtained when integrating the area by the eight-degree rule. This should be factored under certain conditions in the overloading of transformers in cool ambients. The American Standard Guides now permit one per cent overload for each degree that the daily air is below 30 degrees centigrade for self-cooled, and the daily water temperature is below 25 degrees centigrade for water-cooled transformers. The guides also state that the load should be decreased two per cent for each degree that the daily ambient exceeds these values. The question has been raised as to what should be done in cases where the daily average ambient exceeds 30 degrees centigrade for a few days during the summer months. There are, no doubt, many days during the year in which the air is well below 30 degrees centigrade and where the one per cent overloading rule is not taken advantage of. In such cases it should not be necessary to derate the transformer during the few days when the average daily ambient exceeds 30 degrees centigrade.

The necessity for this derating can be overcome by increasing the ambient time period of 24 hours to one year. If the time period is increased to one year, the amount of overload should be based on the equivalent annual rather than the average annual ambient. The equivalent ambient is, of course, higher than the average ambient, since aging is not directly proportional to temperature. For example: by the eight-degree rule, six months' operation in a 30-degree-centigrade ambient and six months' operation in a ten-degree-centigrade ambient, is equivalent to 12 months' operation in 23.8 degrees centigrade or 3.8 degrees centigrade higher than the average value of 20 degree centigrade. Imposed on this, of course, is the difference between equivalent and average daily ambient, but this amount is small, being between 0.5 and 1.0 degree centigrade, and there is enough safety factor in the one per cent rule to take care of this difference. Hence, it is possible to use the daily *average* ambient instead of the equivalent daily ambient in applying the one per cent rule to daily overloads in cool ambients.

The use of the equivalent annual ambient will be advantageous in two ways.

1. It is a means—and apparently the only means—whereby the loading of transformers can be adjusted according to their annual ambient temperatures. In other words, transformers operating in the colder northern zone can be increased to give the same life as transformers operating in the warmer southern zone.

2. It will in many cases eliminate the necessity for derating during the few days in the summer months when the average daily ambient is above 30 degrees centigrade.

Whether the use of the average daily or equivalent annual ambient will permit the larger all-year output depends on local loading conditions. The conditions under which they should be used to determine the overload capability, are:

1. For one day emergency overloads, use the daily average ambient.

2. When the load is constant throughout the year, or when the greater capacity requirements are in the summer months, use the equivalent annual ambient.

3. When the greater capacity requirements are in the winter months, determine the equivalent ambient for the heavy-load months and for the light-load months, and check the load capability for both periods. This permits loading up to normal transformer temperatures in winter whenever the reduced summer load keeps the transformer temperatures to normal or lower on hot days.

The AIEE transformer subcommittee is studying the question of how to use annual equivalent ambients, and it is expected that its recommendations will be published in the near future.

P. H. McAuley: An error occurred in Figure 2 of the paper; curve 5 is for Helena and curve 6 for Bismarck.

Mr. Montsinger expresses agreement with the use of the equivalent annual temperature for determining apparatus loadings. In addition, he discusses operating conditions where it may be desirable to distinguish between daily and seasonal overloads. To obtain equivalent annual temperatures based on time-temperature curves as given for six cities in the paper is a rather laborious procedure and the essential data are not always available. C. F. Wagner has suggested an approximate method of determining the equivalent annual temperature. Only the average annual and monthly temperatures are used, such as given in the Atlas of American Agriculture, part II, section B, United States Department of Agriculture, 1928, for 626 cities. In this method aging units for each month at a given location are calculated with the eight-degree-centigrade rule and added to give annual aging units. A corresponding single temperature is found. From this calculated value the annual average temperature is subtracted. The difference is multiplied by 2 and added to the annual average temperature, which gives the equivalent ambient temperature. The factor 2 was obtained from a consideration of the equivalent temperatures for the six cities for which equivalent temperatures based on complete temperature-time curves are given in the paper. It is believed this method will give equivalent ambient temperatures in error not more than one degree centigrade. Because of the availability of the data required, this method has considerable merit.

Emergency Overloading of Air-Cooled Oil-Immersed Power Transformers by Hot-Spot Temperature

Discussion and author's closure of paper 42-115 by V. M. Montsinger and P. M. Ketchum, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 906–16.

Nicholas M. Oboukhoff (The Oklahoma Agricultural and Mechanical College, Stillwater, Okla.):

PART I

The following remarks primarily relate to transformers whose load is either residential or combined.

While industrial load can be characterized fairly by a single typical load curve, this can hardly be done in the case of the loads just mentioned above; the reason is that industrial load is relatively steady throughout the year, but residential and combined loads vary considerably, being correlated in some way with the seasons, specific months, and so forth. Dealing with industrial load one can be justified in using *daily* load factor derived from a typical curve instead of *annual* load factor; the difference will be small anyhow. Likewise, the period of 24 hours can be assumed as a basic one; operations like the integration of hot-spot temperature curves over this span of time, conclusions, and results referred to it are all in order and legitimate.

Yet with residential or combined loads a single typical load curve is a rather artificial construction; therefore, the proposal is that several ones should be constructed according to the seasons, specific months, geographical location, industrial development, and so forth, as a case may require, while the whole year should serve as a basic period of reference. A co-operation of the operators in furnishing necessary data would be needed and should be secured.

All this is yet to come. In the meantime, however, it is possible to see that the character of conclusions and recommendations already reached and offered by V. M. Montsinger and other research engineers and workers will hold substantially—with an enlargement and more boldness in the application of their methods to the residential or combined load transformers, as soon as the year has been adopted as the basic cycle.

Holding always in mind that the discussion is centered about those transformers, a general substantiation of the above view is as follows:

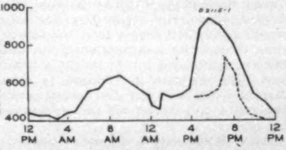


Figure 1. Residential and light industrial load curve

December 21, 1936
Maximum load 940 kw
Average load 595 kw
Ratio: average load to maximum 0.63
Ratio: rms current to maximum 0.65

There is a considerable difference between the highest load peak and the lowest one in the course of one year. The load curves as shown in Figures 1 and 2 of this discussion have been obtained on a local system by me in 1936; new data have been added in 1938 which disclosed that the highest load peak occurred on November 6 and amounted to 2,180 kw averaged over a half-hour, while May 9 of the same year the load peak was as low as 1,400 kw, which makes 64 per cent of the highest one, the difference being 780 kw. This is not a local feature; for other sources[1] give the ratio of the summer load peak to that in wintertime as between 0.57 and 0.70 for the same kind of load curves. Annual load factor was 0.44—also in agreement with reference 1, while daily load factor amounted to 0.53 in November and 0.67 in May, weekly load factors being 0.53 and 0.62 respectively. Those highs and lows were not accidental; they belonged to the most loaded week in November and to the least loaded week in May respectively, while other peaks were close to those designated before, that is, 2,180 kw and 1,400 kw during these weeks, respectively.

Let us apply this situation to two cases:

First, assume that a transformer is in a continuous operation throughout the whole year; then it would be only consistent to apply the three per cent[2] rule to the *annual* load factor with the result that the permissible overload should be 16.8 per cent instead of 14.1 per cent during the high-load week and 11.4 per cent during the low-load one if the weekly load factors were used. The proposal is that the annual load factor be considered in determining overloads if transformers are continuously operated. As a matter of fact, the annual load factor is noticeably lower than the others for a residential or combined load; therefore, the suggestion is in the direction of a more liberal allowance.

Second, let us suppose that a bank of transformers is in operation, each transformer being uniformly loaded throughout the year; then a number of transformers simultaneously operated will be approximately proportional to a peak load with the result that it will depend on the seasonal or other variations of a load curve. In the given example approximately two thirds of transformers (exactly 64 per cent would be required to carry the peak for May 9, while full 100 per cent bank power would be used November 6; thus 780 kw would be in the reserve at the time it is needed the least. Because of the prominent normal peaks it is reasonable to expect overloading primarily during these peaks which at the same time limit the duration of overloading; the latter would hardly last longer than the former; thus, the severity and duration of overloading are determined by the height of peaks and their duration in addition to other factors. The lower are the normal peaks, the milder and shorter will the overloading be.

An available reserve of power at the time of the *lower* peaks would be manifested by the fact that only a part—the major one though—of a whole set of transformers would be in operation, while the others are idle; it would result in the prolongation of their commercial—not technical—life; certainly this would be an advantage under normal circumstances.

It may be questioned whether or not this advantage would justify itself at the present time; one may argue, on the one hand, that what matters now is not a prolongation of the commercial life of a transformer but rather the harnessing of its power reserve for an actual operation *now;* one of the possible ways of doing this would be to allow for such ultraoverloading at the time of the *higher* peaks that would limit or keep the commercial life from increasing, by affecting the technical life of a transformer involved; on the other hand, at the time when replacements might become difficult, a prolongation of the commercial life would be a valuable asset.

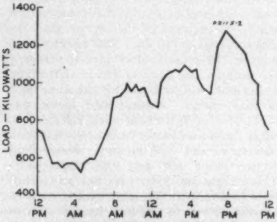


Figure 2. Residential and moderate industrial load curve

September 11, 1936
Average load 891 kw
Maximum load 1,275 kw
Ratio: average to maximum 0.70
Ratio: rms current to maximum 0.72

It goes without saying much that no finality is claimed as to specific values, examples, conclusions, and references given in the foregoing; they are offered to indicate general characteristics and trends and likewise call attention to a specific situation which is still fluid and not perfectly clear but already foreshadows the possibility of crystallization.

PART II

The implications of the discussed paper extend beyond the field of operation to that of design. Referring to the schedule of temperatures and Table VII, the discusser, for instance, reached the conclusion that standard design values of current density could be increased offhand by 18–27 per cent for transformers with 24-hour and 8-hour full industrial loads respectively. With standard design values of current density changed to higher values, cross-section areas of winding conductors will result in becoming smaller in inverse proportion, which means a considerable saving in copper; there will be more kilovolt-amperes per pound of copper or less copper per kilovolt-amperes.

The increased values of current densities thus revised are in agreement with the formula offered in my paper, "A Method of Successive Approximations in Electrical Design, Part II, Transformers," read before the Oklahoma Academy of Science at the annual meeting December 1937:

$$\Delta = \frac{622}{\alpha+0.035}\sqrt{\eta\frac{K_e}{K_s}} \qquad (1)$$

where

Δ stands for current density.
α stands for load factor.
η stands for core loss per pound.
K_e stands for price of winding conductors per pound.
K_s stands for price of steel laminations per pound.

As a companion formula the following one from the same paper is referred to:

$$A_s = C(\alpha+0.035)\sqrt{\frac{\text{(KVA)}}{4f}\,\frac{\Delta}{\eta B}}\times 10^3 \qquad (2)$$

where

A_s stands for net steel cross-section area of a core.
C stands for output constant.
(KVA) stands for output in kilovolt-amperes.
f stands for frequency.
B stands for flux density in the core.

REFERENCES

1. ELECTRIC-POWER EQUIPMENT (second edition), J. G. Tarboux. McGraw-Hill Book Company, Inc., 1932, pages 33–4.

2. EFFECT OF LOAD FACTOR ON OPERATION OF POWER TRANSFORMERS BY TEMPERATURE, V. M. Montsinger. AIEE TRANSACTIONS, volume 59, 1940, November section, page 636.

W. C. Smith (General Electric Company, San Francisco, Calif.): The problem of permissible overloads on transformers, as viewed by operators and maintenance men, has too often been beclouded by two misconceptions:

1. That the 105-degree-centigrade limit for the hot spot is a magical figure above which the transformer will burn up immediately and below which one can operate indefinitely.

2. That if by chance a transformer has been subjected to a high overload with no failure resulting, this is a proof that such overloads can be applied more or less indefinitely without damage to that unit.

We now know that a time-temperature relation exists as regards the deterioration of class-*A* insulation, its life being halved for every increase of from five to ten degrees centigrade operating temperature. At the point of "zero life" the insulation has lost its "water of constitution," still retaining however adequate dielectric strength, but is so brittle that a slight mechanical strain will crack it and lead to electrical breakdown. Many operators have experienced the fact that it is dangerous to move to a new location their old transformers or regulators which have withstood high operating temperatures for many years.

The hot-spot temperature is the weak link in the chain. Every reduction of a degree in temperature, whether secured by various methods of artificial cooling or by a lowering of the effective ambient insures longer life. Wartime demands for increased power, together with the scarcity of materials, are now prompting a careful review of all factors which can be utilized to allow higher transformer loadings with minimum consequent operating temperatures. To the writer it appears that two important possibilities have been quite neglected in any papers or discussions on this question, reference being made to the provision of sunshades together with the use of nonmetallic paint.

Ambient temperatures normally discussed are government shade readings, and it is important to realize that the effective ambient for transformers in the sunshine may easily be 5 to 15 degrees centigrade hotter. I recall measuring the oil temperature, at Redding, California, of an idle 25-kva lighting transformer out in the yard, during the middle of a day when the shade temperature was 114-degrees Fahrenheit (45.5 degrees centigrade). The oil was 136 degrees Fahrenheit (57.8 degrees centigrade). Here was a difference of 12.3 degrees centigrade due to absorption of the sun's rays. A suitable sunshade, presumably a semicircular louvered wood structure on the south side of a transformer bank, could therefore be counted upon to insure several degrees (centigrade) lower operating temperature during the hottest part of the day.

The use of cable leads to the transformer greatly facilitates the placing of the sunshade. In the case of vertical drop leads, it will often be necessary to rearrange them to a 45-degree angle to place the shade up over the transformer, insuring protection from the noonday sun.

In the western states, at least, the use of aluminum paint for outdoor transformers is very widespread. A definite reduction in temperature rise should result from merely painting the north side of the tanks a dull black. A still further reduction will result if a sunshade is provided. Then it is very important to eliminate all aluminum-painted surfaces, and for this dull black is also the best finish. A further advantage at this time of the dull black is to serve as a camouflage.

To summarize, it is believed that over and above the possibilities offered by special forms of artificial cooling, a marked improvement in the maximum operating temperatures of transformers may be secured as follows:

1. Paint the shady side of all outdoor transformers a dull black.

2. Provide a sunshade for protection during the middle of the day and then paint the entire tank with dull black. Provide a dull black paint on all indoor banks of transformers.

V. M. Montsinger: Mr. Oboukhoff's suggestion of using annual instead of daily load factor for use of the three per cent rule under certain load cycle conditions is somewhat outside the scope of the paper, although it does have a direct bearing on permissible overloads at a time when all the overload capacity that is available should be made use of. No work has been done to determine how much could be gained by using annual load factors instead of daily load factors; consequently it is not possible to give a figure at this time.

Mr. Smith brings up two very good points, namely, that of providing sunshades, and that of painting the side of the tank not exposed to the rays of the sun a dull black. There is no question but that a sunshade would effect a material reduction in the temperature of a transformer exposed to the sun's rays. The amount of temperature reduction will depend on the size and contour of the radiating surface. For a small distribution transformer with a plain tank, the reduction could easily be 10 to 15 degrees centigrade, whereas, for a large tank having attached radiators or cooling tubes, the gain would be less since a smaller percentage of the total developed surface would be exposed to the sun. My experience indicates that the reduction in temperature would be in the order of five to seven degrees centigrade for large power transformers. If one per cent load can be gained for each degree reduction in temperature, then the use of sunshades should enable users to gain from 5 to 15 per cent in capacity. In reality we should say that the use of sunshades should prevent the derating of 5 to 15 per cent, since the sun causes the temperature to exceed its normal temperature rise. However, there is for most outdoor installations sufficient breeze to counteract a large part of the increase in temperature caused by the sun's rays.

In connection with Mr. Smith's suggestion of painting the north side of a tank a dull black, the effect of this also will vary, depending on the size and shape of the cooling surface. While it is not possible to give any calculated values of reduced temperatures, some idea of the benefits can be gained by the following statements:

1. A plain tank, with a metallic finish, will give approximately 30 per cent higher temperature rise than one having a nonmetallic finish.

2. As the surface becomes more and more convoluted, the effect of the metallic finish on the temperature rise of transformers in the shade decreases, it being from five to seven per cent for the most complicated surfaces. In the sunshine there is essentially no difference whether a metallic or nonmetallic paint is used, although, as pointed out above, the temperature rise will be greater for either kind of finish. This question is discussed in the AIEE paper entitled "Effect of Color of Tank on the Temperature of Self-Cooled Transformers Under Service Conditions," by V. M. Montsinger and L. Wetherill.

REFERENCE

1. EFFECT OF COLOR OF TANK ON THE TEMPERATURE OF SELF-COOLED TRANSFORMERS UNDER SERVICE CONDITIONS, **V. M. Montsinger, L. Wetherill.** AIEE TRANSACTIONS, volume 49, 1930, page 41.

A New Single-Phase-to-Ground Fault-Detecting Relay

Discussion and author's closure of paper 42-125 by W. K. Sonnemann, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 677–80.

J. H. Neher (Philadelphia Electric Company, Philadelphia, Pa.): While Mr. Sonnemann is to be congratulated on the development of this relay which distinguishes between single-phase- and two-phase-to-ground faults, he is to be especially commended for pointing out before the Institute the possibility of using this or other equivalent devices in conjunction with a simple instantaneous ground relay to obtain high-speed differential bus protection on systems grounded through a relatively high impedance.

Although systems of bus differential protection employing special nonsaturating current transformers have been recently introduced, these systems are difficult to apply where the single-phase-to-ground fault current is appreciably limited. The arrangement proposed by Mr. Sonnemann not only facilitates the application of these systems in this case but also offers a means of obtaining high-speed bus protection against single-phase-to-ground faults with conventional current transformers.

While the possibility of obtaining a solid three-phase bus fault by the inadvertant closing of a switch without removing safety grounds is always present, nevertheless the use of a high-speed single-phase-to-ground fault system will initiate tripping on the majority of faults before the system is rendered inoperative by the spread of the fault to another phase.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The author of this paper correctly points out the severity of the problem encountered when applying bus differential protection to installations where the ground current is small because of high neutral impedance.

Other methods also have been applied to obtain the desired operation under these limiting conditions. In two of these other solutions which have been used, one uses a relay to recognize the difference between the two-phase-to-ground and single-phase-to-ground faults, and the other uses a relay system which makes selective control unnecessary.

The first of these uses another and simpler means of distinguishing a fault which involves only one phase from one which involves two phases. This method likewise depends upon the circuit characteristics encountered when a neutral impedance or relatively high value is used. If we look at figure 6c in this paper we note that the zero-sequence voltage is approximately 120 volts peak for a single-phase-to-ground fault and approximately 60 volts peak for the two-phase-to-ground fault. Associated with these voltage conditions will be approximately a 2/1 ratio in the neutral current. Hence a simple current relay may be used which will operate for single-phase-to-ground faults and not operate for two-phase-to-ground faults. Applications of this type of relay have been used to supervise differential ground relays since 1939. While it can be shown theoretically that such a relay may fail to operate due to impedance in the fault, in practice the value of the neutral impedance is so high that it is difficult to consider this as a very real limitation, when used for bus protection.

The other method of solving the problem in the case of these high neutral impedance systems is to set up each phase separately in a differential system, using a total of only three relays, each one being sensitive enough to operate for ground faults as well as for phase faults. With transformers such as the air-gap core type or the linear coupler there may be a small differential current for through phase-to-phase-to-ground faults due only to manufacturing tolerances. Operation under this condition can be easily prevented by using only a small amount of "through" current restraint and one can still easily obtain adequate sensitivity to provide positive operation for internal single-phase-to-ground faults.

For this type of application the restraining feature just described is a generally applicable solution, regardless of system impedance variations, and therefore more desirable. It is also a method following established practice and with proved reliability and simplicity.

R. C. Ericson (Northern Indiana Public Service Company, Hammond, Ind.): This paper has been particularly interesting, since it has presented us with another tool for combating false residual currents.

It is felt, however, that the relay has a wider application than the author has inferred in the paper.

The requirement that the ratio of the zero-phase-sequence impedance to negative-phase-sequence be of the order of two to one seems a little too conservative. A ratio of 1.5 to 1 should probably be sufficient in most cases.

Another impression obtained from the paper is that the relay is not applicable to solidly grounded systems. It would appear that the question of applicability rests wholly on the apparent ratios of the zero- and negative-phase-sequence impedances, as seen by the relay at the station in question.

The relay should also find some use in "direction comparison" pilot schemes (for instance, carrier-pilot relaying). This is particularly true of the schemes using the three-unit directional relay containing a voltage restraint unit and a polyphase and a residual power directional unit acting on one shaft. Schemes of this type very frequently employ a conventional residual current relay to remove the potential from the voltage restraint and polyphase power directional unit on the appearance of appreciable residual current. Experience has shown that under certain conditions this removal of potential is undesirable during two phase-to-ground faults.

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): No relay session seems to be complete without a paper dealing with improvements in bus protection. For the past two years all efforts have been to improve current transformers. Now that saturation difficulties have been overcome by linear couplers and air-gap current transformers, the design engineers have again turned their attention to designing relays which will overcome the transformer errors still existing, in order to get still more sensitive bus protection.

The relay described by the author seems more complicated, however, than is necessary to do the job. A simple three-phase high-speed induction-cylinder undervoltage relay has been suggested, using two delta voltages and measuring the area of the station-bus delta-voltage triangle. The torque developed by this relay is proportional to the product of the two voltages and the sine of the angle between them. It can be shown that the area of the triangle during a single-phase-to-ground fault is essentially maintained during single-line-to-ground fault except with very low values of neutral impedance, while on phase faults or two-phase-to-ground fault the area will be drastically reduced, thus always assuring positive operation.

In the relay described in the paper the distinction between pickup on single-phase-to-ground faults and two-phase-to-ground faults disappears below a certain value of neutral impedance (R_n) which is one-third ohm in the example given. Below this value the relay torque reverses. Using the undervoltage relay scheme proper distinction is maintained even down to R_n equals zero. As a result it would appear that the three-phase undervoltage relay scheme using a simple high-speed induction-cylinder element is the simplest and most sensitive relay for this application.

W. K. Sonnemann: Mr. Neher's discussion goes one step beyond the points brought out in the paper and discusses the fact that a use of the new relay makes it possible to simplify the differential relays. The points made by Mr. Neher are well taken.

The author shows a ratio of 2/1 for a safety factor, but Mr. Ericson feels that a ratio of 1.5/1 would be sufficient in most cases. However, since the paper was presented, it has occurred to the author that, rather than apply the relay to the limits of some arbitrarily chosen safety factor, it would be better to determine the suitability of application by means of a few very simple calculations in the following manner: Consider an application in connection with bus differential protection. The relay should operate for a single-phase-to-ground fault on the bus. It should not operate for a two-phase-to-ground fault just outside the bus. Furthermore, it is possible that in some cases it should not operate for a two-phase-to-ground fault at the next outlying substation, depending upon the amount of current involved for the distant fault. It would therefore be in order to calculate the zero-sequence and negative-sequence voltages on the bus for the internal single-phase-to-ground fault and for the remote two-phase-to-ground fault as far removed from the bus under consideration as the application may require. Both of these calculations should include a reasonable value for possible fault impedance. Having obtained the voltage values for the two conditions, it is a simple matter to determine from the operating characteristic curve of the relay whether or not it is suitable. It may be that in some cases the curve shown by Figure 3 of the paper would not be suitable, but that some other curve for which the relay is capable of being adjusted would serve the purpose.

Mr. Kennedy mentions another method of discriminating between single-phase-to-ground and two-phase-to-ground faults by means of the relative magnitude of the zero-sequence current. This method is theoretically correct, but it is more than theoretically possible that such a relay may fail to operate because of impedance in the fault. The author knows of one case at least where the impedance of the fault was large enough to prevent the operation of such a relay.

The use of through current restraint has been and is quite practical and widely used. However, on multicircuit busses the complexity of using restraint certainly increases rapidly with the number of circuits. In view of this fact, the author feels that if the restraining coils of the differential relay could be eliminated by the installation of one additional relay as described in this paper, that the over-all installation would be simplified.

A. R. van C. Warrington mentions another method of using a relay responsive to the area of the station-bus delta-voltage triangle. This method has been considered by the author and his associates and considered to be more complex and difficult to predict than the method used in this paper. The area of the voltage triangle depends upon the negative- and positive-sequence voltages and the angle between them. The negative- and zero-sequence voltages at the bus are expressed as impedance drops from the source to the bus. If the positive- and negative-sequence voltages are used as Mr. Warrington would do, an additional factor is introduced: namely, the generated voltage. This enters in the positive-sequence network, and the determination of the relay characteristics is not as straightforward. It is difficult to understand Mr. Warrington's statement that the conventional relay and method he proposes appear to be simpler than the relay the author proposes. Actually, we feel that the reverse is true since no moving contact could be more simply actuated than by a spring supported armature without bearings. Furthermore, the sequence networks in the new relay are straightforward and of proved design.

In the oral discussion at the convention, the author pointed out that the relays could be utilized on systems where the grounding impedance is even less than the one-third ohm Mr. Warrington indicates in his last paragraph by the simple expedient of reversing the operating and restraining coils. However, we feel that this point is actually only of academic interest in that the need for the relay disappears on solidly grounded systems.

Protection of Pilot-Wire Circuits

Discussion and authors' closure of paper 42-136 by E. L. Harder and M. A. Bostwick, presented at the AIEE summer convention, Chicago, Ill., June 22–26, and published in AIEE TRANSACTIONS, 1942, September section, pages 645–52.

R. C. Ericson (Northern Indiana Public Service Company, Hammond, Ind.): The authors are deserving of some praise for their able presentation of the subject of pilot-wire protection. The material presented should prove of value not only to those engineers involved in designing pilot relay circuits, but also those working on communication, remote metering, or supervisory circuits.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The use of pilot-wire protection has made possible the successful design and operation of power-supply systems with significant savings in major equipment. The use of this valuable tool has not increased as rapidly as might be expected, and possibly the greatest retarding influence has been the feeling that sufficiently reliable pilot-wire circuits cannot be secured. Many years' experience with pilot-wire protection[1,2] (principally with leased circuit) have shown that an excellent degree of over-all performance of pilot-wire systems may be secured without requiring anything like 100 per cent continuity of the circuit, providing adequate continuous circuit supervision is included. However, the pilot-wire circuit must be protected from even momentary failures which by their very nature coincide with power-system trouble which may be on the circuit protected. There is still considerable confusion regarding the influence of power-system faults on pilot-wire circuits, and therefore the theoretical analysis of the authors is most timely.

What is believed to be the first installation in the United States of neutralizing transformers to protect pilot-wire circuits against rise of station ground potential was made in 1934 on the system of the former Tennessee Electric Power Company, and it was checked thoroughly by staged short-circuit tests in February 1935.[3] Many installations have since been made of three-winding neutralizing transformers to protect against rise of station ground potential, and for that purpose, these appear to have some important advantages over the two-winding capacitor excited transformers now offered.

1. The three-winding unit provides genuine "neutralization" (see Figure 9 of paper) of the voltage rise so that terminal equipment and pilot-wire cable are unstressed, whereas the two-winding device acts as a voltage divider, resulting (Figure 9c) in impressing some 1,000 volts on the pilot-wire cable insulation for a total rise of only 2,000 volts. Protective devices connected between the conductors of communication-type cable and the sheath will operate to short-circuit and ground the pilot wires at such voltages. It is doubtful if this difficulty can be overcome by the addition of capacitors without introducing other complications.

2. If connected as shown in Figure 6 of the paper, the magnetizing capacitors become equivalent to 0.5 microfarad connected across the circuit at each terminal. With even reasonably short pilot-wire circuits, the cable capacitance will be of the same order, so that the total will be beyond the maximum recommended for use with certain two-wire differential-type pilot-wire schemes.

3. It is possible that the two-winding transformers and capacitors are subject to transient effects such as were encountered with an early design of transformer which was built to neutralize induced voltages.[3] Actual fault tests on this unit showed an initial unneutralized voltage for the first cycle which was nearly as great as the total unneutralized voltage. Thorough short-circuit tests should provide a definite answer, and, if these had been made, the results should be of interest.

The authors' statement that no specific protective measures are required in many cases is true but should be used with considerable caution. For example, it is sometimes thought that the connecting of the power-station ground networks and the communication-system protective grounds to the same water system obviates the need of protection. Short-circuit tests have disclosed as much as a 2,000-volt drop between the water service connection at a power station and remote ground caused largely by the impedance of the meter and service connection.

The authors mentioned an effective primary impedance of a three-winding neutralizing transformer as some 11,000 ohms compared with 100,000 ohms for the two-winding transformer. Several installations have been made of three-winding neutralizing transformers with a primary impedance exceeding 100,000 ohms at 4,000 volts, 60 cycles.

Is circuit neutralization achieved by the connections of the two-winding transformer shown in Figure 6, or must the insulating transformer of Figure 8 be used? If Figure 6 is adequate, then the need for the insulating transformer of Figure 8 is questionable.

REFERENCES

1. THE USE OF COMMUNICATION FACILITIES IN TRANSMISSION-LINE RELAYING, **J. H. Neher.** AIEE TRANSACTIONS, volume 52, 1933, June section, pages 595–602.

2. PILOT-WIRE RELAY PROTECTION, **E. E. George, W. R. Brownlee.** AIEE TRANSACTIONS, volume 54, 1935, November section, pages 1262–9.

3. NEUTRALIZING TRANSFORMERS. Edison Electric Institute publication *H*-12.

K. H. Perkins (Bell Telephone Laboratories, Inc., New York, N. Y.): Neutralizing transformers to prevent extraneous low-frequency longitudinal voltages from appearing on communication or signaling circuits have been in use for a long time and have formed the subject of a number of papers before this Institute. The device which the authors refer to as a two-winding neutralizing transformer might more properly be called a new name for an old tool: namely, the longitudinal choke, under which humble label it has been known for a long time. To regard this choke as a neutralizing transformer may possess advantages when comparing this device with the conventional type of transformer. The most interesting and valuable parts of the paper are the sections devoted to methods for computing the distribution of voltage along the protected conductors.

It should be pointed out that pilot wires are frequently in a cable containing other communication or signaling circuits. The type of service on these accompanying circuits may be such that momentary interruptions, such as might be occasioned by the operation of lightning arresters or protectors during fault conditions on the power system, are not serious. However, it is necessary under these conditions to make certain that the insulation between the pilot-wire pairs and the other conductors is not stressed dangerously, because breakdown of this insulation might result in failure of the pilot-wire circuits. It may therefore be necessary in a situation of this kind to provide all of the circuits with the same kind of protection as the pilot-wire circuit. Many different situations, in which this question of potentials between conductors is involved, may be encountered in practice, and each situation must be worked out individually. It is intended here merely to point out that in order to protect the pilot-wire circuits themselves, attention may have to be given to other circuits in the cable besides the pilot-wire conductors.

In the section dealing with the three-winding transformer, it is implied that the potential of remote earth is reached a few hundred feet from the station ground. While it is true that in most cases the largest part of the potential between the station ground and remote ground takes place within a few hundred feet, frequent cases have been encountered in which it was advisable to carry the primary connection out to as much as several thousand feet. In commercial telephone circuits, the remote ground must be located far enough from the station ground so that the remanent voltage will not cause operation of protector blocks. Local conditions determine the potential distribution, and it is advisable to fix the position of the primary ground by test.

In the discussion of the two-winding transformer, it is stated that the latter is more effective than the three-winding transformer, because neutralization is aided by the exciting current in the case of the two-winding transformer and hindered by it in the case of the three-winding transformer. This comparison has no very great significance, because many other factors enter into the relative merits of the two types of transformer, and by proper design either can be made as effective as desired. By admitting the exciting current to the signal circuit itself, rather severe balance requirements are placed on the longitudinal circuit and upon the grounding capacitors. Otherwise, the flow of longitudinal current may give rise to metallic voltages which might interfere with the relay operation. Also, since capacitors are used to provide a path to ground for the exciting current, the device is restricted to circuits using d-c or low-frequency signals.

In the discussion of potential distribution resulting from combined station-ground potential rise and magnetic induction, it is stated rather positively that the voltages produced by these two effects are 90 degrees out of phase. It should be recognized that this is only an approximation. Actually, the phase angle between these voltages depends upon the earth resistivity and the separation between the power circuit and the signaling circuit. For example, at a separation of 200 feet and an earth resistivity of 100 meter-ohms, the phase angle is about 65 degrees.

L. L. Fountain (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper gives information for which there has been a definite need for some time. This need has been intensified by the rapidly increasing use of the pilot-wire relay. The information given in the paper should serve to prevent the recurrence of difficulties which have been encountered heretofore caused by a lack of understanding of the pilot-wire requirements with respect to induced voltages and station ground potentials. Investigation has shown that, in practically every case, proper pilot-wire protection results in satisfactory relay operation.

In the closing paragraph of the paper, reference is made to leased pilot wires. While there is probably no place in the paper for further amplification of this subject, there is a definite need for more information on the characteristics of the available pilot-wire circuits that can be leased and the conditions under which they may be made available.

The authors make mention of the fact that "the seal-off voltage of the arresters must be above the maximum voltage that can be impressed on them by induction in the pilot wires, and so forth." This point deserves special emphasis. Since leased circuits have a fixed insulation level and utilize carbon protector blocks (arresters) to suitably protect the cable insulation, little variation is possible in these facilities. It is, therefore, essential to determine that the longitudinal induced voltage to which the pilot-wire pairs may be subjected is below the breakdown value of the carbon blocks in order to determine whether or not the circuit is suitable for use with pilot-wire relays. In the past, a few isolated cases of trouble have occurred because of the failure to observe this precaution; however, it is believed that continued co-operation between the telephone company, utilities, and electrical manufacturers will result in a better understanding of the conditions and prevent the recurrence of this phenomenon.

The method of presenting this difficult subject has been admirably handled, and the authors should be commended for their effort.

E. L. Harder and **M. A. Bostwick:** Before commenting on any of the discussion, the authors wish to express their appreciation of the interest shown in this paper. It was prepared to review the problem of protecting pilot-wire circuits, calling attention to methods that can be used to determine the distribution of voltage stresses that are imposed on those circuits, and to present a new type of neutralizing transformer that is of value for protection of these circuits. The discussion of the relative merits of different types of neutralizing transformers was merely incidental, since the primary object of the paper was to call attention to the problem of protecting pilot-wire circuits. Once that problem is appreciated, it is assumed that inherent difficulties, that may have been previously overlooked, will be avoided.

In reply to Mr. Brownlee's discussion we agree that each type of neutralizing transformer, three-winding or two-winding, has certain advantages over the other. As pointed out in the paper, however, it will be noted that the two-winding neutralizing transformer is particularly well adapted to the protection of pilot-wire circuits, which are subject to both induction and differences in ground potential. The "voltage-divider action," cited under the first paragraph of Mr. Brownlee's discussion, and illustrated in Figure 9c of the paper, is only incidental and was illustrated in the paper to guide users so as to avoid the condition. Figure 9b illustrates the correct use of the two-winding neutralizing transformer. The required capacity between the pilot-wire circuit and ground may be provided either in the form of the natural capacity of the circuit or by adding lumped capacity. In either case the required capacity is well within the operating range of a-c pilot-wire relays that are now on the market.

Figure 6 of the paper was used to illustrate schematically the proposed relay circuit. Actually this circuit is made up as shown in Figure 4. Since the 0.5-microfarad capacitors are connected between mid taps of the insulating transformer and ground, it will be noted that this capacity is not introduced into the relay circuit. Consequently, the operating limits of the associated pilot-wire relay are not affected by this connection.

The circuit tests that are suggested in Mr. Brownlee's third paragraph have been in effect performed by several years experience in operating pilot-wire circuits identical with those described in this paper. A consideration of the circuit will reveal that the transient effects questioned are only of interest when they are introduced into the pilot-wire loop. Normal action of the two-winding neutralizing transformer is to introduce a high impedance into the series circuit that is made up by paralleling both pilot wires and connecting them to ground through the terminating capacitors. Since this circuit must be symmetrical—that is, equal impedances are introduced into both pilot wires—, it will be noted that any transient disturbance set up by transmission-line faults will not introduce extraneous voltages in the pilot-loop circuit. Consequently, the effect which Mr. Brownlee has feared, is not encountered. This is borne out by actual operating experience.

Adequate neutralization of all voltages, within the operating range of the two-winding neutralizing transformer is obtained by use of the circuit which is schematically illustrated in Figure 6. The insulating transformer shown in Figure 8 is not required, as the residual voltage on the relay side of the neutralizing transformer is in the order of 1.3 per cent of the applied voltage. However, the circuit illustrated requires the ratio change and mid tap connection that is provided by the insulating transformer shown in the diagram. Since this is standard apparatus, the insulating transformer is used rather than supplying an additional device.

We agree with Mr. Perkins' statement that the two-winding neutralizing transformer has been previously used as a "longitudinal choke." While these two pieces of apparatus are of similar construction, the actual design and use differ quite radically. The device which we have described is of particular value for protection of pilot-wire circuits, while the "longitudinal choke coil" was designed especially for communication circuits.

We appreciate having Mr. Perkins call our attention to the fact that voltages between wires in a given cable must be considered. This is most important, and the problem must be studied to avoid the possibility of transferring voltage stress from one circuit to another within a given cable. As pointed out in a paragraph that was added to the paper for publication in AIEE TRANSACTIONS, "generally, all wires entering a cable at a given point should be protected in a similar manner." We also agree with Mr. Perkins' statement that it is advisable to fix the position of the primary ground by test rather than relying upon the assumption that average earth potential will be found a few hundred feet removed from station ground. The fact that the station ground potential may be less than 90 degrees out of phase with the longitudinally induced voltage was neglected, as a quadrature addition of these voltages is sufficiently accurate for the purpose of this discussion.

As both Mr. Perkins and Mr. Brownlee have pointed out, the design details of the different types of neutralizing transformers discussed in the paper are not fixed. The impedance of either design may be changed to suit the particular application. For example, the shunting action of the exciting capacitors, used with the two-winding neutralizing transformer can be reduced by grounding these capacitors through a mid tapped reactor. The impedance of this reactor can be chosen so as to avoid shunting high-frequency currents out of the pilot circuit and yet to shunt low-frequency currents, such as third harmonic currents, if this is considered desirable.

Current- and Potential-Transformer Standardization

Discussion and author's closure of paper 42-116 by the AIEE current transformer subcommittee of the committee on protective devices presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 698–706.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The interest of the relay engineer in these standards will be largely concerned with the sections covering the specification of accuracy classes for relaying service and the application data to be made available.

In connection with the method of specifying overcurrent performance for example 10*L*400 or 2.5*H*200 it should be remembered that this subject has been under active consideration for many years. During this time many suggestions have been made as to how this specification could be given in such a way as to convey the maximum information. From these several suggestions either or both of two points always seemed to be significant. One was the realization that current-transformer errors increase rapidly after reaching the ten per cent value. The other was the desire to know the performance at 20 times current. Your working committee has succeeded in combining these two ideas in a specification which tells the voltage (or burden) permissible without exceeding a ten per cent error at 20 times secondary current. For those who prefer a burden figure to a voltage figure, it may be in order to emphasize that the last number in the accuracy specification divided by 100 gives the permissible burden in ohms.

Incidentally, I believe the committee should be congratulated for finally establishing ohms as the basis of expressing burden in preference to the volt-ampere expression which was almost meaningless for relay work where operating values other than five amperes are so frequent.

The section on application data recognizes the usefulness of the excitation characteristics as a means whereby the actual error under specific conditions may be accurately determined. It would appear that the best method of presenting and using these data has not been established. Three methods of calculation are indicated in Appendix 2, and figure 5 shows a graphical solution. The text indicates that the published information should include excitation current versus voltage and "sufficient data to determine" inphase and out-of-phase current, or phase angle. Since all of the indicated methods use either φ, directly or the components of I_e which are obtained by knowing φ_e, it would seem to be in order for the committee to definitely recommend that the angle of the exciting current together with the magnitude constitute the basic data.

E. C. Eberhardt (Commonwealth and Southern Corporation, Jackson, Mich.): The paper should be very helpful in giving a better understanding of section 4 of the revised American Standard for Transformer, *C*-57 when it is published. The latter part of the paper obviously reproduces certain parts of section 4 as now revised, although it is not clearly identified as such in the paper. Including this material for reference permits a better understanding of the discussion in the paper and also makes this information generally available in advance of the publication of *C*-57 which should be helpful to those interested in this equipment.

"STANDARD ACCURACY CLASSES FOR CURRENT TRANSFORMERS FOR RELAYING SERVICE"

The paper clearly states the purpose of these accuracy classes for relaying service; namely, they provide a bench mark for comparison and for specifying the overcurrent accuracy requirements and establish in a general way the limits within which a given transformer does not exceed specified errors. That is, the method of classifying the accuracy is standardized, but no recommended or preferred classifications for standard production units have been indicated either by this paper or in that part of section 4 reproduced in the paper. The considerable number of accuracy classifications in the standard are required in order to provide the necessary "bench marks" for standard production units and for special requirements. Another important function of standardization might readily be accomplished by establishing specific accuracy classes as standard or recommended for certain general applications in which current transformers are widely used. Thus wound-type current transformers suitable for applications such as the general requirements of electric-power stations, both for metering and general relaying, might for example have a recommended accuracy classification of 3/10 *B*1/10, 3/10 *B*5/10, 3/10 *B*2; 10*H*100, 2.5*H*100. Such emphasis on specific classifications should serve as an incentive toward standardization in specifying accuracy requirements and for uniformity in standard catalogued transformers.

Under 4.032 (*a*) (2) of the proposed revison of *C*-57 it is specified that a current transformer shall be given an accuracy rating in accordance with the maximum secondary terminal voltage at which the specified error will not be exceeded under the conditions specified for rating purposes. Apparently the intent of this requirement is to insure that the accuracy rating shall be a true bench mark of the unit's ability. That is, where a standard available design having an accuracy rating of 10*H*100 is furnished to meet a specified accuracy class of 10*H*50, it shall be given its rating of 10*H*100 and not the lower rating, which could happen under the maximum error-limit requirement only. It is commendable that this provision has definitely been included in the standard so as to avoid possible confusion and misunderstandings.

MECHANICAL LIMIT FOR CURRENT TRANSFORMERS

The paper provides an interesting discussion on the revised method of expressing the mechanical limit of current transformers and also indicates test conditions by means of which the completely offset requirement may be demonstrated. Since the stated tests are equivalent to the completely offset condition being maintained for six cycles and also definitely establish a crest value of not less than 2.82 times the rated mechanical limit expressed in symmetrical rms amperes, they represent the extreme in completely offset requirements and hence should demonstrate the unit's ability without question. However, the standard does not specify this demonstration, and the paper merely reads "may be demonstrated." Unless the above demonstration or some other common basis is definitely recognized, so that the mechanical limit rating indicates a definite ability, the present confusion as to what is meant by the claimed ability will continue to exist.

The proposed standard specifies that the symmetrical rms mechanical limit rating shall not exceed one-half the thermal limit of six cycles. Such a requirement would be necessary to provide adequate thermal ability for a demonstration test such as that indicated in the paper.

110-KV INSULATION LEVEL

Tables IX and X of the revised standard make the new 110-kv basic insulation level applicable to instrument transformers in insulation class 15*H*. This should permit appreciable savings where instrument transformers are desired having an impulse rating comparable to the power level in the 15-kv insulation class. Formerly, it was necessary to go the 150-kv impulse-rated units or purchase special nonstandard units. It is interesting to note a definite trend in catalogued equipment toward adoption of the 110-kv impulse rating instead of the 95-kv level for 15-kv outdoor units of certain types.

F. E. Davis (The Commonwealth and Southern Corporation, Jackson, Mich.): It seems to me that metermen, in general, should be most interested in and pleased with the fact that we now have a standard method for rating instrument transformers that is of greater significance from a metering standpoint than the method formerly used. Formerly, only arbitrary limits for ratio error and phase angle of the transformers were used for rating purposes. Now, arbitrary limits for the effect of the transformer ratio error and phase angle on the accuracy of measurement are used, and this is the factor with which metermen are primarily concerned. Thus, the ratings given transformers on the new basis define closely the limits of the extent to which the transformer characteristics affect metering accuracy.

I might also call attention to the fact that, in rating current transformers for relaying service by the new method, it is necessary to rate them both under the 2.5 per cent and 10 per cent specification (such as 2.5 *H* 50 and 10 *H* 200). This is for the reason that a rating under one condition is not indicative of the performance under the other in all cases. (For instance, a 2.5 *H* 200 rated transformer would also meet the 10 *H* 200 requirement, but the reverse is not necessarily true. Both, therefore, should be specified.)

S. D. Moreton (General Electric Company, Philadelphia, Pa.): The proposed American Standard for Current Transformers requires that the manufacturers now give the excitation characteristics of the transformer. This makes it possible for the customer to calculate the transformer errors for his particular application resulting in better system co-ordination than was possible heretofore when interpolation between the standard ratio curves was the only method available aside from actual test.

The authors of this paper have mentioned several methods for the calculation of ratio correction factor and phase-angle errors of current transformers. I wish to stress that any mathematical method will be long and tedious unless one is willing to accept the inaccuracies of adding the magnitude of the exciting ampere-turns arithmetically to the secondary ampere-turns to obtain the primary ampere-turns. The chart method as mentioned in appendix 2 under "Method 2" as shown in figure 5 of the paper is in reality a vector method for the addition of the exciting ampere-turns and the secondary ampere-turns to obtain the ratio correction factor and phase-angle (β) directly. This vector method is the ideal answer for rapid and accurate calculations: by the use of vector charts of this nature calculations may be made in one-fifth of the time required for mathematical analysis, with an accuracy that is far superior to slide-rule work.

An example would perhaps show more fully the labor that is avoided by use of the chart method. Let us assume that the value of the exciting current has been determined from the burden requirements:

as $\bar{I}e = 5\sqrt{60} = 2.5 - j4.33$
where $\bar{I}s = 25\sqrt{30} = 21.65 - j12.5$

The following steps must be performed if method 1 in Appendix 2 is used:

$$I_p = \sqrt{(21.65+2.5)^2+(12.5+4.33)^2}$$
$$= \sqrt{(24.15)^2+(16.83)^2} = \sqrt{583+284}$$
$$= \sqrt{867} = 29.4 \quad \text{amperes}$$

$$\text{RCF} = 29.4/25 = 1.175$$
$$\beta = (\tan^{-1} 16.83/24.15) - 30 \text{ degrees}$$
$$= (\tan^{-1} 0.698) - 30 \text{ degrees}$$
$$= 34.9 \text{ degrees} - 30 \text{ degrees} = 4.9 \text{ degrees}$$

The mathematical addition of two vectors requires that squares and square roots be taken as well as the use of trigonometric functions in relatively complicated formulas as indicated by the authors' methods; these calculations are especially laborsome if numerous points are required as for curve drawing.

The steps involved in performing the calculations by the use of vector charts are

Per unit exciting current $= \bar{I}e/\bar{I}s$
$= 5\sqrt{60}/25\sqrt{30} = 0.2\sqrt{30}$

Using the chart given in figure 5 of the paper the ratio correction factor and the phase-angle error (β) are determined directly as:

RCF = 1.177
$\beta = 4.85$ degrees

By the use of the vector-chart method 80 per cent of the time can be saved, greater accuracy obtained, and the calculations readily made. Thus a simple and rapid means for calculation, that does not lose any accuracy through the process used to obtain the result, is the one that should become the predominant method—the vector-chart method.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. This report covering the recent work of the current transformer subcommittee gives some insight into the vast amount of work involved in obtaining a set of standards which meet present-day requirements without any material conflict with past practices. The discussions and explanations on the revisions and new material of these standards gives the user of this apparatus a better picture of the characteristics and limitations of instrument transformers.

2. The new sections covering current-transformer accuracies and application data are of particular interest to the application engineer. The use of the saturation or excitation curve made from data measured on the current-transformer secondary winding with the primary winding open-circuited for determining performance is emphasized. It might have been well to point out, that when a satisfactory method of determining and expressing the secondary leakage reactance of current transformers has been accepted, the standards could dispense with the use of ratio and phase-angle errors as a means of accuracy classification of overcurrent performance. Then, excitation characteristics could be used exclusively.

3. Appendix 2 gives 3 methods of calculating ratio correction factor and phase angle from the magnetizing and loss characteristics. As stated, all three methods are simply different means of obtaining the solution of the vector diagram Figure 3. Method 1 is the fundamental solution and most readily followed. Method 2 has its principal interest in its accompanying chart, Figure 5. Amplification of the use and limitations of this chart would be of interest. Method 3 is another form of the expressions given in method 2. It would seem method 1 is so simple and fundamental that it would be the desired method, eliminating the inconvenience of requiring special charts or formulas to be always available. Likewise, it is a constant reminder that the ratio of the excitation current, I_e, to the load current, I_2, is all that is required for a large percentage of applications.

4. It is felt this report will stimulate the use of the new standards and further investigation of transformer performance.

J. E. Clem (chairman of committee): The members of the committee are gratified that the discussions have been so complimentary and so constructive.

Mr. Moreton expresses a strong preference for the chart method of determining the performance of current transformers in the overcurrent field, while Mr. Woods expresses a strong preference for method 1. It is hoped that those who use any of the methods given for calculating overcurrent performance will compare the various methods carefully and express an opinion so that the committee may recommend a preferred method for inclusion in *C*-57 as an addition to the test code.

Likewise Mr. Kennedy's suggestion that the exciting current data required be on the basis of magnitude and phase angle is also a matter on which an expression of preference is desirable.

Mr. Eberhardt suggests that specific accuracy classes be established as standard or recommended for certain applications. Such material does not belong in a standard of this nature. Such information might take the form of an industry report and, if a nearly unanimous opinion could be obtained, included as an appendix to the standard under the heading "Guides for Application."

Mr. Eberhardt notes that the standard does not require that the mechanical limit of current transformers be demonstrated. Actually the standard does not require that any of the characteristics be demonstrated, either in section 4 or in the other sections of *C*-57. The standard requires that the apparatus have certain characteristics, and whether or not tests are made to demonstrate that the apparatus fulfills the requirements is a matter of manufacturing practice and expression of customer's wishes.

Mr. Davis suggests that current transformers be given an overcurrent rating at the 2.5 per cent point as well as the ten per cent point. This is not practical, because the second point would not really be indicative of the performance of the transformer. Because of the fact that the voltage steps are multiples of 2, starting with 50, the voltage corresponding to 2.5 per cent would be between the standard voltage values, and any rating for the 2.5 per cent accuracy would therefore be misleading.

Study of Driven Rods and Counterpoise Wires in High-Resistance Soil on Consumers Power Company 140-Kv System

Discussion and authors' closure of paper 42-107 by J. G. Hemstreet, W. W. Lewis, and C. M. Foust, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 628–34.

J. R. Eaton (Naval Training School, Purdue University, Lafayette, Ind.): The studies described in this paper show that if lightning strikes a tower equipped with both buried counterpoise wires and deep-driven ground rods, the resulting current in the ground rods is much greater than the current in the counterpoise wires. From this behavior, it is logically concluded that the deep-driven rods provide a more effective ground than do the counterpoise wires.

In comparing the merits of the driven ground rod and the counterpoise wire in the light of the performance observed in Michigan, it may be well to call particular attention to one peculiarity in the geology of the region in which the studies were made. As implied by conclusion 2, the high-resistance sandy soil of this region is underlaid by a different kind of soil (presumably clay) which is of relatively low resistivity. Rods driven deep into the sandy layer provide a relatively high-resistance ground connection. When such rods are driven sufficiently deep that the underlying clay layer is penetrated, the resistance drops rapidly, indicating that by far the greater part of the conductance is through the clay soil. The superior performance of the deep-driven ground rods as observed in the Michigan tests is very likely principally because of the fact that the rods make contact with the highly conducting soil layers considerably beneath the surface.

It must not be concluded from these tests that deep-driven rods will in every case be superior to horizontal buried wires. Only in case the deep rods penetrate lower-resistance soil layers below the surface, can the rods with certainty be considered as superior to the buried counterpoise wires.

S. K. Waldorf (Pennsylvania Water and Power Company, Baltimore, Md.): Hemstreet, Lewis, and Foust have evaluated the relative merits of rods and counterpoise as collectors of lightning current. What must of necessity be of prime importance to transmission engineers is the relative effectiveness of these two types of auxiliary grounding in the prevention of flashovers. It will perhaps help answer this question if the authors in their tabulations of footing resistances and tower currents would indicate which were associated with relay operation and where evidence of flashover was found. In other words, do the data give any indication that rods or counterpoise are the more effective in the prevention of flashovers?

Hamilton Treadway (United States Department of Agriculture, St. Louis, Mo.): The authors have conducted a most interesting and enlightening study of the characteristics of grounding methods under actual field conditions. This paper is a most valuable contribution to the literature on this subject.

A point of interest on which the data do not shed light is the time-impedance characteristic of the ground electrodes during the lightning stroke. The methods of measurement of course did not lend themselves to obtaining such characteristics. However, the existence of a high initial impulse impedance has been a point of some controversy although established by theoretical analysis. It is unfortunate that the data presented do not enlighten us on this point. It is, of course, needless to point out that the existence of an initial impulse impedance in excess of the Megger values would result in some instances in potentials much greater than the flashover characteristics of the line. Of course, such voltages would exist only for a few tenths of a microsecond. The opinions of the authors on this point are desired.

The very low ratios of surge impedance to "normal impedance" is to be expected in high-resistance soils. As has been previously suggested either the counterpoise or driven ground rod is essentially an imperfect capacitor in series with a resistance and inductive reactance. The contact resistance between the soil and the grounding electrode is paralleled by the capacitance of the rod. During the surge the contact resistance is effectively shunted out. Therefore, where higher "normal" impedances are indicative of high-contact resistances accompanied by large rod capacitance, the ratio of impulse impedance to the "normal" impedance will be quite low. This, of course, means that the true criterion of the effectiveness of the ground is the leakage resistance. In other words, the "normal" or d-c or 60-cycle impedance measurements are of value only insofar as they indicate the magnitude of the leakage resistance. It is probably true generally that low "normal" impedances indicate low-contact resistances and high "normal" impedances indicate high-contact resistances. This is probably also true of the leakage resistance.

It is believed that leakage resistances will be reduced most effectively by deep grounding. This is indicated by the data presented and the conclusions of the authors.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): This paper is of considerable interest because it gives results of lightning strokes to lines with unusual grounding conditions. The knowledge gained by this and similar investigations has helped considerably to improve the lightning performance of transmission lines. I believe that considerably more information can be obtained from the data than has been given in the paper.

The records obtained indicate for lines equipped with ground wires:

(*a*). The current waves in the stricken tower have relatively steep wave fronts and relatively short tails.

(*b*). Due to the steepness of the wave front, the ground impedance of driven rods and counterpoise wires is considerably higher than low-frequency resistance measurements indicate at the stricken tower.

(*c*). Measurements of crest currents in ground wires and towers can be used to calculate the approximate wave shapes in the stroke, in the towers, and in the ground wires.

These points are shown in somewhat more detail, from the results presented, as follows:

1. At towers close to the point of contact of the lightning stroke, the driven rod conducts currents of the order of three times as great as conducted by a counterpoise indicating that the impedance of (2) counterpoise wires in parallel is of the order of four times as great as that of a driven rod. (The ratio of currents in the driven rods to the currents in the counterpoise are: Table VIII, *T*-7,708—5.37; Table X, *T*-7,715—2.75; Table XIV, *T*-7,721—3.00.)

2. At towers a few spans distant from the stroke, the counterpoise may conduct as much or even more current than the driven rod (as shown in Tables VIII, towers 7,705 and 7,711; Table X, towers 7,712, 7,716, to 7,723 inclusive). As shown under paragraph *5* of this discussion, the current fronts are slow at such towers and therefore counterpoise resistance may be equal to or less than the resistance measured with low frequency. Since the counterpoise is installed at the top of a very thick layer of sand, seasonal or even day by day variations in counterpoise resistance should be great depending on the moisture content of the sand. The driven rod reaching below the sand layer should be affected to a much lesser degree. This seems to be indicated by stroke 6, Table XIV, where at towers, 7,716 to 7,719 and 7,722, 7,723, in spite of high driven rod currents, no counterpoise currents were measured.

3. At the stricken tower where the ground impedance is effective rather than ground resistance, seasonal variations are small (see paragraph 1).

4. The effective impedance of the grounding system at the stricken tower is several times the resistance measured by low-frequency means (d-c or a-c). Stroke 6, near *T*-7,721, Table XIV, caused flashover of three insulator strings at *T*-7,721, (1.5×40 negative flashover voltage of 760 kv), although the product *IR* (neglecting counterpoise resistance) was 10 ohms × 56,000 amperes which equals only 560 kv. Actually, the flashover voltage will be considerably greater on account of the coupling factor between line wires and ground wires and the short time during which the high voltage is applied as will be shown later. The actual flashover voltage, taking into account these factors, is of the order of 1,200 kv. The effective impedance for the stroke at tower 7,721 is therefore $\frac{1,200,000}{56,000} = 21.4$ ohms. The ratio of counterpoise impedance to driven-rod impedance equals three on the basis of the inverse ratio of their currents. Further, the two counterpoises and the driven rod represent a parallel connection of impedances. From these two relations between the impedances, the ground-rod impedance at current crest calculates as 28.5 ohms or 2.85 times the low-frequency resistance, while the two counterpoises have a combined resistance of 85 ohms or 170 ohms each. Such high values of counterpoise impedance have been shown to exist from tests[1,2] on counterpoises and are due to steep front current waves.

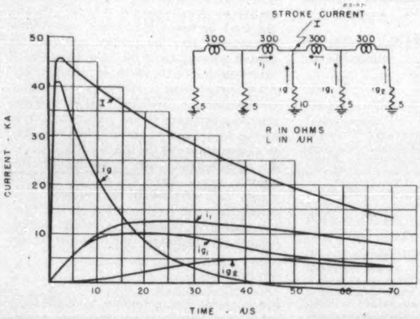


Figure 1. Current wave shapes in ground wires and towers for a 2 x 40 lightning-stroke current to a tower

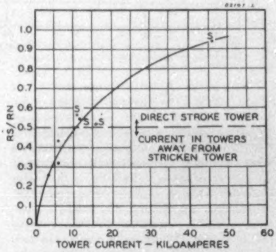


Figure 2. Ratio of surge resistance to normal resistance—R_S/R_N of towers as function of tower current amplitude

Data from Table XVI of the paper. *S* indicates stricken tower

5. From the data showing currents in the different grounding structures caused by the different strokes, a fair estimate of the wave shapes involved at the different towers can be made, using traveling wave theory. However, instead of repeated reflections, use is made of the fact that a transmission line after a few reflections changes from a surge impedance into an inductance. A simplified circuit such as shown in Figure 1 of this discussion results, and this can be solved with second power equations. Further, instead of voltage waves, a current wave of a desired shape is injected into the circuit representing the stroke current. This obviates the use of the unknown surge-impedance value of the lightning stroke. The resulting current waves in ground wires and towers in a section typifying towers, 7,713, 7,714, 7,715, 7,716, and 7,717, stroke 3, Table X are also shown on Figure 1, assuming a wave shape of the lightning stroke as 2×40 and equal ground resistances at all towers, as well as equal inductance per span as shown on Figure 1. This figure does not use actual resistances but shows the mechanism involved at the ground end of a lightning stroke to a ground wire.

6. It is impossible here to go into any detail about the accuracy of the method. However, Table I shows a very good check between calculations and measurements for towers 7,715, 16, 17. Towers 7,715, 14, 13 do not check as well, because tower 7,714 has a resistance of only 8.5 ohms as compared to 11 to 14 for the other towers. The ground currents in towers 7,714 and 7,716 are in inverse ratio to their resistances. In the example resistance at towers removed from the stricken tower have been assumed considerably lower than the stricken tower on account of the slow rising current fronts. To simplify calculations all resistances are made equal except at the stricken tower which is twice as great. In stroke 3, contact actually was made to the ground wire between towers 7,715 and 7,714. However, the distribution of currents is such that it must have occurred within 10 to 50 feet of tower 7715. The stroke current amplitude, therefore, is between 46,000 and 48,000 amperes with an approximate wave shape of 2×50 microseconds. Similar calculations can be made for the other strokes. It can be seen from Figure 1 that the voltage at the stricken

tower ($I_g \times R$) is of very short duration (2×9.5), and therefore the sparkover of an insulator string would occur at approximately 20 per cent to 30 per cent higher voltage. This fact has been made use of under paragraph 4.

7. In Table XVI, the authors give the results of an attempt to calculate the reduction of ground resistance with increase of tower potential calculated from the product $I_t \times R_N$, where I_t is the measured tower curent and R_N the measured Megger or a-c resistance of the ground. From this, they derive an equation and the curve of Figure 2 of the paper. I wish to point out that this curve is much too optimistic, because it says, for instance, that for a ten-ohm ground resistance, a tower current of 1.2×10^6 amperes would cause only 600 kv. Likewise, a tower current of 150,000 amperes and 100 ohms ground resistance would produce a tower voltage of only 600,000 volts and therefore would not cause arc-over of an insulator string of (10) insulators, spaced $4^3/_4$ inches. This is, of course, too much to hope for and is contradicted in the paper by stroke 6, where 56,000 amperes through a ten-ohm ground resistance flashed over three insulator strings. The curve shows these high reductions for three reasons.

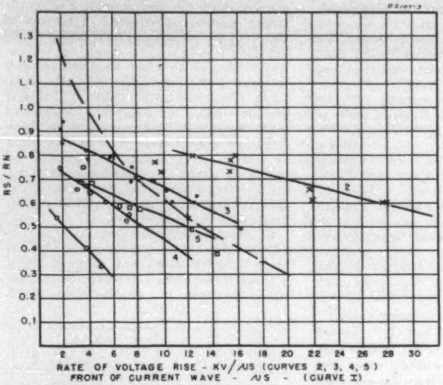


Figure 3. Effect of surge-current front on the ratio of surge resistance to normal resistance—R_S/R_N of a ground

R_S/R_N function of voltage rise

Curve	Grounds	Table	References	Current Front—Micro Seconds
2	*C, D*	II	3, I	6
3	*C, D*	IV	3, I	12
4	*H, I*	VII	3, II	18
5	*M*	VI	3, II	8–12

Curve 1—R_S/R_N as function of current front at 12.6 kv per microsecond

Table I

Ratio of Currents	Towers 7,715 7,716 7,717 Measured	Calculated From Figure 1	Towers 7,715 7,714 7,713 Measured
i_g/i_1	3.15	3.22	2.24
i_g/i_{g1}	3.25	3.97	1.96
i_g/i_{g2}	9.1	9.3	4.6
i_{g1}/i_{g2}	2.8	2.34	3.42
i_1/i_{g1}	1.03	1.23	0.88

(*a*). The assumption of a geometrical multiplication factor of 2.5 for the tower to probe potential. Such a factor should vary considerably for each tower and each stroke. Based on stroke 10, with arc-over of three insulator strings at 4,400, this factor at that tower should be of the order of 6.

(*b*). All tower resistances are treated alike and no differentiation is made between stricken towers, where the wave fronts are steep, and towers removed several spans, where wave fronts are slow as shown by Figure 1. Figure 2 shows the effect of the proximity of the stroke to a tower. Incidentally, it also shows the entirely unexpected increase in surge resistance with current amplitude.

(*c*). Combination of very high and low ground resistances. Ground resistances of the order of 1,000 ohms must not only be subject to seasonal variations to a great extent, but also should react differently to surge currents from grounds of 75 ohms or less, decreasing probably the resistance by profuse arcing across the ground surface, or down toward the water level. Test results with steep current waves are limited.[1,2] These few tests show that the steepness of the current fronts has a great effect on the impedance of a ground. Bellaschi's tests[3] which were made with fronts of 6 to 18 microseconds can be interpolated as shown on Figure 3, indicating that, even at low rates of voltage rise across the ground for short ground rods, the impedance of the rod for a 2-microsecond current front is of the order of 1.4 times the a-c resistance. This, of course, checks with the flashover of tower 7,721, stroke 6, and numerous other flashovers occurring to low-resistance structures.[4,5]

References

1. Theory and Tests of the Counterpoise, **L. V. Bewley.** AIEE Transactions, volume 53, 1934, August section, pages 1163–72.

Discussion by **J. H. Hagenguth** of reference 1. AIEE Transactions, volume 54, 1935, February section, pages 220–3.

Counterpoise Tests at Trafford, **C. L. Fortescue, F. D. Fielder.** AIEE Transactions, volume 53, 1934, July section, pages 1116–23.

Surge Characteristics of a Buried Bare Wire, **E. D. Sunde.** AIEE Transactions, volume 59, 1940, pages 987–91.

2. Über Den Wirksamen Widerstand Von Erdern Bei Stossbeanspruchung, **H. Baatz.** *Elektrotechnische Zeitschrift*, volume 59, number 47, pages 1263–7.

3. Impulse and 60-Cycle Characteristics of Driven Grounds—I, II, **P. L. Bellaschi, R. E. Armington, A. E. Snowden.** AIEE Transactions, volume 60, 1941, March section, page 123; volume 61, 1942, page 349.

4. Experience With Preventive Lightning Protection on Transmission Lines, **S. K. Waldorf.** AIEE Transactions, volume 60, 1941, June section, page 249.

5. Lightning Investigation on a 220-Kv System—II, **Edgar Bell.** AIEE Transactions, volume 55, 1936, December section, page 1307.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The field study presented in this paper is a timely contribution on the performance of grounds. It appears to me that these findings can be compared with mutual advantage to the results of laboratory investigations recently presented to the Institute.[1]

An important characteristic of grounds is the lowering of the impulse resistance with current. This is particularly the case for grounds driven in high-resistance soil. The field data by Hemstreet, Lewis, and Foust are a further contribution on this subject. I have reference to column 9 of Table XVI of their paper. However, it is rather difficult to understand how the authors were able to draw as nice a curve as they have done in Figure 2 on the strength of the data presented alone.

Other important factors connected with the problem of grounds are the effect of the discharge circuit or lead inductance and the conditions in the earth. The lead or tower inductance becomes particularly significant when steep-front currents of high magnitude are discharged to earth. Besides the lead or tower inductance, appreciable inductance may be present also in the ground proper, as is the case for grounds driven deep into the earth, or for a counterpoise spread over a wide area. Consider, for instance, a five-ohm, 100-foot driven ground which taps its low resistance largely at the lower end of the driven rod. The total inductance of the rod is in the order of 50 microhenrys, and, accordingly, this ground would have a time constant of about $L/R = 50/5 = 10$ microseconds. A current rising to crest in a time shorter than this naturally builds up a voltage across the ground which is the sum of the two drops

$$L\frac{di}{dt} + Ri$$

For currents rising rapidly to high crest values, the initial voltage developed across the ground is largely the inductance drop, and this can reach magnitudes considerably greater than the resistance drop corresponding to crest current.

In case the resistance of the driven ground is distributed along the length of the rod, the relationship of the two drops is more complex than previously stated—only a part of the total inductance of the rod is effective. Conclusions 2 and 3 of the paper apparently have a bearing on these factors.

There are still other factors related to the problem of grounds that require study, such as the following: the distribution of electric charges in the earth preceding the lightning-stroke discharge, the variation of the soil resistivity and the soil structure, the geology, and so forth. In high-resistivity grounds, such as rocks, the capacitance of the ground conductor to the mass of the earth is not entirely a negligible factor.

Reference

1. Impulse and 60-Cycle Characteristics of Driven Grounds—II, **P. L. Bellaschi, R. E. Armington, A. E. Snowden.** AIEE Transactions, volume 61, 1942, page 349.

J. G. Hemstreet, W. W. Lewis, C. M. Foust: J. R. Eaton has commented on soil conditions in the region of the lines investigated. Because of his close association with this work at the time the ground rods were driven and the attention he has given to soil and geology in this region, his comments are particularly pertinent. His point that ground rods in sandy soil, even though deeply driven provide a relatively high-resistance ground, should be especially noted. Low resistance is provided only when the rods penetrate the underlying clay layer.

Hamilton Treadway calls attention to the fact that this paper did not shed light on the time impedance characteristic of the ground electrodes during the lightning stroke. Of course, as he points out, our methods of measurement wherein only peak currents and voltages were recorded did not provide oscillographic results. Co-ordination of peak currents and voltages is not directly obtained from the data.

However, general experience has shown quite conclusively that the most important factors contributing to tower potential are current amplitude and footing resistance. A high initial impedance potential does not seem to be an important factor in the average circumstance of insulator flashover. In other words, the inductive potential occurring with the initial rise of current is indicated by measurement to be considerably less significant than the ohmic potential of the earth adjacent to the tower footings.

Mr. Hagenguth suggests that we could obtain considerably more information from the data than have been given in the paper. Undoubtedly this is true; however, studies of lightning on transmission lines have, in the past, more frequently been characterized by overreaching interpretations than by "too careful" ones. Our own efforts have been to stick as closely as possible to the data with simple and direct analyses. We try to measure currents and voltages in various parts of the line structure and to relate these to each other in such manner as to achieve as consistent a picture as possible. Our continued examination of structure measurements has in most cases given us a consistent picture when we consider only resistances, resistance voltage, and current summations. This method of analysis is particularly applicable to local conditions within a few towers of the lightning stroke.

With regard to seasonal changes in counterpoise resistance, considerable variation is indicated by the figures of Table II of this discussion.

At tower 7,720, which is next to 7,721, discussed by Mr. Hagenguth, the maximum variation is 420 to 220 ohms. At other towers variations are high as 6 to 1.

Mr. Hagenguth calls attention to the flashover we reported at tower 7,721 although the *IR* product was only 560 kv. Actually we are uncertain of the occurrence of this flashover. The report from the field read as follows:

> "Links taken from the *N*-14 line were magnetized without the line tripping out, so we cannot give much information on how the transmission system was affected by this particular stroke of lightning, except the insulators on tower 7,721 show indications of recent lightning flashover."

Mr. Hagenguth's interpretation of wave shapes from crest current readings and on the basis of injection of a representative wave shape is interesting but may involve too much assumption. With regard to his comment on our Table XVI and Figure 2 showing the relation between normally measured resistances, lightning-current resistances and tower potentials, he points out that we are much too optimistic because a ten-ohm resistance, and a tower current of 1.2×10^6 amperes would cause only 600 kv, or 100 ohms and 150,000 amperes would give the same voltage whereas for stroke 6, 56,000 amperes and ten ohms flashed over three insulator strings. The latter case is that of tower 7,721 referred to above where no tripout occurred, and the indication of three-phase flashover is, therefore, unlikely to have been associated with this record. With regard to the optimism of the curve of Figure 2 of the paper, our experience on this line to date does not indicate that it is overly optimistic. The method of interpretation we have included in the paper seems to us to be direct and logical. Undoubtedly additional data will permit of more exact location of the curve.

Table II. Resistance Measurements of Right-Angle Counterpoise

	Resistance—Ohms Tower				
Date	7,660	7,670	7,680	7,712	7,720
6—1937	225	350	600	540	420
7—1937	168	250	330	320	232
11—1937	155	230	310	350	250
10—1938	1,200	300	300	400	350
7—1940	175	270	350	350	400
6—1941	190	240	350	425	300
10—1941	125	170	235	275	220

Information on tripouts generally confirms our analysis. Confining our attentions to the experimental section of the *N*-14 line of six high-current disturbances, only one had an associated tripout. This was at a location where right-angle and parallel counterpoises were installed, and resistances were several hundred ohms. All five others were at locations where deep-driven rods reduced measured footing resistances to 13 ohms or less. On the *T*-20 line four tripouts were reported for the eight lightning strokes, and these corresponded in all cases with high-resistance towers not equipped with ground rods.

Mr. Waldorf has asked if the data give any indication that rods or counterpoise are the more effective in the prevention of flashover. Now the answer to this question should be based on directly comparable data on a flashover basis, but with each grounding method functioning independently; these data we do not have. However, we did assume in planning these measurements that, with both types of grounding connected, the relative amounts of pickup current would indicate relative performance, and this is what we are reporting in this paper. However, it is important that the results we are reporting be regarded for the time being as pertaining to the particular and peculiar high-resistance soil condition prevailing in our test section. Based on quite limited data, however, some indication of an answer to Mr. Waldorf's question might be derived from tripout experience on the *N*-14 line for the six strokes reported. Strokes 2, 3, 4, 5, and 6 were to sections equipped with deep-driven rods and were not accompanied by tripouts with the possible exception of stroke 6 at tower 7,721, as explained in our reply to Mr. Hagenguth. Stroke 1 to a right-angle parallel counterpoise section, however, did result in tripout which was not mentioned in the paper. No flashed insulators were reported. The rods seem to have the best of it on this basis.

P. L. Bellaschi suggests that the lightning field data presented in this paper could, with advantage, be compared to laboratory results reported in item 8 of the list of references appended to the paper. The authors appreciated this in the preparation of the paper and believe the natural and artificial surge results are in general agreement. In Mr. Bellaschi's laboratory results the ratio of "normal" to "surge" resistance was much lower than the extreme of 25 to 1 found in the case of natural lightning. However, his low ratio applies to a soil of better conductivity than the sand in the region of this experiment. As regards the importance of inductive potential along the tower and in the long ground rod, field measurement experience does not indicate this to be of major importance in the building up of flashover potential. In some of our early measurements a section of tower length some 40 feet long was bridged by a crest voltage-indicating instrument, for the purpose of measuring voltage difference along the tower. Ninety per cent of all measured voltages were below 40 kv with many cases of no voltage registration (below 10 kv) where high tower currents were recorded. Accordingly, inductive potential along the tower has not been emphasized. With regard to conclusion 3 of the paper to which Mr. Bellaschi refers, as having bearing on this matter, the time displacement between counterpoise currents and rod currents has to do with the longer current path through the deep-driven rods (some 100 feet) into the low-level conducting strata and out to remote portions of the induced earth field. Insofar as this displacement represents inductance, Mr. Bellaschi's comments are pertinent.

Abnormal Currents in Distribution Transformers Due to Lightning

Discussion and authors' closure of paper 42-109 by J. M. Bryant and M. Newman, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, August section, 1942, pages 564–8.

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper presents a very interesting approach to the problem of fuse outages on distribution transformers in the theory that surge currents saturate the core and thus allow abnormal power current to flow through the primary winding. This may be the answer to much of the trouble experienced with fuse outages during lightning storms and merits further consideration.

The point made by the authors in regard to lightning strokes producing large currents in the secondary windings of the transformers may be theoretically possible, but it is doubtful if this is a very frequent occurrence or a serious factor in the failure of distribution transformers.

D. D. MacCarthy (General Electric Company, Pittsfield, Mass.): Messrs. Bryant and Newman have analyzed the effect of long-duration surges on grounded neutral distribution circuits and concluded that these surges on the primary lines are one cause of primary fuse blowing. This is supported by Figure 3 in the paper showing that in service fuses were blown on a single-phase rural line only at locations where the primary currents were the largest. The authors state that the primary currents may become relatively large when the core of the transformer becomes saturated, and a step-by-step calculation is given for a three-kilovolt-ampere 7,200-volt transformer which is based upon the transformer design constants, the magnetization curve, and an assumed surge of 55 kv crest decaying exponentially to half value in 2,500 microseconds.

This conclusion regarding the current in the transformer primary agrees with results obtained in Pittsfield during investigation of the effects of long-duration surges made soon after the first actual oscillographic measurement[1] of long-duration lightning strokes to the Empire State Building in 1937. As a part of this study tests were made on a three-kilovolt-ampere, 6,900-115/230-volt distribution transformer of 3.38 per cent impedance. Surges were applied to one high-voltage terminal of the unexcited transformer from a 56 microfarad impulse generator charged to voltages of 20 or 50 kv; the other high-voltage terminal of the transformer was grounded. Representative oscillograms with the transformer secondary winding open-circuited are shown in Figure 1 and in Figure 2 of this discussion with that winding short-circuited. The crest values of primary current and voltage were about the same, irrespective of whether the secondary winding was open-circuited, short-circuited, or was connected to a resistance corresponding to a full load. When the secondary winding was open-circuited, or with it closed on full-load resistance, the primary current does not reach an appreciable value until the iron becomes saturated. This time to saturation, discussed by Bryant and Newman, can be seen in Figure 1 of this discussion. When the transformer secondary winding is short-circuited, there is no delay in the starting of the primary current, since the currents in the two windings give equal and opposite magnetizing forces. In this case the initial rate of current rise is governed by the leakage inductance of the transformer as might be expected.

Other tests were made with impulse voltages as high as 40 kv applied to the transformer primary winding. In these tests fuses rated three amperes could be blown by one discharge.

Transformer inductance was calculated from the current and voltage oscillograms and is found to drop from the magnetizing inductance of 1,400 henrys to about 2.5 henrys for primary currents of 50 amperes and approach the leakage inductance of 0.9 henry with primary currents of 125 amperes. Values of primary resistance were calculated from the voltage at the crest of the current discharge and were found to be five to 30 per cent higher than the d-c resistance, presumably because the core and eddy current losses appear as a resistance.

After having studied the transformer characteristics, the effect of long-duration surges on single-phase grounded neutral circuits was investigated through the use of model lines and calculation. The results obtained from one of the conditions studied are shown in Figure 3 of this discussion in which a long-duration stroke of 500 amperes constant current was assumed to strike the phase wire at the mid-point of a 6,900-volt distribution line, 40 miles in length. It was assumed that there was one three-kilovolt-ampere distribution transformer per mile and that the line was fed from a 60-kva substation transformer. The voltage between the line conductors at the stroke location was assumed to be limited by a valve-type lightning arrester.

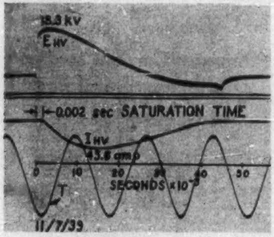


Figure 1. Oscillograms taken with long-duration impulse applied to one high-voltage terminal of three-kilovolt-ampere 6,900-volt distribution transformer with low-voltage terminals open-circuited

E_{HV}—Voltage between high-voltage terminal and ground
I_{HV}—Current through high-voltage winding
T—60-cycle timing wave

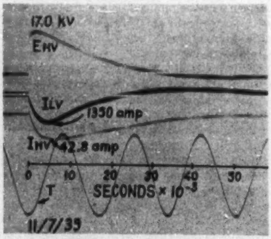


Figure 2. Oscillograms taken with long-duration impulse applied to three-kilovolt-ampere distribution transformer with the low-voltage terminals short-circuited

E_{HV}—Voltage between high-voltage terminal and ground
I_{HV}—Current through high-voltage winding
I_{LV}—Current through low-voltage winding
T—60-cycle timing wave

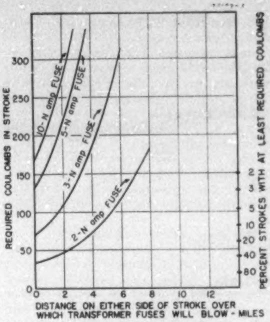


Figure 3. Coulombs required to blow fuses of three-kilovolt-ampere 6,900-volt transformers connected to single-phase grounded neutral line 40 miles in length. Stroke of 500 amperes assumed to strike phase wire at mid-point of line

Curves plotted in Figure 3 of this discussion show the stroke charge required to blow fuses of several different ratings as a function of the distance in either direction from the mid-point of the line where the stroke occurred. The right-hand scale shows the percentage of strokes expected to have charges at least as great as read from the left-hand scale based upon lightning data[1] from the Empire State Building. The distribution transformers and fuses were assumed to be the same for each location. There are a number of factors in service which will affect the locations where transformer fuse blowing will occur. Among these are differences in the characteristics of the fuses, distribution transformers, variations in resistance of the grounds along the line, and the possibility that flashover may occur between the line conductors due to the high current peak of the lightning stroke which generally precedes any long-duration low current component that may occur. However, these curves lead to the conclusion that fuse blowing on such a line will be restricted to the vicinity of the stroke and that the number of fuses blown decreases very rapidly as the fuse rating is increased. The impedance of the distribution line and the density of connected transformer kilovolt-amperes greatly influence the number of fuses that can be blown. The high impedance and light load of the line considered in Figure 3 of this discussion are favorable to fuse blowing by long-duration surges. Yet with five-ampere fuses only three per cent of the strokes that occurred to the Empire State Building have sufficient charge to blow the fuse of the transformer at the stroke location.

As Bryant and Newman state, more data are needed to establish the frequency with which long-duration discharges blow transformer fuses in service. This frequency must, of course, be established in order to judge the permissible cost of any solution proposed. It is difficult to determine the portion of fuse blowing that is caused by long-duration discharges. Probably the most complete compilation of operating data on distribution transformer lightning protection was prepared by L. G. Smith[2] based on reports from approximately 40 utilities covering urban and rural territories for a period of three years; with conventional arresters and solid interconnection, the rate of fuse blowing was 1.85 per cent for urban and rural territory combined in Table X, and 6.26 per cent for rural territory only in Table XI. Some undetermined part of this fuse blowing was probably caused by lightning currents flowing in the transformer primary windings.

In looking for means of preventing impulse currents from blowing primary fuses, the author of this discussion and his associates developed a fuse with a small series inductance and a protective gap shunting the current responsive element of the fuse link and inductance. A number of years ago, a trial lot of these fuses was installed for experimental purposes on the line side of arresters protecting distribution transformers in order to gain service experience. The number of these special fuses blown by the relatively high impulse currents discharged by the arresters was much less than for standard fuses, see Table VI of reference 3. However, it was concluded that the combination of series inductance and shunting gap is relatively ineffective in preventing such fuse blowing as may occur because of lightning currents building up in the transformer primary winding, because, with any practical fuse design, the shunting gap will not spark over on account of the slow rate of current rise. Furthermore, should the shunting gap spark over because of a pulse of current through the transformer capacity, it will seal and long-duration current that may subsequently build up through the transformer primary winding will flow through the fuse element.

Only strokes to the primary lines are mentioned by Bryant and Newman. However, the effect of strokes to other parts of the circuit was investigated by the author of this discussion, and it was concluded that primary fuses can be blown by long-duration strokes to the secondary wires or to the earth near the ground connections at the transformer pole or on the secondary circuit. Unquestionably some portion of fuse blowing is due to strokes that do not reach the transformer over the primary circuit.

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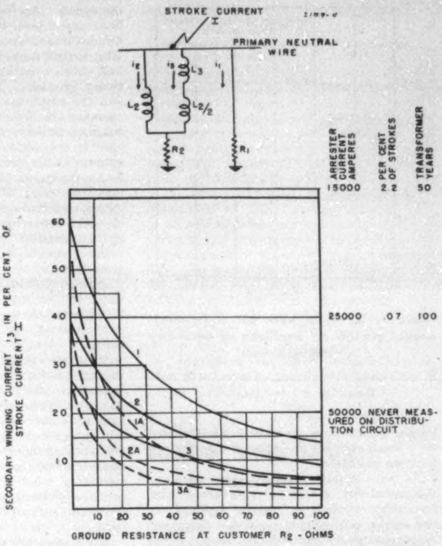
J. H. Hagenguth, H. C. Stewart (General Electric Company, Pittsfield, Mass.): We wish to discuss the first part of the paper describing the failure and deformation of the low-voltage winding of a distribution transformer. A few such cases, as described in the paper, have come to our attention. The number is so insignificant compared with transformers in service that in all probability it would not be economically justified to provide protection.

The authors point out that, "such conditions, to a greater or lesser extent, must occur in almost every case of a direct stroke to line near a transformer installation."

It is, of course, immaterial whether the stroke contacts the neutral wire directly or by way of the high-voltage line wire and the arrester. Some brief calculations have been made to show that the possibility of the occurrence of such phenomena should be extremely rare. Figure 4 of this discussion shows the circuit used as basis of the calculation, which represents the condition of a transformer installed on a line where the nearest transformer is at least five miles removed. This is, of course, purely hypothetical but represents the most severe condition possible, because practically all the current in a stroke has to be conducted to ground at that location. The secondary neutral lead is represented by an inductance based on 0.55 microhenry per foot. The inductance of the two secondary-line leads are assumed to be in parallel and therefore half the value of the neutral wire. (Since all three wires carry current in the same direction and are closely spaced, these inductance values are not mathematically exact, but they are of the right order.) The use of the inductance is justified, because after only a few reflections, the surge impedance of the wires changes into an inductance.

Figure 4. Abnormal secondary-winding current as function of stroke current

Service pole remote from other poles. Solid horizontal lines are 10,000 amperes secondary-winding current levels for respective lightning-arrester currents



Service pole ground
$R_1 = 30$ ohms, curves 1, 2, 3
$R_1 = 10$ ohms, curves 1a, 2a, 3a
Customer's ground R_2 = abscissa
Secondary-winding inductance
$L = 60$ microhenrys

Secondary-neutral lead inductance	$L_2 =$	320	55.4	27.7
Secondary-line wire inductance	$L_2/2 =$	160	27.7	13.8
Length of secondary leads		600	100	50
Curve		1, 1a	2, 2a	3, 3a

$I = 0 \times 00$ wave

The solid curves of Figure 4 show the current amplitude through the secondary winding in per cent of the total current at the neutral wire, when the service pole ground is 30 ohms, as function of the resistance of customer's ground. All ground resistances are effective resistances. The dashed lines show the currents for ten-ohm pole ground. Stroke current wave shape used for calculation is 0×00.

The authors assume that damage to the transformer shown in their Figure 1 was probably caused by a current of 20,000 amperes in each section. If we assume even one quarter of that value through one section or 10,000 amperes through both sections, the three horizontal lines indicate the levels at which 10,000 amperes would flow through the secondary for different stroke currents as indicated at the first right-hand scale.

McEachron and McMorris[1] have shown that the currents through distribution arresters are of relatively low value, the highest measured current from 1,608 records was 25,000 amperes (six records or 0.003 of the records indicated values in excess of 25,000 amperes). The percentage and expectancy figures given in their Figures 1 and 2 are shown on the right-hand scale of Figure 4 of this discussion. Curves 1, 1*A*, 2, 2*A*, and 3, 3*A* are drawn for 600-, 100-, and 50-foot long secondary lines between transformer and customer, respectively.

Figure 4 shows clearly that the chances for even 10,000 amperes through a distri-

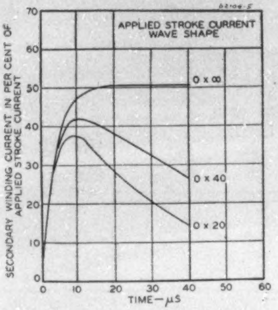


Figure 5. Effect of wave tail of lightning-arrester currents on amplitude of secondary winding currents

$R_1=30$ ohms, $R_2=5$ ohms, 600-foot extension (See Figure 1 for diagram)

bution transformer secondary winding are very small even for the isolated transformer location calculated.

As was pointed out by the authors, the probability of high currents through the secondary winding is rapidly reduced as the service pole ground resistance decreases below customer's ground resistance.

However, this is a solution that should hardly be recommended because of the associated hazards. The resistance at the customer should be kept to as low a value as possible. Figure 4 of this discussion shows that even with 0 ohms resistance at the customer and 10 ohms or 30 ohms at the pole, high currents through the secondary winding are improbable.

The circuit shown in Figure 4 neglects the resistances of the secondary leads and windings. Since the inductances and resistances are relatively small, current flow quickly is distributed according to resistances rather than inductances. The equations for circuit of Figure 4 which are of the form $A(1-\epsilon^{-nt})$ were used because they readily can be evaluated for crest current. The effect of resistances is similar to that of the inductances, and therefore a current time curve is practically the same for both conditions.

There is a very good reason to believe that current peaks to distribution systems are of relatively short duration, perhaps not longer than 40 microseconds, and consequently the duration of the secondary winding currents would be reduced correspondingly to shorter durations. The effect of the length of the current wave is most pronounced for conditions of highest currents through the secondary winding. Figure 5 of this discussion shows the current wave shape through the secondary winding for rectangular tail, 40-microsecond tail and 20-microsecond tail of the current wave through R_1 and R_2 of Figure 4. The marked reduction in current crest is evident.

The wave tail of the current through R_1 and R_2 is determined to a considerable extent by the presence of other pole and customer grounds near the location struck by lightning. The effect of two adjacent grounds on either side, 1,000 feet and 5,000 feet distant, is shown on Figure 6 of this discussion. Again the primary neutral lines are represented by inductance and infinite long stroke current wave is assumed. The current $i_1+i_2+i_3$ is rapidly reduced to half value, even for 5,000-foot distance between grounds. Therefore, the duration and the amplitude of secondary abnormal currents are definitely limited. (This calculation neglects the inductance between stricken pole and customer. The ground resistance at the two far poles are assumed to be normally 30 ohms reduced to half to take account of resistance reduction[3] caused by impulse currents on account of the slow current fronts and customer's ground at their locations.)

From this brief study, it is shown that secondary winding currents of sufficient amplitude to damage the secondary winding in the way shown in the paper are hardly to be expected if the stroke contacts the primary neutral wire, although the possibility exists.

There is, however, a good possibility that damage of this type is caused from strokes contacting the electrical wiring at the customer's premises and due to high ground-resistance current flows from customer's wiring to the service pole. The higher the customer's ground resistance, the greater, of course, is the possibility of damage to the secondary winding, because there are no other additional connections which may drain the bulk of the lightning stroke current.

Three cases are known to us where lightning entered a distribution system through the secondary circuit without even then causing this particular damage. One is described in another paper.[3] In this case, the energy of the stroke was carried to ground through three different grounds.

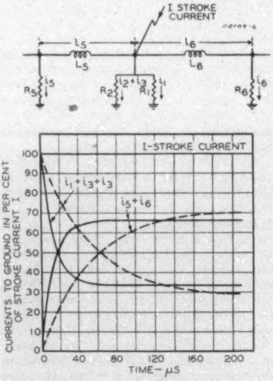


Figure 6. Reduction of length of current tail at point of stroke contact because of grounds at adjacent poles

Primary neutral wire inductance

$L_5=L_6=554$; 2,770 microhenrys

Distance $l_5=l_6$ between service poles

1,000; 5,000 feet

Ground resistance $R_5=R_6$ 15; 15 ohms

Ground resistance $R_1=R_2$ 30; 30 ohms

Another case occurred in the city where the customer was grounded to the water system and a third at a Civilian Conservation Corps camp in wooded country.

The greatest exposure to such damage should be expected on rural lines where the primary neutral wire is grounded at perhaps only a mile interval. The principal protection as shown by the curves of Figure 4 would be low service pole ground resistance and short secondary leads with customer's ground at a minimum economical value.

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E. E. Piepho (The Detroit Edison Company, Detroit, Mich.): During every lightning storm there are a few mysterious cases of blown primary fuses on distribution transformers, with no evidence of flashover or other failure on the transformer side of the fuse. Bryant and Newman's proposition that such events may be caused by long-duration lightning strokes which establish sufficient surge current through the primary winding to blow the transformer fuses, appears a likely possibility. This situation has not been serious in the past, because we have generally avoided the use of low-current fuses, the five-ampere size usually being the smallest. Admittedly, this minimum size does not protect the smaller distribution transformers from secondary faults or even winding short circuits, but it serves the larger purpose of isolating a transformer in serious trouble from the rest of the circuit. Anyone undertaking to use low-current fuses, rated to provide complete transformer protection, may expect an appreciable increase in fuse blowings from electrical storms.

Two years ago, when Bergvall and Beck presented their paper on "Lightning Protection of Distribution Systems," the writer suggested that their treatment of the subject indicated that we might have to revise some of our thinking regarding protection of electric equipment and distribution circuits, in accordance with the new conception of long-duration lightning discharges. A wholesale blowing of customer branch fuses in one locality during a lightning storm was cited at that time, and it was indicated that long-duration lightning discharges into secondary wiring by way of the transformer connections may have been the cause. Bryant and Newman's paper offers good proof that this may occur.

Recently, we experienced an interesting case of lightning damage to the operating mechanism of a street-lighting time switch. From tests we have made we know the insulation level of the low-voltage operating mechanism is approximately 4,000 volts, which is rather low considering the degree of lightning exposure. We devised adequate lightning-arrester protection for this insulation, but a severe electric storm recently demonstrated an unlooked-for weakness.

Paralleling the low-voltage arrester there was a sneak circuit through the high inductances of the series field and armature of the time switch motor. We expected little or no surge current through this branch because of its high inductance; nevertheless, the motor field and armature windings were burned open in one switch. The insulation of these windings to ground was tested and found unimpaired. Our belief is that a long-duration discharge through the arrester could have established enough current through these low-current windings to burn them open.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Bryant and Newman discuss an aspect of lightning on systems which is of particular interest on the lighter rural distribution circuits, since it contributes to outages where the fusing is light. One phase of this general subject has been discussed in the Bergvall and Beck paper mentioned by the authors, the phase dealing with the effect on arrester duty, which is relieved by the currents that flow through the transformers. The Bryant and Newman paper deals with the transformer currents and their effects on the transformer and the fusing. Considering the capacities of the smaller standard fuses, such as the two-ampere links mentioned by the author, for carrying current without damage, it is not surprising that the surge currents blow them occasionally. The danger of primary fuse blowing is diminished with increasing rating. This is almost axiomatic, and it has been shown by statistical data.

It is practically impossible to fuse small transformers on the primary against overload or secondary short circuits and maintain a satisfactory degree of reliability and service continuity. Attempts to accomplish this with conventional fuse links has led to two types of fusing. The first of these is to fuse with small links in an effort to prevent transformer burnouts from overload. This type of fusing leads to a large number of outages caused by blowing of the links from secondary fault, lightning currents, and mechanical damage. The second type of fusing is to select links based upon secondary short-circuit currents. The larger links resulting from this type of fusing reduce the number of operations to some extent from the causes mentioned above but at the expense of possible burning out of the transformer from overload and short circuits remote from the transformer. The economics appear to favor the second type, and there is a definite trend towards this type of fusing by operating companies. There is another type of overcurrent protective system which provides both overload and short-circuit protection to the transformer and at the same time eliminates unnecessary interruptions of service. This method employs a thermally controlled secondary breaker mounted inside the transformer. By having the breaker operation based upon transformer winding temperature, protection of the transformer is afforded for all types of overcurrent conditions, and at the same time it is possible to take advantage of the inverse time temperature characteristics of the transformer to adjust the breaker so as to eliminate most of the interruptions in service encountered through the use of small fuses. There is now a large amount of service experience available with these types of overcurrent protection, and it would be of interest and value both to operators and to manufacturers to have comparative operating data published.

J. M. Bryant and **M. Newman:** The writers are pleased that the paper has led to considerable discussion. D. D. MacCarthy's comments and oscillograms on long-duration currents and fuse failures are interesting and in complete agreement with our work. As a matter of fact we have recorded almost identical oscillograms to those given by MacCarthy, which were, however, not included in this paper as they fitted in more directly in a series of papers on "Fuse Failures Due to Lightning," published by the engineering experiment station, of the University of Minnesota. We were interested to learn that MacCarthy and his associates have had some favorable field experience with a series inductance and shunt gap arrangement of protection of fuses against surge outages, as we had investigated such a scheme proposed a number of years ago by R. R. Pittman of the Arkansas Power and Light Company. It was established on first consideration that such a scheme by itself functions primarily to by-pass around the fuse the initial current surges due to the transformer capacitance under conditions of rapid rate of rise of surge voltage. The long-duration currents penetrating the transformer windings were eliminated as a factor in fuse blowing in several different ways, such as some special saturation by-pass schemes, and proper design of heat storage capacity of the links themselves. The theoretical background on fuse failures because of lightning is presented in the experimental station bulletins referred to above. Field experience is being collected on the simplest combinations of the various methods of surgeproof fuse design.

The use of secondary breakers, as brought up by Edward Beck, is a perfectly good way of preventing lightning-fuse failures, in that it side-steps the problem by making the primary fuse so large as not to respond to surges, and thus to remove the transformer only if it is already damaged, while depending on breakers on the secondary side for overload protection. We have been carrying on some researches to help solve the surge problem in relation to fuses from the point of view that as fuses are being used in any case, to isolate the transformer from the line after burnout, it would perhaps be more economical if they also protected the transformer.

The discussion by J. H. Hagenguth and H. C. Stewart gives a very interesting approximate theoretical estimate of the extent to which other cases of damage of secondary windings may occur. We do not, however, quite agree with attributing a greater possibility of such cases from strokes on the secondary side, since under such circumstances the paths to ground from the transformer case neutral grounding point is the same anyhow, and there is a much greater probability that the stroke would short-circuit the low-voltage leads together so that the transformer windings would be bypassed. While on the subject of strokes on the secondary side, it is of interest to note that it may be possible for surge currents to build up in the secondary winding after the manner of the abnormal currents in the primary winding discussed in the paper. Such build-up would be generally accompanied by sufficient voltage to induce an arrester flashover and primary fuse failure; several such cases have been observed. The case of secondary-winding damage in the paper is, however, definitely due to current entering the winding from the neutral connection, as shown by the opposing directions of twist of the winding sections.

The comment by M. C. Westrate on the desirability of further investigation of effect of abnormal power current on fuse failures under surge transformer saturation conditions seems to us to be well worth emphasizing. The discussion by E. E. Piepho brings out several interesting cases of lightning failures that might be readily accounted for, as he points out, by such analysis as presented in the paper. The writers in conclusion are pleased to note particularly that the discussion has been constructive in leading to further extension of these investigations in the field.

Induced Voltages on Transmission Lines

Discussion and authors' closure of paper 42-103 by C. F. Wagner and G. D. McCann, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 916–30.

W. W. Lewis (General Electric Company, Schenectady, N. Y.): The authors have prepared an excellent and exhaustive paper on the subject. I wish to discuss a few points:

Figure 28 of the paper shows distribution curves of crest current in lightning strokes as determined by various investigators. In the text it is stated that the maximum current which has been reliably measured is 160,000 amperes. This refers to a Fulchronograph measurement of a stroke to a stack at Anaconda, Mont., and in reference 10 of the paper it is stated that this represents the highest known magnitude in which the total current was measured at one point, as contrasted with measurements obtained by summing up currents in parallel paths.

We have shown in several papers[1,2] reasonably good agreement between total current and the summation of individual currents in parallel paths, and, considering, the wide tolerance necessary in this class of measurement, we believe such estimates to be as reliable as those made by any other method.

In 1934 on the Glenlyn–Roanoke 138-kv line of the Appalachian Electric Power Company, measurements were made by magnetic links of a stroke with an estimated total current of 218,000 amperes negative polarity. This current was the summation of the currents in all the towers affected by the stroke, seven towers in all.[3] The maximum current measured in any one tower is 132,000 amperes obtained on the Philadelphia–Delaware River 66-kv line of the Philadelphia Electric Company in 1935. This was also the total current in the stroke.

Another stroke in the same year on this line totalled 156,000 amperes in two towers. Magnetic link measurements have indicated 146,000 amperes in the broadcasting tower of station *WABC* and 156,000 amperes in the tower of the Empire State Building.

In Figure 28 of the paper a curve by Waldorf is shown, giving distribution of crest current in lightning strokes, and reference 15 in the paper is given, presumably for the origin of this curve. Waldorf's original curve (Figure 1 of reference 15 of the paper) shows the distribution of 1,201 current measurements in *towers* and does not give total stroke current. We believe therefore that this curve should not be included in Figure 28, which deals with total stroke currents.

The authors state: "There are very little field data on lightning surge voltages for which the induced voltages can be segregated from those produced by direct strokes." In 1929 at the Wallenpaupack laboratory, 95 cathode-ray oscillograms were obtained of voltage surges on the line. Of these 50 measured under 100 kv, 30 measured between 100 and 300 kv, and 15 measured above 300 kv. Data on the 45 oscillograms above 100 kv are given in reference 4 of this discussion. In the same year at Newcomerstown, ten oscillograms were obtained ranging from 55 to 245 kv. Data on these are given in reference 5 of this discussion. In 1930 at Cherry Valley oscillograms of 23 surges were recorded. Data on these are given in reference 6 of this discussion.

It is very reasonable to assume that the low-voltage positive polarity surges are induced. Peek has classified the 1929 surges as coincident with trip out and not coincident with trip out and has assumed that the latter were induced.[7] It would seem that there is a wealth of untapped information in these oscillograms.

The curves of the paper indicate an increasing induced voltage with decreasing distance between the conductor and the projection of the stroke. Suppose the stroke hit the overhead ground wire, perhaps ten feet or so away from the conductor. How would this work out as far as the induced stroke is concerned?

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E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): During the first few years of field investigations of lightning on transmission lines with the cathode-ray oscillograph, it was my pleasant duty patiently to await the occurrence of induced voltages caused by the release of bound charges on the line conductor. Despite the fact that many strokes were observed to strike the earth quite close to the line, very few oscillograms were obtained, and these were of low magnitude.

Fortescue and Bewley showed that the principal reason for the low induced voltages lies in the current-time characteristics of lightning, and both assumed the charge concentrated at a point in the cloud. Wagner and McCann have applied their knowledge of the lightning-stroke mechanism in an exceptionally lucid manner to show how the actual distribution and neutralization of charges may be utilized to calculate induced voltages much more accurately. I believe their contribution is of great value.

If I interpret the curves of Figures 3 and 11 of the paper correctly, it appears that any stroke which is close enough to induce a voltage of importance on lines at 66 kv and above is very likely to strike the line itself. Now this fact leads to a conclusion not mentioned by the authors and one which I believe may be of importance in practical application.

Positive voltages of the order of 1,000 kv for a 100-kiloampere stroke to a tower would correspondingly increase the potential difference across the insulator string, since the tower would be depressed in potential by the amount of impedance drop in the tower and tower grounding system. The time of voltage crest is such that these voltage-difference components would be additive; hence the net effect is roughly equivalent to that obtained by conventional "direct-stroke" theory if a fictitious resistance of ten ohms is added to that of the tower footing.

The inclusion of this effect may be significant if the tower-footing resistances lie in the range 0 to 50 ohms, and close comparative performance calculations must be made.

Despite certain analytical difficulties, I believe the method presented by Wagner and McCann can be extended to the case of a stroke to the tower or ground wire. Preliminary studies I have made using the same stroke mechanism and L. V. Bewley's method of analysis indicate that such an extension might be the missing link which would give a satisfying aspect of completeness to our theory of lightning effects on transmission lines and dispose of the terms "direct-stroke" theory and "induced-surge" theory.

I should like to call the authors' attention to an apparent discrepancy in the data given by the curves. Most of the curves give a voltage crest of about 1,100 kv for a 50-foot conductor, and a 100-kiloampere stroke at a distance of 150 feet from the line. Figure 29, however, gives 1,100 kv for the same stroke 100 feet from a line of the same height.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have made an excellent study in calculating the induced voltages on transmission lines using the mechanism of the lightning discharge as known from the photographic studies by Schonland (references 4–8 of the paper) and McEachron (reference 9 of the paper) and the oscillographic studies of McEachron and others as the basis of their calculations.

They find that, in general, the gradients are of the order of 100 kv per foot or less, so that basically the calculations of Bewley (references 2 and 17 of the paper) can still be used to advantage in calculations as to the induced voltages. The authors find, however, that the gradients directly at the point struck reach tremendously high values. In Figure 6 of the paper the gradient approaches infinity at $d=0$ as the stroke approaches the ground. Such high gradients, if they would actually exist, presumably should cause flashover every time a tower of a transmission is struck, which, of course, is not the case in practice.

Actually, the induced voltages appear to be considerably below the values calculated. Fortescue (reference 1 of the paper) showed that with a stroke 250 feet from a transmission line, 1,600 feet distant from a cathode-ray oscillograph connected to that line, no voltages could be measured at the oscillograph station. The photograph of this occurrence does not indicate any streamers from the line or from the tower, indicating that the gradients must have been less than 165 kv per foot, which is the critical breakdown gradient in the nonuniform field in air at large spacings (30 feet).

The theory presented appears, therefore, at variance with observations. I question that the calculations of equations 8 and 11 of the appendix are correct when considering a point directly underneath or close to the lightning channel. These calculations are based on the assumption that each charged particle in the uniformly charged channel produced a gradient equal to a point charge at that point. By integrating over the whole channel, the combined gradient is found. In the field produced by a charged vertical line, the effect of the charges distant from the ground on the gradient directly below or close to the path is negligible, and only the charges at the lower end of the channel should contribute materially to the gradients, and therefore the gradients should be considerably lower than calculated.

It appears to me that further point to be considered is the availability of charges on line conductors. These charges can accumulate by leakage over the insulators which is a longtime process associated with the charges accumulating in the cloud. These charges are distributed along the line wire and bound by all the charges in the cloud distributed over the line. While the total bound charge thus may be of considerable magnitude, these charges are not immediately available on the line near the lightning channel. The charges bound in the immediate vicinity of the channel are small, and the charges which can be brought from the distance are limited for two reasons:

1. It takes time to move these charges along the wire.
2. Some relatively short distance away from the channel the effect of the cloud field will be greater than the effect of the field produced by the return stroke, and the charges at those distances will remain bound.

From these considerations, it would appear that high voltages as shown in Figure 12 could not exist on line wires. On ground wires, the voltages at mid-span might be quite high for very short times, because charges are more readily available. However, even for this condition, flashover can hardly be expected. As shown by practical experience, even direct strokes of considerable amplitude contacting the ground wire at mid-span hardly ever cause flashover at mid-span.

R. H. Golde (The British Electrical and Allied Industries Research Association, London, England): The authors are to be congratulated on having attacked afresh the old problem of the calculation of induced voltages which has so far defied all attempts at a correct solution. I have recently made similar calculations using the same fundamental considerations with only a few modifications which prove, however, as will be shown elsewhere, essential for the numerical solution of the problem.

Before discussing the above modifications it may be pertinent to comment on the apparent agreement between the author's calculations and observed values as presented in Figure 33 of the paper. All the calculations are based on the assumption that the highest induced voltages are due to strokes to earth at a distance of three times the height of the line above ground. This, surely, overlooks the fact that much higher induced voltages must be released in the case of a direct stroke to a transmission tower or earth wire without flashover. As these cases must influence decisively the frequency-distribution curves of induced voltages, the authors appear to have started from wrong assumptions, and the agreement obtained in Figure 33 must be regarded as only fortuitous.

If it be conceded that the highest induced voltages occur in cases of direct strokes to earthed parts of the transmission system, it follows that the horizontal distance between the lightning channel and the nearest line wire is very small. This case leads, however, to excessively high-voltage values by the authors' methods, as can readily be seen if the curves plotted in Figures 12 or 29 are extrapolated to very short distances between the lightning channel and the line. The reason for this breakdown of the method adopted by the authors for the important case of short distances lies in their assumption that the induced voltages are determined only by the charge distribution along the leader channel and by the velocity of the return stroke. This results not only in voltages of several million volts but also in substantially infinitely steep wave fronts of the induced surges. It may be mentioned in passing that, on the authors' assumptions, the wave shape of the lightning current would also have an infinitely steep front with an exponential tail and would not give the normal double-exponential shape as claimed in the introductory section.

The above difficulties are overcome by recognizing that the neutralization of the charges deposited along the leader channel cannot take place instantaneously.[1,2] The authors attack this conception by the calculation given in their Appendix II but, omitting all further criticism of the method adopted, it should suffice to point out that the tip of the advancing return stroke cannot be maintained at zero potential. That part of the channel which is just above the tip of the return stroke is still in electric contact with the cloud and therefore, omitting the voltage drop along the leader channel, is practically at the cloud potential of several million volts. It is physically inconceivable that this potential falls to zero "instantaneously" on contact with the tip of the return stroke, and the transition will actually occur in the course of the few microseconds postulated for the time required for lateral neutralization of the channel charges.

Calculations made with this new conception show that the charge distribution along the lightning channel is not immaterial, as suggested by the authors. It thus follows that the voltages induced in overhead wires are determined by three factors, that is, the distribution of charge along the leader channel, the rate of neutralization of these charges, and the velocity of the return stroke. It may also be added that it seemed advisable to use an exponentially decreasing charge distribution along the channel and a similarly decreasing velocity of the return stroke. If the functions which are suggested for these parameters in the paper quoted above are accepted, the resulting induced surges in cases of strokes to earthed parts of the transmission system are of the amplitude and wave shape to be expected. A full description of the procedure adopted and of the results obtained will be published later.

In addition to the above fundamental observations on the method of calculation, a few specific points may be briefly discussed. Thus the charge deposited along the leader channel is calculated by the authors by assuming a constant current of 100,000 amperes flowing for 65.6 microseconds, that is, the time for the return stroke to reach the cloud discharge center. This assumption can hardly be regarded as valid, having regard to the wave shape of the average lightning current. It follows that the numerical value adopted for the charge along the channel is too large. The authors state, furthermore, that, at the instant when the leader stroke makes contact with the ground, the charge left in the cloud center has decreased to zero. Although this charge does not influence the occurrence of induced surges, it may be mentioned that actually an appreciable amount of charge must be left in the cloud, as shown in the paper quoted above, and also by the low current discharge flowing, even on the authors' assumption, for a considerable time after the return stroke has reached the cloud (see Figure 2 of the paper).

In this connection it was interesting to note that the authors incorporate partly in their Figure 1 the mechanism of successive strokes, as outlined in the paper quoted, which differs from Schonland's originally suggested picture. In particular, Figure 1d accepts the idea that the conducting channel which connects the exhausted charge center in the cloud with earth will become positively charged under the influence of the surrounding negative cloud charges. From this it is difficult to see how the authors account for the interruption of the positive current flow into the channel, as shown in Figure 1e. Though the interesting question of the continuous current flow throughout a multiple lightning stroke has been discussed elsewhere[3], a reply with respect to Figure 1d would be appreciated.

In Table I ranges of return-stroke velocities and their mean values are given for three current ranges. It would be interesting to know on what information these values are based, since I am unaware of any measurements of return-stroke velocities with simultaneous determination of the stroke current.

Finally, one small point in nomenclature may be cleared up. In discussing the question of lateral charge distribution the authors refer to the bright portions of the stepped leader which were observed by Schonland and add that these were called "dart leaders" in the paper mentioned above. Actually these short bright steps were called "darts," whereas the term "dart leader" was used by Schonland to distinguish the leader stroke of successive strokes in a multiple stroke from the "stepped leader," which normally precedes the first stroke to ground.

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1. **C. E. R. Bruce, R. H. Golde.** *Journal* of Institution of Electrical Engineers (London, England), volume 88, part II, December 1941, page 487.

2. SURGE PHENOMENA. Electrical Research Association, 1941. Chapter 1.

3. *Journal* of the Institution of Electrical Engineers (London, England), volume 89, part II, December 1942, page 646.

C. F. Wagner and **G. D. McCann:** The most accurate field measurements of direct-stroke currents are those for which the total current passes through a single point, such as a mast. The currents in excess of 160,000 amperes, which Mr. Lewis mentions in his discussion, were not measured in this manner but were the result of summing the individual currents in parallel paths. Several cases for which checks could be made between actual current and the summation in parallel paths showed considerable discrepancy in the two results. This point is discussed in more detail in reference 19.

The curve in Figure 28, labeled as Waldorf's, was computed from the original data supplied by Mr. Waldorf and is plotted for the crest magnitude of the total lightning-stroke current and not for tower currents. A comparison between this curve and the curve of Figure 1, reference 15, to which Mr. Lewis refers, indicates that they are not the same.

Lewis and Whitehead have raised the question of the voltage induced on phase conductors by strokes to the ground wire. This case is not considered in the paper. It requires more detailed knowledge of stroke mechanism, especially near the ground terminus, than for strokes to open ground. In regard to the influence of induction upon the voltage across the insulator string, we are not quite convinced whether its effect can be represented by a resistance, and, if so, whether that resistance is ten ohms. Certainly if this effect can be represented by a resistance, it would be a very welcome simplification of the analysis.

The apparent discrepancy between Figures 11 and 29, referred to by Mr. Whitehead, arises from the difference in assumptions for the two curves. The voltages in Figure 11 were calculated for a crest stroke current of 100,000 amperes and a return streamer velocity of 100 feet per microsecond, whereas for the voltage curves of Figure 29 the variation of return streamer velocity with stroke current has been taken into account.

There is little in Mr. Hagenguth's discussion with which the authors can agree. His reference to Fortescue's experience, reference 1, is not inconsistent with the results of the paper. As shown in Figure 29, for a line 100 feet high a stroke 250 feet from the line could have a current crest of 15,000 amperes without inducing more than 200,000 volts on a phase conductor, assuming there were no ground wires present. The coupling effect of the ground wire reduces the induced voltage still more.

Mr. Hagenguth's criticism of equations 8 and 11 as being inaccurate is incorrect. These equations are a rigorous solution for the assumptions made. They correctly represent the effective charge distribution along the stroke channel at any point along the surface of the earth.

The method of calculation rigorously accounts for the presence of the computed voltages as required for the assumed distribution of charge and current in the stroke channel. The method also includes rigorously the effects of bound charges which seem to bother Mr. Hagenguth.

The authors wish to thank Mr. Golde for his interest in the paper and for the detailed comments which he has given. It is gratifying that two widely separated investigators have come to substantially the same conclusions regarding the general nature of the phenomena. Most of the differences between the points of view regarding the assumptions that the authors use arise from Mr. Golde's application of them to strokes to earthed parts of the transmission system. The limitations of the assumptions used were recognized by the authors. Induced voltages produced by strokes to ground wires and towers, and the preliminary induced voltage preceding a direct stroke were specifically eliminated from consideration. The basis for this action lay in the lack of sufficient fundamental data concerning the nature of rates of rise of currents and the character of upward streamers, factors which have a predominant effect in determining the nature of these voltages.

Mr. Golde is under the impression that the agreement between calculation and statistical data in Figure 33 is only fortuitous, on the assumption that the field data must contain a number of records to ground wires which would vitiate the results. Actually, the data in Figure 33 were obtained on transmission lines which were not equipped with overhead ground wires. They were produced by positive polarity surges which did not cause flashover. The authors recognize that in the case of a stroke to a ground wire the initial portion of the induced stroke on a line conductor will, for a negative stroke, first have a predominantly high positive value, followed rapidly by a surge of negative polarity caused by coupling action.

The authors would like to discuss Mr. Golde's statement that "The tip of the return stroke is still in electric contact with the cloud and therefore, omitting the voltage drop along the leader channel, is practically at the cloud potential of several million volts." The authors picture the phenomenon as one in which the initial leader is a conductor of relatively high impedance, and that the return stroke is a process in which this poorly conducting path is converted into one of higher conductivity. Therefore, it would appear that the potential of the tip of the return streamer would lie somewhere between the actual potential of the cloud and ground, but, because of the higher conductivity of the return stroke, it would be much closer to ground potential than cloud potential.

The basis for the authors' statement that the return stroke velocities increase with current magnitudes is given in the paper in the section under "Velocity of Return Streamer." From a knowledge of the lower and upper range of velocities and similar data for stroke currents and the indications that return-streamer velocities increase with current, the simple assumption was made that these velocities could be segregated in the manner shown according to currents. Mean values were then assigned to each range. This segregation should be sufficiently accurate for the purpose.

In conclusion, the authors believe that, for the subject with which the paper is concerned, namely, the induced voltages produced by strokes to ground in the vicinity of a transmission line, the assumptions and analysis have been sufficiently justified in the paper. The paper does not consider the effect of strokes to the transmission line proper, whether the contact is made to either a conductor or a ground wire. In America, this type of stroke is usually regarded as a direct stroke. Most of the transmission-line design is based upon so locating the ground wires that they adequately shield the current-carrying conductors. Thus it is generally assumed that the stroke strikes a ground wire, and the resulting difference in potential between conductors and tower or ground wire is calculated, taking into consideration such factors as tower footing resistance, coupling factors, and so forth. It was not intended to discuss this type of stroke in the paper.

Practical Design of Counterpoise for Transmission-Line Lightning Protection

Discussion and authors' closure of paper 42-91 by E. Hansson and S. K. Waldorf, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 599–603.

Herbert B. Dwight (Massachusetts Institute of Technology, Cambridge, Mass.): The interesting measurements presented in the paper by E. Hansson and S. K. Waldorf, besides leading to the six general engineering conclusions listed in their summary, provide an unusually good check on ground-resistance formulas, and they show the practicality and desirability of making ground-resistance calculations.

Practical questions described in their paper, such as "What percentage reduction in tower-footing resistance can be expected by putting in one or more additional buried wires?" can be answered easily and reliably by the ground-resistance formulas.

In checking the measurements given in Table II and Figure 1 of the paper, the average resistivity of the ground was first computed from the average of the resistances of the seven individual buried wires. The result is 178,000 ohms per cubic centimeter. Then the various group resistances were calculated, the largest discrepancy between measurement and calculation being 11 per cent.

Table I

Distance Between Two Outer Wires (Feet)	Measured Reduction (Per Cent) Table II of Paper	Calculated Reduction (Per Cent)
10	7.1	7.3
20	8.5	9.8
40	9.6	12.7

Such computations should be made by any engineer installing buried counterpoise wires or groups of ground rods. In order to show how short and easy such calculations are, the first will be given in detail. Only two formulas are needed in this case, namely equations 17 and 18 of reference 9 of the paper.[1]

Average potential of one wire caused by its own charge (by equation 18)

$$=2q\left(\text{logn}\,\frac{4L}{a}-1\right)=2q\left(\text{logn}\,\frac{4\times 2{,}400}{0.125}-1\right)$$

$$=2q(\text{logn}\,76{,}800-1)=2q\times 10.25$$

where q = charge per centimeter of wire. (For the ratio L/a use inch units, $L=200$ feet $=2{,}400$ inches and $a=0.125$-inch radius.)

Average potential caused by the image wire at $s=36$ inches (by equation 17)

$$=2q\left(\text{logn}\,\frac{4L}{s}-1+\frac{s}{2L}-\frac{s^2}{16L^2}+\ldots\right)$$

$$=2q\left(\text{logn}\,\frac{4\times 2{,}400}{36}-1+\frac{36}{2\times 2{,}400}-\ldots\right)$$

$$=2q(5.59-1+0.01-\ldots)=2q\times 4.60$$

Average potential of wire caused by itself and image $=2q\times 14.85$.

Total charge of wire and image, using the length in centimeter $=2\times 4{,}800\times 2.54q$.

$$\frac{1}{C}=\frac{\text{potential}}{\text{charge}}=\frac{2q\times 14.85}{2q\times 4{,}800\times 2.54}$$

By equation 14, reference 9

$$R=\frac{\rho}{2\pi C}$$

$$=\frac{\rho}{2\pi}\times\frac{14.85}{4{,}800\times 2.54}=34.5\text{ ohms}$$

$\rho=178{,}000$ ohms per cubic centimeter

By equation 17, potential caused by a wire at $s=40$ feet $=480$ inches is

$$2q\left(\text{logn}\,\frac{4\times 2{,}400}{480}-1+\frac{480}{4{,}800}-\ldots\right)$$

$$=2q\times 2.09$$

Table II

Conductors	Ohms to Ground	
	Measured	Calculated
1 and 2	18.5	17.4
3 and 4	18.8	17.4
1 and 4	14.1	13.9
2 and 3	13.2	13.9
All	10.1	9.8

The potential caused by the image of that wire $= 2q \times 2.09$.

Potential when there are two wires 40 feet apart $= 2q(14.85 + 2.09 + 2.09) = 2q \times 19.03$.

$$\frac{1}{C} = \frac{2q \times 19.03}{4q \times 4{,}800 \times 2.54}$$

Resistance to ground of two wires 40 feet apart

$$= \frac{178{,}000}{2\pi} \times \frac{19.03}{2 \times 4{,}800 \times 2.54} = 22.1 \text{ ohms}$$

The measured value, from Figure 1 of the paper, is 22 ohms.

Other cases calculated give curves of the same shape as those in Figure 1, and agreeing with them within 11 per cent or less.

The reduction in ground resistance by putting in a middle wire is shown in Table I of this discussion.

In a similar manner, the data in the last half of Table III of the paper can be computed from the average of the first four items, as shown in Table II of this discussion.

The practical check on the accuracy of the formulas is welcome. But, in future work, when an engineer wishes to know what percentage of change in ground resistance should be caused by a given change in counterpoise design, the information can be obtained more reliably by calculations of the type described than by comparing with previous tests. Such calculations are so short that they could be made in the field as soon as a measurement has shown that a tower-footing resistance is too high.

REFERENCE

1. CALCULATION OF RESISTANCES TO GROUND, H. B. Dwight. AIEE TRANSACTIONS, volume 55, 1936, December section, pages 1319–28.

L. A. Terven (West Penn Power Company, Pittsburgh, Pa.): The West Penn Power Company has in operation 3–132 kv single-circuit steel-tower lines which are provided with counterpoise, and one of them has been in service long enough to accumulate data to afford a comparison of lightning-protection characteristics with and without counterpoise. The line extends 58.8 miles from Lake Lynn on the border of West Virginia and Pennsylvania to Cumberland, Md., over a mountainous terrain with high-resistance substrata. The construction is horizontal with 20-foot separation between conductors and two ground wires are provided approximately ten feet above the conductor and separated 20 feet from each other. Because of the poor lightning performance of the line it was decided in the fall of 1936 to install continuous counterpoise between towers with unsually high footing resistance (the 17 towers affected averaged 171 ohms footing resistance). At the time six years of lightning season records had shown 15 interruptions. Some 9,000 feet of number 2 copper weld counterpoise was installed about 18 inches under the surface and was connected from tower to tower.

Table III of the discussion shows the lightning performance of the line before and after the counterpoise had been installed. The year 1937 is not considered, because the counterpoise was installed during the lightning season of the year.

Table III. Lightning Performance Before and After Installation of Counterpoise

Year	Interruptions Due to Lightning	Per 100 Miles Per Year	Remarks
1934.......	2	....3.4	
1935.......	5	....8.5	
1936.......	1	....1.7	
1934–36 Average..	2.67	....4.5	
1937.......	1	....1.7	Counterpoise installed during the year
1938.......	2	....3.4	
1939.......	1	....1.7	Quick reclosing in service from here on
1940.......	2	....3.4	
1941.......	1	....1.7	
1939 1940	*5	....8.5	
1938–41 Average..	2.75	....4.7	

* Instantaneous tripouts which did not result in interruptions due to quick reclosing.

In the spring of 1939 quick reclosing breakers were installed at the line terminals which served to mask the performance of the line. Five instantaneous reclosures were registered in 1939–40 which were attributed to lightning, but which did not result in interruptions to service.

The table does not show any improvement in lightning protective characteristics caused by the counterpoise, the average interruptions per year per 100 miles being 4.5 before the counterpoise was installed and 4.7 after installation. Possibly the amount of counterpoise was not adequate, as a number of towers tested to 100-ohms footing resistance, which were not included in the counterpoise program, or perhaps the incomplete shielding of the conductors by the ground wires prevented the effect of the counterpoise being noticeable.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors give a very comprehensive study of the effectiveness of low ground resistance on the prevention of transmission-line flashovers. They have found by practical experience that the counterpoise is an excellent means of reducing tower-footing resistances to sufficiently low values to prevent flashover of line insulation where ground wires are used for all of the strokes contacting the towers or ground wires of 220-kv lines and in practically all cases of 132-kv and 66-kv lines. In the latter case, however, the tower current limit appears to be of the order of 60,000 amperes even for footing resistances of the order of six ohms. As stated by the authors, there appears some doubt with regard to actual tower currents, inasmuch as it has been found that currents in single legs are not necessarily equal, and further, that considerable percentages of the total tower currents may flow in the tower braces and therefore may not be measured with links installed at the tower legs only. Multiplication factors up to two have been found applicable, depending on the tower structure, to arrive at total tower currents from leg measurements only. In this way the authors calculate a maximum safe current of 120,000 amperes.

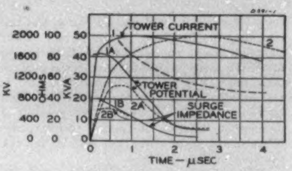

Figure 1. Counterpoise impedance (1*B*, 2*B*) and tower potential (1*A*, 2*A*) caused by tower currents (1, 2)

D-c resistance of counterpoise—six ohms. Plotted for two 200-foot counterpoises

I believe that our knowledge of the behavior of counterpoises, from theory and tests, is sufficiently accurate to permit calculations of actual voltages at a stricken tower for given current fronts and counterpoises installed. While it is not claimed that such calculations are very accurate, I believe that only by means of such calculations can we finally arrive at the true answer to some of the still unexplained phenomena. More accurate calculations can be made if any particular installation is considered where detailed data of counterpoise resistances are available.

The basis of the calculations is that the counterpoise is a variable impedance as shown by tests.[1] The time variation of this impedance depends principally on the length of the counterpoise and its d-c resistance. The counterpoise impedance varies with applied current wave front[2] as indicated by Figure 1 of this discussion. Multiplication of instantaneous values of current and counterpoise impedance results in the instantaneous tower voltage. The tower current is assumed to have a current peak of varying fronts and a tail of ten microseconds to half value on lines equipped with ground wires. The length of the tail of the tower-current wave depends essentially on the length of the span and the value of ground resistances of the towers. There is some evidence that the current peaks in stricken transmission-line towers are substantially of this order.

Figure 2 shows the crest values of tower voltages thus calculated as functions of tower-current wave front (solid lines) and the expected arc-over voltage of the insulator strings in use on the 66-kv lines described in the paper (635-kv negative 1.5×40 wave). The arc-over voltage curve is cor-

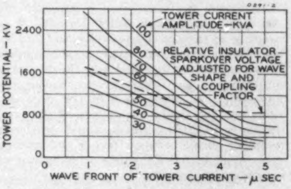

Figure 2. Maximum tower potential as function of wave front and amplitude of tower current for two 200-foot counterpoises connected to tower

D-c resistance of counterpoise—six ohms

rected for the type of voltage wave and the coupling factor given in the paper. Four parallel counterpoises of 200-foot length are used with combined d-c resistance of six ohms. The effective impedance used is the impedance of one counterpoise divided by four. This is not strictly true because of proximity effects between counterpoise wires in the same span, but suffices for the purpose of this example.

Figure 3 of this discussion shows a similar calculation for two 200-foot counterpoise wires with assumed d-c resistance of six ohms. Curves of this type show that the amplitude of the tower current, which will not cause flashover, greatly depends on the current front, as well as on the number of counterpoises installed. The tolerable current for a four-counterpoise ground is of the order of 1.6 times as great as for the two-counterpoise ground, even though the d-c resistance of both systems is equal. If, therefore, the tower current causing flashover on a two-counterpoise tower is 60,000 amperes, this would indicate that the current front of the tower current was of the order of not more than three microseconds. On the other hand, if the current was 120,000 amperes, the current front would have to be of the order of not more than 4.5 microseconds. Similarly, on a four-counterpoise tower, 80,000 amperes require a tower-current front of less than one microsecond and 120,000 amperes a front less than 3.5 microseconds. On this basis, a 60,000-ampere tower current should not cause sparkover on a tower grounded through four counterpoises.

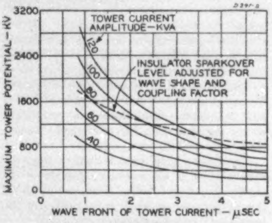

Figure 3. Maximum tower potential as function of wave front and amplitude of tower current for four 200-foot counterpoises connected to tower

D-c resistance of counterpoise—six ohms

It would be interesting if the authors could give in greater detail the conditions existing at towers of the 66-kv and 132-kv lines where sparkover occurred, the number of counterpoises installed and the probable amplitude of the tower current causing the flashover. A comparison of the data obtained in the field with curves of the type shown in Figures 2 and 3, would provide a valuable check between theory, laboratory tests, and actual lightning performance of counterpoise systems.

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1b. COUNTERPOISE TESTS AT TRAFFORD, **C. L. Fortescue, F. D. Fielder.** AIEE TRANSACTIONS, volume 53, 1934, June section, pages 1116–23.

2. Discussion by **J. H. Hagenguth** of references 1a and 1b. AIEE TRANSACTIONS, volume 54, 1935, February section, pages 220–3.

F. O. Wollaston (Commonwealth Edison Company, Chicago, Ill.): Much of the information in this paper was made available to Commonwealth Edison Company by the authors and their associates for use in designing the counterpoise installation on the 220-kv Powerton–Crawford line. This information proved very helpful in the preliminary layout of our counterpoise system.

In the actual installation of counterpoises, which were of the discontinuous type, no practical means of predetermining counterpoise requirements was found. The procedure actually followed was to install successively wires up to 300 feet long, starting each wire at the tower. The combined resistance of tower footing and counterpoise wire was measured after each 100 feet of wire had been buried, and installation continued until the desired ohmic resistance was obtained.

Footing resistances were measured in all cases by the probe method, employing a Megger-type ground tester, and auxiliary current, and potential probes. While this method is satisfactory for determining the resistance of tower footings and driven grounds, it must be used with considerable care for counterpoise systems; otherwise some uncertainty will always exist as to the true ground resistance. The auxiliary grounds must be located far enough from the ground under test to eliminate errors caused by the large area covered by the grounding system. It will generally be found necessary to investigate the effect of varying the spacings of the auxiliary probes, and to determine the minimum distance at which accurate readings are obtained. For tower lines with extensive counterpoise system, the necessary distance to the current probe may be several hundred feet.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): An important characteristic of grounds is the lowering of the impulse resistance with current. This is particularly the case for grounds driven in high-resistance soil.[1]

Other important factors connected with the problem of grounds are the effect of the discharge-circuit or lead inductance, and the conditions in the earth. Conclusion 1 in the paper by Hansson and Waldorf and the field experience reported previously[2] focus attention on the inductance drop and its importance. The lead or tower inductance becomes particularly significant when steep-front currents of high magnitude are discharged to earth. For example: analysis shows that for a 2x40-microsecond, 40,000-ampere current the voltage developed in a 50-foot down lead or a 100-foot tower may well attain several times the ground-resistance drop (five-ohm resistance ground). This inductance drop is practically over in two microseconds.

Besides the lead or tower inductance, appreciable inductance may be present also in the ground proper, as is the case for grounds driven deep into the earth, or for a counterpoise spread over a wide area. Consider, for instance, a five-ohm 100-foot driven ground or counterpoise which taps its low resistance largely at the further end from the tower. The total inductance of the rod or conductor is in the order of 50 microhenrys, and, accordingly, this ground would have a time constant of about $L/R = 50/5 = 10$ microseconds. A current rising to crest in a time shorter than this, naturally builds up a voltage across the ground which is the sum of the two drops, $L(di/dt)+Ri$. For currents rising rapidly to high crest values, the initial voltage developed across the ground is largely the inductance drop, and this can reach magnitudes considerably greater than the resistance drop corresponding to crest current. If the resistance is distributed along the length of the conductor or rod, the relationship of the two drops is more complex than previously stated—only a part of the total inductance of the conductor or rod is effective.

The foregoing are limiting factors. They do not, however, detract from the fact that deep grounds or counterpoise grounds, as the case and circumstances require, provide an easier path to the current discharge to earth.

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2. EXPERIENCE WITH PREVENTIVE LIGHTNING PROTECTION ON TRANSMISSION LINES, **S. K. Waldorf.** AIEE TRANSACTIONS, volume 60, 1941, June section, pages 249–54.

S. K. Waldorf and **E. Hansson:** It is not surprising that the performance of the transmission line described by Mr. Tervan was not improved by the addition of the counterpoise. The installation was made along only about three per cent of the line, and apparently the footing resistances of most of the towers in the line exceeded by a good deal the optimum footing resistances recommended in the summary of the paper under discussion. Undoubtedly Mr. Terven's analysis that the amount of counterpoise installed was not adequate is the fundamental reason for the lack of improvement in the line's performance.

It is interesting to note that Mr. Wollaston has also found field measurements as the only practical guide for counterpoise installation. Perhaps there are some localities where counterpoise characteristics can be satisfactorily predetermined, but Mr. Wollaston's experience around Chicago and that reported in the paper indicate that there are at least two widely separated territories where individual field testing of counterpoise is required during installation. Mr. Wollaston's comments on the spacing of auxiliary probes for ground-resistance measurements is welcome confirmation of the experience summarized in the first paragraph of section VI of the paper.

Professor Dwight suggests that the ground resistance of a counterpoise system can be calculated in the field and that the proper amount of conductor can be predetermined for a given location. This can occasionally be done, but field experience in the territory covered by the paper and that cited by Mr. Wollaston show that this is usually impossible. Changes in soil resistivity are frequently so large and abrupt that the resistance of 100 feet of wire is no sure criterion of the effect of the next 100 feet installed. Usually such changes occur most often when the counterpoise system passes from a wooded section into a cultivated field, or vice versa. A method of designing counterpoise systems was tried in which the required amount of conductor was calculated from the ground resistance of tower foundations, tower-foundation resistances themselves being a measure of soil resistivity. The field results differed so frequently and so widely from those predicted by calculation that the method was abandoned in favor of the cut-and-try or install-and-measure method.

The accompanying Table IV of the discussion complies with Mr. Hagenguth's request for more complete details about towers at which flashovers have occurred and at which counterpoise was installed.

Table IV. Flashovers on Transmission Towers Provided With Counterpoise

Line and Tower	Tower Current in Amperes	Ground Resistance of Counterpoise in Ohms	Tower Potential (I×R)	Type of Counterpoise Installed
Holtwood–York Tower 63 (66 kv)	58,400	13.8	805 kv	Two 1,075-ft conductors west to next tower, two 425-ft conductors east to next tower
Holtwood–York Tower 145 (66 kv)	60,000	19	1,140 kv	Two 500-ft conductors west to next tower, two 425-ft conductors east to next tower
Holtwood–Coatesville Tower 40 (66 kv)	64,400	17.2	1,110 kv	Three 400-ft conductors west, four 600-ft conductors east to next tower
Holtwood–Coatesville Tower 97 (66 kv)	60,200	5.4	325 kv	Two 750-ft conductors west to next tower, three 400-ft conductors east
Safe Harbor–Perryville Tower 15 (132 kv)	52,600	12.2	640 kv	Four 300-ft conductors north, four 825-ft conductors south to next tower

The Holtwood–York 66-kv line with a coupling factor of 0.35 gives an insulating level of about 980 kv. On the Holtwood–Coatesville 66-kv line the corresponding values are about 0.25 and 850 kv, and on the Safe Harbor–Perryville 132-kv line they are about 0.35 and 1,550 kv. The counterpoise systems at the towers of Table IV are all arranged in simple patterns such as shown in Figure 2 of the paper. The tower currents were in every case either the sum of currents measured in the four corner legs of the tower or the current measured in one corner leg multiplied by four.

Mr. Bellaschi's analysis of the behavior of counterpoise and long ground rods helps one to understand the phenomena involved when flashovers occur with low measured ground resistances.

Effect of Lightning on Thin Metal Surfaces

Discussion and authors' closure of paper 42-104 by K. B. McEachron and J. H. Hagenguth, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 559–64.

M. Newman (University of Minnesota, Minneapolis, Minn.): The paper by K. B. McEachron and J. H. Hagenguth presents several very interesting cases of lightning to spheres on top of radio towers, where many of the strokes went to the lower half of the sphere with the top of the sphere apparently shielded to some extent. It would seem that an explanation of this effect could be advanced on the basis of relative gradient distribution about the sphere portion and the supporting shank. Simple approximate gradient calculations indicate that the gradient about a small radius shank could easily exceed the gradient about the much larger radius sphere itself, especially when the opposite electrode, in this case the cloud, is far removed so as to contribute little relative increase of gradient at the top of the sphere. The writer recalls a photograph in a British publication which shows the case of a 60-cycle discharge which took place around the spheres when they were widely separated, and, while no explanation was given with the photograph, it would seem that the considerations submitted by the writer of this discussion of relative gradients about the sphere and support would fit both the 60-cycle case referred to and the case of the long-duration lightning discharges occurring to the lower portions of the sphere.

It would be of interest to have more complete information on the details of the support and whether any pits were visible on the support itself. It would seem probable that many of the streamers would extend from the supporting shank slightly below the shielding zone of the sphere with a possibility of the completed discharge path shifting to the shorter paths to the sphere surface.

McEachron and Hagenguth are to be greatly complimented on presenting in their paper a great deal of new interesting data which lead to new information. It is hoped the new data will be clarified further in this and other discussions. The points in the present discussion are submitted, because they have a bearing on questions of shielding theory and also on the question of whether the discharges necessarily originated from the sphere.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): I agree with the authors that the fusion found on metallic objects hit by lightning is a source of useful information. For instance, initial information of this character from electric conductors provided a valuable guide during the early laboratory research on long-duration low-current components of lightning strokes, so that important developments on protective devices and other electrical apparatus were well under way in 1938.

From the data discussed in a paper on this subject, which I presented to the Institute a year ago (reference 2 of this paper), the mean value of the charge in lightning strokes to electric-power lines, lightning rods, and to other objects near the surface of the earth was estimated to be between 15 and 30 coulombs. The maximum charge observed was 200 coulombs, but there is evidence from the high volume of fusion that this charge might reach 300 coulombs in rare cases. Although the data presented by the authors may differ somewhat from that obtained by the writer in regard to the methods of test and of analysis, it appears to me that the results seem to be in fairly close agreement.

In addition to their basic interest, these investigations are of direct practical value as pointed out by the authors in determining the minimum thickness of metal for containers of flammable or explosive materials. The question has been asked, "What minimum-size conductor can be used on overhead lines that will not be burned in two by lightning?" According to my data for copper, a number 4 conductor should be satisfactory in most cases. A few cases of larger conductors having been sundered by lightning have been reported, but the evidence is not entirely conclusive. In addition to the heating effect of the stroke, consideration must be given to the mechanical forces exerted on the conductor by the action of the high-current component.

The fusion on metals and the various effects associated with the high-current component provide about the only clues that can be found at the scene to assess the nature and severity of the lightning stroke and to reconstruct the probable or true circumstances in a particular case.

K. B. McEachron and J. H. Hagenguth: With regard to Mr. Newman's question concerning pits on the sphere supports, no information is available for the *WSM* sphere. We have recieved another similar sphere from *WLW*, which had been mounted 828 feet above ground for approximately seven years, with a five-inch-long portion of the shank. The sphere had a 14-inch outside diameter and 23-mil thickness of copper. The inside diameter of the shank was $1^{9}/_{16}$ inch and the outside $1^{7}/_{8}$ inch, with a wall thickness of the shank of $^{5}/_{32}$ inch. There are 122 pits on the shank. The distribution of these pits along the shank is of interest, being 28, 41, 36, 14, 3 pits per one-inch band, starting from the sphere. This would indicate that the number of pits at distances of six inches and greater from the sphere should be negligible. While the theory of gradient distribution is attractive, it is very difficult to state whether the shank is hit and the arc travels to the spheres or whether the sphere is hit and the arc moves down the shank. Because the pits on the shank are numerous only close to the sphere, it should be expected that the sphere would shield that portion of the shank and the stroke channel moves down toward the shank.

The distribution of the holes shows a similar concentration in the center belt as for the *WSM* sphere, about 52 per cent of the holes being found in 42 per cent of the six-inch-wide zone at the equator. Clerk Maxwell shows the lines of force (Figure III, article 120)[1] caused by a charged point in a uniform field. If we use one of the nearer equipotential surfaces as a metallic surface, we obtain practically a sphere. The gradients there would correspond to the location of the hole. The shank, of course, should upset this field. The rapid decrease in pits on the shank away from the sphere, as well as the distribution of pits and holes in the center belt of the sphere, would indicate that the gradients at the sphere are substantially the same with shank as shown by Maxwell without the shank.

There were a total of 695 pits on the sphere and the shank, 27 holes in the upper half, and 29 holes in the lower half. The largest hole indicated 200 coulombs and was located in the lower half. The expectancy curve checks very closely the curve obtained from the *WSM* sphere, 50 per cent of the holes indicating about 20 coulombs.

Information such as obtained by holes burned in metal and fusion of conductors is, of course, only a very rough indication of the total charge involved. The current values and duration cannot be obtained by such methods. Only the oscillograph or other devices which can measure both time and current amplitude can give accurate results. Oscillographic evidence of the long-duration discharges were first obtained in 1937 at the Empire State Building and reported in 1939 by McEachron.[2] Evidence of fusion in the field is valuable, however, because it shows that long-duration discharges exist not only on tall structures such as the Empire State Building but also on distribution circuits, dwellings, and other structures of relatively low elevation above the earth's surface. There is no reason why results of the tests presented in the paper should not check with Bellaschi's findings on fusion of lightning rods. Combination of such data will result in a better quantitative understanding of the long-duration discharges.

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2. LIGHTNING TO THE EMPIRE STATE BUILDING, **K. B. McEachron.** *Journal* of the Franklin Institute, volume 227, February 1939.

Transient Recovery Voltages and Circuit-Breaker Performance

Discussion and author's closure of paper 42-120 by R. C. Van Sickle, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 804–13.

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): Circuit breakers used as switching devices for equipment such as motors, capacitors, line-charging reactors, and synchronous-condenser starting present one of the most difficult application problems, and breaker performance for these applications has frequently not been satisfactory. This is evidenced by excessive contact burning and distress during operation, both of which are an indication of improper co-ordination between rate of transient recovery voltage and the rate of recovery of dielectric strength between the breaker contacts. It frequently happens that these breakers are nonautomatic in their operation or that the short-circuit duty is quite low, so that interrupting rating is not a suitable criterion for the selection of a breaker.

The results of the tests presented in this paper and also the data presented in the companion papers indicate that, even though the rate of transient recovery voltage may be relatively high, it is possible to select a type of circuit-interrupting device which will give trouble-free performance at the relatively low values of current which the breaker is required to interrupt in these applications.

In the past it has been the practice to apply a breaker having a higher interrupting capacity as a remedial measure where such troubles have been encountered. This method has met with some measure of success, but, judging from the results presented in this paper, it appears that a far better approach in making these applications will consist in the proper selection of the type of interrupting device used rather than in simply providing a larger breaker.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The author shows that the extremes of transient recovery voltages are higher than had been anticipated, but that generally the values actually encountered in practice are much lower. Companion papers have shown that but few circuit breakers have failed in service caused by high transient recovery voltages; yet many circuit breakers now in service were designed before complete data were available on these phenomena.

The writer of this discussion would therefore like to ask whether the present switchgear may not be overdesigned in this respect, possibly at the expense of other desirable characteristics, and whether worthwhile economies or improvements of other features might not be possible by taking into account the lower recovery rates usually experienced, even though such a procedure might require measures to limit extremes in certain special applications.

Raymond C. R. Schulze (Public Service Electric and Gas Company, Newark, N. J.): Published data in the past have indicated that the recovery rate had a definite effect on the interrupting capacity, but there have been no co-ordinated data or series of tests on this problem. This paper by Mr. Van Sickle supplies some much needed information on the subject.

When I was employed by Public Service Electric and Gas Company my first job was to calculate short-circuit currents and to compare circuit-breaker duties with their ratings. I have had a hand in this job ever since. About the time that we appeared to be up-to-date, something was added to the system, or the manufacturer derated his breakers, and we had to start over again. Sometimes there are too many circuit breakers to be rebuilt or replaced for the money available in any one year, and so it becomes necessary to pick out the worst ones. This is done by comparing the short-circuit amperes, the probability of operation, and the experience on the various breakers. The experience consisted of the recovery-voltage rate and certain miscellaneous factors; before the appearance of Mr. Van Sickle's paper, it was felt that, if the time were available, the recovery voltage rates for the various breakers could be calculated, and then this factor could be separated out of the experience. However, this paper seems to indicate that for oil circuit breakers the arcing time is substantially independent of the recovery voltage rate. (This is above some critical value of recovery rate which is relatively too low to be troublesome.) This is indicated by various figures and statements in the paper. If true, this is fine, because if the arcing time is not appreciably affected by the recovery voltage rate, then there should be no real need for calculating this factor. This should save a lot of work.

On page eleven of the paper, the statement is made that "Published data has frequently shown that breakers with self-generated deionizing action have shorter arcing times at high currents than at low currents." We have had some experience to substantiate this statement. One manufacturer tested one breaker, of which we have a number, and which we felt was rated too conservatively. The rating we finally adopted for this one breaker is 140 per cent of its name plate value, although this particular breaker actually withstood up to 280 per cent of its name plate rating. It has been our practice to replace these breakers when the duties exceeded 140 per cent (when some other work was being done on equipment associated with it). In view of the statement in the paper, and the fact that the breaker actually took more than our rating, perhaps we have spent a lot of money unnecessarily.

Another point in Mr. Van Sickle's paper interests me. Various figures concerning oil breakers show that the arcing time is relatively constant with an increase in either short-circuit current or recovery voltage. However, the arcing time in a compressed-air breaker does increase as the recovery voltage rate increases. I am wondering if this might be an argument for continuing to use oil breakers rather than the compressed-air type.

Considering the data which Mr. Van Sickle must have on the varying rates of decay of current at various natural frequencies, and the value of a curve of decrement factor versus natural frequency for use in future recovery voltage calculations, I am wondering if Mr. Van Sickle could develop such a curve for the use of the industry.

David C. Prince: For discussion, see page 1016.

R. L. Webb (Consolidated Edison Company of New York, Inc., New York, N. Y.): All of the papers just presented on the subject of transient recovery voltages as applied to power switchgear should prove informative and useful to both users and manufacturers of circuit breakers.

The paper giving a "Practical Calculation of Circuit Transient Recovery Voltages" provides a simplified method of calculation that can be used with reasonable accuracy by engineers not expert in this particular field. The method was used by us in assisting with the Association of Edison Illuminating Companies survey, and it was found relatively easy to follow. The data furnished on circuit constants are essential.

The paper on "Transient Recovery-Voltage Characteristics of Electric-Power Systems" shows that recovery rates above 5,000 volts per microseconds may be expected on only a small percentage of all breakers. We do not know yet whether further developments in some breakers may be proved essential for satisfactory performance at recovery rates approximating 10,000 volts per microseconds, but utility engineers can give some thought to means for keeping the recovery rates low.

High recovery rates are usually associated with a circuit of very low capacitance connected between a breaker terminal and a lumped reactance, such as a reactor or a transformer. On one such circuit of our 27-kv system the recovery rate was calculated to be 8,600 volts per microseconds. In experimenting with the calculations we found that this rate could be reduced to less than 5,000 volts per microseconds by connecting a five-foot length of lead-covered cable in series with, or as a stub to, the low capacitance part of the circuit. In this case the cable added about 750 micromicrofarads to the capacitance of the connection. Some such correction might be found for any circuit.

It appears, therefore, that the problem of relieving stresses on switchgear resulting from high recovery voltage rates rests with the user as well as with the manufacturer. The user can lay out his connections initially, in many cases, so as to avoid the type of circuit which inherently gives high recovery rates.

It is encouraging to see the extent to which new testing facilities for high voltage-recovery rates have already been used to check the performance of both oil and air circuit breakers. The paper "Transient Recovery Voltages and Circuit-Breaker Performance" describes laboratory tests at the highest recovery rates used so far in test circuits. The limits reached in voltage-recovery rate seem satisfactorily high for proving breaker performance, in view of the upper values found in the AEIC survey. The tests would be more conclusive, however, if high current at rated voltage could be interrupted with associated high recovery voltages applied to the breaker terminals on the same test.

C. Concordia and **H. A. Peterson** (General Electric Company, Schenectady, N. Y.): These papers on transient recovery voltages present a clear picture of the recovery rates which may be encountered in actual circuits, of methods of calculating them, and of the effect of these recovery voltages on circuit-breaker performance for the simplest form of test circuit. However, a review of actual circuit-breaker performance indicates that factors other than the rate of rise of recovery voltage may, in particular instances, have a greater effect on breaker performance; that is, the rate may be low and yet the interruption difficult.

The conclusion brought to light by several recent analytical studies is that the configuration of circuits containing appreciable amounts of capacitance may make them much more severe than the simple circuit considered as the laboratory test. For example, high reactance grounding[1–3] may reduce the recovery rate and at the same time increase the severity. Again, interruption of line charging current has a very low associated recovery voltage but is known to be a severe duty.[4]

The points above are worthy of emphasis, because they are not merely special cases. Rather, one or more phases of a breaker may be required to interrupt a small current with a slow recovery voltage rate since unbalanced faults are more prevalent than three-phase faults. This small current may be either capacitive in nature (similar to line charging current) or reactive, depending upon the assumed condition, phase-clearing sequence, and system grounding impedance. Consequently the foregoing points are essential factors in the development and in evaluating the performance of any breaker.

References

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3. Critical Analysis of Rotating-Machine Grounding Practice, H. A. Peterson. *General Electric Review*, April 1942, pages 215–20.

4. Circuit-Breaker Recovery Voltages, R. H. Park, W. F. Skeats. AIEE Transactions, volume 50, 1931, March section, page 212.

A. J. Krupy (Commonwealth Edison Company, Chicago, Ill.): As a general comment upon the three papers relating to the system recovery voltages and their effect on the performance of the circuit breakers, I would like to stress a point which was briefly mentioned in the Adams–St. Clair paper and also give some data on the service experience with the breakers which may be of interest to MacNeill and Van Sickle and to others:

When the voltage-recovery calculations were completed and the results analyzed, it was found that a fairly large percentage of the system breakers were subjected to a high recovery voltage. From this, it would appear that considerable trouble should be experienced with the circuit breakers operating under such voltage-recovery conditions. However, in case of our system, a careful check of the service performance of breakers during the period of some 20 years showed that there was not a single case of breaker failure or even any serious distress which could be definitely traced to a high rate of voltage recovery. Such breaker failures as did occur were invariably confined to the breakers with the previously known inadequate interrupting capacity, or to those which failed because of some other clearly established reasons.

This leads us to the conclusion that a substantial well-designed breaker, which has an ample thermal capacity and a sufficient interrupting duty for a given location, has given us a remarkably satisfactory service even at the locations which in calculations gave rather high recovery voltages.

The above service record may be explained in several ways:

1. The calculations were made for the most pessimistic system conditions which exist only a small fraction of the total service time, and, therefore, the calculated results are unduly pessimistic.

2. A three-phase ungrounded fault, which was assumed in calculations, is a very rare occurrence as compared with the variety of other faults experienced on the system.

3. The breaker itself reduces the voltage-recovery rate to an appreciable extent especially for the high values of current.

4. A breaker which was designed to obtain high interrupting capacity inherently possesses a sufficiently good voltage-recovery characteristic.

5. During the past 20 years, our system has been persistently lucky.

It is most likely, of course, that not only one but several of the above factors were responsible for the observed service record. This does not mean, however, that the work and the tedious calculations which have been made in connection with the survey are of no practical value. On the contrary, in case of our system, for example, it was plainly demonstrated that only a very minor change in the design of a switch house, or a minor change in the connection between the reactor and the breaker, can reduce the initial rate of voltage recovery to a fraction of its original value. Thus, we have found an easy and simple way to control to some extent the voltage-recovery rate at a given location, should conditions arise which would warrant it.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The group of three papers on transient recovery voltages form a symposium covering system conditions and circuit-breaker design aspects. This is a valuable contribution and should correct certain wrong impressions which have grown up over a period of years, and which have been associated with the inadequate performance of early switchgear for difficult restored voltage conditions.

The results of the Association of Edison Illuminating Companies study and the work done by manufacturers in their laboratories, as shown by the Van Sickle paper, may be summarized as follows:

1. Extremely high rates of recovery voltage are rather unusual under normal operating conditions.

2. Modern circuit interrupters are not sensitive to recovery voltage rate as were earlier devices. In fact the average modern interrupter will handle without distress, a broad band of recovery rates.

3. The occasional extremely high-speed rate of recovery does not affect a large system area, and, if necessary, means of slowing down the recovery rate are generally available.

The modern oil circuit breaker fortunately handles widely differing rates of recovery without its performance being affected greatly. This, I believe, is due to the nature of the dielectric in the arc stream during interruption. It cannot be assumed, however, that other forms of interrupter will necessarily have the same latitude in handling recovery voltages. Radically different forms of interrupter will need to be verified in this important respect. In general, however, laboratory testing equipments present restoration rates higher than average operating conditions. With the knowledge now available on system conditions, design engineers should be able to provide adequately against extended arcing times caused by any reasonable rate of voltage recovery.

R. C. Van Sickle: The discussions of these papers have brought out a number of important points which arise in interpreting the data for circuit-breaker application.

The scope of the data needs emphasis. The tests were made to determine the effect of varying the transient recovery voltage while the current interrupted and the normal frequency recovery voltage were held constant. To obtain the high natural frequencies associated with the most severe transient recovery voltages reactors were placed in the test cells adjacent to the breaker. They limited the current which could be used in this type of testing. These tests fulfilled their purpose by demonstrating the characteristics of the transient recovery voltages which are found with the maximum arcing times. These characteristics were found on the normal testing circuits at the highest currents.

The data demonstrated that at a given voltage and current, increasing the severity of the transient recovery voltage increased the arcing time up to a limit and that further increase in the severity did not further increase the arcing time. This limit was reached well below the values obtained on normal testing circuits.

To fully demonstrate a breaker for a given voltage and current it is necessary to test it with a transient recovery voltage which produces the maximum arcing. A test or service on a circuit which produces less than the maximum arcing can demonstrate the breaker for that service, but the breaker might fail completely if applied on a more severe circuit.

A circuit breaker passing the high-power laboratory test should perform as well or better in service. It does not follow that the breakers are designed with too high a factor of safety for the majority of applications. As shown by the data submitted in this paper, the maximum arcing time of the breakers tested was reached at relatively low natural frequencies of the transient recovery voltage. The voltage-recovery rates corresponding to them can be obtained on over 70 per cent of the breaker locations of the 11,000 to 13,800 volts range as shown by Figure 10 of the paper by St. Clair and Adams. Of the remaining 30 per cent, part will be on circuits which can cause almost the maximum arcing time. A few will be on circuits where they can never be severely stressed. These few might be built less ruggedly but would be hazardous if moved to other circuits requiring the same interrupting capacity but having more severe transient recovery voltages. The few which could safely be applied for light duty only would hardly justify the development and maintenance of a second line of breakers. If other types of breakers are found to have their performance more dependent on the transient recovery voltage, this conclusion might not hold for them.

The data of the survey and the breakers' performances indicate that the majority of the circuit breakers can be subjected in service to conditions which produce their maximum arcing times. The normal testing circuits of the high-power laboratory have fortunately been sufficiently severe to require breaker designs adequate for any service condition.

The arcing time of the compressed-air breakers tested varied less with transient recovery voltage than did the arcing times of the oil breaker. Figure 4 shows that the arcing time of the plain-break oil circuit breaker varied from 0.5 to 3.8 cycles. Figure 9 shows that the arcing time of the de-ion grid breaker varied from 0.3 to 2.1 cycles. Figure 14 for the 1,500,000-kva compressed-air circuit showed no significant difference in the arcing time at 8,000 amperes and an average increase about three-fourths cycle at 36,000 amperes. The cathode-ray oscillograms of the tests at higher current indicated that the maximum arcing time had been reached. As stated in the text, varying the transient recovery voltage rate on the 2,500,000-kva breaker from 300 to 13,600 volts per microsecond at 8,000 amperes 13,200 volts varied the minimum and maximum arcing times by only one or two tenths of a cycle.

The rate of decay of the current in the transients during these tests appears to be approximately proportional to frequency and corresponds closely with the factor used in the survey. Using capacitors for loading the circuit decreased the rate and using transformers for loading the circuit increased the rate, but the variations were relatively unimportant and demonstrated that the factor was satisfactory.

Transient Recovery-Voltage Characteristics of Electric-Power Systems

Discussion and author's closure of paper 42-130 by H. P. St. Clair and J. A. Adams, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 666–9.

W. F. Sims (Commonwealth Edison Company, Chicago, Ill.): As pointed out by Mr. St. Clair in the presentation of his paper, the papers on this subject are based on a survey of voltage-recovery rates on six large utility systems conducted by the committee on electric switching and switchgear, Association of Edison Illuminating Companies. Conditions leading up to this survey should be of interest in consideration of the information contained in these papers.

In 1935 a report on circuit-breaker testing was prepared by the oil circuit-breaker testing subcommittee of the committee on electric switching and switchgear, AEIC, in which it was pointed out that there was insufficient information available in reference to circuit-breaker transient recovery voltage, and it was recommended that a survey be made to obtain this information.

This subject was under consideration by the committee for a considerable time, and in 1939 steps were taken to make the survey, which was assigned to the oil circuit-breaker testing subcommittee under the chairmanship of Philip Sporn. During this period there was considerable discussion with the manufacturers' engineers, and it was brought out that information of this nature was quite important and would be of considerable value in working out of circuit-breaker designs.

As a result of these discussions six large member companies of the association, whose properties were representative of both the metropolitan type and long transmission type of systems, agreed to take part in the survey and to make the necessary calculations on their respective systems. The actual work was carried out by a working group of engineers, one from each of these companies, under the direction of H. P. St. Clair. As J. A. Adams had already done considerable work of this kind on the system of the Philadelphia Electric Company, arrangements were made to secure his services in standardizing the methods and co-ordinating the calculations made on the various systems in order that all work would be done on the same basis and that the data obtained would be comparable. Much valuable assistance was given to the working group by the engineers of the switchgear departments of the large manufacturers.

The results of this survey are described in detail in a report to the committee on electric switching and switchgear presented in the spring of this year. As the information developed appeared to be of considerable importance permission was given to the protective devices committee of the AIEE to arrange for the preparation and presentation of these papers at this session.

David C. Prince (General Electric Company, Schenectady, N. Y.): For many years it has been recognized that the rating of circuit breakers in volts and amperes is not complete. To a considerable extent, the severity of duty is a function of the recovery voltage rate at the point of application. Manufacturers have been handicapped by having to work very largely in the dark, from a recovery-rate point of view. That is, circuit breakers had to be designed and built capable of handling the maximum recovery rate which might be encountered, although the designers had no information whatsoever on what that recovery rate might be.

This uncertainty has now been dispelled once and for all by the studies on which St. Clair and Adams are reporting.

Prior to receiving this information, we have speculated with the possibility that low-recovery-rate circuit breakers might be generally applicable with high-duty circuit breakers for only a few isolated cases. The evidence in this paper is that a fairly large fraction of all circuit breakers may be exposed to high recovery rates. In a way, this is disappointing, since it closes the door on the low-duty breaker. On the other hand, from this time on, circuit-breaker designers will know the duty which their products must meet, and it is only reasonable to expect that with definite knowledge available, ways will be found to tailor the product exactly to the need and in that way reduce the cost to the consumer for a given service rendered.

The data presented by R. C. Van Sickle do not really negate this conclusion. The factors in circuit-breaker design which cause the circuit breaker in operating to alter the rate at which recovery voltage appears involve the dissipation of energy in the circuit breaker itself. In the cases mentioned by Mr. Van Sickle, the kilovolt-amperes interrupted have been small, and the presence or absence of auxiliary losses which altered the recovery rate consequently unimportant. Where higher interrupted capacities are involved, the conclusion might well be different, and it is to be hoped that with the splendid new high-capacity testing station available to him, Mr. Van Sickle will extend his work in that direction.

R. L. Webb: For discussion, see page 1014.

C. Concordia and **H. A. Peterson:** For discussion, see page 1015.

A. J. Krupy: For discussion, see page 1015.

J. B. MacNeill: For discussion, see page 1015.

J. A. Adams: Mr. Webb and Mr. Krupy mention one of the benefits of the work which has been done on recovery voltages: namely, to suggest to users of circuit breakers that the severity of the recovery voltage characteristics of circuits can be reduced by changes in station design. However, as a word of caution, it should be pointed out that the introduction of lead-covered cable, as mentioned by Mr. Webb, or of capacitors may introduce an additional hazard in some cases. This may be especially true in the case of a switch house where the breakers are located next to the bus with reactors on the line sides of the breakers. If the breakers have been applied on the basis of breaker duties determined for faults beyond the reactors, it is essential that the circuits between the breakers and reactors be made as reliable as possible. Additional capacitance inserted in this portion of the circuit should be obtained in as reliable a manner as possible.

In connection with the Association of Edison Illuminating Companies survey an attempt was made to co-ordinate recovery rates with breaker failures such as was done by Mr. Krupy. This would have given an indication of the effect of recovery rates on breaker operation. However, records in some cases were incomplete, and in others time did not permit the analysis to be made. It is reasonable to expect that in the future this factor will be more carefully studied for its effect on breaker operation.

Mr. Krupy and also Mr. MacNeill point out that actual fault conditions may not result in as severe recovery voltage characteristics as the calculations indicate. This is true, in some cases, because the system connections may not be the same as those for which the calculation was made, and in others, because the fault may not be a three-phase fault or may not be located at the station line terminal. However, the most severe recovery voltage conditions that can be obtained with operating connections should be recognized when applying a breaker. In the survey the calculations were based on connection which gave the most severe characteristics under normal or emergency conditions. In a great many cases these were the connections used for normal operation. Also the calculations were for the highest three-phase fault current that could be obtained for the connections used. In actual practice, the fault might only be phase-to-ground or phase-to-phase and might not be located at the line terminals, both of which factors would tend to reduce the severity of the recovery voltage characteristic.

Mr. Prince refers to the possibility of tailoring breakers to the need and thus reducing costs. It is sincerely hoped that the results of this survey will be of assistance in this respect.

Practical Calculation of Circuit Transient Recovery Voltages

Discussion and authors' closure of paper 42-131 by J. A. Adams, W. F. Skeats, R. C. Van Sickle, and T. G. A. Sillers, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 771–9.

G. W. Dunlap (General Electric Company, Schenectady, N. Y.): In the consideration of transient recovery voltages there are four general methods by which the form of such voltages may be obtained for a given circuit or system. These may be listed as:

1. Direct oscillographic measurement during actual fault interruption.
2. Mathematical analysis.
3. Electrical analysis of miniature circuit representing actual circuit.
4. Electrical analysis by measurements on actual circuit.

All of these methods have their limitations and advantages, and all have been used to obtain valuable information and must be considered as supplemental to each other. The authors have presented in their paper a new and simplified technique for the second method which should greatly increase its usefulness, and it is the purpose of this discussion to point out how the advantages of methods 3 and 4 may be readily made available to supplement calculations by the use of the simple device called the recovery voltage analyzer, which is described in the author's reference 10.

This recovery voltage analyzer may be used on either actual circuits or miniature representations thereof. It performs electrically the same trick of injecting a current into the circuit under study that is used mathematically as the basis for calculation of transient recovery voltages, and, in case of the analyzer, the answer appears on the screen of a cathode-ray tube as a stationary trace of the transient recovery voltage desired. In other words, with the analyzer connected to the circuit for which the curve of Figure 11 was calculated, the Figure 11 curve would appear on the cathode-ray tube to be observed, measured, or recorded photographically as desired.

As pointed out in the paper, it is extremely important that the capacitances associated with higher-frequency circuits be estimated accurately, and in such cases the analyzer may be used to advantage, not only to check calculations based on estimated capacitances with actual measurements of recovery transients, but to measure these capacitances as well. In addition, since the analyzer does not care how complicated a circuit may be, such measurements may be used to check the validity of the simplifying assumptions made for purposes of calculation. The analyzer has been used to make such measurements on a number of power systems. Wherever it is possible to de-energize a circuit for analyzer application the actual measurement is accomplished with such speed and facility that the time of calculation may be saved, and this has also been done in a number of cases.

This feature of speed brings up the possibility of using the analyzer to eliminate the routine work of calculating points on the recovery transient curve. The simplified lumped constant circuit of Figure 10, for example, could be connected to the analyzer, and by the throwing of a switch the recovery transient could be made to appear in its complete form on the cathode-ray tube, and the envelope or any portion of the wave desired could be measured. If there were any doubt as to the validity of estimated capacitances, the values could be varied over any desired range, and the effect on the recovery transient would be immediately apparent.

Still another use of the analyzer is in determining the characteristics of individual circuit elements such as generators, transformers, reactors, and so forth. In this way values of natural frequency, equivalent inductance, and capacitance may readily be obtained for subsequent use in calculation of a system as a whole.

In conclusion, it would appear that the usefulness of the method described by the authors may be further enhanced by including the feature of electrical analysis as embodied in the recovery voltage analyzer.

W. F. Sims: For discussion, see page 1016.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors deserve commendation for the methods of recovery voltage calculation and the data on capacitance that they have presented.

For network problems of the complexity treated in the paper the solutions are not rigorous, and only reasonable engineering accuracy is intended. Bold approximations have been used by the authors to reduce the networks to a more manageable form, for which rigorous and practical solutions are available. These approximations will give good accuracy when skillfully used. Nevertheless, a word of caution is desirable for the benefit of those who are unfamiliar with such calculations but have occasion to use the methods presented in the paper; unless

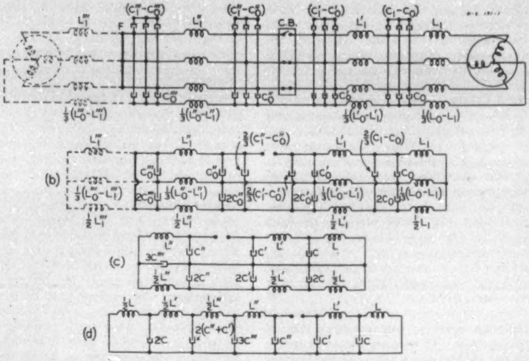

Figure 1. Equivalent circuits for recovery voltage calculations

Symmetrical-component presentation

Network simplifications

the approximations are permissible for the particular case, the accuracy will be considerably reduced.

For setting up the network for recovery voltage calculations, there are several possible methods, including the single-phase method used by the authors. We, however, prefer to start with a netowrk based on symmetrical-component methods, such as described by Evans and Monteith.[1] This is a general method for setting up networks and permits representation of circuit elements which may be unequal for the positive-, negative-, and zero-sequence circuits. This method is convenient because of the practice nowadays of calculating the constants of circuit elements by symmetrical components. The labor of the symmetrical component method is about the same as that of the single-phase method for the cases where the latter is applicable.

One method of using symmetrical components to set up the equivalent circuit for recovery voltage calculation is shown in Figure 1a of this discussion with the inductances and capacitances indicated by the usual notation. This network corresponds to the system of Figures 4 and 5 of the paper but is presented in a more general form which permits differences in the positive- and zero-sequence inductances and capacitances of the various circuit elements. In addition, there is provided a path for zero-sequence current through the feeder beyond the fault, as shown by the dotted line section of the figure. For the opening of the first pole of the circuit breaker for the case of a three-phase ungrounded fault, it is permissible to parallel the two remaining phases which reduces the network of Figure 1a to that of Figure 1b. If it is assumed that the positive- and zero-sequence inductances, and the positive- and zero-sequence capacitances of the various circuit elements are equal, and if the zero-sequence path through the feeder beyond the fault is neglected, the network of Figure 1b reduces to that of Figure 1c, in which the subscripts have been dropped for convenience. By simple rearrangement, the network of Figure 1c reduces to that of Figure 1d.

The network of Figure 1d may be simplified in various ways to reduce it to a more manageable form for calculation. The authors do this by:

1. Reducing the left-hand part to three meshes by combining capacitances $2C$ and $2(C''+C')$ (ignoring the latter) and combining the inductances $^1/_2L$ and $^1/_2L'$.

2. Reducing the right-hand part to one mesh by combining the capacitances C and C' (ignoring the latter) and combining the inductances L' and L.

The procedure just described reduces the network of Figure 1d to the form of Figure 9 of the paper. An alternative method of simplifying the network is to ignore the small capacitances $2(C''+C')$ in comparison with the adjacent large capacitances $2C$ and $3C'''$ and combining the inductances $^1/_2L'$ and $^1/_2L''$. If the inductances in the two right-hand meshes of Figure 1d are relatively large in comparison with the remaining inductances, it would be desirable to use a two-mesh solution for this part of the circuit instead of the single-mesh solution used in the paper.

In setting up equivalent networks for the solution of transient problems, it is preferable to start with accurate network representation for the various circuit elements and then with the network in view to simplify it by the usual methods, such as combining branches in series and in parallel or eliminating unimportant branches. This procedure is preferable to one in which certain branches are eliminated and other branches combined before the network has been set up.

REFERENCE

1. SYSTEM RECOVERY VOLTAGE DETERMINATION BY ANALYTICAL AND A-C CALCULATING-BOARD METHODS, R. D. Evans, A. C. Monteith. AIEE TRANSACTIONS, volume 56, 1937, June section, pages 695-705.

C. Concordia and **H. A. Peterson:** For discussion, see page 1015.

Raymond C. R. Schulze (Public Service Electric and Gas Company, Newark, N. J.): This paper is very interesting to me, since I was one of those people involved in the recovery voltage rate survey. When the survey was started, the working group was called together, and Mr. Adams outlined the method of calculation. The procedure is clear and easy to follow, except that there appear to be certain disadvantages to the application of Boehne's method for the calculation of the various natural frequencies. This method requires the combination of certain inductances and capacitances in order to reduce the number of oscillating circuits, and then it requires the use of certain very complex graphs which appear as Figures 20 and 21 of the paper.

The objection to the method is based on these thoughts:

1. The combination of certain inductances and capacitances results in a circuit with a natural frequency, which does not actually exist in the system; certain actual frequencies, therefore, do not appear in the calculations. This may result in an erroneous value of recovery voltage, and recovery voltage rate. Also, when combining inductances, one must be careful to maintain the true series circuit of inductances (reactances) from the generators to the fault, in order to maintain the proper voltages across each reactance of the system.

2. The graphs of Figures 20 and 21 are quite complex, and one must have blind faith in the accuracy of the graphs and in the factors which one reads from these graphs.

3. Furthermore, there is no way to check one's work when using this method, except to repeat the whole process (and perhaps the same mistakes).

Therefore, the method used in Public Service Electric and Gas Company was to continue with the diagram of Figure 6 and to resolve it into its oscillating circuits without any reduction in the number of circuits. This meant that there were as many oscillating circuits in the final diagram as there were in the original system diagram. This meant, in turn, that each natural frequency in the system was represented in the calculation. Trouble may be experienced because of a change in the natural frequency of a circuit, when the natural frequencies of two adjoining circuits have a ratio of less than two to one, but in these cases the correct natural frequencies can be obtained either by using Boehne's graphs for these particular cases (of which there were very few) or by computing these natural frequencies by the usual methods employed for coupled circuits. An excellent reference for a study of the behavior of oscillating circuits is the Bureau of Standards circular 74.

The method utilized in Public Service Electric and Gas Company should be especially useful after reading the conclusions shown in Mr. Van Sickle's paper.[1] Mr. Van Sickle shows that the various natural frequencies die out at varying rates, so that a truer calculation should be possible when each frequency is identified.

Also, the calculations seemed to indicate that the highest value of recovery rate was for a fault just beyond a feeder reactor, and this recovery rate was largely dependent on the natural frequency associated with this reactor. On this basis an equation of a half dozen terms was developed, which gave answers to within five per cent of those obtained by the long detailed process for faults just beyond the feeder reactor.

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As mentioned before, Mr. Van Sickle's work shows that the various frequencies die out at different rates. This indicates that the one uncertain factor in the method of calculation is the decrement factor to be used for each of the various natural frequencies. The accuracy of the calculation of recovery voltage could be improved, if a graph of natural frequency versus decrement factor could be obtained.

REFERENCE

1. TRANSIENT RECOVERY VOLTAGES AND CIRCUIT-BREAKER PERFORMANCE, R. C. Van Sickle. AIEE TRANSACTIONS, volume 61, 1942, November section, pages 804–13.

R. L. Webb: For discussion, see page 1014.

J. B. MacNeill: For discussion, see page 1015.

A. J. Krupy: For discussion, see page 1015.

J. A. Adams, W. F. Skeats, R. C. Van Sickle, and T. G. A. Sillers: The analyzer referred to by Mr. Dunlap should prove to be very helpful in analyses of this kind and would have been used more extensively in the Association of Edison Illuminating Companies survey, had it been available. It was used in at least one case to check part of a circuit which had been calculated, and the results were astonishingly close to the calculated values. However, other circuits which are more complex would probably show wider discrepancies, and in these cases the analyzer should be most helpful. It is usually not possible, however, to obtain the complete recovery voltage characteristic with an analyzer, as this would mean isolating a large portion of the power system, which is generally not permissible.

With respect to the accuracy of the procedure, it was pointed out in the paper that the recovery rate for breakers having the same voltage and current rating may vary over a range of almost 100 to 1 and, while quite different performance may be expected in some cases at opposite ends of this range, it is seldom that an important difference in performance will result from a change of even a good many per cent. The accuracy required in these calculations, therefore, is not of a very high order, and it is quite permissible to sacrifice accuracy to simplicity to a considerably greater extent than could be tolerated in many other fields. This has been done in many instances throughout the paper, as it was quite necessary in order to keep the job within the realm of economic possibility.

Mr. Evans' discussion echoes a warning also found in the paper to the effect that caution must be exercised in applying the methods of the paper where a high degree of accuracy is required.

With further reference to Mr. Evans' discussion, it may be pointed out that the phase-sequence circuit reduces at an early stage of simplification to the same thing as the circuit used by the authors, as may be seen by comparing Mr. Evans' Figure 1c with the authors' Figure 6. The high desirability of this simplification where accuracy requirements permit is indicated by the difference in complexity between Mr. Evans' Figure 1c and his Figure 1b.

The authors agree that it is preferable to start with accurate circuit representation and recommend that this be done to the extent compatible with the complexity of the job. A great many of the simplifying steps will depend upon the relative magnitude of the capacitances and inductances involved. This was the case with the steps between Figure 6 and Figure 8 of the paper, which, as Mr. Evans suggests, would not have been sound under different conditions.

No hard and fast rules can be set up for the simplification procedure, and a certain amount of judgment is necessary in deciding just what steps to take. This is illustrated by Mr. Schulze's discussion which points out that he made rather wide departures. It is probable that the inclusion of all circuits resulted in improved accuracy in some cases, while his distrust of the curves caused some loss in others.

The factors mentioned by Concordia and Peterson should of course be recognized in applying and designing circuit breakers. This paper covers only the recovery voltage part of the picture and makes no attempt to review these other factors.

A Compressed-Air Operating Mechanism for Oil Circuit Breakers

Discussion and author's closure of paper 42-114 by R. C. Cunningham and A. W. Hill, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 695–8.

H. E. Strang (General Electric Company, Philadelphia, Pa.): Development during the last few years on air-blast circuit breakers, in which contact movement is provided by pneumatic mechanisms, has built up a better understanding of the simplicity, sturdiness, and reliability of this type of operating power. It is quite natural that this has led to the development of such air mechanisms for the operation of oil breakers. Similar to others which have been described, the mechanism disclosed by Cunningham and Hill undoubtedly possesses many of these desirable characteristics.

Without belittling in any way the importance of this new type of operating mechanism, it must not be inferred that it alone made it possible to provide the industry with super-speed reclosing circuit breakers. The first mechanisms for such service, which were of the motor-operated type, were installed on 138-kv breakers in 1936 and have been in regular service ever since. Subsequently, over 50 more of this motor-driven cam type have been placed in service, and, as shown by reports to the Institute, have contributed their share in demonstrating the value of this kind of triple-pole, super-speed reclosing service on high-voltage lines.

Since super-speed reclosing of triple-pole high-voltage breakers was already an established procedure, it was only natural that pneumatic operators should be employed for this service. It has now been established that they perform this same function with ease and with the obvious advantages incident to stored energy and low battery drain. Starting with the first installation during the summer of 1940, well over 100 pneumatic mechanisms have already been placed in service on both new and modernized oil circuit breakers of all voltages from 34.5 kv to 230 kv.

In attempting to reconcile the relatively flat output characteristics of the simple air piston with the rising characteristics of force required to close a circuit breaker, Cunningham and Hill have resorted to a mechanically operated throttling valve which opens near midstroke to give the piston an additional boost. Under ideal conditions this should work satisfactorily, but at best it is another moving part which may require some adjustment, maintenance, and attention. A simple toggle built right into the mechanism between the driving piston and the output crank has been found by experience to serve as the means of matching these characteristics, and eliminates the need for throttling valves or other such critical moving parts.

H. W. Haberl (Montreal Light, Heat, and Power Co. Consolidated, Montreal, Que., Canada): If the control mechanism for oil circuit breakers as described in the above paper is to be used in freezing temperatures where large variations above and below freezing occur, it is suggested:

(*a*). That the inlet air to the compressor be passed over by some type of drying equipment; this could be in the form of a glass bottle in which colored activated alumina or silica gel were used as the dehydrated medium; in this way, the air in the storage tank would be reasonably dry.

(*b*). The mechanism's housing be lined with an insulating material, similar to (ten-test) and a thermostat and heater mounted inside the control cabinet which would maintain the control cabinet at or above freezing temperature.

The preceding suggestions would insure the air being free of moisture and consequently would prevent any of the small valves from freezing.

R. C. Cunningham and A. W. Hill: Mr. Strang has pointed out the relative effect of a throttle valve and a toggle, either of which modifies the flat characteristic of the simple air cylinder to match the typical breaker load. One reason for including a simple mechanically operated throttle valve is to make possible the application of this mechanism to existing breakers. In this way, the pull curve is made to follow closely that of the solenoid formerly used.

Another and more important reason for using the throttle valve with a slotted link is to provide controlled flow of air on ordinary closing, and full air flow when needed for rapid reclosure. A range of adjustment, both as to time of operation and amount of opening of the throttle valve, makes it suitable for use on any of several sizes of breaker.

It is obvious also that with the breaker starting from the full open-contact position, air is admitted above the piston when the cylinder volume is smallest, and full air pressure immediately acts to start movement. A restriction in the air line is permissible and desirable to prevent excessive speed later in the closing stroke. However, for fast reclosure, air is admitted while the piston is part way out in its stroke, so that the full effect of the supply pressure is not felt until after a time interval while air is being poured into this large cylinder volume. A large opening in the air line serves to gain time, so that the utmost over-all reclosing time can be secured.

The use of a drying agent, as suggested by Mr. Haberl, was considered in the early design stages but not adopted, as it would introduce another item requiring maintenance and possibly be the cause of trouble if essential to proper operation and not properly cared for. During freezing temperatures, there is so little water in the air supplied to the compressor that none will be deposited after the air passes the inlet valve. Heaters are supplied to maintain all parts within the housing at a temperature enough above the outside ambient to prevent trouble, although in extreme low outside temperatures it is not considered necessary to keep the parts above the freezing point.

In a test set up at the factory one of these mechanisms was operated at temperatures down to −20 degrees Fahrenheit. Even when air was drawn from outside the cold room, at a temperature of 50 to 60 degrees Fahrenheit, freezing of condensed moisture gave no trouble at any of the control valves. Similar performance is indicated by breakers in regular winter service.

Tests and Analysis of Circuit-Breaker Performance When Switching Large Capacitor Banks

Discussion and authors' closure of paper 42-119 by T. W. Schroeder, E. W. Boehne, and J. W. Butler, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 821–31.

R. M. Partington and **M. E. Scoville** (General Electric Company, Pittsfield, Mass.): The physical arrangement of a capacitor bank and circuit connections determines the inductance and, therefore, affects the magnitude of inrush currents with energizing the bank. Maximum inrush currents are obtained with switching equipments in compact physical arrangements. It may be of interest to compare the inrush currents calculated from physical dimensions to the results of the actual tests made by the authors. Figure 1 shows a compact equipment rated 2,520 kva. A number of such equipments may be grouped together with space to allow for ventilation and accessibility to form a larger bank. One or more of those equipments may be connected to the system through a suitable circuit breaker.

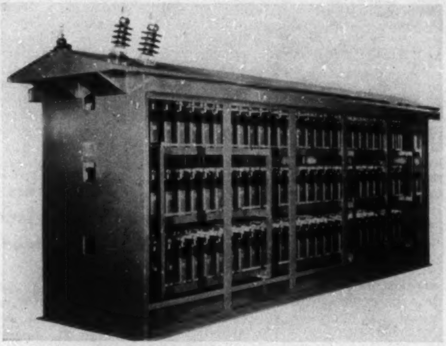

Figure 1. Outdoor large rack-type capacitor equipment, rated 2,520 kva, 13,800 volts, wye-ungrounded neutral, three-phase, 60 cycles

Assuming a typical arrangement for each of the three-phase kilovolt-ampere ratings, wye-connected, inductance and resistance values have been calculated, conservatively leaning toward the minimum practical values. The circuit used for calculation includes individual capacitor units, fuses, bus connections, cable between equipments, and connections up to line side of circuit breakers. The inrush currents were calculated from the relation

$$i\,(\text{maximum}) = \frac{\text{line volts} \times 1.41}{1.73} \times \sqrt{\frac{C}{L}}$$

neglecting resistance which has a negligible effect on the inrush current magnitude.

The inrush currents shown in the table are maximum with assumption of infinite bus at line side of circuit breaker terminals. Consequently the presence of other capacitor banks on the same infinite bus would not increase the values of maximum inrush current. Rarely, however, would these inrush currents be obtained in practice since there would be some system inductance ahead of the circuit breaker which would reduce the inrush current?

Table I

Rated 60-Cycle 3-Phase Line		Calculated Values Per Phase			Maximum Inrush Current (Amperes)
Kva	Voltage	Inductance (Microhenrys)	Capacitance (Microfarads)	Resistance (Ohms)	
2,520	4,160	8.17	387	0.0025	23,400
5,040	4,160	4.08	774	0.0012	46,700
10,080	4,160	3.05	1,548	0.0006	76,500
2,520	13,800	9.15	35.2	0.0046	22,100
5,040	13,800	4.46	70.4	0.0023	44,300
10,080	13,800	3.34	140.8	0.0012	73,000
5,400	25,000	11.27	23.0	0.0042	29,100
10,800	25,000	8.45	46.0	0.0022	47,800

Using the inductance and resistance values from the table, it can be shown that the transient inrush current will decrease to a negligible value in less than one cycle (60-cycle frequency). For example, consider the 10,080-kva bank on a 13,800-volt 60-cycle line. The transient current will decrease to one-half its maximum value in approximately one-quarter cycle (0.00416 second), or to one-tenth its maximum value in approximately three-fourths cycle (0.0125 second). The natural period of the transient would be approximately 7,300 cycles per second.

The above calculated data indicate maximum inrush currents of 76,500 peak amperes with a physical arrangement typical of a practical permanent installation except that an infinite bus is assumed at the circuit-breaker terminals. Actually, the additional inductance in 30 feet of line between the circuit-breaker terminals and an infinite bus, which is typical of a practical installation, will limit the peak currents to less than the values obtained in the tests recorded in the paper.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The transient-recovery-voltage problem for de-energizing capacitors is similar to that which arises from de-energizing transmission lines. The latter problem was investigated several years ago in the paper, "Power-System Transients Caused by Switching and Faults" by Evans, Monteith, and Witzke.[1] It is pertinent to compare the results of these two studies since part of the analytical and transient-calculator work of the present paper follows closely the procedure used in the earlier paper. In that paper a power system with typical sending and receiving stations and with two 100-mile transmission lines were used for the study of several types of switching transients. The case considered included the de-energizing of one line starting with one and two lines in service. The comparison of the recovery voltages obtained is given in the accompanying Table II.

The difference in voltage transients incident to de-energizing transmission lines between switching lines and de-energizing capacitors depends to some extent on the differences between lumped and distributed capacitances and the presence of line resistance. However, the more significant difference for many conditions of operation arises from the difference in the positive- and zero-sequence constants. Capacitor connections may be made so as to give widely different ratios of zero- to positive-sequence constants, whereas with transmission lines the ratio is fixed and for the case covered by the earlier paper was in the ratio of 1 to 1.35. This means that the voltage transients on grounded capacitors with neutrals grounded or interconnected should correspond approximately with the transients of switching lines connected to grounded systems.

It is believed that the differences in the results of tests for the comparable conditions from the two papers may be explained on the differences in the circuit constants.

Table II

Switching Conditions	Transient Voltages for De-energizing Operations Expressed as Ratio to Rms Voltage Line to Ground			
	Voltage to Ground		Switch Volts	
	Transmission Line*	Capacitor**	Transmission Line*	Capacitor**
Grounded neutral				
Transmission line				
Initially 1	2.7		3.6	
Initially 2	2.3		3.4	
Capacitor				
0.5 microfarad		3.0		4.0
0.5 microfarad with 0.2 microfarad unswitched		2.4		4.8
Interconnected capacitor neutral				
0.5 microfarad with 0.2 microfarad unswitched		3.3		4.3
Ungrounded neutral				
Transmission line				
Initially 2	2.6		5.0	
Initially 1	2.2		3.8	
Capacitor				
0.5 microfarad		6.0		6.6
0.5 microfarad with 0.2 microfarad unswitched		6.0		8.2

* Reference 1.

** From the authors' paper.

The earlier paper also gave data on voltage transients for two restrikes. Incidentally, it may be observed that these tests showed lower transient voltages for some value of neutral impedance than with solidly grounded systems.

The results obtained by the authors of the present paper are of interest and show the desirability of reducing transient voltages by grounding capacitor neutrals or by interconnecting them. There is also some advantage in switching capacitors in relatively small blocks.

Reference

1. Power-System Transients Caused by Switching and Faults, R. D. Evans, A. C. Monteith, R. L. Witzke. AIEE Transactions, volume 58, 1939, August section, pages 386–97.

T. W. Schroeder, E. W. Boehne, and J. W. Butler: Partington and Scoville have provided some very useful data for estimating inrush currents and their duration for typical large banks. It should be emphasized that the inrush or discharge currents will be reduced measurably by comparatively short lengths of conductor to the capacitor banks. It is felt that the maximum test value of 52,000 amperes is sufficient for most practical installations because

1. This is a greater equalizing current than that which will flow between two groups of two 10,080-kva 13.8-kv banks each, with an inductance of five microhenries between the groups. This value is about 44,600 amperes.

2. A bank of 10,080 kva which is shown to have higher inrush current than values reached in test (76,500 amperes maximum) could discharge such current through a breaker only in the case of a solid three-phase fault immediately on the line side of the breaker, which would be a rarity.

3. On energizing such a bank, very small values of system inductance will greatly reduce the possible inrush current.

120-Kv Compression-Type Cable

(42-133)

120-Kv High-Pressure Gas-Filled Cable

(42-135)

Low-, Medium-, and High-Pressure Gas-Filled Cable

(42-102)

Discussion and authors' closures of papers 42-133 and 42-135 by I. T. Faucett, L. I. Komives, H. W. Collins, and R. W. Atkinson, and paper 42-102 by G. B. Shanklin, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 652–7, 658–65, and October section, pages 719–26.

L. L. Phillips (General Electric Company, Pittsfield, Mass.): Some of the all-gas-filled terminals to which Mr. Shanklin refers now are in service at 15 kv. Similarly designed terminals for ratings 23 kv, 34.5 kv, and 46 kv, have been developed and are available for gas-filled cable installations pending. These gas-filled terminals utilize an ingenious method for controlling the electric stresses in the insulation at the termination of the cable-shield extension. The ordinary construction of termination regularly used in compound-filled and oil-filled terminals is unsatisfactory for gas-filled assemblies, because of the differences of voltage distribution resulting from the use of gas as a part of the dielectric.

Stress control is accomplished largely by a specially developed cloth having the capacity to divert a part of the charging current which tends to flow directly to the end of the cable-shield extension. The cloth is processed to have a controlled resistance to the flow of current. It is assembled in contact with the metal of the cable-shield extension to provide a partially conducting cylinder for approximately one inch beyond. The special cloth serves to distribute the charging current through a larger cross section of the insulation, thus preventing concentration at the immediate end of the cable-shield extension. Therefore, a part of the charging current flows from the terminal conductor through the insulation to the cylindrical surface formed by the applied cloth and thence to the cable-shield extension.

In addition to the "shielded" extension for stress control, wrappings of varnished cambric tape are applied to exclude filler gas—except in the thin films between tapes—from regions immediately surrounding the cylinder of special cloth. The total thickness of this applied tape varies according to the circuit voltage at which terminals will operate in service. Thus, any appreciable thickness of the filler gas is prevented from entering the dielectric circuit, except at suitable locations.

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): After seeing with his own eyes 29 inches of vacuum in an impregnated-paper cable line which operated at 24 kv successfully for the last 12 years in Detroit, the writer appreciates Mr. Shanklin's contribution to the industry more than ever before. If some means could be found which would enable the cable user to maintain positive (above atmospheric) pressure at all times in a solid-type cable, certainly more efficient operation could be expected than from the above described Detroit cable. As the service record of the solid-type cable is very good, the insulation thickness may be reduced, or the normal maximum operating temperature (cable loading) may be increased. This is essentially what the low-gas-pressure cable accomplished in addition to the "self-supervision," a feature which is very valuable to the operating engineer.

In this paper Mr. Shanklin takes the apparently very logical step and tries to extend the usefulness of this principle into higher brackets, in regards to both pressure and voltage. He divides the field into three pressure classes and prescribes cables suited for each class. The writer will discuss each class separately.

1. Low-Pressure Gas-Filled Cable

There is only one observation which the writer would like to make with regard to this cable. Nowhere in this nor in Mr. Shanklin's previous AIEE paper describing

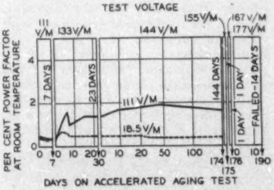

Figure 1

this cable has a direct parallel been drawn nor comparative test results shown which would indicate the superiority of the low-gas-pressure cable over that of an ordinary solid cable. It would be desirable to show for instance paralleling cases for Figures 6, 10, 11, and 12 of Mr. Shanklin's paper where no nitrogen is applied to ordinary solid cables employing the same dimensions and construction as the low-gas-pressure cables shown in Figure 1 of this discussion.

The argument that the low-gas-pressure cable of Figure 6 stood up for more than a year under certain conditions would be more convincing if parallel cases on solid-type cables would be shown. In Detroit solid-type cables stood tests at higher voltages than that shown on Figure 6. One such example is shown on Figure 1 of this discussion.

2. Medium-Pressure Gas-Filled Cable

The economic voltage range for this cable is given as from 40 kv to 69 kv. This is the range which is the most awkward and bothersome from the cable designers' point of view—apparently too high for solid-type insulation and too low to permit the application of reservoirs or other accessories economically. If the medium-pressure system will solve some of the problems in this field, it will be a real contribution. In the opinion of the writer the problem of the high-creep-strength alloy sheath will have to be worked out more completely especially with regard to higher temperatures, resistance to corrosion, and aging. The double-reinforced sheath could be justified in very few cases and under a constant pressure of 30 pounds per square inch. This construction still has to prove itself.

3. High-Pressure Gas-Filled Cable

In paragraph 2 of this discussion the statement was made to the effect that Mr. Shanklin took the "apparently" logical step of extending the gas-feeding principle into the higher-voltage field. As long as Mr. Shanklin tries to improve the solid-type cable, he will find the majority of the cable engineers behind him, because most of them realize the inherent weaknesses. In the higher-voltage field, however, there are several excellent solutions, and naturally these systems should be used as a yardstick when judging Mr. Shanklin's high-pressure cable.

A. Sheaths intact:

1. Compression versus high-pressure: Theoretically the lead sheath is not flexed in the high-pressure system, but it is flexed in the compression system. On the other hand the gas in the pressure chamber is in contact with the insulation in the high-pressure cable from the very beginning, while in the compression system the same conditions will appear only if and when the lead sheath ceases to function as an impervious membrane. Otherwise, the two systems are alike except that the cable of the high-pressure system will cost somewhat more if three-conductor cable can be used, and a great deal more if single-conductor construction has to be resorted to.

2. Oil-filled versus high-pressure: The writer would like to see a comparison in cost between these two types of cable. As the spread between oil-filled and compression cables is not too great, the higher cable cost of the high-pressure system would probably bring it quite close to the cost of the oil-filled system. In such a case the new construction would have to compete with the reliable proved oil-filled system on purely technical basis.

B. Sheaths stripped off:

1. Modified Beaver (*SMD*) versus high-pressure: When Mr. Shanklin strips his sheath, his cable in time becomes an *SMD* cable with the following exceptions:

(*a*). He does not design his cable for such conditions, that is, grades his butt space thicknesses, insures gas supply to all voids, and so forth.

(*b*). He has to use an impregnating compound of lower melting point; otherwise he cannot impregnate. With lower melting point oil drains at corresponding temperatures, and in a large steel pipe there is nothing to hold the oil in the cable insulation. In an *SMD* cable impregnating compound with a much higher melting point may be used.

(*c*). He does not even use strand shielding, because tests on "new" cables do not show the need of it.

(*d*). His cable cost probably somewhat less, because he does not use preimpregnated tape in a low-humidity room. Although in order to obtain good taping, he probably "regulates" the humidity of the taping room.

Between the two alternatives the writer prefers the *SMD*.

Strand Shielding

Although strand shielding did not appear to show any improvement in the long-time tests, it should still be retained, because it influences impulse strength. It is a well-known fact that insulation thicknesses of voltage cables are based on transient voltage requirements. This is emphasized in Mr. Shanklin's paper.

Impulse Tests

The writer would like to add a note of warning to Mr. Shanklin's conclusions. It has been fairly well established that aging does not affect the impulse strength of an oil-filled cable, but it does affect that of the solid-type cable. As the gas-pressure cable constructionally speaking is somewhere between the two, it seems that 21 days aging at 75 degrees centigrade should not be accepted as a complete proof that aging does not affect the impulse strength of the gas-pressure cable.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): Referring to Figure 6 of Mr. Shanklin's paper, it is noted that after 252 days at 85 volts per mil the power factor at 85 degrees centigrade was two per cent. Reference to a published AIEE paper ("The Type-*CB* Impregnated-Paper Cable," by S. J. Rosch, Figure 12), will show that after a similar period at 100 volts per mil, the power factor of type-*CB* cable at 80 degrees centigrade was of the same order (that is, two per cent). From this point on the test conditions are not comparable. Accordingly, from a power-factor-stability standpoint, it would not appear that the added complication of gas-pressure operation were warranted.

Referring to "Strand Shielding," comparison of Figure 6 with Figure 10, insofar as it goes, substantiates the author's conclusion that no great benefit was obtained from stand shielding. Figure 10, however, at the time of writing the paper was incomplete, the cable having been subjected only to the 85-volts per-mil cycles. Since the cable on test was constructed with metallized-paper strand shielding, we presume the author intends to limit his conclusions to this design only.

In reference to "Impulse Tests," it is stated that for all practical purposes, solid, oil-filled, and gas-filled cable have the same impulse strength; this appears reasonable. However, in this connection, consideration should be given to the fact that other investigators have found strand shielding to improve the impulse strength ("Impulse Strength as a Measure of Cable Quality," by L. I. Komives,[2] and "Impulse Strength of 24-Kv Type-*H* Cable," by G. B. McCabe, transmission and distribution committee, Edison Electric Institute, Detroit, Mich., February 17, 1941).

The type *SMD* cable discussed in paper 42-135 is typically American in that it represents a melting pot of ideas. Taking the graduated gas spaces of Beaver, the designers have put the cable in Hochstadter's steel pipe, thrown away the lead-sheath diaphragm, and applied the pressure in direct contact with the impregnated insulation as suggested by Fisher, Atkinson, Bennett, and probably others.

All that is needed to make this a truly up-to-date design would be to incorporate Rosch's carbon-black protection. On this point, however, the designers have apparently been content on simple conductor shielding. Since impulse strength was evidently of some concern (refer to "Impulse Tests"), this seems unfortunate, in view of one of the authors' own data[2] and that of one of his associates ("Impulse Strength of 24-Kv Type-*H* Cable," by G. B. McCabe). These data indicate the highest impulse strength was obtained with type-*CB* construction.

Referring to "Field Tests," in discussing Figure 2, the authors state: "These measurements show no evidence of deterioration of the insulation." So far as it goes, this statement is correct, but it might be added that the data do not show anything else either; certainly not that no deterioration took place. Power-factor measurements at (or below) *room* temperature are valueless as criteria of insulation deterioration. Reference to cyclic-aging data will disclose that during the cycles prior to instability the cold power factor seldom exceeds appreciably the initial power factor. On the other hand, the hot power factor gradually increases until failure (reference 1, Figure 12). It is the high-temperature, not room-temperature, power factor which discloses the presence of deleterious products which cause insulation deterioration.

Referring to "Compound Migration," if a 65-day test at 100 degrees centigrade can be relied upon to indicate the expected life of the cable at rated operating temperature, it would appear that the migration problem has been solved by the use of a high-viscosity impregnant (3,000 Saybolt Universal seconds at 100 degrees centigrade). On the other hand, operating experience on this same system has indicated that viscosity is not the only factor involved in migration. ("Examination of 60 Paper-Lead Cables After Operating in High Vertical Runs," by F. Farmer, underground systems committee, National Electric Light Association, Detroit, Mich., April 4–5, 1933). It will, accordingly, be interesting to learn at a later date the actual field experience with respect to compound migration. Until such time it might be well to accept summary 3, and possibly 4, with some qualifications. This observation is further prompted by the fact that tests in several laboratories and operating experience have indicated that migration will *eventually* take place, no matter how high the viscosity, if the impregnant is operated above its melt point.

I like one important feature of this system, modestly omitted by the authors, namely, that it has a second line of defense. It it doesn't work with gas, it can always be filled with oil; provided rationing has not extended to the Midwest by that time!

REFERENCES

1. THE TYPE-*CB* IMPREGNATED-PAPER CABLE, S. J. Rosch. AIEE TRANSACTIONS, volume 59, 1940, page 1047.

2. IMPULSE STRENGTH AS A MEASURE OF CABLE QUALITY, L. I. Komives. AIEE TRANSACTIONS, volume 60, 1941, October section, page 929.

Robert J. Wiseman (The Okonite-Callender Cable Company, Inc., Passaic, N. J.): I am not able to go along with Mr. Shanklin's statement that a pipe cable system is limited in open ground and not adaptable to paved city streets. I think the Detroit Edison installation of the 120-kv gas-pressure cable proves this, and calculations that we have made on installation costs indicate that our Oilostatic cable system can compete very nicely with a cable system drawn into ducts. It is true that it is necessary to draw the three cables into one pipe and, therefore, the heat is concentrated more than for a duct system, but the heat-dissipating properties of a buried cable or pipe are better than a duct, and, therefore, we arrive at the same conductor size or in some cases a smaller size. A steel-pipe cable system (Oilostatic and gas-pressure) is able to carry as heavy loads as is considered good engineering in concentrating in one circuit large amounts of power. The Oilostatic system has a very high emergency-overload characteristic because of the high heat storage capacity of the oil in the pipe and the surrounding earth, so that, if occasion arises, it can be heavily overloaded for several hours without reaching excessive temperatures, and it will cool rapidly when the load is reduced.

I do not agree with the statement in paragraph *d* referring to the effect of exposure of the unleaded cable as it is being drawn into the pipe to absorption of air and moisture, as it pertains to the Oilostatic system, and I also question it for the gas-pressure system. This is another of the fears that we had in the past that is not borne out by facts. We have drawn the individual unleaded cables for the Oilostatic system into pipes for lengths of 1,500 to 4,000 feet, and power-factor measurements did not show any increase. Many of us have stripped lead sheaths off cables, left them exposed, and then measured the power factor without finding an increase over the values before removing the lead sheath.

I am disappointed that the authors of paper 42-133 have not included in the paper any data regarding the thermal characteristics of the compression-cable system. They refer to load-cycle tests that have been made but do not tell us what temperatures were found for the conductor, lead sheath, and pipe, nor show curves of the temperature rise with time when the load is on, and the rate of cooling when the load is off. A load cycle of 2.5 hours on and off and four hours on and off is a very short cycle, and I doubt that the cable arrived at its maximum temperature at all. The 190-hour load cycle should show good data. The heat-storage capacity of a buried-cable system is very large and results in a slow rate of rise of temperature. In the case of the compression cable you have copper, insulation, lead, gas, steel pipe, and surrounding earth. For the high-gas-pressure cable there is no lead. For the Oilostatic-cable system we have copper, insulation, oil, pipe, and surrounding earth. Tests which we have had made on Oilostatic cable indicate the heat capacity inside the pipe is very high and results in a slow rate of rise in temperature outside the pipe. One test indicated that the temperature of the earth 17 inches from the center of the pipe did not begin to rise until six hours after the load was applied to the cable. I would expect the compression cable to have good but not as much heat capacity because of the use of gas which has a very low heat capacity instead of oil surrounding the cables inside the pipe.

I am also interested in the thermal constants for the compression cable, insulation, gas zone, earth. What formulas are used for calculating the thermal gradient in each zone?

The *SMD*-type cable is a logical development from the Beaver-type cable as used in England and the compression cable; that is, take the pipe of the latter and pull into it preimpregnated paper-insulated cables and apply high gas pressure to fill all the spaces in the insulation with gas. It differs from the Oilostatic system, in that, the latter has oil as the pressure medium, and oil is forced into all the spaces in the insulation. As the oil is gas-free, when pumped into the pipe which has been previously evacuated, and fills all voids, there is no worry about ionization taking place, so that we do not have a critical electric stress at which ionization will start.

We do not consider it necessary to lay up the three conductors in the Oilostatic cable, because there is free movement in the pipe to take care of the longitudinal thermal expansion. The authors found this to be so for their cable. The testing of a 50-foot length for the end thrust as described in the compression-cable paper is inconclusive, as the length is too short as compared to actual installation. Actually we have at least several hundred feet between joints, and on installation we have noted a gradual turning as the cables are drawn in, giving the equivalent of a long lay cable which will result in radial expansion of the three conductors instead of longitudinal, as they are not bound together, and so are free to move radially.

My comments on lack of thermal data in the compression-cable paper also apply here. The authors stated that they have collected a great deal of data; yet they do not give us this necessary information to understand properly how to design this system. We are interested in the temperatures found for the conductor, shielding tape over the insulation and pipe. We would also like to see curves showing the temperature rise with time while the load is on and the rate of cooling when the load is dropped. I question very much the value of such short load cycles as 2.5 hours on and off, and four hours on and off. I doubt that the cable will arrive at its maximum temperature at all. Information on the 190-hour load-cycle test would be helpful in appreciating what this system is capable of doing in the way of taking care of heavy loads. We will be interested to compare the heat-storage capacity of this cable with the heat-storage capacity of the compression and an Oilostatic cable.

The information regarding soil thermal resistivity is very interesting. I am glad that the authors state the formula in the third paragraph is empirical. We have used a sphere in making thermal-resistivity tests and the well-known sphere formula. The values of soil resistivity given in Figure 4 are quite interesting and show fairly low values. However, the important thing is how do they use this thermal resistivity in determining the thermal resistance of the earth term for the total thermal resistance of the system?

The coefficient of friction of 0.37 for pulling the cables into the pipe up to 1,500 feet is very good considering it is a dry pipe. We have found for the Oilostatic system for lengths of 2,000 feet and more a value of 0.26. We have no hesitancy in pulling as high as 4,000 feet of cable without excessive strains on the conductors.

E. H. Kirkham (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Under some circumstances it is desirable that a pressure relay be installed at the far end of a gas-filled cable when gas feeding from one end only. In the event that the feeder is operating as a closed system this relay will function equally well at either end. However, should it be necessary to operate with a constant supply of gas from a cylinder through a regulator, while the regulator will maintain a relatively steady pressure at the feeding end, pressure gradient in the cable may be such as to result in a dangerously low pressure at the far end. In one instance, this occurred with a leak in the order of one-half cubic foot per minute when fed at 12 pounds per square inch from one end; the pressure at the far end was measured at six pounds per square inch. Of course, the temporary feed was supplied at the far end pending location of the leak.

I agree with Mr. Shanklin that it is not necessary to "blow out" gas-filled cable equipped with a solid tube, but experience has shown that the excess compound should be drained from the joints periodically, a procedure which has the same effect as blowing out the cable.

At the present time, field experience indicates that this need not be done oftener than once a year, and probably, as the free compound is eliminated from the cable, the period may be extended.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York, N. Y.): There has been another type of compression cable described before the Institute which was not mentioned in paper 42-133. This was my paper on sheath-compensated cable presented before the winter convention several years ago. The cable described at that time has operated satisfactory at pressures up to 100 pounds per square inch.

From an operating viewpoint several points in the compression-cable paper are of interest. First, the increase of several hundred per cent in the life of the lead sheath, when subjected to diaphragm action, by the elimination of oxygen from contact with the sheath surface, seems to be of practical value. We have occasions in New York to run cables over bridges and other vibrating structures and would like to inquire whether the fatigue strength would be similarly increased under these conditions by the elimination of oxygen from contact with the sheath surface?

Frequently cracked sheaths occur, particularly in the smaller manholes, from longitudinal movement and concentrated bending. Would it not be possible to reduce the cable fault rate from this cause by sealing the sheath surface from oxidation at these vulnerable points?

It was also pointed out in this paper that longitudinal movement was negligible when single-conductor cables were cabled together with a short lay and no external covering used. Under certain conditions such a procedure applied to duct installations may provide a simple solution to the problem of longitudinal movement and its effect on the lead sheath.

The Consolidated Edison Company of New York has made a number of laboratory and field tests on three-conductor cables of special construction for determining the ability of such cables to absorb within the sheath the longitudinal expansion of the conductors. Cables made with short lays, special binder construction, and sheaths of different degrees of looseness were tested. Laboratory tests showed that the greatest gain in reducing the restraining force necessary to prevent longitudinal movement was obtained by using short lays. Field measurements on a number of cable sections having special binder construction and short lays showed cable movements on these sections to be considerably smaller than for equivalent sections of standard construction.

There are several items in the paper on compression cable that the authors may clarify by answering the following questions:

1. How are countersunk type of connectors applied to a cable joint?
2. In Figure 5 of the paper, is the step on which the joint sleeve ends the lead sheath of the cable? If it is, what is the next higher step on the cable?
3. Since a void is left at the end of the cable sheath in the joint, is there any tendency for the joint sleeve to buckle and put undue strain on the wipe when external pressure is applied?
4. What is the 60-cycle dielectric strength of the cable, as compared to that of the joint?

From the standpoint of simplification, high-pressure gas-filled cable seems to approach more nearly the dream of the operating man than any cable available for such high voltages. The test data are very illuminating and convincing. If the future operating record proves as highly satisfactory as is indicated to date, I think high-pressure gas-filled cable will be hard to compete with.

Of particular interest is the fact that pipe cable was well adapted for installation under city streets in a city of the size of Detroit. Under such conditions of installation, I should like to ask how the cost of such a pipe system compares with the conventional duct system and also, how much of this seven-mile pipe system was under paved city streets, as well as what type of street paving was used.

The measurements of soil thermal resistivity are not only interesting but very helpful because of the lack of exact knowledge on this subject. In many cases in this country 80 degrees centigrade per watt per centimeter cubed has been used as the net value for soil resistivity. The variation shown by the Detroit data, from 75 to 120, indicates that 80 is probably not a very conservative figure to use.

H. C. Frank (General Cable Corporation, Bayonne, N. J.): Some supplementary data in connection with the laboratory tests of diaphragm action of lead sheath, mentioned in the paper on 120-kv compression-type cable, are significant. The daily load cycle, it will be recalled, was simulated by a laboratory cycle of 4.8 minutes. Because of the shortness of the laboratory cycle, it was a real problem to insure that the lead sheath would move uniformly throughout its length.

It became apparent that very little volume change takes place until nearly the full-pressure differential is established. Arrangement was therefore made to establish substantially this full-pressure differential in the first one third of each half-cycle. Thus, two thirds of each half-cycle was utilized for producing volume changes in the cable at substantially full pressure.

Experiment showed that this arrangement resulted in uniform longitudinal distribution of diaphragm action, as evidenced by breaks and general damage to the sheath occurring no more frequently at the ends of the samples than elsewhere.

One projected application of the compression-type cable involved installation on a bridge carrying heavy traffic and under severe vibration. Experiments were made as to the feasibility of such an installation and the results are pertinent not merely to this type of cable but to installation of any ead-sheathed cable on a vibrating structure.

Samples of pure lead sheath, about four feet long, two inches in outside diameter, and having an 85-mil wall, were supported at both ends by trunnions and vibrated at their natural frequency (about 1,000 cycles per minute) by means of an electrically powered vibration motor. The amplitude of vibration could be regulated.

The modulus of elasticity, as calculated from the physical dimensions, mass, and experimentally determined frequencies, was found to be constant over a wide range of amplitudes (for the frequencies used in the tests) and to have a value of 1.7 to 1.9×10^6 pounds per square inch for common lead. The peak vibration stresses were readily calculated from this modulus, the physical dimensions, and the amplitude.

The curve showing cycles of life as a function of vibration stress was found to be asymptotic to a line parallel to the life axis. A sample will fail in a limited number of vibration cycles if stressed beyond this asymptotic value but will last indefinitely if the vibration stress is held below this value. For pure lead protected against oxidation the asymptote was found to be 500 pounds per square inch. When oxidation was not prevented, the value dropped to 350 pounds per square inch.

During these tests it was attempted to determine whether unbalanced stress would affect the fatigue limit. It was found that the fatigue limit was unaffected since no unbalanced force could be supported by the sheath when under considerable vibration stress. The sheath would creep rapidly until all such unbalanced forces or stresses were removed. It follows also from this that there can be no complications caused by residual stresses being superimposed upon vibration stresses.

G. D'Eustachio (General Cable Corporation, Bayonne, N. J.): Mention is made in the paper on 120-kv compression-type cable of the injurious effect of oxidation on diaphragm action. Specifically, the substitution of nitrogen for compressed air as the pressure medium in these tests resulted in increasing the life of laboratory samples by a factor of 4.

This effect of oxidation is in line with the generally known fact that corrosion of metals greatly hastens their destruction by fatigue. The following is offered as an explanation of the mechanism involved.

When a fresh surface of lead is exposed to air, a thin coating of oxide is quickly formed. Normally, this acts as a protection against further oxidation. However, in a sheath subjected to vibration or diaphragm action, the brittle oxide film is readily broken, and a fresh surface is continually presented to the effect of oxidation. Repeated bending tends to concentrate the action in definite cracks which ultimately penetrate the entire sheath.

William A. Del Mar (Phelps Dodge Copper Products Co., Yonkers, N. Y.): In the year 1935 I visited many of the principal cities of the United States and spoke before local engineering organizations on compression cable, exhibiting a motion picture of the then recently completed compression-cable system at Copenhagen in Denmark which I had seen under construction.

Engineers discussed this system at these meetings and the discussion was nearly always in one vein. They said, in effect:

"We have troubles on our cable systems and troubles on our high-pressure pipe systems. Now you describe a system that will combine these two kinds of trouble on one system. We prefer to take our troubles singly."

Ten years after the first European installation the Detroit Edison Company takes the plunge and decides to take on both kinds of trouble in their regular stride.

The type-*H* cable, also was held in abeyance except for one installation for about 10 years after its European introduction.

I have made my appreciative comments on the low-pressure gas-filled cable in the discussion of Mr. Shanklin's 1939 paper and will now confine myself to a few critical comments on some of the details.

Both Doctor Wiseman and Mr. Mildner in that same discussion, asked why Mr. Shanklin used average rather than maximum stresses in designing his insulation thicknesses.

I am one of those who believes that the maximum stress, as ordinarily calculated, is of doubtful significance in many cable problems where it has been used indiscriminately in the past, but it seems to me that the gas-pressure cable, at operating voltage, is one case where there can be little doubt of its applicability.

Mr. Shanklin seems to agree with this, for in his reply to Mr. Mildner he said:

> "Mr. Mildner is correct in stating that it is better to deal with maximum voltage stress than average voltage stress. Most of the cable lengths we tested had compact sector conductors with relatively sharp corners. For conductors of this shape it is difficult to express maximum voltage stress accurately, and it is for this reason that average voltage stress is generally used as a practical yardstick in the United States."

Thus, while Mr. Shanklin recognized in 1939 that the maximum stress should be used in the design of gas-pressure cables, and that this was not done only because of the difficulties, he continues to use the average stress for design purposes in his present paper.

Let us see where this leads us in a specific case.

Assume a 350,000-circular-mil 34,500-volt sector cable with the wall thickness recommended for this voltage, namely, 310 mils.

The average operating stress will be 64 volts per mil, and Mr. Shanklin considers this to be a satisfactory basis of design. We have studied the stress around the contour of a 350,000-circular-mil cable with 312 mils of insulation, using the electrolyte method, and have shown the results as a graph.[1] Reference to this graph will show that the stress at the surface of the conductor at the shoulders of the sector is 1.7 times the average stress, or 109 volts per mil, neglecting strand effect.

It is well known that ionization starts, in a drained cable, at a maximum stress of 50 volts per mil at atmospheric pressure. Mr. Shanklin's Figure 5 indicates that this is increased by a factor of 1.63 at 12 pounds gas pressure, that is, to 81 volts per mil.

We thus, have a cable operating at a stress of 109 volts per mil which is 35 per cent greater than its initial ionization stress of 81 volts per mil. It therefore appears that the low-pressure gas-filled cable takes about 70 per cent advantage of the 50 per cent increase in stress permitted by the self-extinguishing effect, and therefore Mr. Shanklin is only literally accurate but really somewhat misleading in saying that this effect "offers an additional factor of safety." In fact, he appears to recognize this, in the next sentence, by admitting that the self-extinguishing effect "allows working voltage stress to more safely approach the critical voltage stress at which cumulative ionization deterioration occurs."

It would help an understanding of this important type of cable if Mr. Shanklin would clarify this matter of voltages stresses, in detail, in the light of his experience and research.

Mr. Shanklin's statement that he has "tried about all of the recognized available types of high-viscosity compounds, including rosin mixtures, and so forth" is probably a more comprehensive statement than he intended it to be, as each laboratory has its own line of development which overlaps with others to some extent but, in the main, proceeds along independent lines, with its own materials, processes, equipment, and methods of test.

I cannot help believing with Mr. Faucett and associated authors, that some of the recognized and maligned blended oils will give better results than the so-called "standard all-mineral compound used for some time past." This last-named compound is capable of becoming electrically unstable as the result of contamination in the impregnation process and should be easily surpassed, for this service, by compounds more permanently immune to ionization.

Also, the particular oil referred to by Mr. Shanklin is entirely incompatible with rosin or rosin derivatives, and cables made with such mixtures are quite unstable, a fact which may explain his aversion to the blends.

Table I

Faucett, Komives, Collins, and Atkinson	Shanklin
1. The saturant used in the Detroit cables has a very high viscosity	Optimum viscosity is 100 Saybolt at 100 degrees centigrade. (This is about the lowest viscosity ever used in solid-type cables)
2. Since it was desirable not to expose the insulation to the atmosphere any longer than necessary, and so forth	Cable exposed to air for eight hours had good endurance at 120 volts per mil but had increased dielectric loss
3. Very thin tapes result in increase of dielectric strength	Ionization voltage with three-mil paper was no higher than usually obtained with standard six-mil tape. Life test with three-mil paper "little if any longer"
4. Cable with thin paper withstands, without damage, normal bending	Thin paper tends to wrinkle and will not withstand cable-bending as well
5. Strand shielding of *SMD* cable believed responsible for its having higher dielectric strength than unshielded oil-filled cable	Ionization voltage same with and without strand shielding, and time to stabilize at 85 volts per mil same

Data on impulse dielectric strength given by R. Davis[2] indicate that highly drained cable at atmospheric pressure has an impulse strength of only about one-third that of normally impregnated cables. This points definitely to the desirability of using viscous compound to obtain minimum drainage and maximum impulse strength.

Mr. Faucett and his associate authors do not share Mr. Shanklin's belief in a low-viscosity impregnant. On the contrary, they are insistent on the use of one of "very high viscosity."

This brings me to the very interesting contrast between the views of these authors on several important points as shown in parallel columns in Table I.

Turning now to paper 42-135 by Faucett, Komives, Collins, and Atkinson, we find a cable system which is virtually the Oilostatic system without the oil and oil-pressure accessories. It should therefore be interesting from a first-cost standpoint.

Whether it will prove economical eventually may not be known for years, depending on how much cable is ruined by water entering the pipe, how much will be perforated by transient voltages or even working voltages in the event of loss of gas pressure. The Detroit Edison Company deserves the thanks of the industry for taking these risks in the interest of progress.

Table II

	Power Factors—Degrees Centigrade	
	85	25
At beginning	0.76	0.42
After 100 load cycles	2.47	0.41

The paper gives a great deal of interesting information among which the data on soil resistivity may be especially noted.

The formula, $g = 128/W(T_1 - T_2)$, while empirical, is derived from the following rational formula for a sphere:

$$G = \frac{4\pi r}{W}(T_1 - T_2)$$

where r = radius of sphere, and the other quantities are as given in the paper. This formula is based on the assumption that heat is dissipated equally, in all directions, from the sphere.

On the other hand, the Kennelly formula for heat resistance from a cable or pipe to ambient is based on the assumption that the heat is all dissipated upward. It is, therefore, obvious that the constants obtained by the authors' tests cannot be used directly in the Kennelly formula for determining the thermal resistance from pipe to ambient.

The formula for the test electrode gives a lower soil resistivity than the true one, because more watts are expended in driving heat through the nonuniform actual path than through a uniformly radial path.

On the other hand the Kennelly formula gives a falsely high resistance, because it neglects the very considerable amount of heat which flows in other directions than upward.

Therefore, if we use an unduly low resistivity in the Kennelly formula, we tend to counteract the falsely high resistance which that formula gives. Judging by *BEAIRA* publication *F/T*128, these errors cancel, for all practical purposes, as we find, that the usual correction factor of $^2/_3$, used in connection with the Kennelly formula, in British practice, is *"unnecessary when the thermal resistivity of the ground is measured in situ by one of the methods described in the appendix."* This is very important to note in view of existing controversies on soil resistivity.

For some years past I have been contending in favor of the use of a soil resistivity of 180 for standard carrying-capacity tables, using it in the Kennelly formula with the

Table III

	Power Factors—Degrees Centigrade	
	60	25
At beginning	1.23	0.45
After 20 load cycles	1.44	0.47
Increase, per cent	17	4.5

usual correction factor of $^2/_3$ so as to give a net value of 120.

Others have contended for a net value of 80 or less for this purpose.

The data presented in Figure 4 of the paper indicate the following safe net values, all of which are to be used *without* the $^2/_3$ correction factor.

Porous loam	100
Clay	105
Sand	120

This suggests a basis of compromise on a net value of 105 except for sandy soil, for which a net value of 120 should be used.

Values as low as 80 are attained only with porous loam during the three coldest months and are, therefore, unsafe.

It should be noted that these values would have to be increased something like 50 per cent for comparison with resistivities, derived from measurement of soil between definite boundary plates, such as given by Shanklin.[3]

The authors use, as criterion of stability of their cable, the power factor at room temperature or less, giving as reason that "it was not feasible to obtain power-factor measurements at elevated temperatures, since it required a relatively long time to switch the experimental cable out of the circuit and connect up the power-factor measuring equipment." This reason seems somewhat inadequate, as it is well known that room-temperature power-factor measurements are not a sensitive indication of deterioration. Data on cables life-tested in accordance with the procedure described by Boehne and Linde (page 207 of reference 4) are typical of results on laboratory made samples of definitely unstable character, as shown in Table II.

Similarly a cable of poor quality, life-tested in accordance with the procedure described on page 209 of reference 4 gave the results shown in Table III.

Here the room temperature reading showed some change but only about one-fourth that at 60 degrees centigrade.

The instability of these cables would have passed unnoticed if room temperature power factors had been relied upon.

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2. **R. Davis.** *Journal* of the Institution of Electrical Engineers, April 1942, part I, number 16, page 201.

3. EFFECTS OF MOISTURE ON THE THERMAL CONDUCTIVITY OF THE SOILS, G. B. **Shanklin.** AIEE TRANSACTIONS, volume 41, 1922, pages 94–100.

J. A. Peterson (The Commonwealth and Southern Corporation, Jackson, Mich.): Three types or methods of installation of high-pressure gas-filled cable are mentioned in paper 42-135:

1. Method used by the Detroit Edison Company
2. English method with reinforced lead sheaths.
3. Oil-filled (Oilostatic) method.

Is there any information available as to the relative costs of these three methods?

It will be interesting to know the expected life of the iron pipe treated with one-half-inch Somastic as used in the Detroit Edison method.

The data on soil thermal resistivities constitute very valuable information, especially to the Detroit Edison Company, but it would be more valuable to the industry as a whole if the actual moisture content of the various types of soil were known for the different values of thermal resistivities. Was this point checked at the time the tests were made?

It would be interesting to know how conductor expansion is taken care of on this installation—by the lay of the cable, by the snaking effect of the cable in the pipe, by expansion bends, or by a combination of these methods. The report states that in the experimental installation the expansion of the iron pipe was taken care of by expansion bends. Was this method used for the entire installation?

The viscosity of cable-impregnating oil used in low-voltage solid impregnated-paper lead-covered cable ranges ordinarily from 100 to 150 seconds Saybolt Universal at 100 degrees centigrade. The viscosity of the oil used in the gas-filled cable is given as 3,000 seconds Saybolt Universal at 100 degrees centigrade. What is the viscosity of this latter oil at 50 and 150 degrees centigrade? What is the maximum temperature at which this cable is expected to operate? Will this impregnating oil remain in the insulation, or will it gradually drain out into the pipe, and, if it drains into the pipe, will it be necessary to drain the oil from the pipe? It is possible that the oil will remain in the insulation and that none of these points present any problem.

Johnstone Wright (Central Electricity Board, London, England): The first departure from conventional solid-type underground-cable practice in Great Britain was an extensive application of oil-filled cables in sections of the British Grid. Such oil-filled cables were used at 66 kv and 132 kv. They have been quite successful, but their use is attended by certain disadvantages, particularly where the contour of the cable route includes steep sections. We were, therefore, interested in any developments which would minimize these difficulties and at the same time be economically attractive.

A gas-pressure cable of the Hochstadter type was, therefore, laid in a London area and put into service on October 1, 1932. This cable was laid in a steel pipe which has given rise to a certain amount of electrolytic corrosion trouble. The source of this trouble was discovered and the necessary remedial measures instituted, and subsequent experience has been all that might be desired. We are, nevertheless, somewhat apprehensive regarding the possibilities of corrosion in steel pipes, and a subsequent gas-pressure cable in the same area is of the double-lead-sheath type. At no time has there been any electric fault on either of these gas-pressure cables.

When the internal gas-pressure cable was mooted, we took a close interest in its development, and when all the necessary works proving tests had been satisfactorily carried out, we put a one-mile trial length of 132-kv cable into service as a field experiment. This cable went into use on December 4, 1937. It has carried the loads, short-circuit currents, and surge stresses encountered during the intensive bombardment of London without trouble. The cable was on one occasion damaged during street excavation, and the necessary repairs were effected expeditiously, the circuit being put back into service much in the same way as is done in everyday conventional mains practice.

The success of this experimental cable led us to utilize the same type of two further short 132-kv routes where it was impracticable to have overhead lines. We are now engaged in the final stages of making a seven-mile circuit of 132-kv gas-filled cable.

This outline cannot do justice to the excellent work which has been done by the manufacturers in producing economical and reliable 132-kv underground cable, but the detailed story must be left to be told when conditions become more normal.

L. Meyerhoff (General Cable Corporation, Bayonne, N. J.): The information contained in the paper on 120-kv high-pressure gas-filled cable regarding temperature stability is amplified and brought to date by the following.

Two lengths of cable representative of that furnished for the commercial run were cut up into a number of 12-inch lengths. These were individually sealed in lead pipe, the free space in the pipe being filled with nitrogen at about five pounds gage pressure. The sealed samples were then placed in an oven and maintained at 100 degrees centigrade. Samples were withdrawn at intervals for test purposes.

In addition to the above samples, two samples from each length were equipped with electrodes and guard rings before being sealed into the lead pipe, and leads were brought out through insulating bushings so as to permit electrical measurements to be made at 100 degrees centigrade during the progress of the aging run.

In Figure 2 of this discussion is shown the variation of power factor at 100 degrees centigrade with time of aging on the four last mentioned samples. It will be noted that the power factor after 220 days of aging has changed very little, what change there is being apparently in the direction of improvement.

Figure 3 shows the temperature-power factor data for several samples of one of the cables, tested after various aging times up to 184 days. The points for all the samples (including the unaged cable) are represented very well by a single curve. The variations between these samples are no greater than would be expected of a similar number of individual samples of new cable.

Figure 4 shows the radial power factor at 80 degrees centigrade for the cable in its original condition and after 40, 113, and 184 days of aging. It will be noted that in the

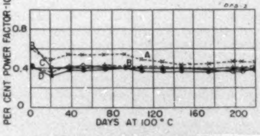

Figure 2. Power factor versus days of aging at 100 degrees centigrade

A, *B*, *C*, and *D* represent four similar samples

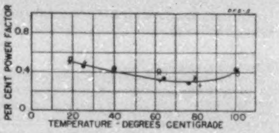

Figure 3. Power factor versus temperature

Showing effect of aging at 100 degrees centigrade for 184 days to be insignificant

× New cable + Aged 113 days
○ Aged 40 days ● Aged 184 days

main the radial power factor is quite flat in all cases and that such changes as appear are in the direction of reduction rather than rise of power factor. In the case of the last two samples withdrawn there appears a slight rise in power factor near the sheath. Whether this rise is due to the aging or is characteristic of the particular samples will not be known until further samples are withdrawn and tested.

In Figure 5 the tensile and tearing strength values have been plotted for the new cable and for a sample which had been aged for 184 days. It is apparent from these results that there is no material change in mechanical strength. The sharp change at 20 per cent distance from the conductor coincides with a change in the type and thickness of the paper.

Thus, from all the standpoints studied there has been no significant change in the electrical or physical properties of the cable during more than six months' aging at 100 degrees centigrade.

I should like also to discuss the benefit of using thin paper in gas-filled cable. Our tests have indicated that gas-filled cable with thin paper has considerably higher breakdown strength than similar cable with thick paper, particularly on short-time and surge tests. The difference in strength appears to be greater than can be justified on the basis of the increase of the specific breakdown strength of gas, by reason of the reduction of the gap distance. From the published data, the breakdown strength of a 2.5-mil gap of air at 200 pounds pressure is about 12 per cent higher than that of a five-mil gap at the same pressure, whereas gas-filled cable made with 2.5-mil paper is apparently considerably more than 12 per cent stronger electrically than cable made entirely with five-mil paper. Thus, the above explanation seems numerically inadequate.

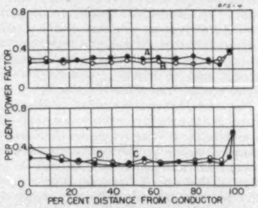

Figure 4. Radial power factor as affected by aging at 100 degrees centigrade

A. New cable C. Cable aged 113 days
B. Cable aged 40 days D. Cable aged 184 days

What appears to be a more adequate explanation is the following. The benefit of the thin layer does not lie so much in the voltage at which the gap breaks down as in the reduction of the amount of energy dissipated in any gap after it breaks down, that is, above ionization voltage.

Compare two cables of the gas-filled type, one, cable *A*, made up of five-mil paper and the other, cable *B*, of $2^1/_2$-mil paper. Assume the power-factor versus voltage characteristics of the two cables as a whole to be identical. Thus, at any voltage above the ionization point, the total energy dissipated in all of the gas spaces will be the same in cable *A* as in cable *B*. However, since cable *B* has twice as many gas spaces as cable *A*, the energy dissipated in any one gas space of cable *B* will be only one half as much as in a similarly placed gas space of cable *A*. This means that in any $2^1/_2$-mil space there is only one half as much energy ready to bombard and start to burn the paper and oil or to find a path around the paper as in a corresponding five-mil space.

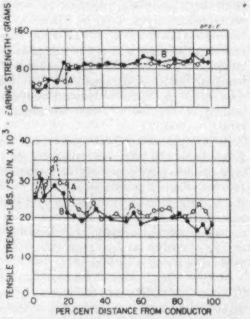

Figure 5. Mechanical strength of insulation as affected by aging of cable at 100 degrees centigrade

A. Cable aged 184 days

B. New cable

Dawes[1] has found that the energy loss caused by the ionization of the gas spaces in a cable may be represented approximately by the expression $K\ (E - E_0)$, where K is a constant, E the voltage applied, and E_0 a voltage substantially equal to the ionization voltage. Thus, in order for the energy in one gas space to be the same in the two cables, $E - E_0$ must be twice as great as in the case of cable *B* as in the case of cable *A*. For instance, if E_0 is 140 kv for the two cables, 200 kv applied on cable *A* will produce the same amount of energy in a given gap as 260 kv will produce in a corresponding gap of cable *B*.

In the above discussion it was assumed that the power-factor versus voltage characteristics of the two cables are the same. As previously stated, however, at 200 pounds per square inch a $2^1/_2$-mil gap begins to ionize at a 12 per cent higher voltage than does a five-mil gap. Thus, in changing from five-mil to $2^1/_2$-mil paper there is a gain in breakdown strength of this 12 per cent plus the additional gain indicated in the above discussion. The amount of this additional gain will depend on the ionization characteristics of the cable and on the voltage applied.

In tests of long duration the value of $E - E_0$ is small, so that the gain in strength caused by the use of thin paper in such tests may be substantially only that caused by the increase in ionization voltage. In short-time tests, however, $E - E_0$ is large, and the gain in strength will be very considerable. On the basis of this analysis, the gain should be even more marked in surge tests. It should be noted further that at lower gas pressures, say 30 pounds per square inch, a given amount of gain due to the thin paper will occur at a lower voltage than at the high pressure because of the large reduction in the ionization voltage. This fact makes it feasible to test cable with $2^1/_2$-mil paper for acceptance purposes at much lower pressures than would be permissible for cable with five-mil paper.

REFERENCE

1. IONIZATION STUDIES IN PAPER-INSULATED CABLES—II, **C. L. Dawes, H. H. Reichard, P. H. Humphries.** AIEE TRANSACTIONS, volume 48, 1929, pages 383–98.

C. Beaver (W. T. Glover and Company, Ltd., Manchester, England): The authors of paper 42-135 and all concerned are to be congratulated on the successful introduction into the United States of the high pressure gas-filled cable on a commercial scale. It is to be hoped that it will have an operating experience similar to that of gas-filled cable in England.

It is, of course, essentially the same cable but differs in the method of installation and in this important respect would appear to carry a greater burden of risk. That is to say, the difficulty of maintaining long lengths of pipe line free from leakage under high gas pressure would appear to be considerable, whereas experience shows that perfect freedom from leakage is obtainable with the reinforced sheath type. In actual practice in England no supplementary gas supply is ever necessary after the initial charging.

Under conditions of laying and installation obtaining in England, it is very doubtful whether the pipe-line method would ever be used; the self-contained perfectly gastight reinforced sheath type having everything in its favor from the user's point of view, that is, embracing economical, technical, installation, and maintenance aspects.

The substantial character of the protective coatings of the pipe is noted, but it has to be remembered that in the presence of vagabond currents in the vicinity of a minute imperfection or damage the current density at such a point might be relatively high and corrosion correspondingly rapid. In other words, the very excellence of the protection from ordinary chemical attack entails possible susceptibility to concentrated electrolytic corrosion.

It is gratifying to note that the authors consider this type of cable to be at least equal, electrically, to the oil-filled cable, and that, physically, the type is superior to the oil-filled cable in that

(*a*). No draining or migration of the impregnating oil is encountered, so that stop joints or other devices are not required.

(*b*). No allowance has to be made for impregnating oil expansion so that oil reservoirs are not necessary.

In the reinforced lead sheathed cables such as have been installed in England the maximum operating conductor temperature is 80 degrees centigrade, and at this temperature the gas-pressure rise is such that the gas density at the conductor surface, where the electric stress is highest, is approximately equal to that at 15 degrees centigrade, and in consequence the ionization voltage is substantially the same at 80 degrees centigrade as at 15 degrees centigrade. (It should be noted that these cables are sealed off and are not in communication with gas reservoirs at the terminals.) In the pipe form of the cable the gas-pressure value at high-conductor temperature will not increase proportionately to the absolute temperature, and hence the ionization voltage will be lowered at high-conductor temperature. This is fundamentally due to the fact that the average temperature of the gas-filled system in the pipe type of cable does not increase much with increase of conductor temperature.

The authors have given the reasons for the radial grading of the paper thicknesses in a very clear and concise manner. In England the thinnest paper tapes used at present are $1^3/_4$ mils thick, and the ionization voltage at 200 pounds per square inch by gauge of a 600,000-circular-mil round conductor cable insulated with 600 mils of paper, of graded thickness $1^3/_4$ mils to $5^1/_2$ mils, would be 165 kv minimum between conductor and sheath, the permittivity of the dielectric being 2.9 to 3.0. Cables have operated at maximum stress values of 335 volts per mil at 200 pounds per square inch by gauge in England for nearly five years as compared with the value of 288 volts per mil at 255 pounds per square inch by gauge for the installation described in the paper.

It has long been known that increasing the density of paper results in high breakdown voltages in solid-type dielectrics. In the gas-filled cable a particular advantage accrues under impulse voltage conditions, because the gas-filled spaces shunt their stresses to the solid (impregnated-paper) component of the dielectric, and the breakdown occurs due to disruption of this part of the dielectric.

As a matter of fundamental design the provision of conditions which suppress ionization up to twice working voltage also provides the dielectric with an asymptotic a-c time–breakdown value sufficiently above the working voltage to take care of such margins as manufacturing variations and installation exigencies, together with sufficient impulse strength to resist the maximum impulse voltages encountered in service.

The authors' work on the latter aspect is very valuable as showing the influence of various factors on the impulse strength of the gas-filled dielectric.

No data are given in the paper as to permittivity and thermal resistivity values. In the cables installed in England the permittivity value is 2.9 to 3.0, while the thermal resistivity value is 700 thermal ohms per cubic centimeter. These cables are insulated with paper dried and preimpregnated in sheet form on a special vacuum paper serving machine (British patent 373,697). On this machine the impregnated sheet of paper is scraped, while hot, by means of heated knives, so that all excess or free impregnating compound is removed. The roll of impregnated paper 15 inches in diameter and 36 inches wide is split into spools and then lapped on to the cable in air with no control of the humidity. The power factor–temperature characteristic is practically identical with that given in Figure 12 of the paper. The cables insulated with this paper can be successfully subjected to mine-shaft cable drainage tests at 80 degrees centigrade.

The authors' remarks in the third paragraph of the section dealing with dielectric strength, regarding the origin of the failures under prolonged a-c tests are interesting.

We have also noticed in similar tests using high-impedance circuits and short time relays to limit the burning at the fault that the puncture generally occurs in the gas-filled space within a distance of ten mils from the edge of the paper strips for gaps width of the order of 60 to 100 mils. We attribute this location of the fault to the fact that the thickness of the gas space here is equal to the paper thickness, whereas toward the center of the gap width the thickness of the gas space is reduced because of bowing of the paper strips bounding the gap.

Referring to Figures 10 and 11, the data given show the effect of moisture on the high-temperature power-factor values and also the effect of control of humidity during the lapping process. According to English manufacturing experience, however, it is not necessary to control the humidity in order to produce power-factor values equal to those of curve *A* in Figure 10.

Further, the absence of free impregnating oil in the gas-filled cable eliminates the possibility of metallic contamination of the dielectric by the copper-conductor screening tape, or lead sheath during temperature cycles. This phenomenon has of course been noted frequently in cable in which the dielectric is filled with oil.

I note that there is little reference in the paper to physical features except with regard to paper density and viscosity of the impregnating medium, and perhaps, without being too elementary, I may be allowed to point out that the high-pressure gas-filled cable—at least in its reinforced sheath form—has an important physical basis of design. It strikes at the root of the ionization troubles which limit the use of solid-type cables, by substituting completely reversible conditions of expansion and retraction inside the confines of the cable sheath for the partially reversible conditions which are responsible for the said limitations.

In providing for expansion and contraction at constant volume it also obviates "cold working" of the lead sheath—a result not obtained in any other type of cable.

The corollary is that the original conditions established in the cable, as regards freedom from ionization, are never subsequently infringed.

In conclusion, the authors' figures for mileages of high-pressure gas-filled cable installed in England may be brought up to date as follows:

Installed:
132 kv—6.5 conductor miles
33 kv—52 conductor miles
(A considerable proportion of this has been in service 5 years.)
Proceeding, about to be put into service:
132 kv—18 conductor miles
33 kv—25 conductor miles

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): My discussion of the two papers by Faucett, Komives, Collins, and Atkinson will deal chiefly with the high-pressure gas-filled type of cable, but I will first comment briefly on the compression-type cable.

We made a close study of the latter type when it was first introduced about ten years ago. The main thing that discouraged us from further work along this line was the fact that safe operation of this design depended upon complete gastightness of the lead sheath, joint sleeves, and wipes. If high-pressure gas in the steel pipe, normally segregated from the cable by the impervious sheath covering, once enters the cable cross section through a haphazard leak, it will gradually saturate the impregnated paper and travel a long distance back in the cable length. Eventually at some distant point gas will replace impregnating compound forming a "dry spot." There is then no means of maintaining sufficient pressure at this point when temperature changes take place, and ionization trouble will occur. Experience with all cable systems indicates that it is not possible to avoid occasional leaks in the sheath envelope.

Concluding that eventual gas saturation and loss of pressure control in compression-type cable were unavoidable in any case, we reasoned that it would be better to accept this fact at the beginning and use gas-filled insulation directly exposed to the high-pressure gas in the steel pipe. In this way full control of pressure could be maintained both radially and longitudinally; also, it represented a simpler and more economical design of pressure pipe cable.

Following out this idea we started initial development work in 1932, the principles of which are described in United States patent 1,991,230, filed in July 1933. Some of the test results are outlined in a paper, "Low-Gas-Pressure Cable,"[1] which I presented at the AIEE winter convention in 1939. Further details relating to this cable system are given in a second paper which I presented at this afternoon's meeting, contemporaneously with Faucett, Komives, Collins, and Atkinson's paper, "120-Kv High-Pressure Gas-Filled Cable."

The cable systems described in these two papers are closely the same in principle, the only difference being the use of mass-impregnated-paper insulation in one case and preimpregnated paper in the other. In pointing this out, I have no desire to detract in the slightest from the credit due Faucett, Komives, Collins, and Atkinson for the initiative they have shown in going ahead with the first full-sized installation of this kind and the perfection of practical detail it involved. They are heartily congratulated for carrying on to a finish the fine job that was done.

On purely technical grounds I am wondering why Mr. Faucett selected preimpregnated paper in preference to regular mass-impregnated-paper insulation for his system, although I readily admit that at 200 pounds nitrogen pressure either type is safe for 120-kv service. The reason for this is that the electrical strength of nitrogen gas at 200 pounds pressure is almost as great as that of the solid insulation.

H. M. Hobart of our company demonstrated this in his development of a high-pressure gas cable system in which bare rigid copper conductors were held in place in a steel pipe by insulator spacers at intervals. Installation difficulties and low impulse strength were the factors that prevented commercial use. Sixty-cycle strength was sufficient for 138-kv service at pressures in the order of 200 pounds and above.

Dry unimpregnated-paper insulation also has sufficient 60-cycle strength at 200 pounds gas pressure for high-voltage service. Low impulse strength and the difficulties of moisture absorption are its chief handicaps. It is absolutely essential to operate dry-paper cable well below the ionization starting voltage, which, at reduced pressure, is lower than that of impregnated paper because of the relatively large volume of void space involved. Ionization action in this type of cable is cumulative, and failure will occur quickly after incipient ionization once starts.

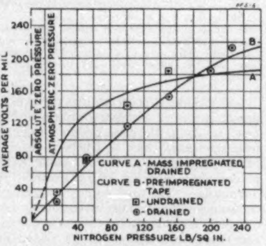

Figure 6. Voltage stress on solid insulation at which initial ionization starts in drained gas-filled cable

Preimpregnated-paper tape applied dry without flushing compound, as used by Mr. Faucett, overcomes the difficulties of ready moisture absorption and low impulse strength, but in all other characteristics it is about midway between dry paper and mass-impregnated paper. At butt spaces, wrinkles, tears, and other such points of nonhomogeneity, the void spaces are just as large as with dry unimpregnated paper. Ionization-starting voltage as a function of gas pressure is accordingly about the same, but incipient ionization is less dangerous because of the barrier effect offered by the impregnated tapes. It is important, however, to operate well below the ionization-starting voltage and eliminate any possibility of incipient ionization. There is no surplus compound surrounding the void spaces, which are not only larger for this reason but lack the ability to extinguish incipient ionization by wax formation and self-healing action.

This characteristic would be more clearly understood if Mr. Faucett had presented charts showing power-factor change and general behavior on long-time full-temperature load-cycle endurance tests at voltage steps always a little above ionization starting voltage. We are running such tests now at 200 pounds pressure on both mass-impregnated and preimpregnated insulation.

In the meantime Figure 13 of Mr. Faucett's paper plainly shows the trend of this characteristic for preimpregnated cable. Even at 225 pounds pressure the cable always failed in from 4 to 16 days at a voltage only a little above initial ionization-starting voltage. This proves a lack of self-healing action such as exists in mass-impregnated gas-filled cable. The latter type would not have failed at all under these conditions. Incipient ionization would have been extinguished, and the cable would have remained stable indefinitely, necessitating one or more higher voltage steps and a longer time to cause failure. Reference to the test charts in my paper will confirm this.

A comparison of initial ionization-starting voltage stress as a function of gas pressure tells the whole story. Such a comparison is given in Figure 6 of this discussion where the ionization data for preimpregnated insulation, from Mr. Faucett's Figures 8 and 9, are superimposed on a corresponding curve for mass-impregnated insulation, from Figure 5 of my paper. It will be noted that at 200 pounds pressure there is practically no difference between the two types of insulation. For 120-kv service either could operate safely at the usual average working voltage stress of 140 volts per mil, although gas pressure could safely drop much lower for the mass-impregnated type without danger of ionization trouble.

When the same comparison is made with low- and medium-pressure systems there is a wide difference and, in fact, the preimpregnated type of insulation is not economically feasible in this range because of the heavy wall of insulation that would be required. Low-pressure gas-filled cable of the mass-impregnated type operates at an average voltage stress of 65 to 70 volts per mil and average gas pressure of 12 pounds. The ionization-starting voltage stress of the preimpregnated type is less than half the required working voltage stress. Medium-pressure gas-filled cable, mass-impregnated, is designed to operate at 95 volts per mil, 30 pounds gas pressure. Here again the ionization voltage of the preimpregnated type is less than half the required working voltage stress.

In Beaver's high-pressure gas-filled cable with reinforced sheath, preimpregnated insulation was necessary, for he had no means of transmitting and controlling gas pressure along the cable length, other than the small butt spaces between tapes. In a high-pressure pipe cable system, however, far better pressure control exists than Beaver could ever hope for, and, in my opinion, the need of preimpregnated insulation no longer exists.

There is another important point to be considered. Aging studies with impregnated paper show that the rate of absorption when exposed to oxygen and moisture, as well as the rate of deterioration from this cause, are inversely proportional to the viscosity of the impregnating compound. For a given quantity of absorbed impurities, however, the ultimate results are the same. It merely takes a longer time to reach these results when heavy compound is involved.

This being so, it is just as important to exclude all traces of oxygen and moisture impurities in a system using preimpregnated insulation as in one using mass-impregnated insulation. Considering the much greater degree of surface exposure of preimpregnated tape from the start of production up to final sealing of the completed system in the field, I do not see how it is possible to exclude impurities to the same degree. This problem is common to all types of impregnated-paper cable and not to gas-filled cable alone.

If anyone doubts the importance of excluding oxygen and moisture he is referred to F. M. Clark's paper, "Factors Affecting the Mechanical Deterioration of Cellulose Insulation,"[2] presented at the convention day before yesterday. The effects of even traces of oxygen are already well known, but Mr. Clark's conclusion that insulation life is halved for each doubling of moisture content is not generally known.

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1. LOW-GAS-PRESSURE CABLE, **G. B. Shanklin.** AIEE TRANSACTIONS, volume 58, 1939, July section, pages 307–15.

2. FACTORS AFFECTING THE MECHANICAL DETERIORATION OF CELLULOSE INSULATION, **F. M. Clark.** AIEE TRANSACTIONS, volume 61, 1942, October section, pages 742–9.

E. Johansson (General Cable Corporation, Bayonne, N. J.): In the paper on 120-kv compression-type cable, breakdown values of 436 and 445 volts per mil of cable insulation are given for two joints tested. It may be noted that the joints used in these tests were not identical with the joints in the field installation but represented a stage in the development of the final joint. A subsequent test on a joint of the final construction produced a breakdown value of 602 volts per mil of cable insulation with the same test program and after a total time undervoltage of 11 hours.

The difference in performance between the two types of joint is explained by the construction, a small quantity of varnished cloth having been used in the first two joints but paper alone in the later joint. The copper conductors are elliptical, so that the cross sections of the two conductors to be joined will not line up normally. This discrepancy is accommodated by using a specially shaped twisted elliptical connector. In order to produce smooth insulation over the connector it was thought essential at first to use a few layers of varnished cloth next to the connector, but later development permitted satisfactory use of paper tapes even at the connector.

In applying paper tape to these joints, special care was taken to minimize access of moisture to the paper. The splicers wore rubber gloves, and the insulation was flushed with hot oil at intervals. As a result there was only a slight increase in the power factor of the tape when applied under severe conditions, that is, during the days in the summer when the relative humidity was high (65–70 per cent).

The following comments supplement the

paper on 120-kv high-pressure gas-filled cable:

One stop-joint for this cable was given a 60-cycle dielectric-strength test. The joint was first subjected to a constant voltage of 140 kv for 101 hours at 225 pounds per square inch gas pressure; then the voltage was increased five kilovolts every 24 hours to and including 180 kv, making the total time under voltage 293 hours. The gas pressure was then lowered to 100 pounds per square inch, and 140 kv was again applied for 34 hours without failure. The preceding test is well above the 170 kv for six hours required under the Association of Edison Illuminating Companies specification for samples of 120-kv oil-filled cable. The dissection of the joint showed no visible effects of electrical stress.

As stated in the paper, tests were made in the laboratory to determine the amount of drainage in the cable at high temperatures. A cable was heated to 100 degrees centigrade in a vertical position for 43 days, and the space occupied by the gas in the insulation was measured before and after heating. The proportion of the insulation occupied by gas increased from five per cent before heating to nine per cent after the 43 days of heating. This determination was made on a sample taken from the top of the inverted *U*, where the maximum drainage took place.

The paper describes incipient or arrested failures produced under test conditions. The following may be added concerning other characteristic appearances of these incipient failures. In a number of cases, burning or tree design was found in layers of paper remote either from the conductor or the sheath, and no apparent connection was found between these and the conductor or the sheath. However, on extraction of the compound from the tapes lying between the conductor and the failure, it was possible, by examination through a microscope, to find punctures in these tapes. These took the form of a large number of small holes and could usually be found in all tapes down to the conductor. It is felt that, while this connection was not actually found in all cases, a sufficiently careful and complete examination would have shown a puncture extending to the conductor in each case.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): Referring to the two papers on gas-filled cables, the results presented show a fine record of progress in experimental and manufacturing development. The principle involved in the fine performances reported is the elevation of gas pressure to a point where the upward break of the usual ionization curve is pushed up to a voltage sufficiently above the operating voltage. However, it must be evident that ionization may be present in small quantities owing to surface irregularities in the gas spaces and other local causes of high stress.

My question is directed particularly to the long-time influence of this small residual ionization. Evidence of its importance are found in the data in the Faucett paper and in noticeable changes in the position of ionization curves as between Figures 8 and 9 of the paper. Mr. Shanklin directs attention to the increase and subsequent decrease of power factor caused by wax formation in Figure 7 of his paper. But even here the presence of residual ionization is indicated by the slightly greater ionization factor in the range 20 to 60 volts per mil at the end of 252 days than at the beginning. Moreover, substantial local ionization is possible without reflection in the value of over-all power factor. Indeed the latter is usually not affected until the ionization assumes such large proportions that breakdown is immediately imminent.

The authors agree that further studies and performance tests are necessary for the gas-filled cable. I venture the hope that future studies will include that of the influence of local ionization of small volume on the immediately adjacent insulation wall.

Referring further to the Faucett paper on gas-filled cable, will one of the authors please give the numerical data back of the statement on page 663, beginning of column 3? What is the over-all specific inductive capacity of the gas-filled cable as compared with that of the same cable before draining? Somewhere in these papers it is stated that comparatively little oil is lost in the draining process. It would be interesting to have the figures leading to the conclusion that the stress in the gas spaces is less than that in other types of insulation.

In the same paper it is interesting to find such very clear data indicating in Figure 13 that the approximate life to failure varies inversely as the 7.5 power of the voltage. Heretofore data on this question have been very meager. As to the dielectric strength–time behavior below one hour, the impulse tests reported indicate that toward very short time values, the curve of Figure 13 will again turn up. This would result in a curve closely similar to those for other types of insulation as reported by Montsinger and others.

K. S. Wyatt (Phelps Dodge Copper Products Company, Yonkers, N. Y.): This discussion of the three papers describing extra-high-voltage cable designs which employ compressed gas parallels the session on the same subject at the Paris conference (CIGRE) in 1939. My contribution to that discussion is on record, and my remarks then hold good today. One of these designs (compression cable) employs compressed gas external to the lead sheath merely as a mechanical means of compressing the cable on the cooling cycle to insure freedom from vacuous and gas spaces; at the other end of the scale are the two designs which employ compressed gas in the insulation itself. These three papers evidently indicate that we are entering a transition stage in extra-high-voltage cable design: the new designs, with high-pressure gas-filled insulation, are challenging the older conservative designs, based on keeping filled with oil all spaces within the cable that are not filled with solid material, namely, the oil-filled, the compression, and the Oilostatic cables. The end result will no doubt be that no one design will be employed exclusively for all conditions, that is, each of several designs will find application in a restricted field.

At the time of the Paris conference in 1939, Beaver had a semi-commerical gas-filled cable at 132 kv installed as a loop of about one mile length on the Central Electricity Board system in England with more than a year's operating experience. He had also in operation for several years a number of gas-filled cables at 33 and 66 kv. Kirsch of Berlin (AEG), and Schneeberger of Brugg (Switzerland), were talking of installing operating lines at 33 kv with thick insulation and low gas pressure and gradually feeling their way to over 100 kv by increasing the stress and gas pressure. Callenders were installing at this time about a mile of 66-kv gas-filled cable of the loose-sheath type in London. While I believe an insulation thickness of 240 mils was employed, the fact that they wished to use only 100 mils for 33 kv indicates their faith in this design. Those who adhere to the principle of filling the insulation with oil rather than with gas should not lightly pass by these new designs, for they merit careful consideration.

The authors and the Detroit Edison Company in making these investigations of the compression cable and gas-filled cables at 132 kv have performed a great service for the industry. It is only to be regretted this was not done five or ten years earlier. The paper on the compression cable makes two important contributions:

1. The studies confirm for American loading conditions and conductor sizes the considerable body of work done abroad continuously since 1926.
2. A highly important design feature has been introduced, which, I understand is due to L. I. Komives, namely, the use of three S.L. cores cabled together with short lay without binders to cushion longitudinal expansion thrusts on the joints, throwing into the discard the cumbersome steel wire armor of the European design. (Europeans like to complicate, Americans to simplify and make foolproof). This gives us a simplified design which may well be called American-type compression cable.

I should like to point out that the terminal design is that of Siemens Brothers of London; so also is the joint design, except the ferrule scheme, which is, I believe, the subject in England of an Enfield Cable Works patent but which for several reasons they did not use. The work on increased fatigue endurance of lead under nonoxidizing conditions is an extension of the work of Haigh and Jones,[1] and of H. J. Gough, of the National Physical Laboratories of London. In the paper by the same authors on gas-filled cable the use of 80 and 100 degrees centigrade power-factor measurements to detect moisture pickup of impregnated-paper tapes is due to R. C. Mildner (with T. R. Scott) who, because of his considerable work on this matter, I think should receive credit.

Figure 7 shows power factors at room temperature or below up to 0.5 per cent which appear rather high. My associates and I have manufactured and installed since 1939 four compression cables at 66 kv, the 20 and 60 degree centigrade power factors of which were as low as 0.2 per cent and never higher than 0.3 per cent.

The British manufacturers have agreed to reduce insulation thicknesses for the compression cable with a limit on maximum stress of 280 volts per mil, instead of 216 volts per mil. Even if the *SMD* cable is in future reduced 20 per cent as the authors suggest, to 560 mils, this construction will still be more bulky (see Table IV).

Gas-filled cable at the lower voltages is understandable; I find it difficult to see any advantage at 132 kv, however, either economic or technical. Consider first the materials for an *SMD*-type gas-filled versus a compression or oilostatic cable; the corrosion-protected steel pipe is common to both; the conductor size for the gas-filled cable will certainly not be smaller; the insulation thickness for the gas-filled cable in the present instance is 20 per cent greater; a lead sheath is put on all three types of cables but is stripped off at installation for the gas-filled and Oilostatic types, while in the case of the compression cable the cost of its approximately half-thickness lead sheath will be partially offset by the cost of stripping and by the greater amount of lead in the larger-diameter gas-filled cable;* the *SMD* cable is protected with a helical *D*-shaped copper armor wire, as is the Oilostatic, while the compression cable has two four-mil copper reenforcing tapes with fiber bedding and protecting tapes. The *SMD* cable therefore requires not less, but if anything, more materials and processing than the compression or Oilostatic designs.

Now consider the technical merits of *SMD*-type gas-filled cable versus compression or Oilostatic types: The factor of safety on long-time breakdown is about 2 for *SMD* as compared with 5 or 6 for compression and probably for Oilostatic cable. Thermal resistivity (insulation) for *SMD* is from 700 to 1,000 compared with 450 for cables completely filled with oil. In the event (rare, it is true), of a large hole in the pipe and loss of gas pressure, *SMD* cable must be taken out of service immediately, whereas with the compression type, the operator has 24 hours to make repairs. During installation in rainy weather or during operation the insulation is exposed to contamination in the *SMD* type, whereas with the compression cable the lead sheath protects the insulation. A large hole in the pipe might let in ground water which would not affect compression or Oilostatic types. Impulse strength of gas-filled cable, similar to *SMD* type, according to Beaver, is about ten per cent below, and according to R. Davis,[2] 66 per cent below standard types (for cable made with preimpregnated paper, but probably with gas films at atmospheric pressure). Many precautions against moisture pickup, and so forth, have to be taken during manufacture of the *SMD* cable because of the necessity for preimpregnating the paper tapes first and then applying them to the conductor in the factory atmosphere, but these are not necessary with compression or Oilostatic cable. The *D*-shaped copper armor wire is essential to the *SMD* type, not only for mechanical protection, but for carrying short-circuit currents, since there is no lead sheath to make contact with the *H*-type shielding. Not least important is the great difficulty of fault location for *SMD* cable in the absence of lead sheath.

Table IV. Comparative Dimensions of High-Voltage Cables

Single Conductor 138 Kv—750,000 Circular Mils

Type	Conductor Type*	Conductor Outside Diameter (Inches)	Conductor Shielding	Insulation Thickness (Mils)	Lead Sheath Thickness (Mils)	Lead Sheath Re-enforcement (Mils)	Over-all Diameter (Inches)	Long-Time Breakdown Stress (Volts Per Mil—Maximum)
Oil-filled	1/2-inch hollow core	1.18	No	560	133	0	2.57	1,000**
Compression	Compack oval	0.90 (Maximum 1.02) (Minimum 0.78)	Yes	560	95	78	2.37 (Maximum 2.48) (Minimum 2.25)	
Oilostatic*	Compack round	0.908	Yes	560	94	0	2.46	
Detroit *SMD**	Concentric	0.998	Yes	672	94	0	2.81	409***

* Dimensions include a helical *D*-shaped armor wire 100 mils.

** Established for the compression cable in England and in Europe; after severe load cycles.

*** Faucett, Komives, Collins, and Atkinson; on new cable.

To my mind it is quite certain that some ionization occurs at spots in the *SMD* cable during operation. Although the authors use thin paper in the conductor region to keep down the radial dimensions of the butt spaces, wafers show that such a cable after bending, pulling, and installing, will have some gas spaces of ten mils and even 20 mils radial dimensions. These being filled with gas, not oil, will certainly ionize at operating voltage. The power-factor voltage curve will not necessarily show up local ionization pockets in a long length, as Doctor Whitehead has shown. The authors state such ionization would be dangerous to their cable, although Shanklin admits such ionization occurs but says it is innocuous. However, Shanklin's ionization spaces are sealed with compound, whereas in the *SMD* cable the ionization pocket is part of a continuous gas film from conductor to sheath. Before the authors can state that no ionization goes on in an operating *SMD* cable, they should submit evidence with the magenta dye test on insulation after a year's operation. Because of this ionization, I feel there is some doubt as to the permanence of the insulation of this type of cable, and careful laboratory evidence after a five-year trial period would be helpful.

While the *SMD* cable will no doubt work satisfactorily, it seems difficult to see the reasons for choosing it in preference to the compression cable. When you strip off the lead sheath, you throw away a very considerable factor of safety against breakdown, expose the cable to contamination, leave the operating man with no alternative in the case of a large hole in the pipe but to take the cable out of service immediately and make fault location difficult, without effecting any monetary saving. If the compression-cable system, American-type, were streamlined with styrene joints and styrene terminals to eliminate compensators and with styrene sheath insulators to eliminate sheath losses, it would seem to me to offer a great deal more to the operating engineer than the gas-filled types. To quote my concluding remarks at the Paris conference, "We do not think it should be taken for granted that the gas-filled cable cannot be made workable. At present, the gas-filled cable with breakdown at only 100 to 150 kv per centimeter maximum stress hardly comes up to the oil-filled and the compression cable with breakdown at 400 kv per centimeter or over at maximum stress. If the operating engineer is to demand a gas-filled cable with the same factor of safety as the oil-filled cable or the compression cable, it means that the gas-filled cable is going to be very bulky indeed in external dimensions, and it is highly doubtful whether it can come within great distance of the compression cable economically."

In this discussion we have been interested mainly in comparing the pipe-line-type *SMD* cable with other pipe-line-type cables. Where the flexibility of the duct and manhole system is desired, the oil-filled cable may be expected to hold its own.

REFERENCES

1. ATMOSPHERIC ACTION IN RELATION TO FATIGUE IN LEAD. *Journal* Institute of Metals (London, England), volume 43, 1930; *Transactions* Institution of Chemical Engineering (London, England), 1929.

2. *Journal* Institution of Electrical Engineering (London, England), volume 89, 1942, part II, page 52.

Herman Halperin (Commonwealth Edison Co., Chicago, Ill.): These three papers on cables employing gas at various pressures are of immediate practical value to some users. The people and companies behind these papers are to be congratulated. In my opinion, the selection of the type of cable to use should be based mainly on the economics involved and on matters relating to usage. By the latter, I mean not only reliability in service but also matters relating to construction, maintenance, time to make repairs, and personnel.

The first detailed discussion will be on the paper by Mr. Shanklin. His article states that to date experience indicates that the low-pressure gas-filled cable is satisfactory from a technical standpoint. On the other hand, solid-type cables operating at 12 kv, 22 kv, and 33 kv are giving satisfactory service. Aging tests, for example, on shielded three-conductor 12-kv cable in Chicago show that there is no appreciable change in ionization nor power factor after severe testing—in other words, it is perfectly stable. We have a low rate of troubles in service, and in any event we must expect, in laying out designs for service, that with any kind of cable failures may occur. If a man drives a steel point into a cable, then one will get a signal that there is trouble on a gas-filled cable, but anyhow the line will be out, and one will get the signal through the regular manner.

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* The saving for the Detroit seven-mile line amounts to $16,400 less cost of stripping and freight, that is, probably about ten cents per foot per core.

For cable made with preimpregnated paper, but probably with gas films at atmospheric pressure see reference 2.

Another point with us is that we use parts of existing 12-kv lines to make up a portion of a new line in many cases, and the use of the solid type simplifies our activities in addition to saving money.

In connection with single-conductor cables, I wonder what trouble might occur because of oil slugs, even if the gas connections are paralleled, when there are severe differences in line profile. These differences will affect all three phases equally.

The power factor–temperature characteristic of this type of cable appears to be less favorable than that of solid-type or oil-filled cable. Figures 6 and 8 of the paper indicate that even after stabilization the power factors at 80 degrees centigrade are two to three per cent as compared with about 0.4 or 0.5 per cent for new solid-type or oil-filled cable.

It is surprising that neither grading of the tape thickness nor conductor shielding produced any effect in test results. Is it possible that by making these changes in cable construction other unknown variables were introduced which offset the expected effects?

From creep tests made by Professor Moore at the University of Illinois under the sponsorship of Utilities Research Commission, it appears that Asarco cable lead should be satisfactory for 225–250 pounds per square inch. Tests in a dummy manhole in Chicago indicate that Asarco cable lead containing about 0.018 per cent calcium will withstand bending in manholes at least as well as ordinary lead.

In my discussion of the two papers by Faucett, Komives, Collins, and Atkinson, I refer first to the commercial installation of the gas-filled cable.

One question of considerable interest concerns the thermal characteristics of these cables. For a given load how does the temperature rise from ambient to conductor compare for these cables and solid or oil-filled cables on the basis of one line or two lines? Also, what maximum temperatures are considered permissible for the gas-filled and compression cables for normal and emergency operation?

The thermal resistivities of soil shown in Figure 4 appear to be lower than those reported by others. More data would be of interest. Since the ionization point is a function of the pressure, it would be of interest to learn what variations in pressure are expected in normal operation.

Figures 14 and 15 indicate relatively good impulse strength for the gas-filled cable which, however, had conductor shielding as mentioned by the authors. In other reports based on the Detroit tests, Mr. Komives has shown that the thoroughness of impregnation and smoothness of conductor surface had marked effects on the impulse strength. In 1938 G. B. McCabe presented data before the Edison Electric Institute showing that drained impregnated paper had only 60 to 70 per cent of the impulse strength of well-impregnated paper. These five types of cable in the figures cover a considerable range in degree of impregnation of the insulation and should, we believe, show some differences in impulse strength. This point is important, because the thickness of insulation used in some of these high-voltage cables is determined largely by the impulse strength.

No data are given on the effect of drainage on impulse strength. That drainage does have an effect on the characteristics of the cable is shown by the fact that drainage reduced the ionization point from 140 to 130 kv and reduced the breakdown strength by ten per cent. What are the possibilities that, through the years of service, considerable draining of compound may occur to cause more severe reduction in quality?

Regarding high voltage–time 60-cycle tests on new cable, it seems from our data that the oil-filled cable is somewhat superior to the gas-filled cable, and the unit strength of the oil-filled insulation will not change in service. This disagrees with conclusion 5 but does not affect particularly the installation of commercial gas-filled cable which has 20 per cent more insulation than is required for oil-filled cable.

It is of interest that failures occurred in the region of wide interturn gaps. May this be due to the field distortion caused by combination of solid and gaseous dielectrics or due to the greater probability of radial lining up of interturn gaps?

The connector used in the joints with a wall thickness equal to the diameter of the strands in the outer layer of the cable conductor is an improvement over present practice. Although we have reduced our wall thickness, still we have been using connectors which are unnecessarily heavy.

How much time is it expected will be required to make repairs in case of failure on such installations in steel pipe?

The half-round copper wire spiraled over the insulation of each conductor will have a voltage induced in it which might cause minute sparks to the steel pipe. Have any test data or calculations been obtained regarding these voltages and their effects?

Regarding the other article, drainage of compound may also be a factor in the compression cable. If cracks should occur in the sheath, there would be no warnings telling the operator that compound is draining from the cable into the pipe. This is important, because loss of pressure in the pipe is not considered hazardous so long as the cable is at elevated temperature. However, if gas has entered the insulation under such conditions, then such operation might produce electric breakdown. The further information on the sample with perforated sheath will be, therefore, of considerable interest.

It should be understood that a lot of questions do not necessarily deprecate a development when it is quite new, as are essentially the matters covered in these three papers.

F. W. Main (The Enfield Cable Works Co., Ltd., Brimsdown, Enfield, Middlesex, England): I am pleased to have the opportunity of contributing to the discussion on the three cable papers read at the Chicago summer convention. I have not had any direct contact with experimental work or experience of any installations of gas-filled cable, as my company has been interested in the development, supply, and successful operation of the compression cable in which a pressure medium, usually gas, although not in actual contact with the dielectric, compresses the dielectric through the medium of the lead sheath which acts as a flexing diaphragm.

The compression cable was formerly known as the pressure cable, being the first cable in commercial use employing gas under pressure; later, cables employing gas, either in the dielectric or in direct contact therewith, were constructed, and the term pressure cable was applied indiscriminately to all cables employing gas, and, therefore, in 1940 the title of compression cable was given to the diaphragm cable to prevent confusion.

I would like to express admiration for the development work carried out in America in connection with the compression and gas-filled cables as indicated by the papers read, and I would also pay tribute to the earlier work published in 1939.

The electrical research work in connection with gas-filled cables has been necessarily involved because of the introduction of gas into the active dielectric hitherto normally consisting only of paper and impregnating oil or compound. In the compression cable, however, no elaborate electrical research has been necessary, as in general terms it is essentially a mass-impregnated straight-type cable. Any developments in the electrical sphere have not been peculiar to the compression cable but have been toward improving, by design and shop processes, features and characteristics that can be, and have been, embodied in general impregnated high-voltage and super-voltage cables.

In the paper by Faucett, Komives, Collins, and Atkinson on the 120-kv compression cable, it is stated that the *HSO* construction has been discontinued abroad. This is not quite correct; although the original compression cable installation, which has been in successful operation in Great Britain since 1932, was of the *HSO* construction, and the subsequent cables in Europe, and more recently in Great Britain, have been of the *HSL* pattern, a cable is being manufactured at the present time of the *HSO* type. This cable is not being drawn into a pipe as in the case of the original British installation, as it is a self-contained type, an outer lead sheath acting as the container for the pressure gas.

As the lead-sheath diaphragm in contact with the dielectric breathes in sympathy with the fluctuations of cable temperature, it was considered by some engineers that the lead sheaths of an *SL* three-core cable, being smaller in diameter than the sheath of a three-core screened cable, would be more satisfactory; any fears, however, of a sound lead sheath not giving satisfaction in service have been dispelled by tests made in Europe and now confirmed by experiments carried out in America as described in the paper. The *SL* type has also met with favor, as jointing in some respects is simplified, but this is not peculiar to compression cables.

The relative merits of the pipe-line and self-contained types of compression cable cannot be dismissed in a sentence; local conditions or even prejudice may affect a decision. Obviously, the pipe-line type affords the greater mechanical protection, and the cable can be manufactured in longer lengths; the pipe-line can be installed at a convenient time and the cable drawn in subsequently; the compression cable can be drawn into an existing pipe provided the line in gastight, and, further, if there be sufficient clearance in an existing pipe-line, the cable in position can be withdrawn and a larger cable substituted to give increased capacity. In Great Britain only one of the compression cable installations is of the pipe-line type.

In the paper reference is made to an additional lead sheath to protect the reinforcement tapes of the self-contained cable from corrosion; this additional sheath is, however, not necessary, as there is a number of finishes available that can be used to give further protection and to render the cable impervious to moisture. For this purpose, unless the purchaser specifies an additional lead sheath, we use a sandwich finish which consists of at least two layers of nonfibrous material such as a rubber-bitumen compound reinforced between the layers with a fabric tape.

Mention is made of a spiral wire between the diaphragm lead and the gas-retaining lead of a self-contained compression cable to ensure a free channel for the pressure gas; this we have found is unnecessary, as we employ a circular gas-retaining sheath over a noncircular lead-sheath diaphragm for both three-core screened and single-core types which automatically provides the necessary space.

It is noted that concrete was applied around the pipe in the trench along the armored section of the experimental line. I assume that there is no significance in applying concrete in this particular section.

The investigation into longitudinal movement, comparing the effect of bound and unbound cores, we find is very interesting, and we would welcome more details.

In the paragraph dealing with cable design, mention is made of impregnated jute fillers laid up with the lead-sheathed cores: we have not found the use of fillers necessary.

We are interested to note that tight lead sleeves have been used successfully in joints, whereas we have favored fluted brass or copper sleeves. We shall be interested to have some notes on the effect of fatigue tests taken, especially in the proximity of the wipes.

With reference to terminals, we have given some attention to the use of a Bakelite tube inside the porcelain as discussed in the paper, but we concluded that the complications involved did not warrant the benefits obtained.

Details of load cycles are given in the paragraph headed "Field Test," but whereas a figure for the copper temperature is given in the paper for the 120-kv gas-filled cable, no figure is given for the compression cable. We anticipate that this would be a lower figure, however, because of the lower thermal resistance of the compression cable compared to that of the gas-filled cable.

With regard to the question of power factor, the figure we obtained for cables manufactured in Great Britain is in the neighborhood of 0.0028–0.0030 at 20 degrees centigrade compared to 0.0040–0.0043 as shown in Figure 7 of the paper.

Coming to the paper on the 120-kv-high-pressure gas-filled cable, there are a number of causes suggested as being contributory to the satisfactory solution of the gas-filled cable. Of the seven causes mentioned, surely five at least have been developed by various cable manufacturers for the improvement of super-voltage cables generally and are, therefore, not peculiar to this one type.

It would appear that in the types of gas-filled cable described too much emphasis is placed on the quality of the compound used to impregnate the paper as, by virtue of the lower permittivity in the gas layers, the stress in the impregnating paper is somewhat relieved through higher stresses being thrown into the gas space.

We shall be interested to have more details of the saturant used. A viscosity figure of 3,000 is given for the compound used for preimpregnating the paper for the gas-filled cable; we wonder whether this figure should be 300. It is noted that in Mr. Shanklin's paper the optimum viscosity of compound used is given as 100; we use a mineral-oil-base compound having a viscosity figure of the order of 285 Saybolt.

In Figures 14 and 15 impulse strength values of the *SMD* cables are given and compared with results of certain other cables. One questions whether the other cables were manufactured during the same period as the *SMD* cable and whether the same improved constructional features were common to all, for it does not seem reasonable to anticipate that a dielectric containing gas, even if under pressure and having an uneven stress curve through the whole dielectric, will have impulse values higher than the more homogeneous oil-filled or compression cable, or even a new well-made solid cable.

It is not surprising to learn that the factory shops, where the gas-filled-cable cores are paper-lapped and laid up, are controlled for humidity. This appears very necessary as the paper is preimpregnated, but these precautions are not essential in the manufacture of compression cable.

The lead sheath of the *SMD* cable and the other gas-filled cables is removed on site, and it is found that depreciation takes place in the cable during the process; this is to be expected, however, as it is certainly a delicate operation and must discount to some extent meticulous care taken during manufacture.

In the paper it is stated that the minimum safe-operating gas pressure for the cable is approximately 100 pounds per square inch; yet earlier, it is stated that the electrical tests which, of course, would be at a voltage higher than the working voltage, should be applied to the cable with only 30 pounds per square inch of nitrogen pressure.

Reference is made in the paper to tests for radial power factor and results are given in Figure 10. There is a difference, however, in the radial power-factor curve for mass-impregnated paper as against a true curve for gas-filled cable; the curve given for gas-filled cable only shows power factor in the papers and gives no indication of the effect of the gas space on the shape of the curve through the whole dielectric. One wonders what the shape of the radial power-factor curve would be if the humidity in the shops during manufacture were not controlled.

Unless we misinterpret the authors' statements, we are unable to agree with the inferences given as drawn from Figure 8. The stress in the paper of the insulation is reduced only because the distribution of stress throughout the dielectric is distorted since the permittivity of the gas spaces is of the order of one third of the value of the impregnated paper, the stress in the spaces being correspondingly higher than that in the paper. Therefore, the electrical strength of the impregnated paper cannot be used to full advantage; otherwise the gas spaces would be overstressed, and destructive ionization result.

One can appreciate the usefulness of the low-pressure gas-filled cable, as described in the paper by Mr. Shanklin, for voltages between 30 and 50 kv, where a high breakdown stress is not so necessary as with the higher voltages. As the gas is only at a low pressure, a reinforcement is not necessary, and consequently there is a reduction in cost and diameter, the latter being important in situations where space is at a premium, but the position is not so clear for higher-voltage cables.

It is very surprising to note that, after the cable has been drained, the finished product was almost as well filled as a solid type cable. This is contrary to our own experience.

The authors express doubt as to the mechanical soundness of employing a reinforced lead sheath to contain a gas pressure of 200 pounds per square inch. This, however, is standard practice in England for self-contained compression cable and has given complete satisfaction.

A statement made concerning the effect upon power factor of exposure to air can be appreciated.

While the difficulties of stripping the lead sheath from a gas-filled cable on site as it is drawn into the steel pipe line have been somewhat overcome, it would seem more logical to draw a compression cable into the pipe line and avoid this stripping operation with attendant risks, and at the best, a certain amount of depreciation. It is admitted that a compression cable has a higher factor of safety, and, in the event of the lead sheath diaphragm of the compression cable being damaged, the cable would then automatically become practically a gas-filled cable without the costly complications involved during the installation of such cables.

G. B. Shanklin: In slightly different forms the same two questions have been raised by Halperin, Komives, Del Mar, and Hickernell. In effect, these two questions are:

(*a*). Why does low-pressure gas-filled cable show more increase in dielectric power factor on overvoltage load-cycle tests than does the best modern type of solid cable, and what significance does this have as related to stability in service?

(*b*). Why is average stress used rather than maximum stress in the design of mass-impregnated gas-filled cable, and why do strand shielding and extra thin paper tape appear to show no appreciable benefit on comparative load-cycle tests?

Initially and in the absence of ionization the dielectric power factor of both types is the same. The increased power factor of low-pressure gas-filled cable on load-cycle aging tests is merely due to the fact that this type of cable has a greater volume of gas space to be ionized, and the signs of ionization, accordingly, show up more clearly when the cable is subjected to an overvoltage stress where severe ionization can take place. This has no significance at all as related to stability in service. The chief goal of service stability is uniform control of ionization and prevention of a cumulative action. Gas-filled cable gives this through pressure control. That is why laboratory tests on short lengths can be made to simulate closely service conditions and furnish a reliable yardstick for design work. Solid-type cable lacks this uniformity and close control under service conditions, requiring heavier wall of insulation as a protection against localized cumulative weaknesses.

Strand shielding is, in effect, a method of carrying stress grading of cable insulation a step further, to better equalize stressing throughout the insulation wall. It is effective only when the inner layers of insulation are of equal or lesser strength than the outer layers. It is not effective when the inner layers are somewhat stronger than the outer. The maximum-stress theory is governed by the same conditions and does not hold when the successive layers of insulation in an outward direction are of decreasing strength.

When a mass-impregnated gas-filled cable is drained of surplus compound, the laws of capillary attraction are followed. The outer layers of insulation are drained the most, and the largest void spaces occur here. The inner layers of insulation are drained the least, and the void spaces in this location are of minimum size. As a result, ionization either starts in the outer layers first or occurs uniformly throughout the whole insulation wall. It never concentrates in the inner layers alone.

To sum the matter up, mass-impregnated gas-filled cable already has, in effect, automatic grading that counterbalances the higher stress near the conductor. For this reason, any further attempt to grade, such as by use of strand shielding or extra thin paper in the inner zone, does not accomplish so much as would be expected. The use of average stress rather than maximum stress in design work is explained on the same basis.

It should be emphasized that these conditions do not hold for preimpregnated gas-filled cable, where the region near the conductor is the critical zone, and means of improving grading are necessary.

In comparing solid and gas-filled types of cable Mr. Halperin stated that if a man drove a steel point into a cable you would get a signal in the regular manner with either type. He means, of course, that this would happen if the damage were sufficient to immediately cause short circuit. A few months ago a man did drive a steel point into a gas-filled cable, but only the sheath and outer wrappings were damaged. After the gas-pressure relay signal, the damage was located and repaired within a short time without service interference. Service records show that the large majority of failures in solid-type cable are due to sheath damage or leakage from various causes. It is reasonable to assume that gas pressure supervision will largely eliminate such interruptions to service and, so far, field experience has confirmed this.

Mr. Del Mar expresses the belief that the highest possible viscosity of impregnating compound is desirable. In the case of preimpregnated gas-filled cable I readily agree. This type of cable would not work well without extreme viscosity of impregnating compound. In the case of mass-impregnated gas-filled cable some years of experience has demonstrated that lesser viscosity of impregnating compound gives the best results. It is important to obtain the best possible initial impregnation of the heavy wall of insulation. Drainage of surplus compound and capillary balance will then give the right degree of automatic grading, as previously described. These results cannot be accomplished so well with extreme viscosity of compound.

Referring to the medium-pressure system, Mr. Komives is correct in stating that high creep-strength sheath is a new development and needs to be proved. We can only say that this work is progressing well and looks promising. Double-reinforced sheath at 30 pounds per square inch, however, is proved thoroughly in service, and with present quality of sheath the pressure can be increased, provided it is within safe strength limits of the lead wipes. A sudden jump to 200 pounds per square inch, as some propose, does seem, however, to be out of proportion.

Dr. Whitehead asks about residual ionization in gas-filled cable. All of the evidence we have been able to obtain indicates there is none of noticeable effect. Long-time load-cycle tests are always run at an overvoltage sufficiently high to produce pronounced ionization and are of no direct value in clearing up this point. A study of the charts will show that, at each voltage step before ultimate breakdown, ionization is extinguished, and the cable stabilizes because of self-healing action. This would indicate that ionization in this cable is retrogressive and that residual or incipient ionization cannot be sustained. Actual field experience is more conclusive. Gas-filled cable operates at a voltage stress only a little below the ionization-starting voltage stress, as represented in Figure 5 of my paper. This must mean that during switching surges and other overvoltage transients there are at least brief flashes of ionization discharge in the cable. These discharges must be immediately extinguished or, if sustained, must be of negligible effect, because no evidence of ionization deterioration has yet been found in service.

Whether or not Doctor Wiseman's belief that pipe cable systems are not limited to open ground but are comparable to the usual flexible duct system under paved streets is something that will have to be proved by experience. It all depends upon how many leaks are going to occur in the pipe systems and how much tearing up of the streets will be necessary in locating and repairing them. If the means being taken to prevent such trouble succeed, then Dr. Wiseman is correct.

His claim that, for a given amount of power to be transmitted, Oilostatic cable should have a conductor size equal to or smaller than that required for single-conductor oil-filled cable in ducts is based on his well-known method of calculating current-carrying capacity. To accomplish these results Doctor Wiseman uses a soil thermal resistivity of 80 net in his calculations for Oilostatic cable. Most of the rest of us use the more conservative and generally accepted standard value of 120 net, corresponding to a moisture content in the order of five to eight per cent. Only a porous soil completely saturated with water could show a thermal resistivity as low as 80 net. It stands to reason, then, that the relative carrying capacities of the two types of cable are not directly comparable on Dr. Wiseman's basis.

I. T. Faucett, L. I. Komives, H. W. Collins, and R. W. Atkinson: The authors are very much pleased with the interest shown in these two papers and wish to thank the many engineers who have submitted discussions. For the most part these discussions either contribute or ask for additional information. A few are somewhat critical of one or the other of the two types of cable which we have described. Mr. Wyatt wonders why preference could have been denied the compression cable, and Mr. Shanklin fails to see how there could have been any doubt in selecting the gas-pressure cable. It is significant that each of these discussers has shown so much confidence in the type of cable of which he has previously been best informed. Our own very complete investigation in the laboratory and in the field of *both* types gives us confidence in *both* and indicates that satisfactory results are to be expected from either.

In his approach to the high gas-pressure cable Mr. Shanklin has a different viewpoint, concerning several aspects, than have the writers. Mr. Del Mar has summarized these differences very conveniently and has also declared his agreement with the writers in most cases. Mr. Del Mar's tabulation makes a very convenient basis for this discussion.

The first two points may be discussed together. Shanklin uses a much lower-viscosity saturant than the writers and expresses concern about the exposure to the air occasioned by the use of preimpregnated paper. He also states that he has tried high-viscosity compound and has found it apparently inferior. It seems that the difference between Shanklin's results and ours is not wholly in the viscosity. For instance, Figure 6 in his paper, which he considers typical, shows that the power factor of his cable below the ionization voltage at 80 degrees centigrade increases rather quickly to three per cent and then drops to around two per cent, remaining at that value for the duration of the test. On the other hand, the power factor of our insulation is less than one half of one per cent from 40 to 100 degrees centigrade and has shown no change as a result of any of the testing. Mr. Shanklin's discouraging results tend to confirm our contention that our very good results are due to other good qualities in the saturant in addition to its high viscosity.

Mr. Shanklin grants theoretical advantages in the use of thin tape but says that thin tape is more expensive and difficult to apply, tends to wrinkle more, and will not withstand bending well. He apparently concludes that these difficulties cannot be overcome. It seems likely that this is the reason for his failure to obtain good results with thin tape. In our cables we overcame these difficulties by combining a number of means to accomplish that end. We used a superdense paper that had not been previously available in such thin tape. Because of the great stiffness and smoothness of this paper, it slides instead of wrinkling during bending of the cable. This sliding is also facilitated by the lubrication given by the presaturation, as well as by use of proper amount of tension during taping. Finally, the amount of thin paper was limited to that required for electrical reasons. In the region of lower stress, where the mechanical requirements are more severe, successively thicker tapes were used. Not merely were we able to increase the electrical strength of this type of cable with such tape, but we have accomplished a similar result in other types. Beaver has also accomplished important gains with thin tapes of graded thickness, even without the special superdense paper, but he too has had the benefit of presaturation. The experimental high-stress 132-kv cable with only 386 mils of insulation which we manufactured for the Commonwealth Edison Company in 1929 and which has been reported on by Mr. Halperin[1] was made with three-mil paper tapes. This cable showed an exceptionally fine record for the $8^1/_2$ years net time of testing after which impulse-strength measurements were made on the cable by the Detroit Edison Company. Maximum impulse stress values of 3,250 and 3,450 volts per mil were obtained with corresponding average stress values of 1,890 and 2,010 volts per mil.

The case for strand shielding is similar to that for tape thickness. For many years the theoretical advantages have been recognized. Beaver and others in Europe have found practical advantage for various types of cable. In two papers in 1940 we pointed out important advantages of strand shielding in solid-type cables and we have been able to demonstrate important values in other types. It may be remarked that it is not improbable that the gain occurs more from the shielding from stress of the nonsolid dielectric between strands than from the reduction of stress that is discussed in the classical theories. At any rate the remarkably high dielectric strength for surges which has been obtained on shielded high-gas-pressure cable seems explainable only if the strand shielding is credited with a very substantial benefit.

Mr. Shanklin would like to know, on purely technical grounds, why preimpregnated paper was selected in preference to mass-impregnated paper. The answer is simply that a very high-viscosity compound was essential in order to prevent migration, and the only means by which it was possible to impregnate the tapes thoroughly with such a compound was by the preimpregnation of the tapes. Thus, from the very important standpoint of compound migration, this produces a cable far superior to that which can be achieved with the lower-viscosity compound which must be used with mass impregnation. At the same time superior electrical properties—low power factor and high stability—have been obtained by the use of this compound as shown by the test data reported in the paper.

Mr. Shanklin offers his own answer to his above question by saying: "A comparison of initial ionization-starting voltage stress as a function of gas pressure tells the whole story," and states that such a comparison is given in Figure 6 of his discussion. This comparison is of sufficient interest to merit careful study. In his present paper, Mr. Shanklin says that additional data obtained since the first paper allow a more accurate construction of this curve than before, and it is noted that the values now given are over ten per cent higher than in the old curve. Mr. Shanklin gives all his data in terms of average stress. As recalled to us by Mr. Del Mar, Mr. Shanklin grants: "It is better to deal with maximum voltage stress than average," but pleads the greater convenience of the use of average stress. It is allowable to serve this convenience only when it does not lead one astray by provoking comparisons under conditions where the ratio of maximum and average stress differ so greatly as to distort the comparison. Since Mr. Shanklin has described curve *A* in his paper just referred to as "a representative average of all tests on single- and three-conductor gas-pressure cable," we may suppose that there was no distortion of the comparisons of his own cable by his basis of comparison. In order, therefore, to compare his tests with others on the basis of actual maximum stress, we have determined the actual maximum stresses for one of the cables which he used and on which he has given dimensions. This cable is a single conductor 400,000-circular-mil stranded conductor with one-half-inch hollow core, 300 mils insulation, the stress being calculated by application of the ordinary logarithmic formula from the average stress as given on curve *A*. Likewise the maximum stress was determined for the cable represented by curve *B*. Thus it is found that, at 200 pounds pressure—where Mr. Shanklin states that there is practically no difference between the two types—the *maximum* stress for initial ionization is 300 volts per mil for our preimpregnated cable, as compared with only 236 volts per mil for his mass-impregnated cable. In other words, a 27 per cent higher ionization voltage for preimpregnated paper certainly justifies its selection.

This difference is emphasized still more when it is remembered that the preimpregnated cable on which he has made the comparison was drained after being maintained at a temperature of 100 degrees centigrade throughout its cross section for 65 days in an inverted *U* position. His 1939 paper indicates that curve *A* was based on drainage from load cycles reaching a maximum temperature of about 60 degrees centigrade "for a number of days." Thus, if we take Mr. Shanklin's suggested comparison of ionization tests on a comparable basis of dimensions and conditions, the results show another very effective reason for choosing the type of insulation we did.

It is rather important to discuss certain comments on test methods and significance of tests as made by Mr. Shanklin. He indicates that we should have supplied test data showing load-cycle endurance runs of the type which he has used and which he has found to stabilize his cable. However, as will be shown, the tests and criteria which we have reported have been much more severe and searching than those used by Mr. Shanklin. Since Mr. Shanklin's data for high gas pressure is limited to initial ionization measurements, we must perforce analyze his comments in the light of his data obtained at low pressure. He mentions a certain cable showing little ionization at 77 volts per mil which he tested at various voltages from 85 volts per mil to 120 volts per mil. The cable "stabilized" at each successive step except the last where "the stress was too high for the same kind of stabilization to take place, and failure occurred in a few days." He names an operating stress of 65 volts per mil for this cable and believes that the experience with similar cable operating at this stress indicates "that there is a good margin of safety and some latitude for increasing present working voltage stress." Moreover, as pointed out by Mr. Del Mar, he makes no distinction between cables having different ratios of maximum to average stress, and thus his recommendation, based on average stress, will lead to operation at considerably higher maximum stress on some other cables than on this one cable. In any event the average operating stress which he recommends is 54 per cent of that at which his stabilized cable sample breaks down "in a few days." The cable at Detroit operates at 70 kv, or only 45 per cent of the 155 kv which is withstood indefinitely by most of the samples, or about 50 per cent of the 140 kv required to break down in 120 hours the sample drained at 100 degrees centigrade for 44 days. This serves to emphasize the effectiveness of the test procedure which we have used and to show that it is even more searching than his and that the rating of cable in this way is considerably more conservative than would be obtained by following his program and criteria.

Mr. Shanklin states in reference to gas pressure cable, "We started initial development work in 1932, the principles of which are described in United States patent 1,991,230 filed in July 1933." Our own much earlier work in this connection was disclosed in United States patents 1,524,124, 1,541,937, and 1,651,590 the first of which was filed in 1920 and granted in 1925. These patents have been recognized as basic for gas-filled cable (see letter by P. V. Hunter in *The Electrician* December 21, 1934 and article by C. Turnbull in *Electrical Review*, page 425, 1936).

Mr. Del Mar suggests that the high-pressure gas-filled pipe-type cable system is virtually the Oilostatic system without the oil and oil-pressure accessories and therefore should be interesting from a first-cost standpoint. We feel that the *maintenance* cost on this type of cable will be equally interesting.

In Mr. Del Mar's discussion on compression-type cable it is intimated that Detroit had taken on the combined difficulties of the pipe-type cable and those of the duct-type cable, whereas actually, the commercial line as installed in Detroit cannot have any of the more common troubles of the duct-type cables, since these cables have no lead sheath. Even though the compression-type cable with its lead sheath were used, the pipe would eliminate the major causes for faults occurring on duct-type cable, namely, external mechanical damage, sheath failures in manholes and at duct edges caused by cable movement, and failure from sheath corrosion.

Del Mar and Hickernell have commented on our statement that the power-factor tests on the installed cable, which were not made above 28 degrees centigrade "show no evidence of deterioration." We quite agree that tests at this temperature are not sensitive to some types of changes that are observable in tests made at higher temperatures. However, some types of change are shown more readily at low temperature than high, and, moreover, if there are changes at high temperatures, these are usually accompanied, as admitted by these discussers, by significant changes at lower temperature. Moreover, abundant information on the stability of the high-temperature power factor is given by the laboratory tests which are reported. Figure 10 shows that there is no change in the radial power factor at 80 degrees centigrade after aging for 113 days at a temperature of 100 degrees centigrade throughout the cable cross section. At the time of this writing, there is still no change in the over-all 100 degree centigrade power factor after aging for over nine months at 100 degrees centigrade. Another sample reported was tested at 75 degrees centigrade throughout its cross section with a total time of heating of 1,250 hours and with voltage applied for 530 of these hours of from 1.7 to 2.4 times the rated voltage, and it showed the power factor to be constant throughout the test at a value of 0.3 per cent. The paper by C. J. Beaver and E. O. Davey gives data on cyclic aging tests made on this type of insulation and reports that no sign of ionization nor deterioration could be detected from such tests. The field measurements serve thus as a helpful supplement to the ample data showing the high stability of this cable. These data on the field installation will ultimately be supplemented by measurements at high temperature.

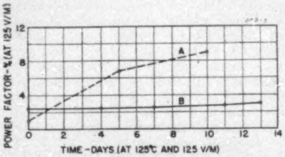

Figure 7. Power-factor aging characteristics metallic shielded conductor versus CB shielded conductor

A—*CB* cable model data from Rosch[2]

B—Commercial three-conductor cable with metal-backed paper conductor shield

Mr. Hickernell makes the comment that all that is needed to make this high-pressure gas-filled cable a truly up-to-date design would be to incorporate Rosch's carbon-black protection. Rosch has urged the value of carbon black as a means of maintaining original low power factor. However, the test data reported for the *SMD* cable show remarkable stability. Original low values of power factor have been maintained throughout the various long-time high-stress and high-temperature tests, and including long-time high-temperature tests conducted at stresses as high as 250 volts per mil. This may be compared with the data for cable "protected with carbon-black paper" shown in curve *A*, Figure 7, taken from Figure 3 in Rosch's paper.[2] This cable during ten days' testing at 125 degrees centigrade at a stress of 125 volts per mil increased in power factor at 125 degrees centigrade from the very low value of about one per cent at the beginning to about nine per cent after ten days. This may be compared with curve *B* of Figure 1 which shows data which we have obtained on solid-type cable with metallized paper shielding maintained at 125 degrees centigrade and 125 volts per mil for 13 days. It will be noted that although the initial power factor of this sample was nearly 2.5 per cent at 125 degrees centigrade, the power factor after 13 days was less than three per cent, showing incomparably greater stability than Rosch's carbon-black sample.

In regard to impulse strength, Mr. Hickernell refers to the data in the reports by L. I. Komives and G. B. McCabe and makes the statement that, "These data indicate the highest impulse strength was obtained with type-*CB* construction." There is nothing in either of these two reports that would justify any such statement. In Mr. Komives' report the only type of strand shielding tested was that having *CB* construction, and therefore it is impossible to make any comparison between it and cable having metallized-paper conductor shielding. In Mr. McCabe's Edison Electric Institute report, the average values reported did show a slight advantage in favor of *CB*-type cable, 1,870 volts per mil average stress as against 1,833 volts per mil for cable using this type of metallized paper. However, Mr. Hickernell evidently overlooks the fact that the value of 1,833 volts per mil is lower, because it includes terminal failures (not cable failures) on two of the three conductors tested and also overlooks the author's definite statement to this effect, that "It should be borne in mind that since conductors 1 and 2 failed in the terminals, the surge strength of the insulation of these two conductors was somewhat higher than the recorded values probably equal to or higher than the maximum value of average stress attained in same 18" (with *CB* tape).

Another point of superiority of the metallized tape used for insulation shielding is that it proved to be a very effective moisture barrier. Two samples of unsheathed *SMD* cable exposed to humid atmosphere for approximately two months showed no increase in moisture content of the insulation. The physical properties of carbon black are such that it is improbable that it would provide this desirable moisture barrier as does the metallized tapes.

It is difficult to understand the basis for conclusions drawn by Mr. Hickernell from the report of F. Farmer on cable in vertical runs. The only comparison we can draw concerning compounds in this report is that the compounds which showed low migration had higher viscosities at all temperatures than the compounds which gave trouble from migration. In this reference it should be noted that those cables made in 1915 to 1920 and impregnated with a so-called nonmigrating compound consisting of petroleum grease and rosin showed extremely low migration, despite the fact that several of them carried 1918 war loads which would produce temperatures in excess of the melting point of about 52–53 degrees centigrade for such compounds. The Saybolt Universal viscosity of such nonmigrating compound is less than 1,500 seconds at 60 degrees centigrade, whereas the viscosity of *SMD* compound is 40,000 seconds at 60 degrees centigrade. At the maximum normal operating temperature of 70 degrees centigrade, the *SMD* compound has a viscosity of approximately 20,000 seconds, which is in the range that the usual solid-type cable oils will have at temperatures of 20 degrees centigrade or below. Thus, there is no more reason to worry about migration in this gas-filled cable at 70 degrees centigrade than there would be with solid type cable at 20 degrees centigrade.

Mr. Halperin has asked what temperatures are considered permissible for the gas-filled and compression cables. It is recommended that the same permissible temperatures that have been adopted for oil-filled cable be used for these two types of cable. In regard to variation in gas pressure, the maximum range of gas pressure caused by temperature changes in operation will occur between the conditions of a sustained maximum load on the feeder at the time of maximum earth temperature and the removal of the feeder from service for an appreciable period at the time of minimum earth temperature. If this minimum pressure is made 200 pounds per square inch, the maximum pressure will be about 230 pounds per square inch for the *SMD* installation. The more probable range of pressure that will occur in normal operation will be considerably less than the above maximum. Mr. Halperin draws attention to the fact that the five types of cable included in Figures 14 and 15 cover a considerable range in degree of impregnation and should therefore show some differences in impulse strength. The data presented in these figures, except for the *SMD* type, are not new but have been taken from published data. The high values of impulse strength for the *SMD*-type insulation tested under high gas pressure, as compared to previous measurements on drained cable, may be attributed in part to the high dielectric strength of gas at this high pressure. As stated by Mr. Shanklin in his discussion, "the electrical strength of nitrogen gas at 200 pounds pressure is almost as great as that of the solid insulation." Also it is very probable that conductor shielding and the thin graded tapes contributed in securing these high impulse values for the *SMD*-type cable. We are not familiar with the data Mr. Halperin has which disagrees with conclusion 5 and wonder if it does not apply to a different type of insulation than the *SMD*-type using high gas pressure, graded tapes, and strand shielding, to which conclusion 5 is intended to apply. Test failures occurring in the regions of wide interturn gaps are probably due to both reasons given by Mr. Halperin. The half-round copper wire is in direct contact with the steel pipe and is solidly grounded to the pipe at all joints so that no appreciable voltage will exist between turns or from the wire to pipe, and there will be no tendency for sparking.

Mr. Wyatt refers to Figure 7 in the compressor-cable paper and states that the values of "power factors at room temperature or below up to 0.5 per cent appear rather high." He goes on to speak of having made compression cables "the 20 degrees centigrade and 60 degrees centigrade power factors of which were as low as 0.20 per cent and never higher than 0.30 per cent." We feel sure that Mr. Wyatt has overlooked the fact that with impregnated paper of very low loss, the temperature for minimum power factor is well above room temperature and that the power factor at temperatures *well below* room temperatures as given in Figure 7 will be greater than at ordinary room temperatures.

We know of no lower accurately determined values of power factor on paper cable than those reported in Doctor Whitehead's extended series of carefully controlled measurements of cable insulation. Thus, though Mr. Wyatt states that none of his values exceeded 0.30 per cent, none of Doctor Whitehead's values at 30 degrees centigrade or below are as *low* as 0.30 per cent. Doctor Whitehead's lowest values were obtained with oil of extremely low power factor, the oil being very fluid and the paper of low specific gravity as compared with usual cable practice. It seems likely from various collateral information, that the lowest of Doctor Whitehead's values represent a condition substantially as low as have been obtained with cable paper and any saturant. We suspect that, if Mr. Wyatt will investigate carefully, he will find that the values he mentions apply not at 20 degrees centigrade and below but at the higher temperature of 60 degrees centigrade or more, where the power factor should be considerably lower than 20 degrees centigrade.

From Mr. Wyatt's statement that: "Although the authors use thin paper in the conductor region to keep down butt spaces, wafers show that such a cable, after bending, pulling, and installing, will have some gas spaces of 18 mils and even 20 mils radial dimensions", it is not clear whether this is a deduction or whether he has prepared or secured a wafer that seems to indicate that conclusion. Also it seems to imply definitely that such spaces are to be expected even in the region of thin tape. We have made a good many wafers and believe that this statement is entirely unrepresentative of the cable that is installed in the commercial line at Detroit. Our wafers, made from samples of this cable, show that the maximum spaces that were found are quite closely equal to the tape thickness. It is most significant, however, that the normal spaces for the $2^1/_2$-mil paper are just about one-half the normal spaces for the five-mil paper and that likewise the maximum spaces for the thin paper are also half as large. The wafers show this advantage of the thin paper very clearly. It is recognized that in long sections of cable there is some probability of spaces occurring that will be equal to twice the tape thickness but extremely improbable that spaces appreciably greater than this will occur in modern cable.

It is natural that such a system as described in each of these two papers did not grow full-fledged at one time and that there are some parts which are not entirely new. The authors have been concerned primarily with giving engineering information concerning a successful system which it has been their job to improve, rather than with writing a bibliography, but they are pleased to have Mr. Wyatt express his opinion as to where certain credit should be given.

We are very pleased that Mr. Thomas has mentioned his sheath-compensated cable as it has many things in common with the compression type of cable. In this connection, it is interesting to note that some of this cable which we manufactured in 1935 is still operating with a record of no inherent faults to date. In this design vulcanized rubber tapes are applied over the lead sheath at a tension of 1,000 pounds per square inch. The elasticity of these tapes causes the lead sheath to compensate for the internal expansion and contraction caused by load changes and thus prevents void formation.

In reply to Mr. Thomas' questions, laboratory tests show that fatigue strength of the lead cable sheath when subjected to vibration or bending is considerably increased by the elimination of oxygen from contact with the sheath surface. In reference to the countersunk connector, this is of the soldered type with two short slots on the top for receiving solder. The outside diameter is the same as that of the cable conductor. In order to apply the connector, the outer layer of conductor strands on each of the cable ends is cut back for a distance of half the connector length. When the connector is wiped in place, these strands are flush with the connector and joined to it. In Figure 5 of the paper on compression cable, the step on which the joint sleeve ends is the lead sheath of the cable. The next step is the termination of the sheath-reinforcing tapes. No void space is left under the joint sleeve. Varnished cambric tape is used to build up the diameter of the sloping ends over the shielding braid to a dimension equal to the inside diameter of the lead sleeve. This diameter is carefully checked in order to secure a tight uniform fit along the full length of the sleeve.

Approximately 60 per cent of the commercial line was installed under paved city streets. About 50 per cent of this line was installed under streets using the heavy paving, 10 per cent under streets using light paving, and the remaining 40 per cent installed where no paving existed. A cost estimate made of the circuit indicated that the total cost to install oil-filled cable in conduit with accessories would be about 20 per cent higher than the cost of a high-pressure gas-filled pipe-type cable installation.

The paragraph questioned by Doctor Whitehead does not make the conclusion that the stress in the gas spaces is less than that in the other types of insulation. This paragraph is intended to point out that to whatever extent presence of gas reduces the over-all dielectric constant, the stresses in the oil and paper are, thereby, correspondingly reduced. Thus at any given applied voltage, the stress in the oil and paper will be (slightly) lower than on other types of cable such as oil-filled, Oilostatic, or compression cable. For all voltages at which there is no ionization in the gas space the electrical stress in the oil and paper will have no effect not found at least equally in these other types; or as stated in the paper, stress below the ionization voltage in the gas should have no effect on the insulation.

It has been pointed out by Doctor Whitehead and others that there is undoubtedly some ionization at voltages below the level necessary for detection by power-factor measurements. It will be interesting to study this by the more sensitive type of measurement such as used by Doctor Whitehead and by Professor Paine. However the shape of the voltage-time curve of Figure 13 shows that no harmful ionization occurs appreciably below the voltage at which we have measured ionization. Above the critical voltage indicated by the short horizontal section of the curve, the life decreases rapidly with increasing voltage. Below this critical stress, application of voltage has no effect on the life of the cable. An additional point on this curve has now been secured—one cable length has failed after a life of over 1,600 hours at an average voltage of 155 kv. This test was begun at 148 kv, voltage being raised uniformly, ending at 163 kv. While there is some variation from one length to another as shown by some scattering of points on the curve, a constant voltage that does not produce failure in 100 or 200 hours apparently will never produce failure.

It is obvious that, above the critical stress, the extent and destructiveness of the ionization increase rapidly with increasing voltage; the converse is also true, the extent and destructiveness rapidly disappear as the voltage is reduced below the critical value. Since there is, of course, some longitudinal nonuniformity, the critical stress on some specimens will be lower than on others and will be lower than for the average curve that is drawn. From the evidence from severely handled specimens, the minimum critical value appears to be not very far below 140 kv, or twice the working pressure of this cable. Thus, as far as concerns the normal 60-cycle voltage, there is a wide margin permitting future increases of stress by reduction of insulation thickness. This margin is indeed greater than the very considerable margin that exists in the surge strength so that the ultimate reduction in insulation thickness will doubtless be determined by surge strength rather than by 60-cycle strength.

Doctor Wiseman considers the restraining force measurements on the 50-foot sample as inconclusive because of the short length. It is true that the section was short as compared with actual sections, but still it was of sufficient length to permit the cable to take freely the same configuration in absorbing the longitudinal expansion as would occur on a long section, and therefore it is felt that these values are representative of the maximum values of thrust that may be encountered on an actual section. At the most the criticism of the test is merely to the effect that the small easily handled thrust measured may be even less in an actual installation. In calling attention to the coefficient of friction for *SMD*-type cable when pulled into steel pipe, Doctor Wiseman states that 0.37 is very good for dry pipe, but for Oilostatic, where the cable oil also acted as a lubricant, they got a value of 0.26. In the paper it was stated that 0.37 was the maximum value (not average value) that was reached for any section in the seven-mile line including the sections with bends. The average value was considerably below this. Since the coefficient of friction inherently varies over such a wide range, depending on many factors that cannot be evaluated exactly, the average value was not given, since it is felt that without knowing these controlling factors the average value of this coefficient is of doubtful significance. It should be pointed out that petrolatum was used as a lubricant and was applied over the half-round open spiral wires before the lead sheath was applied. Since petrolatum is a better lubricant than cable oil, the *SMD*-type cable can be expected to have a lower coefficient of friction than Oilostatic cable, since otherwise the external structures are similar, and therefore the maximum pulling length would be at least equal to Oilostatic cable.

It is very helpful to have Mr. Main's supplemental information concerning compression cable. His confidence in the value and reliability of this type of cable is based upon the very practical ground of successful production and operation of a considerable amount of it. In addition to his general comments, he tells of significant modifications of earlier designs. These should be especially helpful wherever the self-contained type is applicable.

Mr. Main grants certain advantages of the pipe-type cable but evidently is more impressed by the merits of the self-contained type. For smaller amounts of power than being carried at Detroit, these merits will often predominate, but where large conductor sizes are required as is generally the case in this country, the pipe-type system proves to be the more practical.

We are glad to have Mr. Main's figures of power factor at 20 degrees centigrade for the British-made cables and note that these are somewhat more than 0.1 per cent lower than the values given in our Figure 7 for the field measurements on the experimental compression cable. Much of this difference is accountable by the fact that density of the paper in the Detroit cable is very high, with a corresponding reduction in the amount of oil. With very low loss oil, such as used in this cable, the power factor of the paper substance is higher than that of the oil. Thus the greater paper content means greater over-all power factor. Any difference that cannot be accounted for in this way is only a fraction of a tenth of one per cent and can be explained by the difference between measurements made in the field as compared with the more accurate factory measurements. Thus Mr. Main checks our values which were questioned by Mr. Wyatt.

There is not space here to discuss fully the practical importance of the high-density paper. It suffices to point out that the large reduction in oil content results in proportionately less volume change with temperature than with lower-density paper. Incidentally, there is also a gain of this sort from the compact strand as compared with conventional. The much wider temperature range thus required for a given volume change is vastly more important than any minor difference in loss due to the difference in power factor.

In referring to the causes "contributory to the satisfactory solution of gas-filled cable," Mr. Main suggests that several of these have been developed by various cable manufacturers for the improvement of super-voltage cables generally. That is fully conceded. Some of these are of course particularly significant for the gas-pressure cable.

Mr. Main suggests that the reduction in stress in the impregnated paper by reason of the lower permittivity of the gas may reduce the emphasis that need be placed upon the quality of the saturant in the gas-pressure cable. We have drawn attention to this reduction of stress but would emphasize that it is purely incidental and actually amounts to only a few per cent. He asks if the viscosity of 3,000 given for this saturant is correct and not wrong by a decimal point. Three thousand voltage is correct, and we are glad to have the emphasis placed upon it that comes from the question. We believe that the properties of the saturant used, including this high viscosity, are very significant.

We quite agree with Mr. Main concerning relative impulse strength of different types of cable. We would be surprised if oil-filled cable made with the same type of paper and with strand shielding would not have as high impulse strength as this cable, although it is to be noted that preimpregnation facilitated the use of very thin tapes without the mechanical difficulties which Mr. Shanklin has found in applying unimpregnated thin tapes.

Mr. Main brings together for comparison our statement that the minimum safe-operating pressure is 100 pounds per square inch and our statement that the cable may be given standard acceptance voltage tests at 30 pounds per square inch. The operating limitation is, of course, conservative. There is, however, no particular advantage in setting a lower limit, since the line and accessories will readily take care of 200 pounds per square inch or more, and the cable will be operated at that pressure. As far as concerns test practice in this country, acceptance tests are made at voltages far above what anyone would consider to be a safe-operating voltage. It is true that this is exaggerated by the suggestion of making a normal test at a pressure lower than suggested for operation. However, it is to be noted that the dielectric strength for a period of a few hours is reduced not more than 10 per cent or 20 per cent at 30 pounds per square inch as compared with 225 pounds per square inch. It is this fact, as well as the results of dissection of tested samples, that warrants the testing for 15 minute periods to be made within the same range as is customary for oil-filled cable.

Mr. Main comments concerning the radial power-factor curves, saying these give no indication of the effect of the gas space on the shape of the curve. We see no reason for this concern; the curve actually obtained explores fully the variations of the impregnated tapes, and this seems to us to complete its purpose. Mr. Main himself recognizes this function when he "wonders what would be the shape of the curve" without humidity control in the factory. Actually these tests show how effectively that control was accomplished, and similar tests were a helpful means in setting up required standards for that control.

Mr. Main brings question as to the meaning of the same paragraph which Doctor Whitehead questioned. Our answer to Doctor Whitehead substantially answers also Mr. Main. The statement we wish to emphasize is a corollary of one Mr. Main makes that "the electrical strength of the paper cannot be used to full advantage; otherwise the gas spaces would be overstressed and destructive ionization result." But this means that if we avoid destructive ionization in the gas spaces we need feel no concern about the electric stress in the oil and paper. This fact is so very obvious that there was a tendency to look for some different and less easily accepted meaning. But this leads directly to the conclusion that the electric stress in the oil and paper in this cable is well below that which long experience with other types of cable has shown to be without harmful effect.

Mr. Main refers more than once to "difficulties" of stripping the sheath; he feels that this involves "costly complications." Before making the experimental installation, this seemed somewhat of a problem, but, by the time of the commercial installation, no problem remained. We can assure Mr. Main that his fear on that score is without foundation. Indeed we are constrained to remark that Mr. Main's comments emphasize our previous remark that both of these systems are good and very practical. Those acquainted with one will be partisan to that while fearing unfamiliar problems of the other. Acquaintance with both emphasizes the value of both.

Mr. Peterson has asked the expected life of the pipe when protected by one half inch Somastic as was applied on the Detroit commercial line. We have not been able to find any case of corrosion or deterioration with this type of protection, and installations have now been in service up to ten years. In regard to expansion, the expansion bend was provided only in the experimental installation and was used only to take care of expansion in the armored compression cable. No expansion provision is necessary for the pipe itself. In the *SMD*-type cable the three conductors have considerable freedom for movement and can absorb the longitudinal thermal expansion without any other special provision. As to the viscosity of the impregnating compound used on the *SMD* cable, it is about 80,000 at 50 degrees centigrade in terms of Saybolt Universal seconds.

No effort was made to determine the moisture content of the soils encountered because of the poor correlation that appears to exist between moisture content and thermal resistivity in an actual installation. This may be attributed in part to the difficulty in securing a moisture-content sample that is representative, as well as to the fact that for the Detroit installation the ground water level has a greater effect on the thermal resistance of the soil. A record was maintained of the rainfall, and it was found that this as such had practically no correlation with the seasonal soil resistivities reported.

Mr. Del Mar has concisely summarized the limited available knowledge on the subject of earth resistivity, and it was because of this inexact knowledge that these earth resistivity measurements were made and presented at this time. Several discussers have asked for more information concerning the thermal-resistivity measurements or concerning the thermal characteristics of the high-pressure pipe-type cable systems, or have asked why more data were not presented on this subject since more were obtained. These tests have not been completed.

The discussions by Meyerhoff, Johansson, Frank, and D'Eustachio, all of whom are associated with two of the authors may be considered as supplementary to the authors' closure.

REFERENCES

1. LOAD RATINGS OF CABLE—I, II, **Herman Halperin.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–56; volume 61, 1942, pages 930–42.

2. THE TYPE-*CB* IMPREGNATED-PAPER CABLE, **Samuel J. Rosch.** AIEE TRANSACTIONS, volume 59, 1940, pages 1041–50.

The Dielectric Strength and Life of Impregnated-Paper Insulation—III

Discussion and author's closure of paper 42-92 by J. B. Whitehead, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 618–22.

G. B. Shanklin (General Electric Company Schenectady, N. Y.): There has been a good deal of speculation about Doctor Whitehead's breakdown results being at variance with actual solid-type cable experience, as regards paper density. The following explanation is offered for what it is worth.

Because of entire absence of void formation, Doctor Whitehead's test samples are actually representative of oil-filled cable and not solid-type. The mechanism of breakdown he is studying is, accordingly, applicable in all detail only to oil-filled cable. This is borne out by the fact that his results are in full accord with oil-filled-cable experience.

For times expressed in seconds or fractions of a second (impulse, and so forth), the dielectric strength of oil-filled cable increases with increase of paper density. For times in hours, such as Doctor Whitehead used, the dielectric strength decreases slightly with increase in paper density, just as he found. For times expressed in months or years, change in paper density, over the usual commercial range, has practically no effect on dielectric strength.

Ionization in voids of solid-type insulation can easily change these trends, particularly for the longer periods of time. The action is more complicated and more difficult to reproduce in the laboratory, because it depends on size, quantity, and location of these voids, as well as pressure and numerous other factors. For this reason I suppose we will always have differences of opinion about the effects of paper density in solid-type cable, or in any other type of cable where incipient failure is brought about by gaseous ionization in voids. Personally, I cannot get very excited about it, because the differences are not great in any case.

If Doctor Whitehead needs any support in his belief that Robinson was wrong in concluding that *all* failures are instigated at the conductor surface, I stand ready to give it. Like Doctor Whitehead, I have examined plenty of incipient failures at locations other than the conductor surface, and this was in full-sized cable, some of which had relatively large ratios of maximum to minimum stress.

Too many factors besides quantitative stress are involved for one to say that the location of breakdown in paper-insulated cable follows a simple physical law, based on the geometrical stress field alone.

In overstressed gas-filled cable, for instance, ionization deterioration is usually more pronounced in the outer zone of the insulation thickness rather than the inner zone, and final failure usually starts here. This occurs, because compound drainage is considerably more in this outer zone, and voids are larger.

There are many champions of the maximum-stress theory and some champions of the minimum-stress theory. The fact remains, however, that practical design work in the whole cable industry is based on neither but, instead, is based on average stress. My experience has been that over the usual commercial range of conductor sizes, insulation wall thicknesses, and so forth, average stress expresses practical results better than anything else. This middle of the road course is a safe one to follow in all but extreme and special cases.

William A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Doctor Whitehead's modification of the Robinson coring theory is very interesting.

Like Robinson he finds that initial trouble arises in an oil space and that the oil space involved is one where the greatest electrical stress occurs.

However, because of the circumstance that his insulation is thin compared to his conductor diameter and that his conductor is smooth, he regards it as a matter of chance whether the stress is greatest at the inner surface of the insulation or at some intermediate layer.

Perhaps there is another reason. Reference to P. L. Hoover's 1926 paper shows that the maximum stress at very high stress is not at the conductor surface but within the insulation, and this may influence the results obtained.

Again, Doctor Whitehead agrees with Robinson that failure originates in gas in the oil space, but, whereas Robinson postulates a gas occlusion, Whitehead says that if there is no gas occlusion present, and the stress is sufficiently high, the stress will generate the gas by ionizing the oil.

This immediately suggests two levels of dielectric strength, one applying to practical cables from which complete gas exclusion is impossible, and a higher level applying to completely gas-free laboratory specimens like those of Doctor Whitehead.

I shall try to establish the fact that Doctor Whitehead's specimens fail at a higher stress level than cables and will then try to draw the consequences.

Reading over Doctor Whitehead's three papers on stability of oil-impregnated-paper insulation, I find that all stresses referred to are average stresses and that, where the expression "maximum stress" is used, it means "the maximum or final value of the average stress."

Thus, Figure 1 of the paper gives an average final stress of nearly 600 volts per mil for a specimen made with paper of specific gravity of 0.896 and designated as *B* paper, after being subjected to 13 stages at average stresses starting at 400 volts per mil.

The same Figure 1 gives, for this paper, a life of 53 hours, practically all of which was consumed in the 13 preliminary stages.

We have no exact way to correlate the preliminary steps with the final steps, but we may make an approximation by assuming that each stage causes deterioration at a rate proportional to the 20th power of the stress. If you ask the source of this figure, I can give you nothing better than some test data on oil-filled cables which suggested it. In the case of single-conductor solid-type cable, the index is about 6.

On this basis the preliminary stages are equivalent in the aggregate to about $4^1/_2$ hours at the final stage for all the papers tested.

Returning to the *B* paper, if we plot the original test data with volts per mil as abscissas and hours as ordinates, we obtain a gunshot diagram with average stresses ranging from 581 to 700 volts per mil and lines ranging from 0.2 to 3.83 hours.

If we average these lines, we obtain about 2.3 hours, but this would not be a significant average, as it would not take into account the various stresses at which the individual lives were obtained. This may be allowed for in an approximate way, by the same assumption made above, that is, by assuming the life to be inversely as the 20th power of the voltage and obtaining a weighted average based on that assumption. This gives a life of $3^1/_2$ hours.

If we add this to the $4^1/_2$ hours from the preliminary stages, we obtain, for paper *B* specimens, an empirical life of eight hours at 600 volts per mil.

The nearest comparable cable test data are those from the Association of Edison Illuminating Companies "Time Test," which give failures for single-conductor cables with thin walls at average stresses of about 400 volts per mil after

Table I

Paper Density (Gurley) Seconds	Specific Gravity	(Volts Per Mil) Overlap	
		$33^1/_3$%	$37^1/_2$%
33	0.74	349	342
480	0.78	420	367
3,000	0.94	390	428

6 hours at 220 volts per mil
3 hours at 300 volts per mil
3 hours at 350 volts per mil

In the case of solid-type single-conductor cable, an index of 6, instead of 20, gives a more accurate means of weighting the lines. On this basis, the preliminary stages give the equivalent of about two hours at 400 volts per mil. The tests usually run from one quarter to three quarters of an hour at 400 volts per mil, a fair average being not over one-half hour. This added to the two hours from preliminary stages, give $2^1/_2$ hours at 400 volts per mil.

By this roundabout route we arrive at the following comparison between Doctor Whitehead's specimens and ordinary cables.

	Failure Stress (Average) Volts Per Mil	Time at Failure Stress (Hours)
Doctor Whitehead's specimens	600	8
Cables	400	$2^1/_2$

This seems to establish that Doctor Whitehead's specimens fail at a definitely higher stress level than solid cables, this level being at least 50 per cent greater. If the comparison is made on the basis of final stresses only, Doctor Whitehead's specimens appear to have at least 75 per cent greater dielectric strength.

In the case of failure by generated gas, the stress on the oil is all important, as the generation of gas is directly dependent thereon and becomes violent when this stress is exceeded. Therefore, in the laboratory specimens, the specific inductive capacity of the paper is of prime importance, and a high value works against high dielectric strength, as pointed out by Doctor Whitehead.

Where, however, failure is due to occluded gas, ionization starts at a low stress, whatever the density of the paper, and the stress at failure depends on the ability of the paper structure to prevent spread of the ionization streamers, an ability which increases with increasing density, provided that the tangential path is not weaker than the path through the paper. We have made extensive tests on specimens made purposely with poor impregnation which completely support this view. A progress report on this was presented to the National Research Council's Williamsburg conference in 1941 and referred to by Doctor Whitehead in his paper. Details of this investigation will be offered to the Institute when the exigencies of more important war work permit.

In the meanwhile typical data on poorly impregnated specimens ten tapes thick are offered in Table I for comparison with Doctor Whitehead's.

The voltage was increased by five-minute steps, each step being 20 per cent higher than the preceding one. These were converted to an arbitary equivalent one hour dielectric strength by means of the sixth root law.

The above figures are the average of eight breakdowns. The maximum deviation from the average was 8.9 per cent, and the average deviation 4.5 per cent.

It will be noted that at $37\frac{1}{2}$ per cent overlap, dielectric strength increases with density whereas at $33\frac{1}{3}$ per cent overlap, the relative dielectric strengths of the papers and tangential paths produce a condition wherein a maximum occurs at a medium density.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): Doctor Whitehead's studies are leading to a better understanding of the phenomena occurring in dielectrics under high electric stress. The very fact that certain of the conclusions from his tests were originally so unexpected by "dielectricians" greatly emphasizes the importance of these results in our understanding of the effect of electric stresses.

Why some of the relationships differ from those found in commercial cable insulation is not yet obvious, although the explanation may be found among the theories that have been suggested by various people. Since these things are susceptible of effective experimental attack, the explanation will be forthcoming when sufficient effort can be devoted to the subject.

J. B. Whitehead: When our first results on the influence of paper density on dielectric strength were announced, there was considerable disquietude amongst cable engineers who were following closely the progress of our work, and some of whom advocated the use of high-density paper. There was some evidence that the accuracy of the results was questioned. As they were substantiated by further data, cable engineers united in an effort to collect from their own laboratories results of tests in favor of high-density paper. Reports were received from five or six laboratories, but, with one exception, all the tests had been made on continuous sheets of paper and indicated increasing dielectric strength with increasing paper density, in accordance with theory and long-established behavior of other materials. The single exception noted was one impulse-voltage test on each of two cables, one with high density and the other with low density paper—the higher density giving a somewhat higher breakdown strength.

Much of the uncertainty of the situation thus created was due to the impossibility of reconciling our results with the behavior in breakdown tests on solid-type cables; this, in spite of the fact that we have emphasized the absence of free gas in our samples, and that our results, if applied to cable behavior, should be confined to those of oil-filled type.

We have never ventured to suggest the extent to which our results should be considered in cable design, leaving this question to the vastly wider experience of cable engineers. However, in the face of some questions and criticism, we have ventured from time to time to ask for the experimental evidence on cables, which was at variance with our own. None worthy of the name has been forthcoming. Mr. Shanklin goes further than others in stating the results of manufacturers' experience. It would be helpful if he would give some quantitative data substantiating his statements as to the relative influence of paper density in impulse, short-time, and long-time testing, respectively. Requests for quantitative data from those manufacturers who advocate the use of high-density paper have always been met with the response that their experience was of general character over a period of years and that it would be difficult to separate the particular studies which had led to conviction that a denser paper results in higher dielectric strength. Hardly a convincing reply, in view of the number of factors which have entered into the general improvement of cable quality within recent years.

As stated above, it is not our intention to question the experience and practice of cable manufacturers, nor even to suggest the extent to which weight should be given to our results. I believe, however, that there is now no question of the accuracy of our results on the influence of paper density in our type of sample. This means that the influence of the relative values of the dielectric constants of cable tape, and of oil channel or gas space, is such as to throw an increased value of stress into the oil channels and gas spaces, and that this stress increases approximately as the density of the paper. Whether or not this fact shows itself only at relatively high stresses, as in our experiments, as pointed out by Mr. Del Mar, the fact remains that, if failures do originate in oil channels and gas spaces, the use of higher-density paper increases the stresses in these most vulnerable locations. It does not appear to me that the question has been satisfactorily answered, and it is to be hoped that somehow, somewhere, after the present unhappy period, further experiment may be possible for clearing it up.

Load Ratings of Cable—II

Discussion and author's closure of paper 42-111 by Herman Halperin, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 930–42.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): Mr. Halperin has made another valuable contribution to cable engineering. Systematic operating data of the kind he presents can only be obtained directly in the field by a large, well-organized operating company. This is particularly true of the behavior of cable under heavy load conditions, and it is about this that I wish to comment.

For a number of years some engineers have been advocating rather extreme loads and copper temperatures for power cable, limits that appeared to many of us to be contrary to actual experience. It is interesting to note that, based on the extensive field study he has been making the past two or three years, Mr. Halperin has reached relatively moderate views about normal safe loading. This is summarized in Table VII of his paper, representing the latest maximum copper temperature practice in Chicago. For normal operation of solid-type cable he follows the well-known standard rule of $(90 - E)$ degrees centigrade in all but three instances. At the minimum and maximum voltage ratings he increases copper temperature by five degrees centigrade, while at one intermediate voltage rating he decreases temperature by five degrees centigrade, as compared with the standard rule.

My only disagreement is with the recommended limit of 65 degrees centigrade for 66-kv solid-type cable with solid filled joints. I do not believe there is enough actual field experience to verify this and feel that even the present standard rule limits for 33-kv three-conductor and 66-kv single-conductor cable are too high.

For wartime loading conditions Mr. Halperin is recommending from three degrees centigrade to ten degrees centigrade increase over normal conditions, but emphasizes that increased service failures and shortened life of cable resulting therefrom must be expected and accepted. This, to my mind, is the core of this whole problem of heavy cable loading. It is not a question of obtaining something for nothing but of weighing the gains against the losses.

Mr. Halperin is recommending occasional short-time emergency overloading of five degrees centigrade to 27 degrees centigrade above normal loading and seven degrees centigrade to 35 degrees centigrade above wartime loading. Only in the recent past have we begun to think in terms of a 15 degrees centigrade tolerance for emergency overloading. I suggest that we obtain some experience with this limit before going too far. Mr. Halperin points out that he does not know what might happen to accessories under such extreme temperature ranges, and it is again a question of weighing the gains against the losses.

Mr. Halperin states that in some cases considerable thermal advantage may be gained by making relatively deep installations of underground ducts. This is undoubtedly true and is particularly true of the Chicago district, but it depends upon the permanent underground water level. If this can be reached, there is a material cooling effect on the loaded cables. If not reached, there is practically no difference in cable heating at any depth from $2^{1}/_{2}$ to 12 feet, the cooler earth at greater depth being counterbalanced by the higher thermal resistance to earth's surface and other factors. It is for this reason that standard load ratings of cable would be greatly simplified if we standardized on a depth of three feet and earth ambient temperature at that depth, ignoring the actual depth at which the cable is laid. The chances are, in any case, that the depth will vary from one end of a line to the other.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): In reference to Mr. Halperin's paper and to the evidence of discharges from an oil layer between the insulation and the surface of the sheath, contact with both lead and paper of this oil layer of appreciable thickness would certainly result in an increased ionic content of the oil. This would lead to space charge layers at the surfaces of both sheath and insulation with resulting high local stresses leading to local bridges, or local failures, or abnormally high stresses because of surface irregularities.

To refer to Mr. Halperin's paper and to the question of the mechanism of failure, the following may be said: A thorough failure with even a moderate amount of burning is usually sufficient to completely conceal the first cause of failure. Failures in this type of insulation almost invariably originate in an oil channel in which the spark is due to high local stress at the interface between paper and oil. A gas bubble is formed by the initial spark, and cumulative ionization then begins. The consequent burning or boring through the insulation wall is not reflected in a change in the over-all power factor of the sample, until the failure is almost complete and is such that a heavy increase in the conduction component of the current may result. These statements are largely based on accounts of many partial failures reported in my paper on today's program.[1]

Will Mr. Halperin please explain the statement that cable samples made with very thin insulation will show more definite deterioration than thicker samples?

REFERENCE

1. THE DIELECTRIC STRENGTH AND LIFE OF IMPREGNATED-PAPER INSULATION, **J. B. Whitehead.** AIEE TRANSACTIONS, volume 61, 1942, August section, pages 618–22.

L. F. Hickernell (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): This paper is so complete that it leaves few, if any, important questions to ask the author. In referring to the section titled "Oil-Filled Cables," it is noted that cable *T*-0.500 developed an increase in power factor due to the excessive sheath expansion drawing in core oil of high power factor. The cause of the high power factor of the core oil is not stated. If it is assumed that the oil was not abnormal, the operation could be considerably improved by carbon-black protection over the conductor. This would not only reduce the maximum stress, but would also improve the power factor of the core oil by adsorption. Similarly, if carbon-black tapes were used over the insulation, the severe discharges between the sheath and insulation would be eliminated, and the additional volume of carbon black would be helpful in conditioning the oil.

In conclusion 2 of this section, in the "Note," it is suggested that these investigations may be applicable to other oil-impregnated insulation, especially transformers. No doubt the transformer designers will discuss this point. However, in the event a cable session may escape their attention, I should like to point out that, to the best of my knowledge, the paper insulation used in transformers is quite different from the wood-pulp paper developed for cables, and hence the results obtained on the latter do not apply to the former.

In "Aging Tests of Three-Conductor Solid-Type Cable," the following statement is noted: "The special shielding tapes were either metallized-paper tapes or carbon-black tapes." Shielding, or stress control, is only one of several functions exclusively obtained in the carbon-black construction as ably demonstrated by Rosch[1] and further substantiated in subsequent investigations which will be published shortly.

In reference to "Mitigation of Sheath-Crack Troubles," it may be of interest to know that a terminal employing a flexible copper hose has been in successful service where formerly trouble had been experienced with lead sheaths due to fatigue from vibration. Similar construction could be employed in manholes though subject to some of the difficulties of the Sylphons pointed out by the author.

REFERENCE

1. THE TYPE *CB* IMPREGNATED-PAPER CABLE, **S. J. Rosch.** AIEE TRANSACTIONS, volume 59, 1940, pages 1041–9.

J. G. Stelzer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Halperin mentions that the movement of cable in ducts clogged with fine silt was much less than in ducts free of silt, this lesser movement being attributed to the increased friction caused by the silt. Isn't it possible to attribute, at least in part, this difference in movement to the fact that the heat radiation was much better in the ducts that were clogged with silt, thus reducing the over-all cable temperature with a corresponding reduction in expansion? It seems that this would be particularly true if the silt was moist.

Is joining any particular problem when cables are worked to the emergency war temperatures as given in Table VII? Are there any particular problems to be expected in connection with the migrations of the solid compounds?

In connection with the mitigation of sheath-crack troubles, we have had numerous installations where the cable movement at duct mouths was strictly limited by heavy concrete fireproofing. These cables all have a long record of continual operation with no reports of cracked sheaths or sheath abrasion troubles.

In localities where there are lead sheath corrosion troubles, will the increased temperatures under the emergency war loadings have any effect on the rate of corrosion? If so, in what way?

Table VII shows the maximum allowable conductor temperatures for normal and emergency operation. Conductor temperatures do not ordinarily mean much to operating men, and it is necessary to convert such ratings into terms of amperes for given periods of time. This conversion from temperature to amperes is a subject on which there does not seem to be much information available that does not involve calculations having factors that are not ordinarily available without considerable investigation. What method is used for converting these temperature ratings to amperes for the use of the operating personnel?

No mention is made of sheath swelling and bursting due to migration of impregnating oils. This is probably not much of a factor in the Chicago area where the contours are in general comparatively flat. Troubles of this nature probably are to be expected in hilly sections of the country if the emergency war loadings in Table VII are permitted.

Robert J. Wiseman (The Okonite-Callender Cable Company, Inc., Passaic, N. J.): Mr. Halperin's paper will be an aid to all who operate power cables in pointing out some of the important factors in installation and operation. I was very much impressed with the second paragraph under "Introduction." It summarizes very clearly the difficulty we have in trying to set up rules of procedure regarding power cables, and why it appears at times that the manufacturer will not give a clear-cut statement as to what a cable will do under certain conditions. I think everyone should read this paragraph and memorize it.

When comparing the results Mr. Halperin gets for the change in tearing strength of the paper after aging as a measure of deterioration with the results obtained at Massachusetts Institute of Technology in 1926–29, we should understand that the technique of cable manufacture has improved so that modern cables have less air and moisture, and better paper and oil, so that a longer life insulation should result.

Mr. Halperin refers to the breakdown of the outer tapes next to the sheath. As this is oil-filled cable, it seems as if the normal working stress would not cause failure, even though the oil had shown signs of deterioration, but the lightning and switching surges could start failure.

The views expressed regarding improvement in the thermal conditions surrounding a conduit system by replacing soil of high thermal resistance by soil with low thermal resistance or burying the conduit deeper are also applicable to buried cable systems. As Mr. Halperin states, going to greater depths results in a more constant ambient temperature independent of air temperature and a higher moisture content in the soil which is very helpful in lowering the thermal resistance and improving the heat dissipating properties of the duct or cable.

The emergency load temperatures proposed by Mr. Halperin seem reasonable for his company in view of his experience. As we can expect the loads on present-day cables to be raised because of war conditions, we can also expect the cable failure rate to increase. This will have to be accepted, but, fortunately, as Mr. Halperin brings out in his paper, some factors add and others subtract in their effect on operation, and so the net result seems to be that the cables will operate, even though we expect them to fail.

R. W. Atkinson (General Cable Corporation, Bayonne, N. J.): The subject of overload ratings of cables is being placed on a scientific basis by such contributions as Mr. Halperin's. In the absence of numerical data, the manufacturer was inclined to point out the dangers of any increase of rating above normal, sometimes indicating some of the directions from which such danger might come. On the other hand, the user who wished to be able to obtain an increased rating was inclined to discount the general warnings and to wish to base ratings only on proved limitations. The result was a wide difference in recommendations and no engineering basis by which these could be reconciled.

A further important difference has had to do with acceptance that exigencies or even economics of operation may justify deliberate shortening of cable life. The manufacturer was opposed to accepting a rating that might shorten life; the user was inclined not to recognize that increased ratings might affect the life unfavorably. The war emergency has made easier the resolution of this difficulty.

With this better viewpoint and with the data and analysis presented by Mr. Halperin, we have now reached a common ground where these ratings may be considered objectively and determined on a scientific basis with little difference of opinion between manufacturer and user.

William A. Del Mar (Phelps Dodge Copper Products Company, Yonkers, N. Y.): Mr. Halperin's paper stands out as a valuable contribution to the war effort, inasmuch as it points clearly and specifically to means of conserving copper and enabling the utilities to carry on, for the duration of the war, with existing equipment. He also suggests new and higher temperatures for normal operation.

The conclusions were reached as the result of studies on one of the largest cable systems in the country, where no effort has been spared to obtain the best possible operating conditions, even to the extent of enlarging a great number of existing manholes, and eliminating thermal bottlenecks.

Many cable systems do not enjoy these advantages, largely because of less favorable economic conditions; high temperature operation may, therefore, prove less successful. This is particularly the case where manholes are restricted in size, load factors are low, cables are old and deteriorated, and maintenance less than ideal.

It is, therefore, to be hoped that Mr. Halperin's proposals for normal operation will be accepted by other utility engineers as a challenge to bring out data either in substantiation or rebuttal.

Herman Halperin: As Mr. Shanklin correctly points out, in establishing increased load ratings, the gains due to greater utilization of cable circuits must be carefully weighed against the losses caused by reduction in life and by an increased rate of failures. In the present national emergency the problem is to obtain the maximum possible utilization in order to preserve critical materials without having these losses become very serious. As indicated in my discussion of the group of papers by Sporn, Gaty, Cole, and others at the AIEE winter convention in January 1942 (AIEE TRANSACTIONS, volume 61, page 432), it seems, however, shortsighted to increase loadings of cable, transformers, and so forth, so much that numerous troubles requiring extensive replacements of equipment will occur in a few years or, perhaps, before the end of the war. The emergency ratings are based upon the consideration that the cable would be subjected very rarely to such conditions and that the reduction in life caused by such occasional operation would, therefore, be comparatively small, while the advantage of the consequent general higher level of loading would be large.

Mr. Atkinson and others give emphasis to the advantage of obtaining and studying considerable field and test data in arriving at allowable temperatures and loading. As a result, the differences in opinions are decreasing. This sort of observation gives emphasis to the continuing need for convention sessions and discussions such as today's. These advantages become further obvious when it is realized that available information shows that European temperature limits for cable in 1939 were 0 to 20 or 25 degrees centigrade less than prescribed in regular American specifications and standards, and were still further below the limits given for emergency operation in my 1939 paper "Load Ratings of Cable."[1]

With regard to Mr. Shanklin's discussion of the advantage of deeper installation of conduit and cable, this method is used as an alternative to replacing poor soil, only if at the lower level better soil conditions prevail. Our test data on temperatures in and around conduits indicate definitely that the heat does not all come to the surface, but is conducted in all directions to the surrounding earth in accordance with the temperature distribution and thermal resistances. The thermal sink is not necessarily at the surface of the ground, especially in the summer. Also, with the lower conduits, the maximum base earth temperatures become four or six or eight degrees less in the summer, and this is usually of advantage because summer load ratings are becoming more and more the limiting ratings on cable. His suggestion to standardize on a definite depth for thermal rating is followed by the Commonwealth Edison Company in that all standard ratings are based on an assumed distance of four feet between the center of the conduit and the surface of the ground.

Concerning Mr. Shanklin's point on my proposed temperatures for normal daily loading usually being not greatly different from those in regular American specifications, it should be noted that the temperatures in those specifications were almost always used for special conditions. In other words, the usual daily temperatures were actually much below the temperatures given in the specifications. With the new temperatures given in my paper for emergency loading, a utility can, as a practical matter, load cables to have in normal daily operation temperatures such as in the specifications, or somewhat higher for wartime, as I indicated.

As to Mr. Shanklin's criticism of my 65-degree limit for solid-type 66-kv cable, I refer to the Halperin–Shanklin paper, "Studies of Stability of Cable Insulation,"[2] wherein it is shown that in laboratory tests at 2.5 times operating voltage, cables were stable with heating cycles to 65 degrees. These cycles were with a room ambient of 20 or 30 degrees centigrade, whereas in conduits the ambients would almost always be higher. Other Chicago tests showed stable cables with heating cycles to 70 degrees.

In connection with the point on the possible ill effects of high temperatures on accessories, it should be noted that these possibilities apply principally to extra-high-voltage accessories and that the accessories usually operate at temperatures substantially less than those for cable in conduit. The ill effects in Chicago were found in a laboratory where the ambient was the same for the accessories and cable.

Regarding Mr. Stelzer's point, I think that silt, provided it is wet, has some effect in reducing the copper temperatures, but the effect is almost certainly inadequate to account for the reduction found in cable expansion. Migration of asphaltic joint compound into cable insulation can cause high power factors adjacent to the joint, but we never experienced any trouble from this cause, because such compounds are used in joints operated at 12 kv and less, where high power factors are of minor importance, and in joints on old 22-kv lines consisting of belted cable having heavy insulation.

The method of converting allowable temperatures into allowable currents cannot be given here because of lack of space but has been described in a paper on "Operating Temperatures of Underground Cables" by C. A. Bauer (1930–31 report of engineering section, Great Lakes division, National Electric Light Association).

In answer to another of Mr. Stelzer's questions, increased temperatures will increase the rate of lead sheath corrosion where such action is already present to an appreciable extent. As to sheath swelling and bursting in hilly country, it is to be expected that impregnating compounds in solid-type cable will flow at usual operating temperatures, and the effect of higher temperatures is to increase the rate of flow. In Chicago we have many risers where the vertical elevation of cable is 20 or 30 feet.

Mr. Hickernell's point on conductor shielding for oil-filled cable seems to me to be particularly applicable for those three-conductor cables with sector conductors having maximum stresses over 200 volts per mil and for hollow-core single-conductor cable. I agree with him that conductor shielding has other benefits besides stress control. As to his point on the difference in papers used in cable and in transformers, some transformer manufacturers advertise their use of "kraft" and "cable" papers and I do not see any substantial reason to expect any large differences between performance of these various cellulose materials and of power cable paper subjected to heat.

In reply to Doctor Whitehead's question, the cables with thinner insulation showed more deterioration because all samples regardless of insulation thickness were tested at 24 kv, three-phase, with consequently higher stresses on the thinner insulation.

I agree with Doctor Wiseman that the insulation in modern cables should be expected to deteriorate less under heat than it did in the tests of 15 and 18 years ago at Massachusetts Institute of Technology.

Operating experience and studies thoroughly substantiate Mr. Del Mar's points that reasonable manhole sizes and cable training and good maintenance pay excellent returns in the way of longer life of underground circuits and good reliability of service.

The interest shown by all discussions is greatly appreciated.

REFERENCES

1. LOAD RATINGS OF CABLE, **Herman Halperin.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 535–56.

2. STUDIES OF STABILITY OF CABLE INSTRUCTION, **Herman Halperin, C. E. Betzer.** AIEE TRANSACTIONS, volume 55, 1936, October section, pages 1074–82.

Frequency Control of Load Swings

Discussion and authors' closure of paper 42-113 by J. E. McCormack and R. J. Lombard, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 623–4.

J. E. Hancock and **H. L. Clark** (General Electric Company, Schenectady, N. Y.): This system of load control really gives to the manually controlled generators a portion of the duty of the frequency-controlling unit since the manual load changes are to be made partially dependent on system frequency. It would appear that in addition to improving load swings on the tie feeders this load-dispatching method should also improve the system frequency regulation.

The two methods described for obtaining the necessary accurate frequency indications show considerable advantage over the conventional frequency meters. The usual frequency meter has also proved inadequate in investigations of steam turbine governor performance. To facilitate these investigations the General Electric Company engineers have recently developed a frequency meter operating on a entirely new principle. This new frequency meter is applicable in the load control method described by Mr. McCormack and Mr. Lombard.

The frequency meter is contained in a metal case approximately 5 by 7 by 10 inches and indicates the frequency of the 110- to 120-volt 60-cycle power system to which it is connected. The total power consumed is approximately five watts. Figure 1 and Figure 2 show the frequency meter which has two scales, one marked from 59.875 to 60.125 cycles per second, and the other from 59.75 to 60.25 cycles per second. Selection between the two scales is made by means of a toggle switch on the instrument panel.

This frequency meter uses a mechanical resonant system consisting of a small 60-cycle tuning fork instead of the usual reactors and capacitors. Since it is well known that the resonance of a mechanical system is, in general, sharper than that of an electrical system, it is readily seen that a frequency meter utilizing this principle may be made extremely sensitive. In fact the range indicated is the broadest that is feasible while a scale of plus and minus 0.01 cycle per second can readily be obtained.

Any mechanical system combining mass and a spring has at least one natural resonant frequency. If a disturbing force is applied to this sytem, the resulting vibration will depend on the relation between the applied frequency and the resonant frequency of the mechanical system. The relative phase angle between the disturbing force and the resulting vibration will shift through nearly 180 degrees in the narrow frequency band corresponding to the resonant peak.

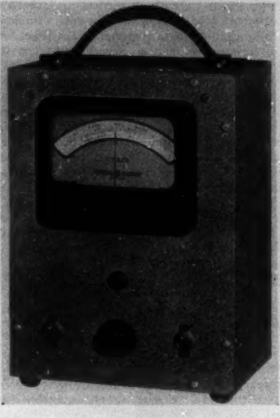

Figure 2. Supersensitive frequency meter—oblique front view

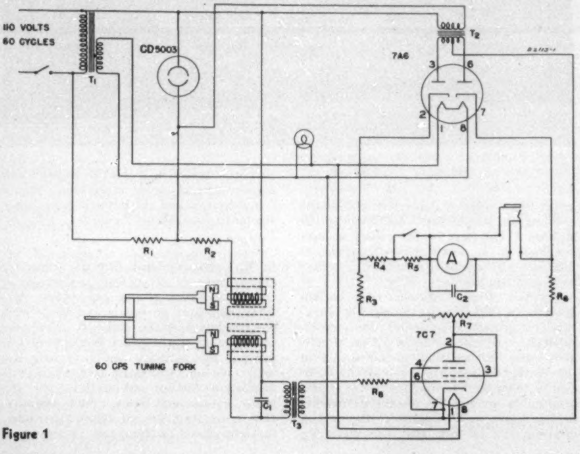

Figure 1

This frequency meter then consists of the tuning fork driven from the system frequency, and a vacuum-tube circuit measuring the phase angle between the system frequency and the resulting motion of the tuning fork. The accuracy of the tuning fork is so good that the over-all instrument accuracy depends only on the accuracy of reading the indicating instrument.

The marked scale of this frequency meter corresponds closely with the normal frequency band of a generating system, and each scale division is 0.005 cycle per second. Therefore, it will be seen that by estimating quarters of divisions, this frequency meter could be read to one part in 50,000. Thus, we believe that this development has brought the indicating frequency meter into a category which is comparable with master frequency methods.

S. B. Griscom (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have made an important contribution to the operating technique of interconnected systems. While some of the principles brought out have been appreciated for some time and have been incorporated in automatic controls, these have not been well understood by the operating personnel, nor were they particularly adaptable to manual control.

In the operation of a large interconnected system, it will always be necessary to exercise a certain amount of manual control or direction, no matter how refined the controllers nor how widespread their use. Load curves have a definite pattern on a given system, but vary with the day of the week and seasonally. Availability of generating capacity varies with maintenance schedules, rainfall, and unscheduled outages, with individual station and system economy also playing a major role. Fitting these requirements together is the function of the system operator, according to flexible rules based on economy, reliability, and interchange contracts, all occasionally modified by contingencies.

The authors have shown that the best job of forecasting the system operator could do was not good enough if the orders were executed "woodenly." In other words, if a load drop was expected in a given area at a given time, this reduction should be ordered by the system operator, but not executed, unless it will tend to restore frequency to normal. With this procedure deviations from expected load changes will have a minimum disturbing influence on frequency and tie-line loadings.

Most of the control necessary for maintaining frequency and tie-line loadings can be done automatically, usually with advantage with respect to closeness of control, and with respect to freeing the operator from an onerous task. In addition to frequency and tie-line controllers, there is also available the "program load controller"[1] whereby the fluctuation in station output, instead of being taken by one machine, is distributed among all operating machines in the station, achieving the best possible thermal economy. This scheme has great practical value, as it relieves both system and station operators of continuous consideration of incremental thermal economy. The value of automatic control for any of these functions is, of course, dependent upon the rapidity with which load changes take place.

The stroboscopic means of showing each station operator visually the direction and amount of deviation from standard speed has much to commend it. In addition to permitting an improvement in manual control of load, it gives the operator a far better mental picture of cause and effect when manipulating the generator speed changers.

REFERENCE

1. TIE-LINE CONTROL OF INTERCONNECTED NETWORKS, T. E. Purcell, C. A. Powel. AIEE TRANSACTIONS, volume 51, 1932, pages 40–7.

J. E. McCormack and **R. J. Lombard:** The prime requisite for frequency control of load adjustments at generating stations is that the same indication of system frequency be presented to the operator at each station. This can be accomplished either by providing a standard frequency common to all stations and a suitable device for comparing with system frequency, or by individual frequency meters which have been compared for identity of indication and which will sustain their accuracy over a long period, regardless of atmospheric or other influences.

The instrument described by Hancock and Clark could be applied to frequency control of load swings, provided that it has the aforementioned characteristics and is suitable for use in generating stations. It appears to be a laboratory instrument with possibilities for conversion to a suitable operating meter if equipped with a readily visible scale and pointer, or possibly it could be adapted to provide the comparison standard for stroboscopic indication of frequency.

Mr. Griscom has taken this occasion to stress some of the problems encountered by load dispatchers and generating-station operators. Because of the innumerable situations that may occur in a station or upon the interconnections, automatic control must be supervised by the station operator and on occasion superseded by manual control. Automatic controllers relieve the operator of the labor of control, but the responsibility for correct operation remains with him. In view of this responsibility, he should have a thorough knowledge of system characteristics and be provided with sufficient information and adequate instruments for intelligent performance of his duties.

Series Capacitors for Transmission Circuits

Discussion and authors' closure of paper 42-112 by E. C. Starr and R. D. Evans, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 963–73.

J. W. Butler (General Electric Company, Schenectady, N. Y.): I have reviewed and studied this paper very carefully. My interest was greatly stimulated when reading the synopsis, wherein it was indicated there had been some new developments in protective equipments for series capacitors. My interest continued throughout the body of the paper, but I confess I was unable to discern just what new developments were being described. Therefore, I will outline my reasoning in order that my quandary might be appreciated—and I hope clarified.

Previous series-capacitor equipments in power lines utilized protective equipment fulfilling the requirements set down by the authors. The first series capacitor ever installed in a power line—described in reference 6 of the paper published in 1928—used such equipment.

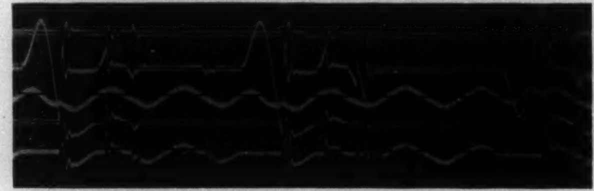

Figure 1

Figure 1 of this discussion is an oscillogram dated 1935 showing the speed of operation of a protective equipment for a series-capacitor installation on a 22-kv circuit. This setup was adjusted so the closing of the contactor reduced the current sufficiently to cause it to open, which of course resulted in an increase in current making it close again, so it "pumped," closing and opening. It is seen that the contactor makes a complete round trip in $2^1/_2$ to 3 cycles, which is within the time intervals specified by the authors. For applications on long lines where some short-circuit currents can be relatively small, it may not be desirable to effect immediate reinsertion of the capacitor—as suggested by the authors—because of unnecessary operation of the protective equipment.

Therefore, I would like to ask: "What are the new developments that prompted the writing of this paper?" And in spite of the statement in reference 1 of the paper, I would like it explained why "previous equipments decreased rather than increased" transient stability.

The conclusion in respect to the necessity of suitable damper windings on machines to be operated with series-capacitor lines checks with our previous analyses and experiences. The following quotation is taken from June 28, 1941 *Electrial World*, in an article "Amortisseur Windings for Hydro Generators" by Crary and Dungan. "If series capacitors are used in the line connecting the machine with the system, the tendency for the generators to hunt may be aggravated to such an extent that amortisseur windings would be required."

The authors tested two motor-generator sets to determine the effects of damper windings. The basic criterion of the effects of a damper winding on machine performance is the effect the dampers have on the machine subtransient reactances and time constants. I wonder if the authors could give us these values for the two machines in question.

The ratio of the inertias of these units as given is about 4/1, the copper damper machine being the larger. Thus, I would expect its natural frequency to be about one-half the no-damper machine, but the measured frequencies show the copper damper to have approximately twice the frequency of the other.

Our previous studies agree with the authors' conclusion as to the location and arrangement of series capacitors. Figure 3 in reference 7 of the paper is remarkably similar to the authors' Figure 5.

S. B. Crary (General Electric Company, Schenectady, N. Y.): Over a year ago at the 1941 AIEE winter convention, Mr. Evans indicated he was in agreement with our conclusion of the value of series compensation over the other methods of improving the stability limits of long a-c lines. We are particularly glad to have this conclusion further substantiated in the paper by Starr and Evans. Such confirmation is important since it allows the emphasis to be placed on the details of how series compensation may be best accomplished. In this respect, the paper by Starr and Evans is a valuable contribution, because it deals with those detailed questions which must necessarily enter into any practical application of series capacitors.

We have made studies of series capacitors somewhat similar to those reported in this paper. In 1928 a rather complete experimental test setup was made to check the stability of the system with and without series compensation for two and three synchronous machines. These tests indicated the safe limits of compensation and the degree to which the stability limits could be increased. Further analytical work has given additional quantitative information as to the limits of compensation. According to this work, the experimental results reported in the Starr and Evans paper indicate but one of the previously well-recognized boundaries of possible instability, that is, the hunting produced by large ratios of resistance to reactance in the transmission tie. As pointed out in a discussion by Concordia[1] there also exists a limit for the minimum amount of resistance that may be in the circuit. For practical systems this limit may be of greater importance than the one shown in the paper.

Our studies have also shown the advantages of placing the capacitors at the intermediate switching stations in the center section of the line where they will not be switched out with the line section.[2] Such an arrangement is desirable in order to reduce as much as possible the duty on the capacitor protective equipment.

Under "Types of Capacitors," the authors mention two kinds, the nonlimited- and the limited-voltage type, and indicate the desirability of using the limited type in order to reduce the cost of the capacitor. This is in general correct. However, it may be found desirable to prevent operation of the protective equipment for currents exceeding normal values and at least equal to those obtained during the largest angular swing at full-load which may be obtained without loss of synchronism. If the capacitors are capable of remaining effective in the circuit during such large angular swings, the over-all stability of the system will be improved for severe faults.

Under "Location in the Circuit" the authors point out there is little advantage in using more series capacitors than the number of intermediate switching stations. This statement is substantially correct when the switching stations are equally spaced along the line including the center portion of the line. The amount of compensation necessary is, of course, a matter of economics and performance characteristics. For a 234-mile line 66$^2/_3$ per cent compensation reduces the equivalent line length from a reactance standpoint to 78 miles. Accordingly, such a degree of compensation for this length of line may not always be necessary but may be required for longer lines. The per cent compensation will tend to increase with the line length as well as with the amount of power it is desired to transmit. We would like to ask at what rate the required amount of compensation would decrease if the assumed transient stability limit margin were decreased. Since the economic study depends upon the assumed stability margin, this becomes an important factor for the application of series capacitors for improving the stability limits. It would appear that a 20 per cent transient-stability margin for the Grand Coulee system were unusually large if this were for the more severe type of transmission-line faults. Ordinarily a much smaller margin for the more severe faults may be used in the analysis, since the probability of obtaining these severe faults is itself small. Possibly the authors would explain in more detail why they elected to choose what seems to be a conservative transient-stability margin on which to base their economic study.

It has now become generally recognized that series compensation is entirely a practical and economic way to increase the power limits of long-distance transmission lines. The chief differences of opinion will probably arise from the assumed conditions of operation upon which their use is justified.

References

1. Discussion by **C. Concordia** of Stabliity Limitations of Long-Distance A-C Power-Transmission Systems, AIEE Transactions, volume 60, 1941, page 1299.

2. The Effect of Series Capacitors Upon Steady-State Stability of Power Systems, **E. J. Allen, J. L. Cantwell.** *General Electric Review*, May 1930, page 281, Figure 3.

A. J. Krupy (Commonwealth Edison Company, Chicago, Ill.): There is one point which has not been mentioned by the authors in connection with the economic justification of the series capacitor as compared with the three-circuit alternative for the 230-kv transmission described in the paper. The cost figures as shown in the paper assume an equal degree of reliability for both schemes of power transmission either for normal or abnormal conditions. In my opinion such an assumption is not justified.

I would be interested to know whether the authors are in position to make a statement as to the relative degree of reliability of the schemes considered? It would also appear that under the existing conditions, the vulnerability of the single concentrated capacitance center both to an accidental electrical failure or an intentional mechanical damage, is much greater as compared with that of the conventional transmission circuit for the third 230-kv line.

Aside from the obvious difference in the inherent reliability of the two schemes, in the case of a complete outage of the newly added equipment or structure, the switching out of one half of the circuit shown in Figure 5c of the paper still leaves a superior system from the stability viewpoint as compared with as shown in Figure 5a, should there be an electrical failure or a mechanical destruction of the series capacitor.

A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The use of series capacitors to compensate for the reactance of transmission circuits has been recognized as a technical possibility for about 50 years. However, before such applications of capacitors could be put on a practical basis, a number of technical developments were necessary, including:

1. Reduction in cost and increase in reliability of capacitors.

2. Development of high-speed circuit breakers and relays for transmission circuits.

3. Development of protective equipment that permits the use of capacitors with voltage ratings determined by load currents.

The authors' investigation has established the practical aspects of the use of series capacitors so that the electrical industry should feel more confident in making applications.

We have heard much about the desirability of doing everything possible to conserve vital materials. Here is a scheme that accomplishes this desire at no sacrifice in system operating reliability.

E. C. Starr and **R. D. Evans:** In preparing this paper, the authors had in mind three principal elements:

1. Reactance compensation is economically sound when applied to appropriate major circuits. Voltage transformation results in line-resistance as well as line-reactance conversion. The most economic combination of impedance conversion and reactance compensation is desired.

2. The usefulness of series capacitors on transmission circuits is to improve transient stability. Application requirements of capacitors and protective equipment have been determined for a particular case, and the necessary functional arrangement for a protective scheme has been provided.

3. Systems with normal degrees of reactance compensation will not have impractical operating characteristics.

These views have been well supported by the discussion on the paper.

Mr. Crary's comment concerning our paper as a contribution, because it deals with the questions which must enter into any practical application of series capacitors, takes on added significance when it is realized that

1. Previous work was concerned principally with steady-state conditions, whereas the controlling stability condition occurs from transient disturbances.

2. Our paper not only presents the first theoretical analysis and miniature-system test results of series capacitors under transient conditions but also describes the combination of equipment necessary to make the scheme practical.

The reference quoted by Mr. Crary relative to the minimum as well as maximum amount of resistance that may exist in the circuit for stable operation indicates that for the typical case of a 230-mile line with 66.7 per cent reactance compensation, which was analyzed in the paper, the minimum stable length would be of the order of 75 miles and the maximum several times the assumed length. Consequently, the two limiting resistance conditions, as pointed out in this reference, are remote from the example given.

Mr. Crary has raised a question as to the selection of a particular margin of stability as the basis for the economic study. The choice for a selected stability condition must, of course, be based on experience in connection with the particular system and the judgment of the operating engineer. There are obvious advantages in having a margin above the calculated limit, since this makes operation less dependent upon fault-clearing speeds and upon the actual distribution of load and generating capacity. In answer to this question we have, however, made a comparison between the addition of a series capacitor and the addition of a third transmission line for the extreme increase in load which would eliminate all margin in transient stability for the same terminal impedances. Under this condition the first cost of adding the series capacitor would be about 40 per cent of the first cost of a third transmission line. If capitalized figures for additional loss and reactive kilovolt-ampere generation are added to the first cost of the capacitor, the series capacitor can still be installed for 75 per cent of the cost of adding the third line. It is to be emphasized that the economic study was made on a very conservative basis in regard to cost of construction and in regard to load factor which controls the capitalized figures for line loss. For this reason when the actual stability margin, line-construction cost, and capitalized losses are taken into account, the results, we believe, will be more favorable than shown in the paper. If the kilovolt-amperes of an individual generator is less than the rating of a line, added series capacitors can be proportioned to the particular case whereas added lines will come in relatively large steps.

Mr. Crary concludes his discussion with the statement that it is now generally recognized that series capacitors provide an entirely practical and economical way of increasing the power limits of long transmission circuits. This is a definite change in the point of view concerning the usefulness of series capacitors previously expressed in the AIEE TRANSACTIONS, volume 56, 1937, February Section, page 264, the pertinent excerpt from which is as follows:

> "... To protect the capacitor against puncture the usual practice (in using series capacitors) is to use a normal voltage capacitor with means to short-circuit it during excess current conditions. The series capacitor as proposed is thus rendered ineffective at the time when it is most needed. The alternative is to use capacitors capable of withstanding the high voltages arising under fault conditions. In no major project in America has the series capacitor received serious consideration."

The foregoing was prepared by the AIEE subcommittee on interconnection and stability factors. Both Mr. Crary and Mr. Evans were members of this subcommittee and concurred in the views expressed. The change in viewpoint is believed to have resulted from the work reported in the present paper on economic gains and on transient stability with series capacitors. Previous articles, including items 7 and 8 of our list of references, have been limited to voltage-regulation studies and to steady-state stability conditions.

Mr. Butler's question of how series capacitors can reduce transient-stability limits and his request for more information on the protective scheme indicates a lack of understanding of the essential features of the series-capacitor scheme described in the paper. It is not sufficient merely to provide a series capacitor and means for short-circuiting it and subsequently restoring it to the circuit in response to the indications of a current-sensitive device. If a "protected" series capacitor is short-circuited upon the application of a fault, and if the fault is not quickly cleared and the series capacitor is not quickly restored to the circuit, the transient-stability limits will be reduced below the values which would obtain on the system without the capacitor in the circuit.

Table I. Subtransient Reactances of Miniature-System Machines

100-Kva Machine Damper	Reactance—Per Cent x_d''	Reactance—Per Cent x_q''
None	26.0	57.7
Connected copper	15.7	14.6

This condition results from the facts that

1. The excitation on machines will be slightly lower.
2. The initial angle between sending- and receiving-end machines will be slightly less.

Both of these conditions tend to reduce the transient-stability limit of the system with the series capacitors in comparison with the system without series capacitors. This viewpoint was back of the statement on series capacitors which was made by the AIEE committee on system stability and given in its report, an excerpt of which was previously quoted.

The essential features of a series-capacitor scheme for increasing transient stability have adequately been described in the paper. This scheme is based on using series capacitors of the protected type in combination with high-speed line-sectionalizing circuit breakers and relays. In this scheme the series capacitors are (1) short-circuited upon the occurrence of a fault which would produce excessive voltage and (2) quickly restored to the circuit after the fault is cleared, the combination functioning in a sufficiently short time to make a substantial improvement in transient stability over the value obtainable without the series capacitor. While the essential elements of the scheme are simple and easily understood, it involves a conception that is expected to make feasible the application of series capacitors for transmission circuits. Quite separate from this functional scheme are the details of the protective equipment. Space limitations made it desirable to omit the details of the protective scheme from this paper.

In general we are in agreement with the technical observations of Crary and Butler. However, the inaccuracy of Mr. Butler's characterization of the series-capacitor installation at Ballston in 1928 makes it necessary for us to point out that it was not an anticipation of the scheme described in our paper, because it does not meet the essential requirements of a scheme to increase transient-stability limits. Despite the complete mastery of the problem prior to 1935, certain misapplications of series capacitors subsequent to this date have placed the device in unfavorable light among some engineers.

We do not understand that Mr. Butler contends that either the protective equipment for Ballston or for the 22-kv installation which he mentioned is suitable for use with a 50-kv 100,000-kva capacitor installation on a 230-kv circuit, such as considered in our paper. Such a view of these protective schemes is consistent with the committee members' subsequent agreements to the statements on the limitations of protected capacitors from the standpoint of transient stability as given in the AIEE subcommittee report previously referred to. Such a view would also explain why no definite proposals of series capacitors for major transmission circuits have been made previously.

Mr. Butler has asked for additional information on the subtransient reactances of the miniature-system machines. The values are shown in Table I.

In reply to Mr. Krupy, we agree that there is probably more concentrated vulnerable equipment in the compensated two-circuit system than in the uncompensated three-circuit system. However, capacitors as now manufactured and provided with means for isolating a damaged unit should be essentially as reliable as switching or other terminal equipment and when properly installed should constitute no greater hazard to continuity of service than the more common equipment. They should be no more vulnerable to intentional damage than are generators, transformers, and circuit breakers.

The third circuit will under some abnormal conditions provide greater reliability, particularly if the third line is located on a separate right of way at increased cost. However, we should like to point out that the compensated two-circuit line can lose one section and remain stable under essentially the same loading as that for which the three-circuit line can lose one section and retain synchronism. If the two-circuit line loses both sections between the same switching stations, service will, of course, be interrupted. But if the three-circuit line similarly loses two sections between the same switching stations, the power capacity of the remaining circuit would be so low that if the line were loaded materially above 60 per cent of its rating, it would also lose synchronism and interrupt service. Consequently, as we view the comparison, the three-circuit uncompensated line has some advantage over the compensated two-circuit line under abnormal conditions, but, starting from normal circuit arrangements, their performance will be comparable for all of the more frequently occurring types of outages.

Mr. Monteith has outlined the steps in the technical developments necessary to put series capacitors for transmission lines on a practical basis. Consideration should be given, as he has suggested, to the conservation of vital materials made possible by this type of series-capacitor application. For the case considered in the paper the material required for the capacitor installation would be only a small percentage of that required for the third circuit.

Stability Study of A-C Power-Transmission Systems

Discussion and author's closure of paper 42-117 by John G. Holm, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, pages 893–905.

J. A. M. Lyon (Ebasco Services Inc. New York, N. Y.): The author of this paper has made two useful contributions on the subject of stability:

1. Stability studies have been made on a variety of hypothetical systems including some with lines of great length and with operating voltages which in some cases are beyond those now in use.
2. The many well-known contributing factors to system stability have been listed and studied.

According to the author, equation 2 of the paper enables the transient stability of a system to be found by comparison with a similar system of known transient-stability limit merely on the basis of the ratios of the reactances of the systems, the voltages, and the ratings of the systems. This relationship seems consistent with the factors affecting transient stability. However, it appears that the equation should be recognized as a means of obtaining an approximate result rather than an exact one. The ratio of system capacities does not seem capable of exact evaluation. The author's definition of system rating is too simple and requires further explanation. In the same way the author's claim that the expression is valid for systems of "similar configuration" is somewhat indefinite and should be explained further.

Figures 3 and 4 of the paper seem useful, but it is desirable to know if the author has checked either of these sets of curves by the known stability limits of actual systems which fall in the rather broad range of the systems of his computations. The curves of Figure 3 are called "estimating curves," and the range of usefulness should be proved as well as clearly defined.

Part II of this paper has indicated possible corrective measures to be applied to a system to increase the stability limits. The listing of these methods, together with the statement of the results of using some of these methods according to calculating board studies, seems valuable. Obviously the problem of improving the stability of a system involves many operating and economic considerations which the author has not discussed.

Upon reading the text on the use of reactors to increase stability limits it is not at all clear whether or not Figure 9 represents the increase of the stability limit due to the reactors alone or due to the reactors together with the larger generators required with reactors.

Many of the above comments have been in the nature of specific questions; the answers to these may very well increase the usefulness of the paper. The author should be commended for making available information on a large number of theoretical systems, as well as reviewing and checking many fundamental considerations of the stability problem.

S. B. Crary (General Electric Company, Schenectady, N. Y.): It will be realized by anyone familiar with stability calculations that the author of this paper has done a tremendous amount of work. He has made a worthy contribution by presenting the results of his study which include many of the factors entering into the transmission stability problem. This paper is concerned primarily with three methods of improving stability: intermediate synchronous condensers, generator bus reactors, and resistance grounding of the sending-end transformers.

All of these methods, as indicated by the author's references, have been considered, although no comprehensive study of their characteristics has been previously published. However, they have been carefully studied for particular systems. The conclusions, in general, which we have arrived at from these system analyses are:

1. The intermediate condenser for improving the stability limits of straightaway transmission lines is not so effective nor practical as the series capacitor which, in effect, reduces the equivalent line length.

2. Generator bus reactors have been found in general to be more expensive and less desirable as a means of improving the stability limits than the use of higher short-circuit ratio generators. Their use appears to be limited to stations which after completion have been found to have too low stability limits.

3. The use of resistance in the neutral of the sending-end transformers does provide a means for improving the transient-stability limits. The benefit from its use decreases with shorter switching time, higher generator inertia and is, of course, of no value for three-phase faults which are the most severe type. Accordingly, the use of resistance in the neutral may or may not be found necessary.

We would like to ask the author how he arrived at such a high value of equivalent reactance for the synchronous condensers (128 per cent). We would expect this reactance to be considerably less than this, particularly if the synchronous condensers are operating near full leading kilovolt-amperes.

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): The author presents an unusually complete and readable paper on the subject of power-system stability. The premises on which the studies of the 31 assumed systems are based are of practical significance, and therefore the results are understandable and of value to those who must cope with stability problems in the industry. The reduction of the data to a series of tables and curves which can be interpolated and extrapolated for intermediate values is noteworthy and considerably extends the usefulness of the results.

Present conditions have greatly aggravated the problems of stability on many power systems because of the deferment of important system reinforcements and plant additions, upon which has been superimposed greatly increased war loads. These situations are a challenge to management to make the fullest use of the engineering ingenuity at its disposal so that the best possible service to essential loads may be provided by skillful disposition of existing facilities.

It therefore seems worth while to call attention to the fact that, in order to make full use of the potential stability limits of a power system, there are a number of factors to consider in addition to the power-angle relationships. Of these, the adequacy of protective relay and machine control systems is perhaps the most important.

High-speed fault clearance is most effective in mitigating stability troubles. Conversely, slow fault clearance can bring with it a host of complications. Unfortunately, slow fault clearance is quite prevalent, and often little can be done about it under present conditions, especially with older types of switchgear.

Practically all types of transmission-line relays may be subject to false operation during system oscillations, even within the theoretical stability limits of the system. The application of such devices should therefore be particularly well considered under these conditions. Auxiliary control devices may be required to assist in correct discrimination, for example, between an actual fault in a line section or the presence of an electrical center of oscillation there. In extreme cases, such correct discrimination may be impossible, even with the best equipment available, but often considerable improvement may be effected by comparatively simple rearrangements and minor additions.

Adequate control features for synchronous are also of importance, especially on condensers and on generators which may be motoring as condensers prior to the fault. Adequate excitation control and governor oil capacity may easily be the difference between success and failure of the system to ride through the oscillations incident to line faults, and again simple rearrangements, often within the scope of present restricted construction, can effect great improvements.

While the considerations of electrical and mechanical characteristics of machines, loads, lines, and switchgear are of fundamental importance in stability studies of electric-power systems, the application of controls to such equipment must be made with great care and with special emphasis on their behavior under emergencies so that they will permit the theoretical ability of the system to be actually attained. The practical power limits of systems may often be extended with only existing facilities by engineering ingenuity. Under the stress of the times, such efforts should easily be justified.

John G. Holm: The discussions presented do not lend themselves to a common reply; they will therefore be handled separately.

Mr. Crary prefers a series capacitor to an intermediate synchronous condenser for improving the stability limits of trunk transmission lines. It is true that the capacitor reduces the equivalent line length, but in the long lines the resistance is very large, and, if the line is to be economical, it will presumably transmit a large block of power, so that its current will also be high, even if the highest voltages are employed. Consequently the resistance drop is large even though it is assumed that lower-resistance conductors are justified economically. The longer the line, the larger the resistance drop which must be compensated by inductance, after allowing for some resistance to be left between the ends of the system. Therefore in the longer lines there will be a limit to the series-capacitor compensation that may be used, and consequently a limit to the increase in the stability limits obtained from this source.

It is embarrassing to disagree with a man of Mr. Crary's knowledge, but it seems to me that the series-capacitor compensation has definite advantages over the intermediate condenser only in lines from 150 to about 375 miles long, and no longer, assuming that the analysis of systems is made with actual loads and not on the basis of an infinite bus. However, so far as the economics of the question are concerned, it remains yet to be proved that even in 150- to 375-mile lines the advantage lies on the side of the series capacitor. So far, no economic comparison has been made between a line using series-capacitor compensation and the same line whose stability limits are raised to the same levels by the use of other means than the series capacitor.

As Mr. Crary points out, shunt reactors on the generator bus are more expensive than the higher short-circuit ratio of generators. So far as I know, the highest short-circuit ratio of generators ever used is somewhere around 3.0, and it is very unlikely that the increase in stability limits obtained from this short-circuit ratio approaches that obtained from shunt reactors. Moreover, the short-circuit ratio has a far greater effect on the steady-state-stability limit than on the transient limit, and is to be resorted to primarily where the steady-state stability limit is to be raised first. The shunt reactors therefore may be used in addition to the increased short-circuit ratio in cases where the latter fails to bring the transient-stability limit to the desired level. The cost per unit of improving the transient stability by the use of the shunt reactor will therefore not be so expensive as might appear at first.

Besides the particular applications of the shunt reactor outlined in the paper, the shunt reactor, as well as the resistance in the sending-transformer neutral, is particularly well adapted for application to existing systems with prevailing slow fault-clearing times when it is desired to raise their stability limits. The resistance may, of course, be used if the transformer insulation is sufficient.

Mr. Crary asks why the synchronous reactance of synchronous condensers was taken at 128 per cent. In the first place, I wish to make clear that while the saturated synchronous reactance of the receiving-end condensers was taken at 128 per cent, that of the intermediate condensers was taken at 72 per cent (0.8 of the unsaturated value for both the receiving-end and the intermediate condensers). I regret that this figure was omitted from the second abridged draft of the paper. The unsaturated direct-axis synchronous reactance of synchronous condensers is given at 160 per cent in the First Report of Power System Stability and in other sources.[1–3] At any rate, it seems that the value of the synchronous reactance of the receiving-end condensers has a very slight effect, if any, on the system stability limits.

Mr. Lyon has asked a number of questions in his discussion. I regret that the limitations on the length of the paper did not permit going into the various details, and I am glad that the questions have been brought up. They will now be answered.

Equation 2 of the paper gives results within five per cent of those obtained by actual measurement on the network analyzer. By systems of "similar configuration," to which the equation applies, are meant trunk transmission lines. The number of circuits, the sectionalizing of the lines, or the lines, or the units of the end equipment are of no importance. As for the definition of the "system rating," I may say that much thought has been given to it. Whatever definition is given has only a theoretical significance, because for the two systems in question the quantity P enters the equation as a ratio, so that care should be taken only that exactly the same definition is applied to both systems. If the two transmission lines are designed on the same principles, so far as the conductor current-carrying capacity, corona power loss, and the voltage regulation are concerned; if the power factors and line and transformer efficiencies are properly taken care of; and if the transformer capacity is installed to fit the above conditions, in a similar manner in the two systems; then the installed transformer capacities may be taken for the quantity P. If the two lines are designed on entirely different principles, then either design correction factors must be applied, or what is known as the reliable rating of the line when there are no faults on it must be used. Care should be taken that criteria applied to both systems are as nearly as possible alike.

Mr. Lyon asks if curves of Figures 3 and 4 of the paper have been checked with stability limits of existing systems. There are very few systems whose stability limits have been reliably reported. The curves, however, have been checked on two systems, and a check was obtained well within engineering accuracy. The difficulties of such a check, however, should not be underestimated in view of the approximations necessitated by the network analyzer and the difficulties of evaluating and representing the system load. The stability limits determined on an actual system will never completely agree with those obtained through measurements on the network analyzer. The curves of Figures 3 and 4 are based on the study of a certain range of systems. They can be used for any fairly economical combinations of kilowatts, mileages, and voltages. When a case is taken of transmitting a certain block of power at a voltage entirely out of proportion to the transmission distance, a point on Figure 3 will be obtained which might lie either between the separate curves or entirely outside them.

Figure 9 represents the increase in the stability limits due to reactors with a corresponding increase in the kilovolt-ampere generator capacity, as discussed in the paper. I regret that this point was not made clearer. However, the increases in the stability limits due to reactors alone may be calculated by one of the methods outlined in the paper.

Mr. Lyon regrets that the paper does not discuss the economic considerations of the improvement in stability limits. There are so many factors entering into system costs that perhaps it would be desirable to present in a separate paper the costs and the effect on them of the various methods of stability improvement.

Mr. Von Voigtlander justly points out the importance of high-speed relaying and fault clearing of the transmission line. So far as system stability is concerned, there is nothing so effective as high-speed fault clearing. Mr. Von Voigtlander's statement that the system control equipment should be adequate for responding properly during emergencies as well as when the system is near its stability limits could not be made any stronger.

In conclusion I wish to thank the discussers kindly for the trouble they went to in preparing their discussions and for the contributions they made.

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Analysis of Short Circuits for Distribution Systems

Discussion and author's closure of paper 42-88 by Charles F. Dalziel, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 757–64.

F. S. Benson (Pacific Gas and Electric Company, San Francisco, Calif.): This paper is timely as a source of additional information for computing short circuits on low-voltage distribution systems when delta-wye or delta-delta transformer banks are installed, serving both three-phase and three-wire single-phase loads. The equations and formula developed in the paper will be helpful in determining the magnitude of short circuits on low-voltage or secondary systems.

I agree with the author that resistance plays an important part in limiting low-voltage line-to-ground short-circuit currents; all resistances should be included in the computation, even though they are of such low magnitude as bolted connections between wire and bar, or wire and wire. Field experience has shown that the magnitude of low-voltage line-to-ground short circuits is usually considerably less than that computed by ordinary methods; using reactance values only, we have found, if resistance is included, the computed values of short-circuit currents (line-to-ground) are in a reasonable agreement with those occurring in practice.

The graph in Figure 2 shows that the effect of ground resistance in limiting short-circuit currents is greatest at low voltage, that is, 800 volts or less, and is least when the transformer-bank secondary voltage is on the order of 12 kv. If, however, internal faults occur within such transformer banks, the effect of the ground resistance is quite important in reducing the magnitude of the line-to-ground voltage, even though the transformer secondary voltage may be as high as 12 kv. This is probably true because high-voltage systems are usually solidly grounded at the source of power and a transformer-bank internal fault adds an extensive zero-phase sequence network in series with low-voltage ground resistance.

The analysis of the magnitude of transformer reactance during transformer internal faults is interesting; the author's tests tend to confirm his conclusions that the transformer reactance can be entirely neglected when computing the value of such faults. The elimination of the transformer reactance from computations during transformer internal faults will aid in determining the correct and economical size of case ground wire or secondary-neutral ground wire to the grounding device.

Figure 16 is instructive and again substantiates my experience that ground resistances on the order of one ohm or less do not necessarily limit the rise of voltage on the transformer secondary neutral to the prevailing idea of a low value, when transformer internal faults occur. When distribution transformer banks are installed near substations having large power supply, and the ground resistance is a large proportion of the total impedance to the fault, the voltage on the distribution transformer secondary neutral may approach the line-to-ground value of the primary voltage supplying the bank, although the ground resistance is considerably less than one ohm.

Charles F. Dalziel: The author is pleased to note that experience on actual power systems confirms the conclusions derived in the paper. This is significant, since all of the experimental data were obtained from tests made on small laboratory transformers operated at reduced voltage. The interpretation to the effect that, during transformer internal faults, the transformer may be neglected entirely, should be qualified slightly. As stated in the paper, it is believed that this simplification should give satisfactory results for cases in which the feeder impedance is at least equal to 50 per cent of the transformer impedance in high-voltage ohms. The change from solid to dash lines in Figure 16 indicates the proposed limit. The approximation may result in excessive error for transformers located near the bus. Obviously, neglecting the effects of the transformer would result in very serious error for transformers directly connected to a bus of maintained voltage.

Attention should be called to the fact that practical units are specified throughout the paper. Although per units may be used directly in most equations, corrections involving $\sqrt{3}$ are necessary when using per unit quantities in analyses involving unsymmetrical conditions in wye-delta transformations. This is true for the power-leg and light-leg equations derived in the paper, and practical units were specified to avoid introducing unnecessary complications.

Steady-State Theory of the Amplidyne Generator

Discussion and author's closure of paper 42-89 by Troy D. Graybeal, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 750–6.

J. L. Bower and **B. H. Caldwell** (General Electric Company, Schenectady, N. Y.): The author has developed an interesting and useful method of predicting the characteristics of an Amplidyne generator with the aid of constants which can be measured from open- and short-circuit tests. We have been using a similar method, which is based on tests made on a large number of machines for a wide range of applications and have found in the general case that certain modifications of the theory presented by the author are required.

The author has confused speed-voltage coefficients with inductances. In general the author's terms L_{AA}, M_{A-s}, and so forth, which are speed-voltage coefficients, are not equal to the corresponding inductances, even though the units are the same. Also the mutual inductance between two windings is the same when viewed from either winding, while the mutual speed-voltage coefficient between two windings usually has different values when viewed from the different windings.

The steady-state equations 6 and 7 do not include terms which represent the speed voltage developed in the main- or direct-axis armature winding due to direct-axis armature current and the speed voltage developed in the cross- or quadrature-axis armature winding due to quadrature-axis armature current. These coefficients are primarily functions of the centers of current collection of the direct- and quadrature-axis currents. The effective center of current collection is a function of the brush position and all of the factors affecting commutation, such as magnitude of current, commutating field adjustment and brush characteristics (both mechanical and electrical) which vary with brush material, speed, commutator film condition, and so forth. Unsymmetrical spacing of the windings also gives rise to some degree of mutual coupling between all of the various windings, but experience has shown that for the normal case only the ones discussed above have sufficient effect to warrant their addition to the author's equations 6 and 7.

Adding the term $SM_{BB}i_B$ (the speed voltage developed in the quadrature-axis armature winding by current flowing in the same winding) and the term $SM_{AA}i_A$ (the speed voltage developed in the direct-axis armature winding by current flowing in the same winding), we obtain the steady-state equations

$$-i_A(R_A+SM_{AA})+i_BS(L_{AA}-M_{A-qB})=V_A$$

$$-i_AS(L_{AA}-M_{A-dA})-i_B(R_B+SM_{BB})+i_sSM_{A-s}=0$$

The open-circuit equations become

$$i_s=e_s/R_s$$

$$i_B=\frac{SM_{A-s}}{R_A+SM_{BB}}i_s$$

$$V_A=\frac{S^2(L_{AA}-M_{A-qB})M_{A-s}}{R_B+SM_{BB}}i_s$$

and the short-circuit equations are

$$i_s=e_s/R_s$$

$$i_A=\frac{S^2M_{A-s}(L_{AA}-M_{A-qB})i_s}{(R_A+SM_{AA}(R_B+SM_{BB})+S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})}$$

$$i_B=\frac{SM_{A-s}(R_A+SM_{AA})i_s}{(R_A+SM_{AA})(R_B+SM_{BB})+S^2(L_{AA}-M_{A-dA})(L_{AA}-M_{A-qB})}$$

and the load equations are

$$i_B=\frac{-S(L_{AA}-M_{A-dA})i_A}{R_B+SM_{BB}}+\frac{SM_{A-s}i_s}{R_B+SM_{BB}}$$

$$V_A=-\frac{(R_A+SM_{AA})(R_B+SM_{BB})+S^2(L_{AA}-M_{A-qB})(L_{AA}-M_{A-dA})i_A}{R_B+SM_{BB}}+\frac{S^2M_{A-s}(L_{AA}-M_{A-qB})i_s}{R_B+SM_{BB}}$$

The terms SM_{BB} and SM_{AA} appear in the same manner as the resistances R_A and R_B. These terms SM_{BB} and SM_{AA} can be and frequently are considerably larger than R_A and R_B. It is immediately apparent that the term SM_{BB} plays a very important role in determining the load characteristics; and since the coefficient M_{BB} is a function of load and speed, this variation instead of saturation and nonlinearity of brush drop, may well account for a considerable part of the curvature of the curves of Figure 8. Also the difference in characteristics with the different grades of brushes shown in Figures 6 and 7 might well be due to differences in commutating characteristics of the brushes rather than to difference in brush drop.

The author uses a negative sign in equation 5 before the speed-voltage coefficient, $SM_{A-qB}i_B$, which is the speed voltage developed in the direct-axis armature winding by current flowing in the stator coil in the quadrature axis. This is the convenient convention for the author's case, since the coil qB is assumed connected so as to partially compensate for armature reaction in the quadrature axis. Although the validity of the analysis is unaffected, it is of interest to note that in the usual case, particularly for small size machines, this winding is connected so as to increase the flux of armature reaction.

The author gives an appropriate summary of the difficulties involved in calculating the characteristics of the Amplidyne generator without the benefit of the constants which he determines experimentally. The designer must have the solution of this more difficult problem in order to build a machine which will have characteristics required by a specific application. However, as is often the case, the simplified method outlined by the author is a definite contribution toward the solution of the more difficult problem, since its use aids in the development and intelligent application of experience factors which simplify and improve the accuracy of the designer's calculations.

P. Lebenbaum, Jr. (General Electric Company, West Lynn, Mass.): The author is to be complimented on his analysis of the Amplidyne generator and the excellent check he has obtained between theory and test.

As has been pointed out, the Amplidyne generator is a dynamoelectric power amplifier. The three fundamental requisites of a good amplifier are its amplification ratio, its fidelity, and its speed of response. Commercial Amplidyne generators have calculated amplification ratios of 30,000 to 150,000 to 1, and time constants of less than one eighth of a second between signal current and output voltage. The time constant of the machine, while slow compared to the vacuum-tube amplifier, is nevertheless extremely small when compared with those of other machines in the circuits where the Amplidyne generator is most frequently applied.

The importance of good commutation and correct brush position in both short-circuit and load axes cannot be overstressed, for the brush short-circuit currents and magnetizing or demagnetizing ampere turns because of brush shift affect all three amplifier properties. The author's analysis assumes that the brushes are on the inductive neutrals in both axes and that the brush short-circuit currents are zero. If these assumptions were not true, equations 4 and 5 would contain terms involving mutual inductances between the direct and cross axes, and succeeding equations would be changed. However, if the Amplidyne generator has been properly adjusted for commutation with all brushes on their respective neutrals, these additional terms are not needed in the steady-state analysis.

An example of the importance of brush short-circuit currents is the author's test shown in Figures 6 and 7. The difference in saturation curves of the experimental Amplidyne generator because of change in cross-axis brush material is due as much to change in cross-axis brush short-circuit currents as to change in brush drop. The experimental machine was apparently compensated properly with the soft graphite brushes in place, for the curves of Figure 7 agree closely with theory, the open-circuit voltage varying approximately as the square of the speed for constant signal current. However, the change to a harder brush, while keeping the same compensation in the commutating field, results in undercompensation of the coils undergoing commutation. It can be shown that undercompensation in the cross axis results in a directly demagnetizing effect in the signal field axis. The higher the speed, the higher are these demagnetizing ampere turns. Thus the open-circuit voltage no longer varies as the square of the speed, and hence the "crowding" and nonlinearity of the curves of Figure 6 results.

Troy D. Graybeal: The discussions by P. Lebenbaum, Jr., J. L. Bower, and B. H. Caldwell are both pertinent and interesting. The representative data for commercial Amplidyne generators given by Mr. Lebenbaum are particularly significant.

Concerning the addition to the equations of the two speed terms which represent the voltages generated in either axis of the machine by current in the same axis, these terms theoretically should be included if a more accurate solution of the steady-state operation of the machine is desired. However, the apparent increase in accuracy cannot be realized in actual practice, because the coefficients M_{BB} and M_{AA} are critically dependent upon the condition of the brush contact surfaces, as well as upon the brush position. For example, the slight "play" between ordinary brushes and brush holders will cause a large increase in these constants when the direction of rotation is reversed. A slight shift of the brush position from the true neutral, or a slight change in the surface conditions, because of the minute arcs between brushes and commutator caused by normal load current, might make the coefficients several times larger. Figure 1 shows the values of ohmic resistance and of SM_{BB} plotted against speed. The two nearly linear curves show that while M_{BB} was constant for each test, a change in cross-axis current changed the value of the "constant" by a factor of four. This increase was due to the change in the distribution of current over the brush surface, even though no change had been made in brush position or commutating pole adjustment.

For the calculation of the curves given in the paper, an average of the cross-axis circuit equivalent resistance was taken as shown by the dotted curve of Figure 12 of the paper. The actual ohmic resistance was 1.16 ohms, which was increased to 1.55 ohms to give the equivalent resistance used for the calculations. This procedure is justified by Figures 10 and 11 of the paper which show that the curves calculated on this basis do give a very close approximation to actual test results.

The theory presented in the paper was developed by calculating the speed voltages which are generated in the armature of the machine. The coefficients which appear in the final expressions are ratios of flux linkages to currents and therefore are coefficients of inductance. These coefficients would be identical with the open-circuit inductances if perfect commutation could be obtained in the exact geometric neutral. The fact that these conditions cannot be fulfilled in an actual machine gives rise to effects which reduce the output of the machine. Actually, these effects represent a reduction in the numerical values of the coefficients of inductance compared with those obtained from measurement with the coils open-circuited and the brushes of the machine raised. The fact that the performance of the Amplidyne generator cannot be calculated on the basis of inductances obtained from the usual methods of calculation or from usual inductance test procedure does not mean that the coefficients which appear in the equations are not inductances, but rather that the inductances are different under actual conditions of operation. These differences arise because the distribution of flux is not the same as is usually assumed for calculation, or as that which exists during open-circuit inductance measurements. Since Bower and Caldwell prefer to call the coefficients which apply in actual operation "speed-voltage coefficients" rather than inductances, no doubt they find that this terminology avoids confusion in their work with Amplidyne generators.

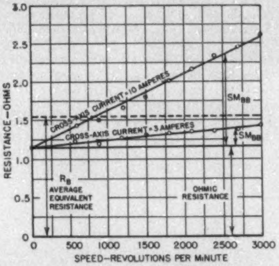

Figure 1. Curves showing the components of the equivalent cross-axis circuit resistance of the Amplidyne generator and the variation of these components with speed

The negative sign mentioned was used in equation 5 because the flux of the commutating pole in the cross axis always opposes the flux of armature reaction. If in addition an additive coil in the cross axis is used in an attempt to increase the cross-axis flux, only a slight increase can be obtained. The additive turns increase the resistance of the cross-axis circuit and therefore decrease the current. This smaller current flowing through the increased number of magnetizing turns produces only a slight increase in flux. The additive coil will increase slightly the machine efficiency and also tend to mask out the nonlinear effects introduced by the brushes, the commutating voltages, and so forth, in the cross-axis circuit. On the other hand, omission of this coil will improve the speed of response of the Amplidyne generator to changes in the signal current.

The purpose of this paper was to present the theory of the Amplidyne generator in a form suitable for use in applying the generator to control mechanisms. For this purpose, simple expressions of the operation are essential, because the Amplidyne generator must be studied, not as an isolated unit, but in relation to all other units of the control mechanism. Information on details of design can be obtained from tests of experimental machines; these "experience factors" are helpful to the designers but are seldom published, and therefore are not available to the purchaser. It is hoped that this paper will encourage the publication of the results of other investigations which no doubt have been made upon the Amplidyne generator.

Precision Speed Control for World's Largest Induction Motor

Discussion of paper 42-132 by R. R. Longwell and M. E. Reagan, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 634–8.

E. Herzog (U. S. Army Air Corps, Wright Field, Dayton, Ohio): A few explanatory notes may be in order in regard to the authors' presentation of this particular control problem.

Essentially two controls, the start–stop switch and the speed setting, permit the operator to control the entire assembly in normal operation. The one brings the fan motor to standstill, ready to run, or shuts it down from that point; the other controls it through the entire speed range from standstill to maximum speed. As long as the equipment performs properly the operator merely supervises the correct functioning of the control. The total investment in the installation is, however, so great, and the need for its use so urgent at certain times that the drive could not be held inoperative because of a failure of the automatic control. For this reason control can be transferred from automatic to manual at any point in the operating cycle. To facilitate repairs after completion of the run, the control can be rapidly subdivided and reassembled in various groupings.

With modern tendencies toward automatic control there may be no need to explain the particular set-up, but the authors pointed out the elaborateness of the control, and, as has been stated in this discussion, the control is in some respects only semiautomatic.

The large investment in the equipment is obvious. The new type of equipment precluded the possibility of using operating personnel familiar with its operation, and the enormous expansion of the plant made it most improbable that trained operators could be obtained in the present state of the labor market. The expectations were actually fulfilled; trained personnel have to be scheduled carefully for key positions. The automatic control described has made it possible to operate equipment of considerable complexity with a minimum of trained personnel leading a large group of semitrained employees. The government engineers specified the control requirements, and the co-operation of the electrical industry has been most gratifying in this and many other cases when it has been necessary to fulfill unusual requirements.

Electrical Features of Design and Operation of the Plantation Pipe Line

Discussion and author's closure of paper 42-143 by M. A. Hyde and H. B. Britton, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 638–44.

W. E. Stueve (nonmember; Oklahoma Gas and Electric Company, Oklahoma City, Okla.): Since I have been interested in the electrification of crude oil and oil-products pipe-line pumping stations for a great number of years and have conducted negotiations for my company resulting in the complete electrification of certain transcontinental crude-oil pipe lines, I was given an opportunity to discuss this paper by the authors.

I think the engineers are deserving of the highest praise in working out the design of the motors used in the pump stations, which design enables the operators to increase the motor output 50 per cent by merely increasing the ventilation or air-cooling arrangement of the motor. Thus, on the 12-inch section of the line 600-horsepower motors are used for original capacity, and the same motors will deliver 900 horsepower later on, after the intermediate booster stations are installed. This problem always arises in most crude-oil pipe lines being installed to handle the flush production of newly discovered oil fields.

Another contribution, which the design electrical engineers have made to the pipeline industry, is the recognition they give to high load factors or capacity factors at which most pipe-line pumping stations operate. The motor is designed for low starting torque since the centrifugal pumps are always started against a closed header gate valve with consequent low rotor resistance, resulting in very good efficiency of the motor itself.

Another achievement accomplished by the design engineers is the elimination of a fire wall between the pump and motor. Most early-day pipe-line operators were reluctant to install electric motors for pump station use on account of the apparent fire hazard. The elimination of the fire wall reduces the investment in the pump station which tends to make the use of purchased power more economic.

The automatic control features embodied in the design of the pump stations are also an achievement of note, since they eliminate the human element to a great degree in the operation of the pump station. Certain labor savings are thereby accomplished which again tend to make the use of electric power more economic.

The only criticism I would care to offer is one of a mechanical nature perhaps but which has to do with the over-all flexibility in the pumping operation. In my experience in negotiating contracts for pump-station electrification, I was always confronted with the statement of manufacturers of other forms of prime movers that, since the electric motor speed could not be efficiently changed, and the induction motor was essentially a constant-speed prime mover, it did not offer the proper flexibility, when changes in capacity were desired. This operating flexibility was discussed very briefly by the authors, but in my judgment this should have been enlarged upon to a greater degree by showing what actual capacities could be achieved at high efficiency points of both motor and pump in a long pipe line similar to the Plantation Pipe Line containing a great number of pump stations. Thus, as an example, if we assume a level pipe line employing four pump stations with two pump units in each station arranged in series, the following capacity rates can be realized:

Eight pump units or all motors on this hypothetical line, of course, will provide 100 per cent capacity; six pump units, properly selected, will provide 86 per cent capacity; and five pump units, properly selected, will provide 78 per cent capacity. One pump unit in each station will provide 70 per cent capacity. Intermediate values of capacity between those selected above can be achieved by throttling the discharge to a certain extent without lowering the pump or motor efficiency very much.

As stated previously, the dual horsepower design of a motor, permitting the increased loading of the motor or decreased loading of the motor, as the case may be, is an outstanding achievement, for in the case of crude-oil pipe lines the loads are always greater at the outset, because of the flush oil production being handled with a resulting decrease in capacity later on. In the case of gasoline or oil-products pipe line the reverse is true, in that the original capacities are low until the business is built up, and as more capacity is required the larger loading of the motor becomes necessary. All of this eliminates tearing out motors and purchasing new equipment as time goes along, and this design accomplishes results by merely adding or taking off a fan on the rotor to increase or decrease the ventilation.

M. A. Hyde: It is of interest to note that the provisions made in the original design of the line for capacity increase already are justified. Since the paper was written, it has been found desirable to increase the capacity of the main line, and fourteen new intermediate stations are now under way, which will provide 50 per cent more throughput. When this work is completed, the aggregate motor horsepower on the system will be 51,000 horsepower.

Analytical Treatment for Establishing Load-Cycle Ratings of Ignitrons

Discussion and authors' closure of paper 42-94 by D. E. Marshall and E. G. F. Arnott, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 545–8.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis): In their paper, the authors D. E. Marshall and E. G. F. Arnott, have solved in a very clear and practical way the problem for rating of rectifier tubes subjected to different load cycles, but by doing so they have considered only their thermal characteristic and acceptable arc-back frequency as the limiting factor.

During the last two years, the Allis-Chalmers Company has been conducting field as well as laboratory investigations in order to determine the influence of the inverse current on arc-back frequency. It was found that the arc-backs are in a certain relation to the inverse current and that this current is an important factor for determining the rating of rectifier tubes. In most cases it is possible to cool the rectifier in such a way that no internal parts of the tank or tube reach a critical temperature. However, it is not so simple to arrange the baffling in order to limit to a certain degree the ionization during the inverse cycle or the inverse current without introducing disturbing arc phenomena. Therefore, besides the temperature of the cooling water, the deionizing properties of the tube also will have to be taken into account. This means that the inverse current, which is no doubt responsible for terminating the length of life of the tube, has to be considered when rating a rectifier.

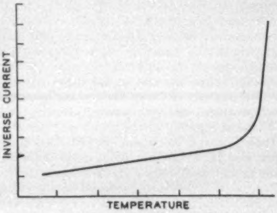

Figure 1

As can be seen in Figure 1 of this discussion, after a certain temperature the inverse current increases very rapidly and may reach a value which can endanger considerably the life of some internal parts of the tube. It is therefore necessary, when

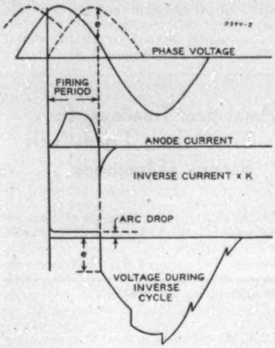

Figure 2

attempting to rate a tube, first to determine the temperature at which the inverse current does not exceed a certain safe value, and only then is it advisable to employ an analytical treatment such as outlined by the authors. The backfire test, as outlined by the authors, is no criterion for the rating because at higher d-c voltage the backfire frequency is not only a function of the cooling (or of the inverse current) but also of the anode voltage during the inverse cycle, especially of the value following the firing of the anode. (See *e* in Figure 2 of this discussion.) In other words, the inverse current of a given tube is a function of the cooling as well as of the voltage impressed between the anode and cathode during the inverse cycle.

In case a tube is being used in a welding circuit, or a six-phase rectifying circuit, it will have to be realized that for the same temperature of the tube a considerable difference in the backfire frequency may be found, due to the difference of the negative voltage (e) at the end of the firing period.

For instance, a curve illustrating backfire temperature against anode current, as shown in Figure 4 of the paper on "Sealed-Tube Ignitron Rectifiers" by M. M. Morack and H. C. Steiner holds good only for the circuit used during this test, and therefore, the rating cannot be based on the level of load which the tube will carry with acceptable backfire frequency.

In order to obtain very definite operating and rating characteristics for a given tube, it will therefore be necessary to take into account the above considerations regarding the current and voltage during the inverse cycle.

REFERENCE

1. SEALED-TUBE IGNITRON RECTIFIERS, **M. M. Morack, H. C. Steiner.** AIEE TRANSACTIONS, volume 61, 1942, August section, pages 594–9.

D. E. Marshall: Mr. Marti has offered several interesting and pertinent remarks concerning methods to use in determining the basic rating of a mercury-arc rectifier. We are, in general, in agreement that among other things the arc-back rate should be a function of the inverse current and the wave form and magnitude of the inverse voltage. In fact, our paper is actually based on such an assumption. We assume, that, for a given magnitude of inverse voltage at a given wave form, the other variable factors influencing arc-back are the demand current and the pressure of mercury vapor. The latter two determine the inverse current. Experiments performed in the Westinghouse Electric and Manufacturing Company on an unshielded ignitron a number of years ago indicate that for a given water temperature the inverse current is a nearly linear function of the demand current, other factors being constant.

In welding control circuits, the inverse voltage wave form depends on the line voltage, the power factor of the circuit, and the degree of phase control. Sealed-off ignitron tubes are rated for the worse combination of the above factors. This determines the voltage wave form. The current wave form is also set by the above setting of power factor and phase control of the test circuit. Thus the transition voltage is definitely set, and the rate of change of load current is directly proportional to the magnitude of the demand current. The water temperature is set at the maximum rated temperature.

Under the above conditions, the problem of rating the tube reduces to specifying the magnitude of the demand current allowable for a given duty cycle.

Our method assumes that the probability of arc-back at a given duty cycle will not exceed that of the basic test if the equivalent temperature of the vapor between anode and cathode does not exceed that of the basic test. This, we assume, will be the case if the heat energy stored in the tube structure and vapor never exceeds that stored in the basic test. We further assume that the stored energy is equal to the tube loss minus the heat carried away by the water and air-cooling facilities of the tube.

This would seem to agree with Mr. Marti's comments as follows:

1. The inverse current will be directly proportional to demand current.

2. If the arc-back rate is proportional to inverse current, then by item 1 it is proportional to the demand current.

3. Since, in general, the number of arc-backs that can be tolerated is measured in elapsed time and not in actual conduction time, then it would seem that the product of conduction time and demand current should be made constant. This means that the average current in the tube should be constant. This conflicts with the original assumption that the maximum equivalent vapor temperature be not exceeded. However, if the equivalent vapor temperature is kept constant, the average current criterion is within safe limits.

Mr. Marti's comments regarding rectifier operation as directed to the paper by Morack and Steiner are well taken in that the following factors need to be specified when the quality of performance of a given rectifier tube is judged.

1. Type test circuit.
2. Type load $\left\{\begin{array}{l}\text{Resistive}\\ \text{Inductive}\\ \text{Counter electromotive force}\end{array}\right.$
3. Inverse voltage wave form.
4. Rate of change of anode current at commutation.

The preceding four factors are interrelated and can be specified quite accurately by specifying the type circuit, the regulation, phase control, and loading.

Material for many papers could be found in the thorough investigation of the effect of circuit constants on the reliability of rectifier operation. It is hoped that these discussions will stimulate further research along this line.

Sealed-Tube Ignitron Rectifiers

Discussion and authors' closure of paper 42-106 by M. M. Morack and H. C. Steiner, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 594–9.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper outlines a very interesting account of the development which has taken place during the last few years in connection with sealed-off single-anode Ignitron type of rectifiers.

I would like to point out that a practically backfire-free operation and a low arc-drop single-anode rectifier can also be obtained when using a continuous ignition-excitation system, in spite of the fact that a cathode spot is maintained during the inverse cycle. In other words, it is not necessary to use an ignitor for initiating a cathode spot every cycle in each tank. Operating experiences with the Allis-Chalmers "Excitron" rectifier with a continuous ignition-excitation system have definitely proved that the small excitation arc does not increase deionization during the inverse cycle to such an extent that it contributes to the arc-back susceptibility.

I had occasion to call attention to this fact in my paper "Excitron" rectifiers, presented during the 1940 winter convention of the AIEE.

A cross section of a rectifier tank with continuous excitation, as introduced by the Allis-Chalmers Company over three years ago is shown in Figure 1A of the paper. This tank is somewhat larger than the one shown by the authors in their Figure 2 but is about equally shielded. A unit with six such tanks was in operation for several months on 600 volts and finally went into operation, supplying load to an industrial plant at 250 volts, 2,000 amperes, with occasional overloads of up to 3,000 amperes. This unit has been carrying such loads for over a year without a single backfire, in spite of the fact that the shielding is very slight, having a grid with relatively large holes. This particular "Excitron" rectifier tank had an arc drop comparable to that of the "Ignitron" rectifier, even at a lower current, that is, up to the current rating of the ignitor tube. This may show that the "Excitron"-type rectifier does not require more deionizing properties in spite of the fact that the excitation fires over the whole cycle. Naturally no difficulty is encountered in building a sealed-off rectifier tube using the continuous ignition-excitation system.

The use of two ignitors and a holding anode as shown in Figure 1 of the authors' paper complicates the design of the Ignitron rectifier and also requires a relatively complicated circuit with considerable auxiliary equipment. We would like to know if the authors consider such complication of the ignition-excitation system necessary only in connection with rectifiers used in the industrial and railway field, where the load fluctuates greatly and where periods of very low loads are encountered. Furthermore, does the introduction of a holding anode reduce the arc drop and the surges at low temperature?

Under the heading, "Ignitor-Excitation," the authors refer to a rather serious limitation, which is encountered during low load periods with the ignitor system. It may be of interest that the previously referred to "Excitron" unit was for several months operating on very low load, sometimes in parallel with a motor-generator set or convertor, but no difficulties were encountered in carrying loads of a few amperes.

Reference

1. "Excitron" Mercury-Arc Rectifiers, O. K. Marti. AIEE Transactions, volume 59, 1940, pages 927-31.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I was happy to note the statement in this paper regarding the relationship between arc-drop and arc-back frequency in any given type of mercury-arc rectifier. This relationship exists in all types of rectifiers and should be recognized.

In Figure 3 of the paper it is noted that the arc-drop curve of the Ignitron described is plotted in terms of instantaneous anode current. In general, the user of rectifiers is interested in the arc drop in terms of cathode current. I would like to ask whether the authors find that the cathode current per group may be applied to this curve or if the average arc drop is influenced by the various rectifier transformer connections because of the difference in the length of the conducting period. In general, the arc-drop voltage is higher at the beginning of a conducting period, before maximum ionization density has built up, and this would suggest that those circuits utilizing the shorter conducting periods would have somewhat higher average arc drops My own experience indicates that the difference is negligible.

I was interested in the authors' statement that the Ignitron described does not exhibit surging characteristics down to a cooling water temperature of the order of ten degrees centigrade. In general, Ignitrons are much superior to multianode rectifiers from this point of view, because of the short arc length and small amount of shielding. In our experience with pumped-type Ignitrons we have never found any instability in the arc, with suddenly applied loads up to 300 per cent rating at water temperatures as low as six degrees centigrade, the minimum water temperatures available in Pittsburgh in the wintertime.

In Figure 5 the authors indicate double two-phase circuits for the utilization of four tubes. All multiple-group rectifiers require interphase transformers and enough inductance in the load circuit to maintain multiple-group operation. The advantage is a more effective use of the transformer windings and lower maximum currents in the anodes. In the case of six tubes the advantages far outweigh the disadvantages. However, in the case of double two-phase operation, the disadvantages become greater and the advantages less. A straight four-phase transformer is definitely no larger than a double two-phase with interphase. The lowering of anode duty is not so great as in the six-tube case. The operation of a double two-phase rectifier is definitely more critical, and the simplicity of the straight four-phase circuit has much in its favor.

The figure of 100 watts per ignitor for the Rectox-charged capacitor excitation circuit seems low, and I believe that 200 watts would be more nearly correct.

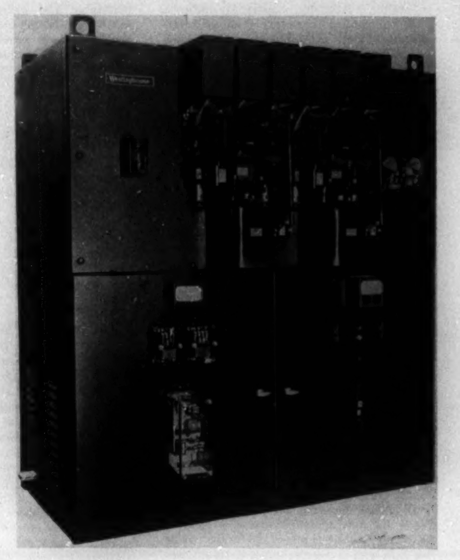

Figure 1. Sealed-off Ignitron assembly

I do not agree with the statement that fuses for the interruption of arc-back currents are satisfactory. We have hardly arrived at the point where arc-backs are so rare that the inconvenience of fuse replacement for every arc-back would be negligible, and in view of the relationship between arc-drop and arc-back frequency, I do not think that such a low arc-back frequency would be desirable. I believe that the simplest and most attractive over-all arrangement for these small rectifiers is the use of disconnecting fuses in the primary circuit of the rectifier transformer and anode breakers in the secondary circuit of the transformer with only a disconnect in the cathode circuit. Such an arrangement provides the most complete protection, since modern air breakers are considerably faster than oil breakers, and by the use of anode breakers maximum protection is provided both for the rectifier and for the associated transformer.

By the use of anode breakers the complete switchgear, with the probable exception of the primary disconnecting fuses, can be mounted in a cubicle with the rectifier and the a-c switchgear unit shown by the authors in their Figure 10 can be eliminated. Figure 1 of this discussion shows a sealed-off Ignitron assembly in which the complete switchgear, with exception of the primary disconnecting fuses, is included.

The authors indicate, and suggest, the use of raw water directly in the Ignitron tubes. In many cases the cooling water available is such that trouble is encountered with scaling and deposits which interfere with water flow. Furthermore, best sealed-off Ignitron-rectifier operation is obtained with a high water rate, and such a rate is not possible with the circuits shown in the authors' Figure 13, unless temperature control is abandoned and water consumption prohibitive. Of course, by the addition of a pump, the raw water can be recirculated in a local path and still retain direct water cooling with high velocity and control.

In many of the authors' statements they give figures of efficiency and losses without mentioning the voltage of the application. I assume that in all these cases they are discussing 250-volt rectifiers.

M. M. Morack and H. C. Steiner: Mr. Marti's experience with the continuously excited single-anode mercury pool rectifier is very interesting. As Mr. Marti points out and as stated in the paper summary, it is the single-anode design with its low density of ionization during the inverse cycle which reduces the shielding necessary to prevent arc-back and thus lowers the arc losses.

In considering the method of cathode-spot excitation, the principle of igniting the spot each cycle offers two advantages:

1. With continuous excitation it is necessary to provide some form of cathode-spot stabilization or, as shown in the cross section of the Excitron tube, to insulate the pool in order to prevent the arc leaving its surface and anchoring on the tube walls.

2. It is felt that phase control of the output voltage is essential in the sealed type of rectifier.

The ignitor provides these functions with a minimum of complication in the design of the tube itself. Control of the ignition impulse serves the dual purpose of ignition and phase control.

The addition of a second ignitor is simply insurance. Actually, experience indicates that the ignitor is a very reliable device. Failure is more often due to abuse or misapplication, or lack of understanding its characteristics than anything else.

The holding anode was added as a result of field experience which indicated that minimum load conditions were likely to be encountered in sets of relatively high current capacity. Its addition did not change appreciably either the arc drop or surge conditions.

The arc-drop data in Figure 3 were determined in a three-phase single-way circuit using a vacuum-tube amplifier and cathode-ray oscillograph. The data are the average of the voltages at the beginning and end of the full-current conducting period. The arc drop is roughly a volt higher at the beginning and a volt lower at the end, when there is an excess of ionization. It would seem, as Mr. Cox suggests, that the arc drop should be higher in circuits with shorter conducting periods. However, our experience indicates that the difference is negligible.

In regard to the circuit, we believe that circuits of the multiple type with interphase are preferable because of the greater transformer utilization and lower tube arc losses. Our experience indicates that the double two-phase circuit is fully as stable as the double three-phase. The control is particularly stable since the average output voltage is the same at light load and 90 degrees phase control (four-phase operation) as at full load with 0 degrees phase control (two-phase operation).

The excitation loss in the capacitor circuit was determined by wattmeter measurement. The measured loss, including an assumed 50 watts in the Thyratron-tube heater circuit was 110 watts. Volt-ampere demand is several times this value.

Whether fuses are satisfactory for arc-back protection depends primarily on the frequency of arc-back. For industrial use we do not believe that frequent arc-backs (one or two per month) are permissible in sets of the capacity considered here. If the rate is one in several years, as field experience is beginning to indicate in certain installations, fuses become practicable. Practice seems to indicate that a-c feeder breakers are usually required for transformer and arc-back protection and that double pole d-c breakers are required to remove short circuits. Anode breakers to provide the arc-back and short-circuit protection functions have proved quite satisfactory in certain installations.

Whether it is preferable to design the rectifier and switch gear as one unit or to combine separate units with the advantage of highly specialized design and manufacture in each would seem to depend primarily on the manufacturing organization.

The efficiency and loss figures were all based on 250-volt rectifiers. This omission has been corrected in the final paper.

Energy Flow in Electric Systems—The *Vi* Energy-Flow Postulate

Discussion and author's closure of paper 42-141 by Joseph Slepian, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 835–41.

C. E. Bennett (Washington, D. C.): Ever since the time of Maxwell, students of electricity and magnetism have been intrigued by the problem of the mechanism of electric-power flow. Doctor Slepian's paper is a further contribution to the literature and presents interesting mathematical speculations without appearing to add anything tangible to our fund of knowledge regarding flow of electric energy or to impugn the validity of established engineering principles.

Doctor Slepian's paper is concerned, in general, with what he refers to as energy-flow "postulates." If the term applies to the mechanism of energy flow, one cannot quarrel with its usage. But if it refers to the flow itself, that is something that is not a "postulate." Whether it be a "phenomenon which can be actually physically observed" is a matter of indifference. Its effects, like the effects of heat, may be observed, and, as long as it may be measured accurately through its effects, the engineer will not worry about its observability. Whatever the nature of electric energy and the mechanism of its flow, the energy transfer itself is as much a reality as the metal conductor by which its flow from generator to load is implemented.

Throughout the paper, it is necessary to distinguish between energy flow as a reality and the mathematical assumptions and conclusions used in visualizing the mechanism of such flow. In his opening statement Doctor Slepian says:

> "Let *V* be the electric potential at any given point referred to some arbitrarily chosen point of zero potential."

Applying that definition to a three-phase circuit, if the neutral wire is assumed at zero potential, the result is the universally accepted one of energy flow in or about the three conductors. But if the point of zero potential be assumed on one of the three line conductors, then it appears from the application of these mathematical conventions that there is no energy flow in (or around, or adjacent to) that conductor (although it gets as hot as the others), and all of the energy flow is in or about the remaining conductors. Changing the hypothesis to assume another conductor at zero potential changes the apparent energy flow in that conductor to zero and seems to cause energy to flow in or about the conductor that previously carried nothing. Yet every engineer knows that grounding one conductor or another of an ungrounded three-phase system does not alter the energy flow. The writer of this discussion does not deny the occasional usefulness of such concepts, but he wishes to caution against confusing the consequences of mathematical assumptions with physical realities.

For practical reasons, engineers measure power flow by methods related to the totality *VI* product rather than by the summation of point functions discussed by Doctor Slepian. There does not appear to be anything in the paper to raise question as to the propriety of such methods, nor does the present commentator know of any meter or other device capable of integrating the point functions of Slepian and Poynting.

It should be noted that the voltages and currents associated with the conductors needed to interconnect generators and loads are determined by terminal conditions of the network and by the constants of the individual circuit elements. In consequence, the amounts of energy transmitted, lost, and stored or received, at any instant in association with any circuit element are fixed by the indicated parameters. Established engineering principles reveal the direction and magnitude of power flow through these elements but not the mechanism. The point-function "postulates" discussed by Doctor Slepian merely attempt to provide a mathematical picture of the mechanism of such flow. So far as concerns his use and control of electric energy, the engineer does not care whether the power flow takes place in the ether surrounding the conductors or in the conductor through the interatomic space by electron displacement, by a flow of electrons, by rotation of atomic dipoles, or by other means.

Against such a background some electrical engineers have read with surprise the eighth paragraph of the "Introduction" to Doctor Slepian's paper in which he says:

> ". . . . . the conditions just given are not sufficient for uniquely determining an energy flow . . . . which generation point is to feed which consumption point, and by what route, is completely undetermined so far as concerns any phenomenon which can be actually physically observed. Infinitely many postulates may be devised which will all be equally valid and equally well established by the conditions which have been described, and which are the only conditions which are available for defining an energy flow."

Doctor Slepian chooses not to elaborate on this point but allows it to appear as an incidental corollary, the relevancy of which is not disclosed in the paper. But Mr. Wagner, who presented Doctor Slepian's paper in the author's absence, and one of the engineers who took part in the discussion indicated that current interest in the mechanism of flow of electric energy has been aroused among engineers by its having been injected into certain regulatory Federal Power Commission cases,* in which decision turns upon the tracing of energy flows in a network. Doctor Slepian appeared as an expert witness in these cases and presented testimony for some 22 days regarding the flow of energy according to various energy-flow "postulates."

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* It seems likely that the cases referred to (one of which is still pending before the commission) were Federal Power Commission dockets *IT*-5,563 and *IT*-5,565.

The full implications of the quoted statement become clearer from a consideration of Doctor Slepian's sworn testimony in the above cases. The following statements are noteworthy:

> "There is no location in the geographic sense to the energy *per se*. You may calculate a certain energy by means of certain measurements made of objects having certain locations in space, but that does not mean necessarily that the energy resides there." (Slepian, FPC docket *IT*–5,563, transcript page 3,011, line 10.)
>
> "The best location I can find for the energy, if I must have one, is in the mind of the calculator, strange as that may sound, if you must have a location." (Slepian, FPC docket *IT*–5,563, transcript page 3,012, line 10.)

The Federal Power Commission summarized Doctor Slepian's testimony regarding application of his views as to energy flow in a formal opinion (FPC docket *IT*-5,665, opinion 75, page 27), as follows:

> "Such a method, *it was admitted* [italics by the discusser], would permit allocating to a generator in Hartford, Conn., the supply of a load in Shanghai, China, and to the Shanghai generator the supply of the load in Hartford, if such equality were observed, notwithstanding the fact that the Hartford generator appears to be electrically connected to the load in Hartford and the Shanghai generator to the load in Shanghai, while there appears to be no electrical connection between Shanghai and Hartford."

This is interesting as mathematical speculation, but, if considered as applicable to the physical world in which power companies operate, a theory of energy flow leading to such results would raise question as to a utility's legal right to collect money for electric energy used by its customers.

A certain difficulty of understanding Doctor Slepian's paper is due to the notation and may be clarified by an examination of section V, "Example of Failure of Simple *VI* Postulate." Discussing the case of a varying current flowing in a conductor having inductance but no resistance, he says:

> "Hence if the simple *VI* postulate is valid, there should be appearing in the section an increasing amount of some form of energy, such as heat, for example. But no such energy appears there! The simple *VI* postulate fails!"

The explanation—his assumptions make it fail! By hypothesis, *V* is a scalar quantity determined by the location of point charges. It therefore excludes potential caused by induction. Doctor Slepian is simply pointing out that the *Vi* product excludes energy stored by induction. The "*V*" and "*i*" as used in his paper are not, therefore, to be confused with the corresponding Poynting vector quantities nor with the instantaneous values of potential difference and current which the engineer uses in the ordinary differential equations of electric circuits. Statements regarding the inexactness of the simple "*VI* postulate" should not, therefore, be interpreted as proving or disproving the exactness of the differential equations of electric circuits ordinarily used by electrical engineers.

S. W. Roland (Arlington, Va.) and **J. J. Jessel** (Falls Church, Va.): Doctor Slepian states that "the conditions which a valid postulated electric energy flow must satisfy . . . are insufficient for its unique determination. An analysis of this statement first requires that the term "postulated electric-energy flow" be defined.

The author obviously makes no distinction between measured and postulated electric-energy flows; that is, between energy flows that are continuously being measured and recorded at numerous points throughout an electric-power system and postulated flows which may be arbitrarily assumed for analytical purposes to exist at any point in space. He takes no cognizance of the fact that measured energy flows on the transmission and distribution circuits of a power system arise from consumer demands for electric power and that postulated flows, wholly independent of those demands, arise only when and if they are introduced by the analyst.

If one grants this distinction between measured and postulated electric-energy flows, it is evident that the question of uniquely determining energy flows on a power system concerns itself only with measured energy flows, particularly when such determinations are made in connection with legal proceedings. The term "energy flow" is a convenient expression used ordinarily to denote the transfer, transmission or conveyance of electric energy by electric currents.

The second question involved in the aforementioned statement by Doctor Slepian then becomes: "Can a unique determination be made of the measured electric-energy flows on a power system?" Before proceeding with an analysis of this question, it should be pointed out that it is common practice for electrical engineers to prepare diagrams showing both the magnitude and direction of power flows on the circuits of electric-power systems.

The 1942 edition of the "American Standard Definitions of Electrical Terms" includes the following definitions:

> *A-c transmission* is the transfer of electric energy by alternating current from its source to one or more main receiving stations for subsequent distribution.
>
> *Radial feeder* is a feeder supplying electric energy to a substation or feeding point which receives energy by no other means. (The normal flow of energy in such a feeder is in one direction only.)
>
> *Interconnection tie* is a feeder interconnecting two electric supply systems. (The normal flow of energy in such a feeder may be in either direction.)
>
> *Power* is the time rate of transferring or transforming energy.
>
> *Wattmeter* is an instrument for measuring electric power.

The above definitions indicate clearly that the Sectional Committee on Definitions of Electrical Terms under the sponsorship of the AIEE, "representing some 33 organizations, including national engineering, scientific and professional societies, trade associations, government departments, and miscellaneous groups," recognizes that

> 1. Electric energy is transferred by electric currents from its source to points of consumption.
>
> 2. At any instant electric energy is transferred in a single ascertainable direction.
>
> 3. The time rate of such transfers can be measured by means of wattmeters.

Whether or not the afore-mentioned definitions are considered by the "trained mathematician" to be naive, they do, nevertheless, in the words of their authors, "express for each term the meaning which is generally associated with it in electrical-engineering work in this country." It cannot be truthfully said that the authors of these definitions are "lay engineers."

The ascertainment of the magnitude and direction of energy flow on a circuit of a power system by means of appropriate instruments thus constitutes the unique determination of energy flow for that particular circuit. In many cases this information is sufficient to enable the engineer to make the unique determination of the energy flows from the various generating stations to the various loads on the system. In other cases, however, a knowledge of the magnitude and direction of energy flow in each circuit of a network may not suffice to enable the engineer to establish uniquely a continuity of individual flows from one circuit to another from a particular source to some specified point on the system. To illustrate, assume that each of several transmission circuits delivers energy from a separate source to one end of a substation bus; also, that several feeder circuits connected at the other end of the bus transmit energy to a number of distribution points. From which particular source or sources is each distribution center supplied? This question is a particularly sterile one to the engineer; yet it has been propounded to a number of engineers in legal proceedings involving the transmission of electric energy in interstate commerce. It appears that a solution to this question appropriate to jurisdictional problems involving the transmission of electric energy in interstate commerce will ultimately be forthcoming from administrative bodies and from courts. Thenceforth the engineer can guide himself accordingly in treating with problems of this type.

W. A. Lewis (Cornell University, Ithaca, N. Y.): I agree with Doctor Slepian in his derivation and his conclusions, as presented in this paper. However, from the standpoint of a reader whose background, in comparison with Doctor Slepian's, is somewhat more that of an engineer, and considerably less that of a physicist, I would express his conclusions in a different form. Doctor Slepian states that the *VI* postulate, as defined in the paper, is not generally valid when applied to any arbitrarily chosen volume in space, unless the additional term derived by him is included. I would prefer to say that the *VI* postulate, without the correction term, is valid when applied to a properly chosen volume, the properly chosen volume being one for which, at every point of the bounding surface, either the E_2 component of the electric intensity, as defined by Doctor Slepian, or the magnetic intensity H, is zero or perpendicular to the surface. With this restriction, the contribution to the energy-flow normal to the surface provided by the correction term of Doctor Slepian becomes zero, so that the integral obtained by the uncorrected *VI* postulate gives the entire energy flow through the bounding surface and therefore is completely valid as applied to this surface. This is true for the following reasons. If E_2 or H is zero, the correction term becomes zero. If either E_2 or H is perpendicular to the bounding surface, the energy flow, proportional to the vector product of E_2 and H, must be perpendicular to both E_2 and H and will therefore be tangent to the plane of the bounding surface. If the energy flow at the surface is zero, or if the direction of the energy flow is tangent to the bounding surface, the integral over the surface becomes zero, and the total correction term is therefore zero.

If this concept is applied to the examples cited by Doctor Slepian, it will be found that the volumes selected in the examples conform to the restrictions just given, whenever the unmodified *VI* postulate is valid, and do not conform whenever Doctor Slepian finds it to be invalid. The electrical engineer, in treating energy flow, generally obtains correct results because the bounding surface conforms to the restrictions imposed even though the engineer may not have been entirely conscious of that fact in planning the connections of his meters.

With these facts in mind we may say that Doctor Slepian has shown how the *VI* postulate may be corrected whenever the volume over which we wish to apply it does not conform to the stated restrictions.

The idea that the bounding surface involved in a problem must be specially chosen is not confined to the uncorrected *VI* postulate. For example, the Maxwell scalar potential given by equation (1) of the paper is obtained by integrating over all spaces, as stated by Doctor Slepian. As a matter of fact, the integral may be correctly obtained if it is extended only over all charges which are sufficiently close to the point in question or sufficiently large to provide a contribution to the potential integral. Any surface so selected that it includes all pertinent charges will enclose a suitable specially selected volume over which the integration may be carried out.

One additional caution must be exercised in the practical application of the *VI* postulate, in the unmodified form. Since V (of the *VI*) is actually V_m the Maxwell scalar potential, referred to a single common reference, care must be exercised to insure that the potential used, and impressed on the wattmeter potential coils, actually corresponds to V_m. As V_m at any point is obtained by taking the line integral of E_1 from the arbitrary potential reference point to the point in question, care must be used to select the reference point so that E_2 makes no contribution to the potential, or that it makes the same contribution to the potential at every point of the bounding surface where the current density is not zero, so that its effect will cancel when the complete integral is formed. The last follows because the total current flow normal to the surface of any closed volume is zero.

If the bounding surface is so selected that E_2 is zero or perpendicular to the surface at every point, the point of reference potential may be any point in the surface, and the paths of integration from the reference point to points where the several conductors cross the bounding surface may follow any path lying in the surface. Further care must be exercised, however, if some portion of the bounding surface has been selected perpendicular to H instead of E_2, since E_2 may then have a component tangent to the surface which might then contribute to the V_m integral if the path of integration passes through such a portion of the surface.

Myron Zucker (The Detroit Edison Company, Detroit, Mich.): This paper is important in its implications as to power-system interconnections, including questions of where the power generated in one station is consumed either through conductive transfers or by wireless methods.

It is unfortunate that the conventional analysis of this problem is treated so condescendingly in the paper. While it is true that the "*Vi*" postulate is tenable only under certain boundary conditions, these conditions are perfectly reasonable and are understood by the engineer in solving his problems. They are the same type of restrictions that must be set up in any scientific analysis and are similar to some of the assumptions that the author has had tacitly to make in his more general deductions.

The impression of the argument is also not improved by the neglect of resistance loss in the second example, when it provided the entire "sink" for energy in the case of discharging the sphere. Everybody knows that resistance loss accounts for an appreciable portion of the energy in the case of flow along wires, and it would seem as easy—and much more convincing—to admit that this occurs but to point out that it could not begin to satisfy the energy input under the conditions laid down by the author. To the layman (speaking from the mathematician's heights) who would have occasion to follow the analysis, the case would be much stronger for this modification.

At any rate, the paper shows definitely that there is more to the story of energy flow than simply adding together a few wattmeter readings.

Joseph Slepian: I wish to thank the three discussers from Washington for their remarks, which so well bring out and emphasize the major points of my paper: namely, that there are infinitely many, equally valid, energy-flow postulates and that the particular one frequently used by power engineers gives correct results as usually applied, for usual engineering purposes on power systems, but needs a correcting term to have the universal validity of the other widely used postulate, namely, the Poynting vector, and the many other valid postulates which may be and have been devised.

As Mr. Bennett brings out, electrical quantities are observed, defined, and even measured by the effects which are associated with them. In the case of electric-energy flow, these effects are the appearance of electric energy at generating points and the disappearance of electric energy at consumption points. All energy-flow postulates which are valid associate properly with and may be said to produce these observed effects. In this sense, all the various valid energy flows are equally well observed.

In his third paragraph, Mr. Bennett presents the interesting family of valid energy-flow postulates given by $P_3=(V+a)I$, where a is an arbitrary constant. The convention as to the zero of potential most generally accepted by power engineers is to take the earth as having zero potential. With this convention, the *VI* postulate makes the energy flow in any conductor proportional to its voltage relative to earth. If one conductor of a normally ungrounded three-phase system is grounded, the energy flow in that conductor according to the *VI* postulate is zero, and the reading of a wattmeter with potential coil connected to that line and ground is also zero. With a balanced load, the wattmeters in the other two lines show different readings, and one may even read in the opposite direction to the other. Nevertheless, the engineer will add up these readings, knowing that they will serve properly for the usual proper purposes for which valid energy-flow postulates are applied by power engineers.

Now Mr. Bennett points out that we may equally well take the neutral point of the system as the zero of potential. This will shift all potentials of the system by a constant a. All wattmeters will now give different readings and indicate different postulated energy flows in the individual lines. Nevertheless, as Mr. Bennett says, these different energy flows lead to the same results for usual proper purposes. This is because $P_3=(V+a)I$ is a valid energy-flow postulate. As Mr. Bennett says so well in his fourth paragraph, the engineer is indifferent as to the detailed individual energy flows asserted by the particular postulate he uses, so long as it meets his needs in "his use and control of electric energy."

I am very glad that Mr. Bennett referred to the Federal Power Commission hearings at which I served as an expert witness. The transcripts of these hearings make very interesting reading and may be obtained from Elecreporter, Inc., Washington, D. C. My own testimony in *IT*–5,563 runs from page 1,458 to 1,669 and from page 2,264 to 3,890. In *IT*–5,565 my testimony runs from page 2,102 to 2,242.

At the hearing (in *IT*–5,563), serving as expert witnesses and expressing opinions essentially similar to my own, in so far as they were permitted by the extraordinary rulings of the examiner, were R. S. Shankland, head of the physics department at the Case School of Applied Science; W. A. Lewis, head of the electrical engineering department at Cornell University; R. W. Sorensen, head of the electrical engineering department at California Institute of Technology, and past president of this Institute; V. Karapetoff, emeritus professor of electrical engineering at Cornell University. Also ready to testify, but not permitted by ruling of the examiner, was J. Stratton, professor of physics at Massachusetts Institute of Technology.

These hearings were carried on professedly following the procedure of a law court. The aim and purpose of the cross-examining attorneys was not the discovery and elucidation of the truth but quite definitely and solely the winning of their side of the case. Hence the questions put were designed as far as possible to elicit replies which might sound bizarre and strange to technically less expert people. Apparently the best of such replies which Mr. Bennett could find are given in his seventh paragraph. These quotations from my testimony are correct and are my well-considered opinion now. However, to understand them properly, they should be read in the context of the hearings at which they were given and particularly in relation to the questions to which they were answers. Space does not permit me to supply this context here, and the reader is referred to the available transcripts of the hearings.

The Federal Power Commission's opinion, given in the eighth paragraph of Mr. Bennett's discussion, shows a complete misunderstanding of my testimony at Hartford, Conn., and misquotes it by the omission of a very important qualifying word.

Also, in this eighth paragraph, Mr. Bennett implies that the legal right of a utility to collect money for electric energy used by its customers somehow depended on the validity or invalidity of particular energy-flow postulates, presumably because some energy-flow postulate might show this energy as not deriving from some generator belonging to the utility. This is absurd. I say that my news dealer supplies me with my newspaper and pay him for it without thereby implying that he himself publishes it or creates it. In the same way I say that my light company supplies me with electric energy and pay my monthly bill, without implying or needing to be shown that the energy I consume does actually and recognizably come from some particular generator owned by my light company. That is a matter of indifference to me and, I think, has been made sufficiently clear in the last example of my paper.

In Mr. Bennett's last paragraph he implies that the potential V which I used is not the potential used by electrical engineers. This is not true. The potential V is the reading which a voltmeter would give if connected from a point in the line to the ground directly below. VI is the reading which an instantaneous wattmeter would give, if connected into and to the line at any point. The example given is one in which the assumption that electric energy flows only in conductors and is given by wattmeter readings there fails. The correcting term, which I have described in my paper and which gives a zero contribution in usual applications of electrical engineers, cannot be neglected in this example.

Roland and Jessel, who like Mr. Bennett are Federal Power Commission employees, try to distinguish between "postulated" and "measured" energy flows on a power system. By the "measured" energy flow in a line, I presume they mean the reading of a wattmeter connected to and into the line in question.

Now a wattmeter is an instrument with two pairs of terminals connecting respectively to two coils and so designed that its moving element is deflected by an amount which is a function of the average value of the product of the voltage applied to the one pair of terminals and the current flowing into the one and out the other of the other pair of terminals. If the first pair of terminals is connected, one to the line and the other to ground, and, if the second pair of terminals is connected in series relation into the line, then the wattmeter will read the average value of the product of the voltage of the line and the current in the line. About this there is no question. The wattmeter will read and thus measure the average value of VI for the line. But now when we assert that this average value of VI measured by the wattmeter is an electric-energy flow in the line, we make a postulate, which can only be justified, and that not uniquely, by showing that it does fulfill the only observable requirements, properties, or effects of ~~a~~ valid energy-flow postulate, namely that it does carry away from sources the proper amount of energy and bring to loads the proper amount of energy.

Now, subject to the correction developed in my paper, the VI postulate is a valid one, and wattmeters, properly connected to read VI, do measure energy flow according to the VI postulate. But Mr. Bennett has pointed out that wattmeters may also be connected to read $(V+a)I$, where $-a$ is the potential of the neutral of a three-phase system to ground. Asserting the existence of an energy flow in lines given by $(V+a)I$ is also a valid energy-flow postulate (again subject to the correction of my paper), and hence we may say that wattmeters so connected measure the energy flow according to the $(V+a)I$ energy-flow postulate.

In general, for any energy-flow postulate we may devise an instrument for measuring the quantities entering into the postulate, making the appropriate calculations with them, and exhibiting the result. Such an instrument may be said to measure the energy flow according to that energy-flow postulate. In general, for an arbitrary energy-flow postulate, the instrument will be much more complicated than the wattmeter. This, however, does not affect the principles involved in the question. The distinction which Roland and Jessel attempt to make between postulated and measured energy flows is without meaning.

In the last sentences of their discussion, Roland and Jessel reveal their understanding of the lack of physical meaning which is to be attached to questions to which different but equally valid energy-flow postulates give different answers. After describing a not particularly complicated system of several generators and several loads connected together, they ask, "From which particular source or sources is each distribution center supplied?" They anwer, "This question is a particularly sterile one to the engineer." Of course it is sterile. There is no one answer. Various energy-flow postulates, all equally valid, will give different mutually contradicting answers. They suggest that the answer will be forthcoming from administrative bodies and from courts. It is our duty as members of the engineering profession to ensure as far as possible that these bodies and courts will be technically well advised.

A New Multiple High-Speed Air Circuit Breaker for Mercury-Arc-Rectifier Anode Circuits and Its Relation to the Arc-Back Problem

Discussion and author's closure of paper 42-139 by J. W. Seaman and L. W. Morton, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, November section, pages 788–96.

Otto Jensen (I-T-E Circuit Breaker Company, Philadelphia, Pa.): The authors have made a very thorough study of and have described in minute detail the conditions which exist in rectifier installations during backfires. Their conclusions regarding the advantages obtained by using anode circuit-breaker protection are clear and obvious. The initial effort to build anode circuit breakers for rectifier protection has developed to such an extent that there are now, installed and in successful service, 188 large rectifier units protected by I-T-E breakers; 148 more units are being installed, and 175 additional breakers are in production. We then have a total of 511 units of anode circuit breakers protecting rectifiers made by all of the three manufacturers. This means that there are 1,128 poles in operation, 888 poles being installed, and 1,050 poles now in production, a total of 3,066 poles. With these figures in mind we must realize that the anode-breaker protection scheme is not new.

We note in Figure 4 of the paper that the customary high-voltage oil circuit breakers for the protection of the rectifier transformer are still advocated, and, from paragraph 3, in the conclusion, that the maintenance of the a-c power circuit breaker is reduced, because the circuit breaker is no longer required to do any work.

Our opinion may be a little biased, but we do not see any reason for installing a piece of electric equipment which has no duty to perform, particularly in times like these when our manufacturing facilities could certainly be better utilized if they were devoted to the manufacture of necessary equipment. We can however, understand the fact that, if an oil circuit breaker has no duty to perform, it is not a too undesirable piece of equipment.

We note from Figure 10 of the paper that the initial rate of rise of anode circuit 1 is approximately 6,300,000 amperes per second, and the initial rate of rise for anode 5 is 9,000,000 amperes per second. We assume that the steeper rate of rise for anode 5 is due to the d-c saturation of the transformer core caused by backfiring current of anode 1.

We do not understand the quarter-cycle delay in the reversal of the cathode current. If this is a typical condition, it further emphasizes the undesirability of cathode switching, because there is evidently a quarter-cycle delay before the cathode breaker would "know" that a backfire had occurred, and consequently the opening of the cathode breaker has been unnecessarily delayed.

Evidently, any number of backfires could have occurred up to the time that the medium-speed cathode breaker opened the circuit, and evidently the successive backfires are increasingly severe. We therefore, raise the question: Would it not be highly desirable to use a high-speed rather than a medium-speed cathode breaker in order to cut down the time interval in which sympathetic backfires could occur?

We notice the statement that the artificial backfires are more severe than the natural backfires because of the limiting effect of the arc drop in the rectifier element. This statement does not seem to be substantiated by the oscillographic record in Figure 10 of the paper, because the natural backfire in anode 5 is more severe than the artificial backfire in anode 1 both as to the amount of current and the elapsed time.

Great stress has been laid upon the design feature of the circuit breaker which prevents it from opening on any amount of forward current. We see no particular advantage in

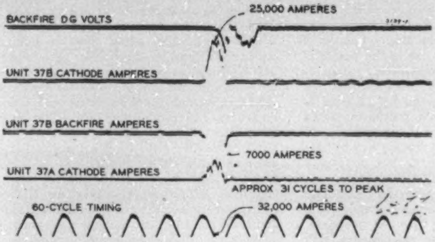

Figure 1. Backfire on rectifier 37-*B*

this. We believe a safer installation would have resulted had the breaker been deliberately designed so that it would not open up under the amount of forward current supplied by the anodes in the companion tank during backfire but would open up under forward anode current produced by the healthy anodes in case of a direct short circuit of one rectifier.

I-T-E oscillograph 7699 might better illustrate this point. A backfire was made on rectifier 37-*B*, and rectifier 37-*A* is the only other source which could feed the fault. Under this condition, the forward current which can be furnished by any other rectifier on the bus becomes the greatest obtainable from any one rectifier anode. Rectifier 37-*A* anode breaker did not trip, and this rectifier, therefore, continued to feed the d-c bus. If a short circuit had been applied to the bus, we know from a-c tests of our breaker that it would have tripped if any of the anodes had contributed more than 30,000 amperes to the fault, thereby furnishing high-speed short-dircuit protection.

We cannot agree with the statement on the basic requirements that the breaker would be fully acceptable if it limited the fault current in one half-cycle. We feel that a quarter-cycle circuit breaker is more desirable than a half-cycle circuit breaker and that a tenth-cycle circuit breaker is certainly more desirable than a quarter-cycle breaker. Our oscillogram 11, published in AIEE paper 42-38 entitled "A Fast Circuit Breaker,"[1] shows the comparison between a half-cycle breaker and a quarter-cycle breaker.

Inasmuch as all the rectifier and circuit-breaker manufacturers now agree that the anode-breaker protection scheme is the most desirable, and since this scheme is based on ultrahigh speed operation, why isn't it good engineering to use the fastest circuit breaker known in the art?

REFERENCE

1. A FAST CIRCUIT BREAKER, **D. I. Bohn, Otto Jensen.** AIEE TRANSACTIONS, volume 61, 1942, March section, pages 165–8.

H. Winograd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors have presented a lucid exposition on the arc-back problem in rectifiers and the requirements for protection. Additional comments on this subject will be made here.

The arc-back tendency of a rectifier is related to the current and voltage rating and circuit constants by two basic factors:

1. The residual ionization in the vicinity of the anode at the conclusion of its firing period.

2. The magnitude of the negative voltage appearing between the anode and cathode at that instant. This may be termed the recovery voltage (see Figure 2 of this discussion).

At the completion of the firing period, the residual ions are drawn toward the anode surface by the negative voltage gradient, causing the flow of a small "inverse" current. The magnitude of this current depends on the density of ionization and the value of the recovery voltage. If the inverse current density at any point on the anode surface becomes sufficiently high, a cathode spot and arc discharge may be initiated. This is one of the generally accepted theories on the causes of arc-backs. Arc-backs resulting from this cause would be most likely to occur at the end of the firing period of the anode. Investigation made in one rectifier installation indicated that most arc-backs do occur at that part of the cycle. The random nature of such arc-backs is probably related to the distribution of the forward current at the surface of the anode, which would affect the density of residual ionization.

It is obvious that the higher the load current and operating voltage, the greater would be the inverse current of a rectifier and its tendency to arc-backs.

This might also offer an explanation of the effect of circuit constants such as the transformer and a-c system reactances on the tendency to arc-backs, mentioned by the authors. The commutating reactance determines the length of the commutating period for the anodes. The higher the commutating reactance, the larger is the commutating period, and the more gradual is the decay of the anode current. This provides more time for deionization, and the density of residual ionization is thereby reduced. On the other hand, the longer commutation period results in a higher recovery voltage at the end of the firing period. The two effects are opposite in their relation to arc-backs. The net effect would probably depend on the speed of deionization while the current is declining, that is, on the design of the rectifier.

It is well known that, the more the firing of the anodes is retarded, by grid or firing control in order to reduce the d-c voltage of a rectifier, the greater is the tendency to arc-backs. This also can be explained on the basis of residual ionization and recovery voltage.

The greater the retardation of the firing period, the higher is the recovery voltage, and the shorter the commutating period, which would increase the residual ionization. The inverse current would therefore be increased by both factors.

Another point of interest in connection with arc-backs, not mentioned by the authors, is the effect of the point in the cycle at which an arc-back occurs on the rate of rise of the current to the faulty anode and the value of current reached in one-half cycle. The highest value is obtained if the arc-back starts at the completion of the forward firing period. It becomes progressively smaller as the arc-back point is moved back along the reverse voltage wave. Beyond a certain point, depending on the circuit constants, the current would drop to zero, and the anode could fire in the forward direction. Arc-backs occurring during that part of the cycle might extinguish themselves without tripping the breaker. These are called "silent" arc-backs.

It would be of interest to add a historical note relating to the introduction and development of the high-speed anode breakers. Credit for this development in the protection of rectifiers is due to D. I. Bohn, electrical engineer of the Aluminum Company of America. In 1938, the first rectifiers used in this country for production of aluminum were put in operation at the Alcoa and Massena plants of the Aluminum Company. These had the conventional protective system—a-c breakers on the primary side of the transformers and high-speed cathode breakers.

In 1939, when the Aluminum Company decided to order rectifier equipment for another pot line at Alcoa, Mr. Bohn (being unfettered by tradition) proposed using anode breakers, in order to eliminate oil circuit breakers for the individual rectifier units. As engineer representing the manufacturer of the rectifiers, the writer readily agreed, since the advantages of the protection of the rectifier equipment were obvious.

As there was no high-speed breaker of this type available, Mr. Bohn proceeded to develop one himself. The writer had the pleasure of assisting him on the tests of this breaker. The I-T-E Circuit Breaker Company co-operated in this development. The rectifier equipment with the anode breakers was placed in operation in the spring of 1940.

This installation included other features relating to the design of transformers, protection, switching, layout, and cooling, which set the pattern for many subsequent rectifier installations in the electrochemical industries, as described in some of the other papers presented at the summer convention of the AIEE.

This is a tribute to the good judgment and foresight of an engineer in an important industry, and it again points to the benefits derived from close co-operation and exchange of ideas between engineers of operating and manufacturing companies.

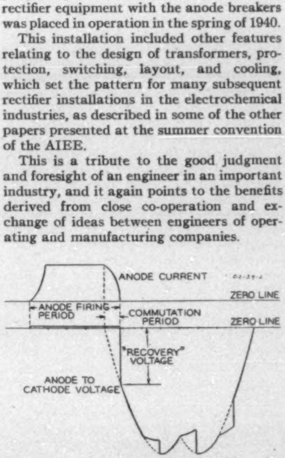

Figure 2

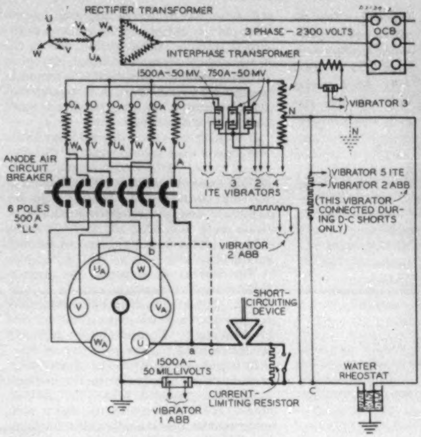
RECTIFIER TRANSFORMER
U
V_A
W_A
W
V
U_A
3 PHASE – 2300 VOLTS
OCB
INTERPHASE TRANSFORMER
1500A–50 MV
750A–50 MV
VIBRATOR 3
N
O_A
O
W_A
V
U_A
W
V_A
U
A
ANODE AIR CIRCUIT BREAKER
1 3 2 4
ITE VIBRATORS
VIBRATOR 5 ITE
VIBRATOR 2 ABB
(THIS VIBRATOR CONNECTED DURING D-C SHORTS ONLY)
6 POLES 500 A "LL"
VIBRATOR 2 ABB
b
SHORT-CIRCUITING DEVICE
a
c
1500A – 50 MILLIVOLTS
CURRENT-LIMITING RESISTOR
WATER RHEOSTAT
C
VIBRATOR 1 ABB

Figure 3. Short-circuit test on I-T-E six-pole type-*LL* air breaker together with *B*-46 rectifier 625 volts, 1,360 amperes, 850 kw

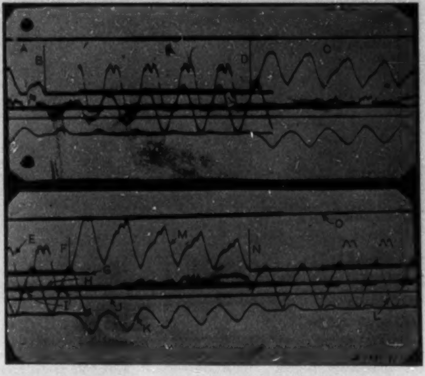

Figure 4 (above). Test of anode air breaker I-T-E type *LL*

One anode short-circuited to cathode

A—Current
B—End
C—Duration of short circuit
D—Start
E—Voltage before short circuit
F—Start of short circuit
G—Current zero
H—Voltage calibration 112 volts
I—Voltage zero
J—Primary-current calibration 5,000 amperes
K—Current in transformer primary winding
L—Voltage after short circuit
M—Current during short circuit
N—End of short circuit
O—Current calibration 15,000 amperes

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of this paper deserve a great deal of credit for having submitted an excellent outline and valuable data illustrating the arc-back phenomenon, as well as interesting design material on a new anode breaker. During the last few years considerable attention has been given to improving means for anode switching in order to minimize the effect of backfires. During the last winter convention of the AIEE a paper was presented by D. I. Bohn and Otto Jensen referring to the development of "A Fast Circuit Breaker," which has been used successfully in connection with many rectifiers.

It is felt that the following notes concerning the earlier pioneering history of anode-breaker development may also be of interest to many engineers.

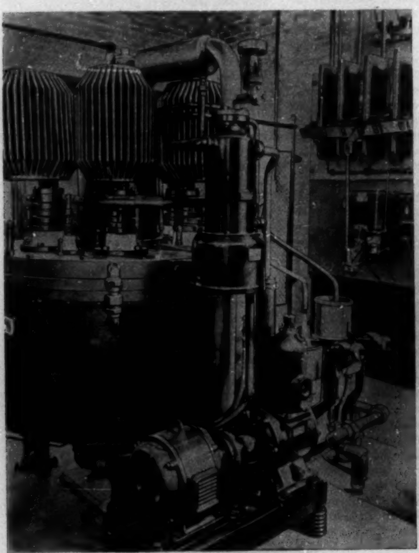

Figure 5. 850-kw 625-volt automatic rectifier unit

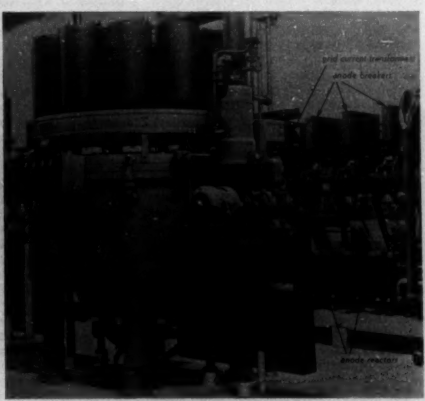
grid current transformers
anode breakers
anode reactors

Figure 6 (below). Grid-controlled mercury-arc rectifiers rated 2,750 kw, 600 volts, direct current each, in electrolytic metal reduction plant

Anode breakers and anode reactors in background

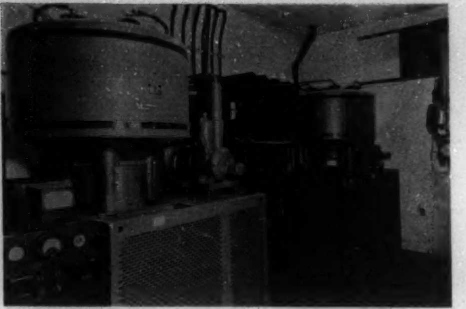

Figure 7. Two direct air-cooled multianode rectifiers, each rated 300 kw, 600 volts direct current with common six-pole anode breaker

In 1931, we were asked to prepare data in connection with the modernization of an electric-railway system, using mercury-arc rectifiers of medium capacity. During the early stage of this investigation it was found that the cost of the a-c switching equipment was quite out of line compared to the cost of the total equipment, because of the high voltage of the power circuit from which the rectifiers would have to be fed. The study showed that a very economical and satisfactory installation fully protecting the rectifiers could be obtained by using six single-pole d-c breakers for the anode leads and replacing the relatively costly a-c line breakers.

To determine the feasibility of such a scheme, the necessary equipment was set up in the factory in order to test a six-pole breaker made up from standard d-c breaker elements. The testing equipment consisted of a 3,700-kva rectifier transformer with interphase transformers, a six-anode rectifier, a short-circuiting device, and a load rheostat, and so forth (see Figure 3). During these tests the six-pole breaker was first closed and the rectifier brought up to a load of about 350 kw on a water rheostat. Then several short circuits were applied, first between one anode and cathode and second between cathode and the transformer neutral. In all of these tests the breaker interrupted in less than six cycles (see Figure 4), and the contacts showed practically no signs of pitting or burning so that the breakers could be shipped without changing the contacts. One of the installations equipped with such anode breakers and without an a-c line breaker is shown in Figure 5. Excellent service was obtained, and, considering that this breaker was not designed for such an application, the results were very encouraging.

It was not until 1939 that an occasion arose to use this scheme in a large plant. However, for this application, a specially designed anode breaker was employed.

Figure 6 shows the first installation of these anode breakers, described by Bohn and Jensen, in a plant with a number of rectifiers connected in parallel. The breakers in this installation are called on to interrupt not only the regular backfire current, but also the current component fed from the rectifiers connected in parallel. In the above case the installation consisted of 12 4,500-ampere units.

Figure 7 shows one of two recently installed substations of a 600-kw air-cooled rectifier unit which has as its only protection a six-pole anode breaker. This application of anode switching has also given excellent service for over a year.

J. W. Seaman and **L. W. Morton:** The authors are pleased to note the unanimity of agreement, that advantages obtained by using high-speed anode switching are clear and obvious. It was especially the purpose of part I, "Analysis of the Arc-Back Problem" to make a record of the reasons for these advantages. As Mr. Jensen states, high-speed anode-breaker protection is not new, but it is, however, the latest method for protection from arc-back, and, in the authors' opinion, superior to several other methods.

Use of anode switching to protect from arc-back is by no means a substitute for adequate a-c power-circuit-breaker protection of the rectifier transformer from internal faults or short circuits. Incidentally, the authors did not specify oil circuit breakers, preferring to use the broader description, "a-c power circuit breaker." Wide engineering acceptance is given to the practice of protecting any transformer of substantial rating (and many rectifier transformers are as large as 7,000 kva), by means of its own a-c power circuit breaker. Such a power breaker does have a duty to perform, and that is to protect its associated transformer from extensive damage caused by internal faults and to assure continuity of production by clearing such faults from the system instantly. It is quite conceivable that with less than adequate a-c power-circuit-breaker protection faults other than arc-back might produce damage to apparatus and loss of war production, far out of proportion to any that could be made by omitting a-c switchgear.

It is likely that transformer saturation did contribute to the higher rate of rise for the anode involved in the natural arc-back in Figure 10 of the paper. In addition, the inductance of the short-circuiting cable in circuit 1 may have had some effect. A further contributing cause may have been the exact time that the short-circuiting breaker contacts happened to close. It is believed that the contacts closed slightly after the end of conduction in circuit 1, during the test illustrated by Figure 10. A slight difference in time, even though not detectable on the oscillogram, may reduce the rate of rise and the ultimate short-circuit current. Other oscillograms in the same series of tests show higher current in circuit 1 than in other anodes involved in natural arc-back, and it is suspected that the main reason was that the short-circuit contacts closed earlier.

The statement that artificial arc-backs are more severe than natural ones and therefore err on the safe side, should be qualified by adding that the reactance and resistance in the short-circuiting cables must be kept to a minimum, and the comparison should be made using tests when the short-circuiting contacts closed before the end of forward conduction.

The authors have noted the quarter-cycle delay in reversal in cathode current in tests illustrated by Figure 10 and in other tests also. No convincing explanation has been prepared. The delays were probably caused by some obscure inductive effect.

It is true that any number of arc-backs, even up to all six anodes in the rectifier, could have occurred before the cathode breaker opened. It is also true that a high-speed cathode breaker rather than medium-speed breaker offers the maximum in protection from sympathetic arc-backs. However, actual experience proves that this is unnecessary. Seldom, with high-speed anode switching, do anodes in the other wye become involved in the arc-back. Furthermore, as is shown in Figure 10, the maximum reverse current from other rectifiers on the same bus is periodically reduced by the opening of the anode-breaker switches whose anodes are in arc-back. Finally, this limited reverse current is usually supplied and shared by all the other rectifiers so that the duty imposed on any one rectifier is far lower than its momentary overload capacity.

The feature of this breaker which prevents it from opening on any amount of forward current makes the breaker applicable to a wider variety of installations. Referring to Figure 2 of the paper, it can be seen that forward current contributed by anodes 4 and 6, reached values dangerously near such a low forward tripping current as 31,000 amperes. There will be many applications when it is desirable to trip only the one anode which arcs back and not the cathode breaker at all. Such provision preserves continuity of service by allowing the rectifier to remain in service on the remaining healthy anodes. This breaker also, as was pointed out, is designed to permit reclosing any open pole or poles, without interruption of service. This consideration alone justifies the "no trip on forward current" feature. Protection from short circuit on the rectifier is afforded by a discriminating overcurrent tripping mechanism in the medium-speed cathode breaker.

The authors agree that the faster the interruption, the better the breaker for arc-back protection up to the point where voltage surges are created. It has been observed that faster arc extinction than that employed in this breaker does give rise to such surges. Attention is called to the fact that no surges were observed during tests on the breaker described. Any improvement in speed of mechanically starting contact separation is safe and desirable, but this is not true of arc extinction.

Dr. Marti's discussion presents interesting and valuable historical notes bearing on the subject. It furnishes a good example of the efforts engineers have made to solve satisfactorily the arc-back problem in the past and shows an appreciation of the advantages of anode breakers. Many such instances could be related by the various groups who have been working with power rectifiers.

Mr. Winograd's comments constitute valuable additional data regarding mechanism of arc-back to that presented in the paper. The point raised about the effect of the time in the cycle at which arc-back occurs, influencing the rate of rise and ultimate value of arc-back current, is particularly timely. This effect was noted in connection with the whole series of tests conducted on the anode breaker and is of sufficient importance to emphasize.

Mr. Winograd is another who suggests that the anode breaker is a substitute for adequate a-c switchgear. Such a tendency seems dangerous to the authors, as it overlooks other sound and sufficient reasons for not omitting rectifier-transformer protection. The many advantages of high-speed anode switching for arc-back protection alone, set forth in the paper, surely in themselves justify the use of the breaker, and it is not necessary to resort to doubtful omissions to balance the scales.

Ignitor Excitation Circuits and Misfire Indication Circuits

Discussion and author's closure of paper 42-105 by A. H. Mittag and A. Schmidt, Jr., presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and published in AIEE TRANSACTIONS, 1942, August section, pages 574–7.

O. K. Marti (Allis-Chalmers Manufacturing Company, Wilwaukee, Wis.): This paper illustrates in a very interesting way what can be accomplished by approaching a problem step by step in a systematic procedure. It is therefore of value not only in its particular field but in a general way as well.

In a paper, "Excitron Mercury-Arc Rectifiers," presented at the winter convention of the AIEE in 1940,[1] I called attention to some of the shortcomings of the intermittent ignitor excitation scheme and discussed the continuous ignition-excitation system used in connection with the Allis-Chalmers single-anode Excitron rectifier. This paper was not very kindly received, especially the remarks regarding misfiring.

The paper under discussion, now, however gives an outline of some interesting work directed mainly toward overcoming the very disturbing phenomena to which I drew attention in 1940 and to developing means for recording these distrubances. Since my paper was presented, we have seen the introduction of a holding and relieving anode in addition to the ignitor in the ignitron rectifier, as well as a rather complicated circuit, in order to obtain faultless operation with reduced power consumption. This is the case in spite of the fact that the authors have greatly contributed to the simplification and successful operation of the ignitor excitation system. On the other hand, in connection with the Excitron rectifier using continuous ignition excitation, it was possible during the last two years to simplify the circuit somewhat, as well as the rectifier tank, since for most services satisfactory operation is being obtained with one auxiliary anode used as an ignition and excitation electrode. The circuit of this new scheme is simpler than the one presented two years ago, and the phenomenon of misfiring does not have to be contended with. In this system, if the excitation should fail, it will be automatically restored. Since the auxiliary anode is removed from the mercury, any change in mercury level or metallization of the ignition-excitation anode does not have any detrimental effect in connection with the function of the ignition-excitation system.

From the paper of J. H. Cox and G. F. Jones, "Ignitron Rectifiers in Industry,"[2] it can be seen that in connection with the ignitor rectifier a grid is located next to the anode for shielding. Such a grid is also used with the Excitron rectifier for phase voltage control, employing the well-known method commonly used in connection with multiple-anode tank rectifiers. Therefore, in order to regulate the output voltage, the ignition-excitation does not have to be disturbed as in the ignitron rectifier, which may lead to further misfiring, especially if the firing is delayed beyond the first half of the positive cycle.

REFERENCES

1. "EXCITRON" MERCURY-ARC RECTIFIERS, O. K. Marti. AIEE TRANSACTIONS, volume 59, 1940, pages 927–31.

2. IGNITRON RECTIFIERS IN INDUSTRY, J. H. Cox and G. F. Jones. AIEE TRANSACTIONS, volume 61, 1942, October section, pages 713–18.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors indicate that the ignition impulse desired for ignitron excitation should be as steep as possible. This is true only within limits and may hold for magnetic impulse circuits since it is impossible to attain really steep impulses with such circuits. It may be undesirable to use impulses as steep as can be produced with thyratron-capacitor circuits. There is considerable evidence which indicates that, if the impulses are too steep, more instantaneous energy is applied to the ignitor than necessary, and difficulties may be encountered. On the other hand, if an ignitor becomes contaminated with surface impurities, it may be desirable to use a more sloping impulse which will apply more heating energy to the ignitor and increase its temperature. Therefore, the most desirable impulse shape is one which provides a balance between a slope which applies the correct amount of average energy and, a steepness which avoids variability in the time of ignition and a resulting unbalance between anode currents. We have found that such impulses are produced by those saturating reactor circuits which have special iron, such as Hypernik or Hypersil, in the saturating reactor and no additional reactance to further increase the length of the wave front.

The authors mention a relieving anode to take the arc current which follows the creation of the cathode spot and thus relieve the ignitor of this current. Actually, the ignitor will be relieved in any event, because after the appearance of the cathode spot any follow current will pass between the ignitor holder or rod, and the mercury and the ignitor will be shunted. Therefore, it is only necessary that the holder have sufficient area to conduct for follow current provided by the excitation circuit. With the practical construction used in large pumped-type ignitrons, this holder area is ample for any circuit that has been used. In the smaller sealed-off ignitron construction the holder area and the lead-in conductor are both much smaller and would be overloaded with the more powerful circuits. However, the smaller tubes require less ignition power, and the lower energy circuits are entirely adequate.

The autotransformer, which the authors show for the purpose of directing the successive impulses of opposite polarity to opposing ignitors and thus enable one circuit to service two ignitrons, performs the same function as the Rectox arrangement shown in Figure 4 of the paper "Excitation Circuits for Ignitron Rectifiers" presented at the summer convention of 1941.[1] The transformer connection imposes full voltage across the series Rectox, increasing its size, whereas a parallel Rectox imposes only its forward voltage drop across the series Rectox. There is probably little to choose between the two methods.

I question the statement that misleading results are obtained from the determination of circuit output characteristics by replacing the ignitor with a resistance load. The curve obtained by a variable resistance load should give an accurate determination of the maximum capacity. It frequently occurs that a circuit will operate stably with resistance load and have unstable characteristics when loaded with ignitors. This characteristic, of course, will show up in tests which include ignitors, but a circuit which is stable with ignitors can be calibrated by the substitution of a variable resistor. Also, in our recent production, excitation equipment adjusted at the factory by the use of a resistor required no further adjustments in the field.

In connection with misfires, there is some question whether the complication imposed by misfire indicators is justified. There is a great deal of operating experience which indicates that no serious consequences result from a failure of an anode to pick up, either intermittently or continuously, for either a variable type of load such as is encountered in railways or with steady loads as encountered in electrochemical service. In the design of anode balance coils it is entirely feasible to provide open-end impedance which avoids the overloading of an anode when the anode with which it is paired goes out of service. Actually, if an anode goes out of service it is indicated by several things, such as a lower voltage, lower current when paralleled with other units, noise in the anode balance coil when these are used, or fluctuating ammeter reading if the ammeter happens to be a fairly sensitive type. As the authors have indicated, it is a relatively simple matter to find the particular anode which is missing when it is known that a misfire exists in an assembly. Of course, in an automatic station some type of misfire indicator might be desirable to give indication at some distant point, and the authors have listed various satisfactory circuits that may be used.

REFERENCE

1. Excitation Circuits for Ignitron Rectifiers, H. C. Myers and J. H. Cox. AIEE Transactions, volume 60, 1941, October section, pages 943–8.

A. H. Mittag and **A. Schmidt, Jr.:** The relieving anode performs the following functions:

1. Reduction of duty on dry-plate rectifier in series with ignitor.
2. Reduction of duty on ignitor in circuits with high total energy.
3. Stabilization of low energy circuits where aging of dry plate rectifier may change the circuit resistance and resulting performance.

It is our experience that a correct picture of maximum energy for ignitor firing is best obtained by use of the circuit shown in Figure 6 of the paper. In particular, the increase in firing energy in a "twin" circuit, shown in Figure 7, is not evident with a resistance-testing circuit. If the excitation circuit has considerable excess capacity, a testing method with a large margin of error may indicate acceptable operation.

The need for misfire indication equipment is not always present. Such equipment would seem desirable in certain applications, particularly in automatic stations and in certain industrial applications, particularly nonattended stations.

A 600-Volt Enclosed Limiter for Network Use

Discussion of paper 42-99 by P. O. Langguth, H. L. Rawlins, and J. M. Wallace, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, July section, pages 536–8.

Charles P. West (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Limiters comprise an essential feature of secondary distribution and industrial-network systems and provide the best-known means of selecting faults in the loop cables. Their time characteristics and fusing points, when properly co-ordinated with the cables, give isolation of a faulty conductor, without power interruption or disconnection of any sound part of the system. The authors have clearly covered the design and development of the extension of limiters from 250-volt to 600-volt service. Their places in the circuits, with the breakers and other devices, have been shown on the diagram (Figure 10 of the paper). Their physical association with other equipment should also be carefully considered, to obtain the maximum advantages of the device. When the limiters have cleared the defective cable, a new conductor must be installed. A well-planned system includes structures designed to safely allow replacement of this conductor with the remainder of the apparatus in operation.

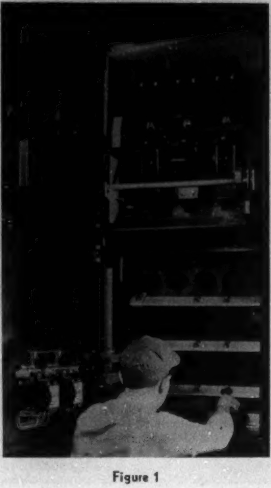

Figure 1

Figure 1 of this discussion shows part of the front of a low-voltage metal-enclosed switchgear structure designed for application to an industrial network, sections of which provide compartments for the loop-isolating switches (nonautomatic air breakers, in this case) and limiters. The limiters are normally bolted to the three-phase busses carried across the unit, and a workman is shown unbolting a limiter from the lower-phase bar. Phase isolation is provided so that the limiter can be removed safely while alive, if necessary.

Figure 2 of this discussion shows the rear of this compartment, with the three conductors of one loop cable in place and the limiters for the second cable. The lower right limiter has the insulating cover removed. The workman is holding a limiter and cover showing how they fit together. When such a structure is in operation, a faulty limiter and cable can be disconnected from the bus and drawn into the rear compartment. It will then reach into the clear space behind the gear, and the cable can be pulled out of the conduit. The new cable is pulled in and its limiter attached and connected to the bus in a similar manner.

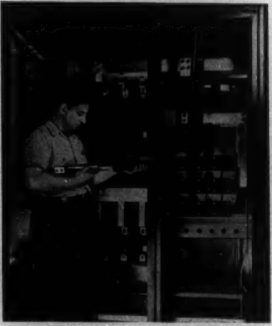

Figure 2

Thus, proper housing and mounting of limiters provides means for replacing faulted cables in the shortest time with no loss of power to the remaining parts of the system. The use of compartments in the switchgear provides isolation for the various devices, gives a co-ordinated design, permits shipment of structures completely assembled, and makes possible all the advantages available in standardized groupings.

Electric Equipment for Large Electrochemical Installations

Discussion of paper 42-138 by T. R. Rhea and H. H. Zielinski, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 733–41. The following discussion was presented at the 1942 summer convention.

W. E. Gutzwiller (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper is a very timely one and covers thoroughly all phases of rectifier application to modern electrolytic plants.

It is apparent from the statistical figures in the paper that the national defense program and the war have been responsible for an unprecedented demand for power rectifiers in the electrochemical industry. This demand raised, in the short span of approximately three years, the total capacity of rectifiers installed and on order in the United States from approximately 900,000 to nearly 3,000,000 kw.

Such an enormous increase in production, on short notice and of a rather intricate piece of equipment, could be met only because some farsighted electrochemical companies had early recognized the advantages of rectifiers for electrolytic processes and had made some trial installations. The manufacturers of rectifiers were thus able to gain valuable experience and, when the defense program got under way, were well acquainted with the peculiar requirements of electrolytic processes which the authors justly state are usually of a secret nature.

The first rectifier application to an electrolytic zinc refinery was made in 1929 in a Canadian plant. Rectifiers were used for the first time in an American chlorine plant in 1934, where, since then, they have had a revolutionizing effect on cell circuit layouts, having been responsible for an increase in operating voltage from 250 to 600 and 750 volts. The year 1938 saw the first use of rectifiers in an American aluminum plant. Step by step the rectifier conquered all the electrolytic processes where electric power is an important item in the production cost. Each of these processes in the order mentioned presented new problems of rectifier application.

Because of the large amount of power required for operation of modern aluminum-reduction circuits (45,000 to 55,000 amperes at 650 volts) high-speed grid blocking and high-power high-speed rectifier switchgear had to be developed to protect the equipment against destructive fault currents.

It is only appropriate to give the Aluminum Company of America credit for having not only encouraged the manufacturers but actually initiated and taken an active part in the development of a high-speed anode breaker which is now being adopted generally by all rectifier manufacturers and operators for high-power rectifier installations.

It is interesting to note that all rectifier pioneering in the electrolytic field, as mentioned before, was carried out with the continuously excited multianode rectifier, which is still being purchased by many users on the strength of its good operating records. However, the single-anode, intermittently excited ignitron type, as well as the continuously excited Excitron type, are being chosen for many high-power installations of medium d-c voltages because of their higher efficiency and greater operating flexibility.

The authors state that continuous use of grid control should be limited to four or five per cent because of the low power factor and greater tendency to arc back. The main reason for the increase in arc-backs, which should be of interest to operators, is as follows: The more grid or igniter control is applied, the more is the firing or "pickup" point of an anode delayed; likewise, is the drop-off point at the end of the firing period delayed and shifted toward the negative half of the voltage wave. The result is that with increased firing delay the voltage between anode and cathode at the end of the firing period rises faster to a negative value, thus reducing the deionizing time of the electronic valve and increasing the reverse current to the anode caused by the residual ionization. Excess reverse current at the end of the firing period causes electron emission at the anode, a common cause for arc-backs. The effect of grid control on arc-backs at reduced load is less pronounced because of the reduced ionization prevailing under this condition. Grid control may thus be used for starting duty on electrolytic cells.

Referring to the matter of high-speed d-c switchgear, in chlorine plants, where, as a rule, one or two rectifiers are paralleled with a total bus load of 7,000 to 8,000 amperes, semihigh-speed cathode breakers with grid or igniter blocking have been found satisfactory. Likewise, installations with anode breakers and a maximum of 8,000 amperes per bus, are now being made where the semihigh-speed cathode breakers are eliminated and d-c relays tripping the anode breakers on overload are being substituted.

The authors describe how a cell or pot line is energized and de-energized by the use of igniter control. In case of rectifiers with continuous excitation, a pot line is started up by first applying blocking voltage to all grids, then establishing excitation and closing anode and cathode breakers of all units supplying the pot line. The load is then picked up by releasing simultaneously all the grids through a master contactor. A pot line is de-energized in the same manner as described in the paper, that is, by simply tripping all cathode breakers from a common trip switch.

The paper deals exclusively with the intermittently excited, ignitron-type rectifier. The application data given in the paper naturally also hold good for the continuously excited single-anode rectifier, which, like its predecessor, the multianode rectifier, maintains an auxiliary arc as long as the unit is on the line.

Interim Report on Guides for Overloading Transformers and Voltage Regulators

Discussion and author's closure of paper 42-156 by the AIEE transformer subcommittee of the committee on electrical machinery, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, September section, pages 692–4. This discussion was presented at the Pacific Coast convention.

J. E. Clem (General Electric Company, Schenectady, N. Y.): The war emergency has focused attention upon the problem of overloading electric equipment as an aid to winning the war. The interest in this problem is based upon either a desire for immediate increased power output or upon the hope of saving critical war materials.

Whenever overloading of equipment is necessary to meet a rising war load requirement, such overloading should be done, provided that the added load does not cause the equipment to fail within the period of its vital war need. If the desire is to save critical war materials, a great deal may be accomplished by the use of new and modern system layouts, by a more accurate predetermination of the required load, and by the adoption of new and modern practices.

The output rating of transformers is based upon a definite temperature rise under specified testing conditions. The temperature rise upon which the rating is based has been selected from consideration of the maximum temperature at which the insulation of the transformer may be continuously operated and still achieve a reasonable life expectancy, together with the anticipated ambient temperature in which the equipment may operate.

The maximum continuous temperature for a reasonable life expectancy has been taken as 95 degrees centigrade; the temperature rise has been set at 55 degrees centigrade with a hot-spot allowance of ten degrees centigrade above the average temperature rise or a hot-spot temperature rise of 65 degrees centigrade. Then the condition that normal life expectancy be based on continuous operation at 95 degrees centigrade, together with the condition that the output be based on a hot-spot temperature rise of 65 degrees centigrade (average temperature rise of 55 degrees centigrade plus ten degrees centigrade allowance for hottest spot), establishes a continuous temperature of 30 degrees centigrade as the reference ambient for normal life expectancy. Estimates for the effect of varying ambient temperatures upon the life expectancy should, therefore, be made using 30 degrees centigrade as the reference ambient at which normal life may be expected.

It is pertinent to review the basis upon which the estimate of the effect of overload on the life of transformers is made. The useful life of class-*A* insulations, such as is used in transformers, is dependent upon the temperature to which the insulation is subjected. However, it should be fully realized that laboratory tests upon which estimates of the life of transformers are based are tests of a mechanical nature. In these tests, the loss in tensile strength after exposure to a definite temperature for a definite time is determined under very carefully controlled laboratory testing technique. There is a broad spread in results of such tests from different sources. The correlation between the life expectancy of transformer insulations as indicated by laboratory tests and the actual life of a transformer is largely a matter of speculation. Therefore, all estimates of the effect of overload upon the life of transformers must be tempered by sound judgment based upon operating experience.

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): In New Jersey we have been following the procedure of operating transformers according to temperature for some time, but we have been puzzled with the practical means of instructing operators of average intelligence to handle such a technical question. In this connection, I am impressed with the data and curves presented in the interim report. In practice, short-time overloads cannot be used as such, because when the emergency arises no one knows how long it will last, nor whether a high overload should be carried for a short time, or some load dropped and a smaller overload carried for a longer time. This is particularly true when maintenance men are off duty and are difficult to get on duty to cut spares into service. Therefore, I should like to suggest to the transformer subcommittee that these technical data be put into shape to be used directly by operating personnel.

We have been experimenting with the possibility of expressing overload rating as a relationship between top-oil temperature, time, and load, so that the operator, by reading the top-oil temperature at the time of the emergency, will know from a curve the overload which can be carried. Then, as the top-oil temperature rises, the overload must be reduced until finally the overload is almost back to normal with the maximum permissible hot spot.

Another question which has perplexed us is: "When is a transformer forced air cooled?" Of course, we appreciate the fact that each manufacturer likes to install his particular air-blast equipment in his own make of transformers. However, we find that ordinary blowers or fans separated from the transformer are quite effective. Transformers are installed initially without fans, and then fans are added later. After an additional transformer is installed, fans are not needed for a period and can be used elsewhere. Therefore, the program alter-

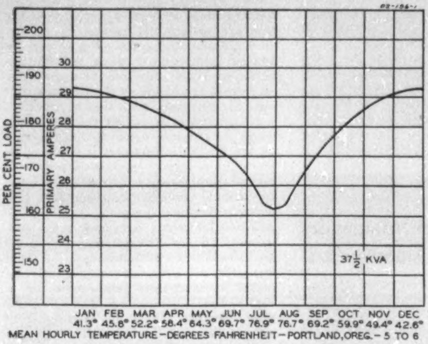

Figure 1. Maximum permissible load in per cent and primary amperes versus months of year (if not limited by voltage regulation)

A 37½-kva 2,400/120–240-volt single-phase overhead distribution transformer. Based on 50 per cent initial load followed by a two-hour peak period. Curve represents points at which the carrying charges on the investment and loss in life plus core and copper losses of a 37½-kva transformer equal the carrying charges on investment, loss in life, and installation cost plus core and copper losses of a 50-kva transformer plus carrying charges on the removal cost of a 37½-kva transformer. Carrying charges are 14 per cent per year. Core and copper losses amount to two mils per kilowatt-hour. Loss in life is based on study by L. C. Nichols[1]

nates between fans and additional transformers, as the load grows, so that mobile fan installations constitute the most acceptable fan cooling.

Corbett McLean (Northwestern Electric Company, Portland, Oreg.): The interim report, by the transformer subcommittee, on overloading of transformers and regulators is a pertinent one in these times of emergency when every ounce of copper and steel should be loaded to the fullest capacity.

For some time, we have been using curves on permissible transformer loading for our overhead 2,400-volt distribution transformers. A combination curve is shown in Figure 2, and an individual curve for a 37½-kva transformer is illustrated in Figure 1.

These curves give the maximum permissible economic loading for ambient temperatures during each month in the year and represent points at which carrying charges on investment, loss in life, and total transformer losses are equivalent to the carrying charges for the same items for the next larger size transformer plus the removal cost on the old transformer. They are based on a 50 per cent initial load followed by a two-hour peak period. Naturally, ambient temperatures, load curves, carrying charges, installation and removal costs, costs of power, and so forth, will vary considerably with other companies, and some will vary for any given company. However, the curves provide some reasonable and simple method of rapidly determining the point at which a transformer should be replaced.

During the present emergency we believe that these curves should be materially exceeded with the realization that life expectancy will be greatly reduced.

Our past experience has led us to permit a greater loading for induction voltage regulators than for distribution transformers. The carrying capacity of current transformers, cable, disconnecting switches, and so forth has generally been the limiting factor on primary feeders.

We have used water spray for some time on 5,000- and 2,000-kva single-phase 66,000/11,000-volt transformers to provide for overload capacity and have found the main drawback to be the frequent painting required on case and tubes.

We appreciate the value of this paper on transformer and regulator loading. It is most opportune and provides a standard which can easily be applied by all in the operating field.

REFERENCE

1. EFFECT OF OVERLOADS ON TRANSFORMER LIFE, L. C. Nichols. AIEE TRANSACTIONS, volume 53, 1934, December section, pages 1616–21.

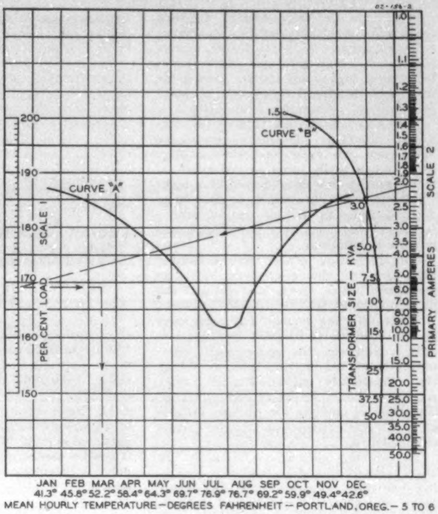

Figure 2. Maximum permissible load in per cent and primary amperes versus months of year (if not limited by regulation)

2,400/120–240-volt single-phase overhead distribution transformers. To find if 2.1 primary amperes on three-kilovolt-ampere transformers exceed maximum permissible loading in the middle of March, project a line from 2.1 on scale 2 through three kilovolt-amperes on curve *B* which indicates a 169 per cent load on scale 1. From the latter point project a line horizontally toward curve *A* to the period of the month (shown below curve *A*) in which test was made. The horizontal line from 169 per cent terminates below curve *A*, showing that maximum permissible loading has not been reached. To determine the limit of loading, project a line vertically upward from the middle of March to curve *A*, which shows 183.5 per cent or 2.3 amperes

C. T. Hurd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): No one will deny that the present war emergency is going to make it necessary, in many cases, to operate transformers at an overload, but what will probably be overlooked is the premature aging of the transformer insulation from operation at these overloads.

The data brought out in the interim report showing allowable overloads are, I understand, based on the supposition that these emergency overloads will not be recurrent in nature. The basis for the report is therefore the assumption that transformers will be necessarily overloaded, but that means for relieving these overloads or for eliminating the necessity for such overloads will be promptly taken.

The term, "reasonable loss of life," is, according to my understanding, intended by the committee to mean a loss of life not to exceed one per cent, and, obviously, if an overload condition is recurrent with a loss of life of one per cent every time it occurs, the full life of the transformer insulation could be readily expended in serving the overload, unless prompt relief is provided.

Since it has been common operating practice in the past to operate power transformers at loads below their name-plate rating, and, since depreciation rates have been predicated on past operating experience, it could be easily overlooked that the cost of supplying any load, which results in overloading of power transformers serving it, will be considerably higher than normal, since the overload will result in an accelerated depreciation rate or, in other words, in a shortened transformer life. In addition, of course, other increased costs must be met, including the cost of supplying increased losses to the overloaded transformer, the effect of increased regulation of the overloaded transformer, and other factors of this nature.

Insulation in transformers does age and tends to become brittle at any operating temperature. When transformers are operated below their name-plate ratings with usual ambient conditions, insulation deteriorates at a very slow rate so that the life of the transformer is very long. Any overloads, even within the limits suggested by this report, simply mean that the life of the transformers affected will not be as great as it would have been with past normal operation, and, while it is entirely possible that no noticeable increase in the failure rate will occur during the emergency, this increased failure rate will probably have to be met later.

Transformer insulation ages by becoming brittle, and brittleness produced by transformer overload carried during the present emergency will persist throughout the entire life of the transformer. The creation of brittleness in transformer insulation obviously lessens the mechanical life of the transformer insulation, and movement of the transformer windings from any cause will be more apt to result in failure than would be the case if the transformer insulation were in normal condition. Under present conditions, overloads are doubly serious because of the rising system load factors. Overloads which follow light load conditions naturally will not result in so much loss of life as will prevail in most instances now, since most systems are operating under greatly increased normal loading. Even without the overloads, transformer copper and oil temperatures will follow a higher average value; consequently any overload which can be carried will of necessity be of shorter duration if serious loss of life is to be avoided.

It is particularly interesting to note in passing that the curves presented in this report coincide rather closely with data presented in one of the earliest papers ever written on the effect of overload on transformer insulation, namely, that by the late L. C. Nichols, entitled "Effect of Overload on Transformer Life"[1] and presented before the Institute's winter convention early in 1935. Much work has been done on transformer insulation since that date, but the figures on insulation loss of life used by Mr. Nichols are well supported by this paper.

As previously stated, it will be impossible to avoid all power-transformer overloads, but the cost of such overloads from such factors as loss of life, increased losses, and so forth, will ultimately have to be met.

REFERENCE

1. EFFECT OF OVERLOADS ON TRANSFORMER LIFE, L. C. Nichols. AIEE TRANSACTIONS, volume 53, 1934, December section, pages 1616–21.

M. S. Oldacre: We believe that the statements in the third and fourth paragraphs of Mr. Clem's discussion should be considered as his own interpretation of the American Standard *C*-57, "Standards for Transformers," in regard to normal ambient temperature, hottest-spot temperature, and life of insulation. This portion of his discussion has no bearing on the interim report, since the temperatures discussed do not appear in that report. Also we believe that Mr. Clem's interpretation of American Standard *C*-57 is not that generally accepted by the committees which prepared the standards and the interim report.

Ignitron Rectifiers in Industry

Discussion and author's closure of paper 42-118 by J. H. Cox and G. F. Jones, presented at the AIEE summer convention, Chicago, Ill., June 22–26, 1942, and at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, October section, pages 713–18. The following discussion was presented at the 1942 Pacific Coast convention.

J. H. Steede (British Columbia Electric Railway Company, Ltd., Vancouver, B. C., Canada): The expansion of the electrochemical industry has provided an enormous field of application for the mercury-arc rectifier, as the authors of this paper have pointed out. It is exceedingly interesting to note the tendency toward single-anode tanks in the general field of rectifier design on this continent. This design appears to be a definite improvement over the multianode metal-tank type for all classes of application, because of the more exact control of the ignition point, the extinguishing of the arc during the time that reverse potential is applied to the anode, which reduces the likelihood of back-fire, and the lower internal voltage drop which results from the shorter arc path.

Two characteristics of the multianode metal-tank rectifier are still retained, however, namely, the necessity for water cooling with possible attendant corrosion problems, and the need for occasional pumping to maintain vacuum. Both of these features add considerably to the amount of auxiliary equipment required with metal-tank rectifier installations.

The British Columbia Electric Railway Company has had a limited experience with rectifiers for traction purposes since 1929, when a 1,000-kw 12-anode metal-tank unit was placed in service. Operating experience with this unit was disappointing. In 1936 a new tank assembly was provided, which, with close control of the cooling-water temperature, has given satisfactory service since that time.

Three more recent installations have been of the glass-bulb multianode type, consisting of two two-bulb units and one four-bulb unit, the rating of each bulb being 165 kw at 550 volts. Some data relative to these installations may be of interest.

One of the two-bulb installations has been in practically continuous service since December 1936, with only two or three short outages, caused by a-c power failure.

A similar installation made in 1937 has almost as good a service record, although this unit was put out of action on one occasion as a result of a lightning stroke on the trolley in the immediate vicinity of the substation which presumably caused the rectifier to backfire, blowing eleven of the twelve anode fuses with which this unit is equipped. These fuses were replaced and the unit returned to service without further servicing.

The four-bulb unit, which was installed in 1938, apart from occasional cleaning, has been out of service only for short periods during temporary failure of a-c power supply.

All of these units are in unattended substations, and the maintenance costs have been extremely low. One man's time for approximately two days per month has been ample for maintenance purposes, and of that time only approximately one-half day per month is actually spent inspecting and servicing the equipment.

Lysle W. Morton (General Electric Company, Schenectady, N. Y.): This paper, entitled "Ignitron Rectifiers in Industry," covers in a very satisfactory manner conventional large power installations. In general, most of the installations which have been recently made conform to the rules of application described in the paper.

However, there is one feature described, namely, water cooling by means of copper tubes soldered on the walls of the tanks, which is often accomplished otherwise.

Early experience with water-cooled mercury-arc rectifiers indicated that the direct use of most raw untreated tap waters caused corrosion of the steel vacuum chambers. Manufacturers and users immediately set about to search for a solution for that problem. Early in the 30's two satisfactory answers were found:

1. The use of noncorrosive metal surfaces in contact with the cooling water, such as copper or stainless steel. One of these the authors describe.
2. The use of sodium chromate or dichromate as a rust inhibitor.

The second method is also widely and successfully employed. A water-to-water, or water-to-air heat-exchanger system must be used, because it is necessary to recirculate the treated cooling water. The recirculating coolant is treated by adding either enough sodium dichromate to provide a 0.5 per cent solution or enough sodium chromate to make a 0.1 per cent solution.

Inspection of water-cooled surfaces of rectifiers which have been in contact at high temperature with treated waters for many years proves that it is a most successful rust inhibitor and a satisfactory application.

Still another advantage accrues from the use of sodium dichromate. Tests made on evacuated steel chambers, particularly if the temperature is elevated slightly, indicate hydrogen ion penetration of the steel envelope from either the surrounding air or from ordinary water if the chamber is water-jacketed. It was found, however, that the addition of sodium dichromate to the water eliminates this penetration and prevents contamination of the vacuum.

Using a solution of sodium chromate or dichromate as the recirculating coolant and employing the simple principle of an ordinary steel water jacket around the vacuum changer secures the following advantages:

1. A greater part of the vacuum-chamber area contacts the cooling solution, which means better heat transfer and less chance of ion penetration from the air.

2. A severely restricted material is saved.

3. The recirculating coolant does not pick up products of corrosion, such as even untreated distilled water may, from the various odd ends of the cooling system. This is true, because even though copper may be used on the rectifier and the heat-exchanger surfaces, it is usually impractical to make the surge tanks, tube headers, and reservoirs of other materials than steel or iron.

4. There is no chance of deterioration of the rectifier equipment or blocking of cooling passages as a result of corrosion or products of corrosion.

It is sometimes desirable to ground the water-to-water heat exchangers to eliminate the inconvenience to personnel of having the heat-exchanger surfaces at various potentials above ground. This can be done both with the soldered-on copper tubing employing distilled water, or by using 0.1 per cent sodium-chromate solution, as the leakage currents are satisfactorily limited. However, if it is not too inconvenient, it is better to insulate the heat exchanger from ground and allow it to operate at rectifier potential. This has the advantage of keeping the leakage currents back to ground in the cooler and more highly resistant part of the water circuit where they can be minimized.

H. K. Sels (Public Service Gas and Electric Company, Newark, N. J.): Cox and Jones have dealt rather briefly with the problem of telephone interference. There are many other factors involved besides those mentioned, such as the susceptibility and configuration of the communication or signal system and the present influences and electrical characteristics of the power system. This problem cannot be dismissed lightly considering the inductive co-ordination measures usually present in every system where some additional influence such as rectifier harmonics may produce trouble.

The suggestion of the authors that 36-phase operation gives satisfactory operation in most cases raises two questions:

1. Speaking in general terms for average conditions, what proportion of system load can rectifier load become before trouble will be experienced?

2. Have there been any cases where more than 36-phase operation has been required?

This is a question involving several parties; namely, the utility, their customer, communication or signal interests, and equipment manufacturers. From a utility standpoint, it is expected that the customer and the manufacturer of his equipment will be responsible for continuing good relations with communication and signal companies.

J. H. Cox: Mr. Steede has discussed glass-bulb mercury-arc rectifiers and has presented a case where they have proved satisfactory. However, it must be admitted that these large glass bulbs are inherently fragile wherever installed and are suitable at all only for relatively low-power installations. For large installations such as are common in the electrochemical industry the glass bulb has two very serious limitations:

1. The current per anode is low, and the number of main and secondary wires would be prohibitive.

2. On large busses high-speed switching is necessary to avoid destruction of apparatus, and the cost of high-speed switching would be prohibitive for even the largest glass-bulb rectifiers.

Mr. Morton has discussed the use of ordinary steel water jackets on the rectifier, together with sodium-dichromate treatment of the recirculating water. It is true that this system is also quite satisfactory. I question the statement that there is hydrogen-ion penetration through the steel where exposed to air. Such penetration would depend upon some corrosion taking place, and, obviously, an exposed portion of the tank would be finished to prevent such corrosion. The only functions of cooling are to dissipate the losses and to maintain a mercury vapor pressure which provides satisfactory rectifier performance. Since this performance actually is highly satisfactory with soldered-on copper cooling coils, there can be little argument about the effectiveness with which such a system cools the tank walls. Furthermore, where copper is used on the rectifier the extent of corrosion that takes place is purely nominal in a recirculating system where oxygen is excluded, even on the portions of the circulating system that must still be made of iron. It will be agreed generally that the grounding of a piece of apparatus is always desirable, and the sodium-dichromate treatment of the recirculating water certainly increases the difficulty of grounding the heat exchanger.

Mr. Sels discusses the question of harmonics generated by large rectifier installations and asks what proportion of system load can rectifier loads become before trouble will be experienced. There are several installations where the rectifier load constitutes practically the entire generating output. In at least one case this local system, involving over 100,000 kw, is connected to a public-utility transmission system, and no difficulty has been experienced. In the Bonneville territory the rectifier load is becoming a very important percentage of the total. Although no serious difficulty has been encountered, the sheer magnitude of the powers involved in this location has caused at least one user to decide to increase the number of phases to 108 at those stations where this is possible.

Three-Winding Transformer Ring-Bus Characteristics

Discussion and authors' closure of paper 42-153 by G. W. Bills and C. A. MacArthur, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 848–9.

A. H. Frampton (The Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): The type of switching diagram proposed by the author is one which, in slightly different form, has been proposed for several major developments in eastern Canada. The selection of a proper diagram in such cases is often difficult, and it has been found helpful to draw up the following specification for an ideal diagram against which alternative proposals may be compared, and their relative merits weighed. Probably no one diagram will ever meet all of these requirements, but at least the exercising of personal judgment may be limited to the weight accorded each item in the specification.

1. The switching diagram should be sufficiently flexible to permit the available capacity being subdivided into as many components as there may be independent groups of load to be supplied, while retaining:

(*a*). Convenient and flexible control of real and reactive power flow in each load subdivision.

(*b*). The ability to isolate any piece of equipment, in event of disturbance or for purposes of routine maintenance, without affecting other equipment which should remain in service.

At the same time the highest practical efficiency of operation should be effected in the combined plant, whether measured in terms of utilization of river flow or fuel consumption.

2. The clearance of any disturbance in any one load subdivision should not affect the continuity of service in other load subdivisions.

3. The loss of one transmission circuit in any one load subdivision should not isolate from the load generating capacity in excess of the operating reserve capacity in that load subdivision.

4. The operating duty imposed upon both new and existing circuit-breaker equipment should not exceed the safe capacity of such equipment.

5. The diagram should be simple, preferably symmetrical and of a repetitive form, so that the switching connections in each of the load subdivisions will resemble each other and also the combined station diagram. Undue complexity that would hamper the operating staff, particularly under emergency conditions, should be avoided.

6. The over-all cost should compare favorably with alternative proposals.

As the diagram shown in Figure 2 of the paper is presumably not advanced as a practical proposal, but only as representing any form of diagram resulting in bussed connections at the sending and receiving ends, no comments are offered beyond the fact that a saving in low-voltage bus copper, and some reduction in short-circuit values would result from the use of conventional double low-voltage windings in the step-up transformation. However, if the diagram proposed in Figure 1 of the paper is judged on the basis of the above specification, it will be noted that it will rank high only if it is granted that the station will always supply only one load group. In that case, all but two of the conditions will be satisfactorily met, the exceptions being that all breakers are inaccessible for maintenance work without the associated equipment being taken out of service, and the over-all cost is likely to be high.

In the latter connection, the author refers to the additional cost of the increased step-up transformer capacity as being partially offset by the elimination of high-voltage breakers. Actually, if it is granted that in Figure 2 double low-voltage windings could have been shown, then the author's diagram requires at least double-capacity breakers and connections in the low-voltage transformer circuits, with a further consequent increase in cost. Approximate estimates would appear to indicate that the total increased cost could be offset only if the transmission voltage were considerably higher than 230 kv, thus effecting a very substantial saving in high-voltage switching. In the optimum case, it is possible that the author's proposal might result in the saving of one complete transmission circuit.

On the other hand, if it is required that the station illustrated be subdivided into two load groups, or say a nine-unit station into three groups, it will be noted that it will be extremely difficult to satisfy the requirements of condition 1 in the preceding specification, even though additional connections are added to form further loops in the low-voltage circuits. If these connections are added, then the diagram becomes quite complicated, conflicting with condition 5. It is considered therefore that this form of diagram would find application only in single-load group generating stations or their equivalent.

H. N. Muller, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have described the results of an a-c network-calculator study performed to investigate the merits of a particular generator bussing scheme. This ring-bus arrangement was studied over 15 years ago at the time the Boulder Canyon project was under consideration, but results of such studies have never been presented in the technical literature. The investigation performed by the authors shows that application of this bussing arrangement results in increased transient power limits and decreased short-circuit currents, both of these attributes being of prime significance on certain systems.

The authors chose for study a ring-bus system composed of four groups of two generators connected through three-winding transformers to four high-voltage lines. All these lines terminate on one load bus. Since each generator is specified to have a capacity of 55,000 kva, this means that the total output of the assumed station, 440,000 kva, is transmitted to one load center. This is not improbable, but the more usual case would be for these transmission lines to carry the station output to two or more load centers. In the introduction to this paper, power concentrations of over 1,000,000 kw are directly referred to, and in developments approaching this size it would be extremely unlikely to have the entire output delivered to one load bus. As a matter of interest, the authors name Boulder Dam, Grand Coulee, and the proposed St. Lawrence project as exemplifying large power concentrations where the bussing scheme studied might be of advantage. Note that none of these projects named do, or probably would, deliver their total output to one load center.

The question then naturally arises as to whether the smaller ideal system chosen for study by Bills and MacArthur can be used to reach conclusions that can be extrapolated to apply to the larger and more general layouts. With this point in mind the discusser has performed additional studies of a more general nature, concerning the results of which brief comments are offered.

If a larger generating source is assumed than that chosen by the authors, and if the transmission lines emanating from this source are of varying length and deliver load to two or more independent load centers, the three-winding transformer ring-bus scheme becomes less ideal. Significantly higher transient-power limits and lower short-circuit currents still prevail, but other operating limitations can appear. If one independent load center is drawing heavy load, while the load supplied over other lines to another receiver is light, unbalanced division of the reactive kilovolt-ampere burden between generators will result, and one or more generators may exceed their maximum field limitations while other units operate with light field current. To alleviate this difficulty it would be necessary to sacrifice bus voltage regulation. If it is desired to control the voltage of the transmission lines feeding one load center independently of those lines feeding another receiver, the proposed ring-bus arrangement appears inflexible compared to certain more conventional schemes.

The above points are not mentioned with an intent to condemn but are raised to show that the bussing scheme in question requires careful study before its use is recommended for a particular installation. Very broad conclusions cannot be drawn from study of one system. On systems embodying the comparative simplicity of the one studied by the authors, this transformer ring-bus arrangement appears to offer valuable advantages without introducing any difficult problems of operation. It deserves serious consideration on projects whose complexity is limited by size but of necessity must be carefully analyzed on large projects, since the operating problems and limitations which can result may overbalance the inherent advantages.

G. W. Bills and C. A. MacArthur: It is granted that the three-winding ring-bus arrangement is not a cure-all, but no known system will satisfy all the requirements specified by Mr. Frampton. To bus as much as 1,000,000 kva of generators at a station is not only risky in that the entire station may be lost during a disturbance, but problems involving the control of real and reactive power flow may arise when different load groups exist. For this reason we consider only single-load systems although more extensive systems have been investigated and found feasible.

Mr. Frampton stated that only the use of transmission voltages higher than 230 kv would offset the increased cost of larger transformer capacity. We would like to point out that it is permissible to invest more in the three-winding transformer ring-bus system than in the sandard system because of the increase in firm power it is possible to transmit. However, an economic study was beyond the intended scope of this paper.

From transient-stability and short-circuit considerations, however, the three-winding transformer ring-bus system has been shown to have very definite advantages.

It is extremely interesting to know that Mr. Muller investigated the ring-bus arrangement years ago and arrived at the same conclusions as the authors concerning the stability and short-circuit considerations.

We would like to point out that the output of six 82,500-kva generators at Boulder Dam sends power to a common bus at Los Angeles; that in the proposed St. Lawrence project as much as 500,000 kw may go to Rotterdam, N. Y.; and in the Bonneville Power Administration transmission system similar blocks of power will be transmitted to concentrated load centers such as at Seattle, Spokane, and Vancouver, Wash.

Any scheme which busses all the generators will be inflexible when it becomes necessary to regulate different receiving-end bus voltages by changing the sending-end potential. However, most modern hydroelectric projects resort to the use of synchronous condensers and transformers having tap changing under load for purposes of regulation so that this is not a serious problem.

Mr. Muller's point that this ring-bus arrangement must be carefully analyzed for a particular system is well taken. We did not intend to imply this bussing arrangement was suitable for *all* large systems but had hoped to show it might be very suitable for certain projects.

A Practical Discussion of Problems in Transformer Differential Protection

Discussion and author's closure of paper 42-146 by P. W. Shill, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 854–8.

John Teasdale (British Columbia Electric Railway Company Limited, Vancouver, B. C., Canada): There are two paragraphs near the beginning of this paper which, in generalizing on current-differential schemes, seem to suggest that various measures used to secure good balance on differential circuits no longer have the same value as formerly. This view is scarcely in accordance with the facts.

As experience is extended, and as larger units come into service, the trend is rather in the direction of still greater care in the application of current-differential protection and instances to the contrary imply the acceptance of a lower standard of engineering.

Accuracy in reproducing the primary conditions in the secondary circuits at all times is still the ideal sought after and is approached as nearly as possible on important installations. When all that is possible has been done in this direction, *then* consideration is given to dealing with inaccuracies that cannot be avoided, so that they do not affect relay operation adversely.

A high-setting instantaneous relay combined with a low-setting timed relay, as referred to in this paper, is one method of evading trouble on account of these causes, but it should not be regarded as modifying the need for a good balance in the secondary circuits. Otherwise, to be entirely safe, it will require settings that involve material sacrifice in speed or sensitivity. For this reason, its value on installations of any importance, without any additional protection, is likely to be inferior to ordinary differential protection using restraint on through faults, and these relays cost little more and are probably no more difficult to obtain in wartime.

E. G. Ratz (Canadian Westinghouse Company, Ltd., Hamilton, Ont., Canada): Induction-type relays having very inverse characteristics, with fairly sensitive current settings and a high-speed instantaneous trip device included, are difficult to improve upon for transformer differential protection.

The maximum through-fault current can usually be determined from the reactance of the transformer bank, and the difference currents in relays can be reduced to a minimum on through faults by exact matching of the current-transformer ratios. We have found this to be a very desirable procedure in order to make such difference currents a minimum and thus reduce this limitation to the permissible sensitivity setting. If proper care is taken, difference currents on through faults will certainly be small, and, even if they do exceed the current setting of the relay-induction element, faulty tripping will almost certainly be avoided because of the difference currents being such as to cause the relay to operate on the inverse and longer timed portion of its curve.

Difference currents on through faults are also minimized by keeping the burden on the main current transformers at a minimum, and this makes the omission of auxiliary balancing transformers desirable, as such transformers usually have a relatively high burden.

The current setting of 15 amperes and 15 cycles suggested for magnetizing inrush seems high. Even if the maximum inrush current considerably exceeds the current setting, this current falls very quickly, and a very inverse characteristic will prevent faulty operation. Further, relay elements of the induction type are less susceptible to difference currents with d-c components than plunger-type relays.

With respect to the author's objection to the use of suppressor devices to prevent operation on magnetizing inrush, it is suggested that a relay of the ratio differential, or any other type, can have restraint added with a momentary compensating magnetic characteristic in proportion to the *rise* in voltage. This restraining action would be the maximum at the moment the transformer bank is energized and would die out at approximately the same rate as the magnetizing inrush, thus providing restraint only when required and approximately in proportion to the requirements at various moments during the magnetizing inrush period. If the transformer bank were picked up with a fault, the fault-current torque would exceed the restraining coil torque within a cycle or two, providing the required operation without undue delay.

We have used impedance relays, with the current coils connected in the differential circuit, and the voltage coils energized from the main circuit and set to operate instantly at a differential current of approximately three times full load current, at full voltage. In case of bad faults, the voltage drops, thus reducing the differential current required to trip, in proportion to the drop in voltage. This relay is more sensitive on faults than a straight instantaneous overcurrent relay and is particularly effective and fast if the transformer bank is large compared to the connected system, for example, if the fault current delivered to the transformer bank is unusually low because of reduction of connected capacity.

We feel that it is highly desirable to provide devices to limit the sensitivity momentarily to meet inherent conditions such as magnetizing inrush, and through-fault currents, if necessary, rather than to reduce the sensitivity of the equipment under all conditions.

L. L. Fountain (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject paper calls attention to some very important considerations in the differential relaying of transformer banks. The admonishments in regard to washing out zero-sequence currents, where the transformer bank is grounded, and the methods used in calculating ratio of current transformers are adequate.

I feel, however, that not enough emphasis has been placed on the calculations necessary to obtain the transient values mentioned under the heading "Relays." In order to arrive at the assumed values given for maximum differential current caused by through- and magnetizing-current transients, a rather complicated set of calculations must be made, requiring a knowledge of the current transformer's saturation characteristics, time constants of the system, and relay characteristics when energized by transients containing a large d-c component. Probably Mr. Shill considered that the practical nature of his paper did not permit delving too much into these calculations. Mention of them, however, would prevent any false notion that the values he gives are readily available on the majority of applications.

The percentage differential relay with restraint windings is certainly to be preferred over and above the simple overcurrent relay. This type of relay permits the use of current transformers that are not evenly matched so far as saturation characteristics are concerned. Also the complicated calculations necessary in the application of the simple overcurrent relay can generally be omitted when the percentage differential relay is used, since its restraint characteristic allows a larger differential current to flow without false operation.

The percentage differential relay used in transformer protection is of the same construction as the induction time-delay overcurrent relay recommended in this paper. Because of its popularity and the similarity of its parts to those used in the induction overcurrent relay, it can be obtained from the relay manufacturers as readily as any other relay.

The discussion of magnetizing-current inrush trip suppressors seems to deal only with those types that depend on time delay or shunting the relay operating coil to prevent false operation. There is available a type of trip suppressor that has none of these undesirable characteristics but depends on system voltage indication as a monitoring means to prevent false relay operation. A breaker closing to energize a transformer bank normally applies full voltage across the windings. If these voltages are normal, then the trip suppressor prevents the differential relay from operating on the magnetizing-current inrush. Should the transformer be faulted, then one or more of the three-phase voltages will be below normal, and the trip suppressor will allow the differential relay to function in a normal manner without time delay or decrease in its sensitivity.

S. Minneci (General Electric Company, Pittsfield, Mass.): In his discussion of the problems in transformer differential protection Mr. Shill describes a practical solution for the protection of power transformers with *fixed* ratios. Transformers equipped with load ratio control have their ratios changed by taps in any of their windings. A *variable-ratio* transformer will therefore allow an unbalance of 10 to 20 per cent in the secondary currents of differentially connected current transformers, in its leads. Since the unbalanced current thus produced does not increase greatly as a percentage of the through current, percentage differential relays in which the operating current is a certain percentage of the through current are usually employed to obtain a sensitive setting at low currents without danger of tripping the transformer breaker on through faults. This problem was the subject of an article written by J. W. Dodge and published in the May 1929 issue of the *General Electric Review*.

Another consideration in the problem of differential relay protection is the effect on the duration of inrush currents when transformers are paralleled. Studies made by C. D. Hayward, relay engineering department, General Electric Company show that inrush currents appear in both the transformer which is to be energized and the transformer already in service and that the inrush currents may persist at nearly full-load current magnitude for a period of 66 cycles after the second transformer is connected in parallel. Factors affecting the inrush currents and a means for adequate protection under these conditions are described in a paper prepared by Doctor Hayward.[1]

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REFERENCE

1. PROLONGED INRUSH CURRENTS WITH PARALLEL TRANSFORMERS AFFECT DIFFERENTIAL RELAYING. **C. D. Hayward.** AIEE TRANSACTIONS, volume 60, 1941, pages 1096–1101.

P. W. Shill: While the paper was intended originally to deal with the subject of the present exigencies and the limitations in the selection of relays and other equipment which have been imposed on protection engineers by our governmental regulations, the discussion has tended more to the consideration of ideal conditions and designs.

The exact matching of current transformers to suit the inherent ratio of the power transformers presupposes that there be a series of taps on one set of current transformers to match the taps on the power transformers for the ratio-adjusting or tap-changing switchgear. While it may be quite easy to make changes in the cases of off-load ratio adjustments, it would be necessary to provide tap-changing switchgear for the current transformers in the case of automatic tap changing on load. This is hardly feasible economically; neither is it necessary, because small permanent differential currents can be taken care of easily. Slight differences in current-transformer ratios are no more serious in the cases of transformer banks with fixed ratios than they are in the cases of those with automatically variable ratios.

It is true that auxiliary current transformers usually have rather high burdens; in fact, most of them have burdens which are considerably higher than they need to be. The errors they may intoduce are most serious in the cases of through-fault currents. In these cases the burdens of auxiliary current transformers are shared by two of the sets of power current transformers in the differential system. While the portions of the total burden of the auxiliary current transformers which are carried by each set of power current transformers may still be high enough to impair the ratios of the latter, both sets will be subject to the same conditions so that any differential between their secondary currents under these conditions will be relatively small. The chief objection to auxiliary current transformers is that they may provide additional d-c transients under fault conditions. The relays must be arranged to pass these transients. In the case of faults in the differentially protected zone, the auxiliary current transformers, by upsetting the ratio of one set of power current transformers, tend to make the protection more sensitive.

The calculations of expected transients were considered to be outside the scope of this paper, and they have been dealt with very well by other authors at different times. It should be noted that the figures given in the paper cannot be considered as typical by any means. Each case must be carefully considered on the conditions to be met, if there be any uneasiness of mind about transient conditions to be expected, or experience proves that such transient currents do reach troublesome values in any particular case. The case reported verbally at the convention meeting of a transformer bank having a charging current inrush of five times normal full-load current and requiring 2.5 seconds (150 cycles) to decay, shows what can be expected in extreme cases.

There is no argument that restrained relays which drop their restraint under fault conditions in the protected zone are very desirable and should be used if possible. From the writer's experience, current-ratio differential relays requiring as high as a 50 per cent unbalance to operate are not so satisfactory for passing transient currents as are ordinary overcurrent relays.

With regard to the availablility of relays, the writer has just received three ordinary induction-type overcurrent relays with instantaneous attachments for which the order was placed over 11 months ago. Of course, the standard explanation is "priorities." This may be sufficient and quite satisfactory for the devotees of this new quasi religion, but it does not help the man with a job to do, who has to meet dates when apparatuses are required to be put into service. How long it would take to get any special types of relays is anybody's guess these days.

Anyone who has had experience in practical engineering design knows that in almost all cases, in all branches of engineering, pinpoint accuracy seldom is finally attainable or necessary. After one has made the calculations for one's ideal design, one must immediately look around for the equipment and materials which most closely meet the ideal requirements. Equipment and materials usually have to be selected from standard sizes and available stocks and adapted for one's purposes. Special equipment is, usually, relatively quite expensive and is not often justified by engineering considerations. If one accepts the principle that lack of pin-point accuracy is proof of a low standard of engineering, then there is and has been very little high-standard engineering done in any branch of the profession. In the writer's opinion, high-standard engineering consists of:

1. Determining the ideal arrangement and conditions that are desirable.
2. Determining the tolerances that are permissible.
3. Selecting the equipment and materials which are most available and economical for the purpose.

Pin-point accuracy is no more necessary in a properly designed current-differential protection than in any other piece of engineering.

Method for A-C Network Analysis Using Resistance Networks

Discussion and author's closure of paper 42-149 by Waldo E. Enns, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 875–80.

Mabel M. Rockwell (Lockheed Aircraft Corporation, Burbank, Calif.): The author of this paper deserves commendation for his ingenious application of the circuit theory to the solution of the always difficult network problem using the d-c board. This development should be of considerable practical value to utility companies, since most of them have d-c boards available for short-circuit studies, but comparatively few have a-c boards, and under present conditions the likelihood of obtaining such is undoubtedly remote.

In operating a power system practical questions of load distribution, substation location, and so forth, occur even more frequently than relay problems and in the past have often had to be solved by very approximate or laborious methods. While Mr. Enns' method is, of course, more laborious than the simple solution of using an a-c board, in the absence of the latter it presents a powerful and useful tool for solving many urgent problems with the equipment on hand.

V. B. Wilfley (Westinghouse Electric and Manufacturing Company, Seattle, Wash.): The author of this paper should be commended very highly for his development of the method he has described.

Heretofore, it has been considered necessary to use the a-c calculating board for the making of complicated system load studies. However, there are few such boards available, and the means of making use of the standard variable resistance d-c board, as described by Mr. Enns, is of decided importance.

In these times, the very best possible use of all system facilities is necessary, and load division studies are essential for best system operation. Practically every utility owns a d-c board, and a description has been given as to how it may be used to obtain valuable information.

In the past, the use of d-c boards has, generally, been confined to short-circuit studies. Even for this type of study, the results were subject to some error, because either the resistance had to be considered as negligible or, if impedance values were used, those in one circuit had to be assumed to be inphase with those of other circuits. The method covered by the paper will be much more accurate for all types of studies, as the resistance and reactance are used in their correct relationship to each other.

A review of the paper indicates that a certain amount of paper calculation work is necessary, but these calculations seem quite straightforward and involve only simple slide-rule mathematics. Also, such studies are not made frequently of a general rule.

In the first part of the paper, reference is made to a design of d-c board which will be particularly well suited for use with this method. It would be appreciated if Mr. Enns would give a little additional information about its general features and how it differs from the standard d-c board. I assume that it will make use of four circuits connected in series parallel to save time in obtaining readings.

Charles A. MacArthur (Bonneville Power Administration, Portland, Oreg.): One difficulty with Mr. Enns' proposal is the necessity for estimating voltages at points removed from the reference bus. The initial estimate should be relatively close in order to avoid the necessity for a second solution with the four network combinations (real and reactive current with resistance and reactance networks). For networks which are radically changed from any previously studied, this may be very difficult.

A short cut to obtain this initial result is possible by using the real component of current with the resistance network and the reactive component of current with the reactance network. These two combine to give nearly the correct magnitude of voltage drop to remote busses. The bus voltages thus obtained may be used as described by Mr. Enns in section 3 of the "Application of Method to Network Problems."

The use of resistance networks for a-c network analysis is a valuable tool for companies which cannot justify an a-c calculating board.

W. W. Parker (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Enns' paper is very interesting in describing an ingenious method of overcoming the inherent limitations of a d-c calculator board in the study of a-c networks. By successive solutions of four equations with four separate setups, the real and reactive power flow and bus voltages can be obtained.

As indicated in the paper, wherever a-c network calculators are available, they will always be used in preference to this method, as obviously the direct replica approach gives accurate solutions much more quickly and easily. For other than short-circuit or the simpler regulation problems, that is, for transient-stability and load-control interconnection problems, the application of the method discussed in the paper would be decidedly limited.

In the last few years, as outlined in the recent paper, entitled "The Modern A-C Network Calculator"[1] which was presented at the 1941 Pacific Coast convention, the number of a-c network calculators has greatly increased. The latter two, mentioned at that time as being under construction, have since been installed and are now in practically continuous operation.

Many of the a-c network calculators are available for use by engineers other than the owners, so that it is now possible for a-c calculator analysis of any of the major problems of the design and operating engineer.

REFERENCE

1. THE MODERN A-C NETWORK CALCULATOR, W. W. Parker. AIEE TRANSACTIONS, volume 60, 1941, November section, pages 977–82.

Waldo E. Enns: The discussions presented indicate the acceptance of the method outlined in the paper as a practical tool for making load distribution and voltage studies with a d-c calculating board. This favorable opinion is appreciated, coming as it does from engineers close to the subject of a-c network analysis.

One point brought out in the discussion is that the use of this method applied to existing d-c boards is more laborious than the a-c calculator. This point must be admitted; however, where an a-c calculator is not close at hand, in many cases load studies can be solved more economically on an existing d-c board.

Although it was beyond the scope of the present paper, the writer hopes to describe soon a resistance board designed especially for this new method which greatly simplifies its use.

A New Single-Pole Service Restorer

Discussion and author's closure of paper 42-147 by E. E. Tugby, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 889–92.

G. F. Lincks (General Electric Company Pittsfield, Mass.): Mr. Tugby has described a very interesting development in restorers or reclosers. One of the features of the restorer is the adjustability of time-current characteristics in the field, so as to trip at 150, 200, or 250 per cent of the rating within a variable time range of from about 2.5 to 9 seconds. While apparently desirable, there are some practical application problems which make the value of this feature somewhat questionable.

It is unlikely that many operators, even if they could, would consider throwing short circuits on their distribution systems in order to make the field adjustment Mr. Tugby states was necessary in properly co-ordinating the system described and shown in Figure 5 of the paper. In making system co-ordination studies for overcurrent protection, it is customary to demand a very high degree of reliability and accuracy in both the published time-current characteristics and the product. This permits making a completely co-ordinated system setup on paper with the expectancy of securing 100 per cent successful operation when installed. For example, a number of utilities with more than four years operating experience on sectionalized systems protected by single element and reclosing fuse cutouts report that they have yet to experience one false operation.

To make use of the adjustment feature of the restorer described for making a co-ordinated system setup on paper, it would be necessary to have a series of time-current curves such as is provided for relays. The time-delay adjustment of each restorer would have to be accurately calibrated for a number of intervening settings between the minimum and maximum so as to meet curves published for each one. Such a procedure naturally would involve some variables in commercial manufacture which would have to be known so they could be taken into consideration in making co-ordination studies. It would be of value if Mr. Tugby could give some data on just how much this variation would be. A comparison of the curves of Figure 2 of the paper when replotted on log-log co-ordinate paper shows a difference in shape between the maximum and minimum time delay. This would indicate the need for individual curves for each adjustment.

In setting up a co-ordinated system it is customary to start with the protective device farthest out on the system. The economic justification for the use of restorers or reclosers would be premised on services feeding appliance loads, such as ranges, and so forth, requiring at least 60-ampere service entrance fusing. As pointed out in the AIEE paper "Trends in Distribution Overcurrent Protection,"[1] in order for 60-ampere 250-volt fuses to operate ahead of the fuse on the primary circuit of the distribution transformer, the primary fuse link must be rated at least three amperes at 7.5 kv, five amperes at five kv, and 15 amperes at 2.5 kv. A majority of the restorers will be used with a minimum three-ampere fuse rating on 7.2/12.5-kv grounded-wye circuits. Thus, for example, proper sequence of operation with the 60-ampere service entrance and five-ampere primary fuses will require setting three-ampere restorers to trip at 250 per cent of the rating and with the maximum time delay. Progressive heating during the two subsequent closures cannot be depended upon to cause a higher rated fuse link to melt before lockout, since the seven seconds reclosing time of the restorers would permit too much cooling of this link. Some advantage might be gained from the adjustable feature if the restorers closer to the source of supply could be made to operate more slowly than the setting of the three-ampere restorer. But if the restorer at the extreme end is set at or close to the maximum tripping current and time delay, the desired number of restorers to be connected in series may restrict the permissible lower setting of the restorers closer to the source of supply.

The preceding illustration uses the three-ampere restorer as an example. However, other factors such as a "minimum fusing practice of five or eight amperes" at the transformer primary winding would preclude the use of a three-ampere restorer. Instead, a six-ampere restorer would be required with minimum 200 per cent and 250 per cent current pickup for five- and eight-ampere fuses respectively and with the maximum time delay. Transformers with internal fuses, especially the earlier product, might necessitate adjusting even 20- or 25-ampere restorers for maximum current and time-delay settings. Service experience has proved repeatedly that long-time outages amounting to as high as 12 or more hours may be anticipated if the transformer fuses and line-sectionalizing equipment are not co-ordinated properly.

In addition, economic considerations may dictate the use of single-element fuse cutouts at the branches. At many such installations fuse links will be employed which are rated higher than those just considered for transformer protection, thereby affecting the settings of higher-rated transformers. The AIEE papers, "Relative Value of Different Types of Overcurrent Protection for Distribution Circuits"[2] and "Relative Expense for Service Restoration With Different Types of Overcurrent Protection for Distribution Circuits",[3] as presented at the 1942 winter and summer conventions respectively indicate that in the general case single-element fuses provide the major portion of the improvement in service continuity that is obtainable by branch protection. Very little additional improvement can be secured by the use of either reclosing fuses or automatic-resetting reclosers or restorers. The savings in restoration expense were not proportional to the difference in initial cost of reclosing equipment over the low-cost single element fuses. This proportion would be further out of line if the adjustment feature added anything to the cost of the restorer.

It would appear, therefore, that the impracticability of making settings in the field and the application problems involved in co-ordination with fuses located farther out on the line definitely limit the advantage of the adjustability of the restorers' time-current characteristics.

If Mr. Tugby has any suggestions which will assist in making greater use of the adjustment feature of the restorer, they will be of value to those making system-co-ordination studies for overcurrent protection.

REFERENCES

1. TRENDS IN DISTRIBUTION OVERCURRENT PROTECTION, G. F. Lincks, P. E. Benner. AIEE TRANSACTIONS, volume 56, 1937, January section, pages 138–52.

2. RELATIVE VALUE OF DIFFERENT TYPES OF OVERCURRENT PROTECTION FOR DISTRIBUTION CIRCUITS, G. F. Lincks. AIEE TRANSACTIONS, volume 61, 1942, January section, pages 19–25.

3. RELATIVE EXPENSE FOR SERVICE RESTORATION WITH DIFFERENT TYPES OF OVERCURRENT PROTECTION FOR DISTRIBUTION CIRCUITS, G. F. Lincks, C. R. Craig. AIEE TRANSACTIONS, volume 61, 1942, November section, pages 813–21.

E. E. Tugby: Mr. Lincks apparently misinterpreted some passages of the paper when he assumes that it would be necessary to "throw short circuits on a distribution system" to determine restorer adjustments. It is customary to make system short-circuit studies before attempting any kind of co-ordination work, and the computed values are used directly for restorer settings.

On a test setup with five restorers in cascade, all operated 100 per cent correct after having been adjusted by lever setting.

Interpolation should be sufficiently accurate, if three time-current curves are available: namely, maximum, intermediate, and minimum time delay, with calibrated identification of the related points on the time scale.

Selective tripping is assured if time-delay settings between adjacent restorers of the same rating are 45 cycles or more apart. On 30-cycle settings occasional interference was observed because of manufacturing variations.

The assumption made by Mr. Lincks that installation of service restorers is only justifiable in certain highly loaded feeders does not stand up under practical scrutiny. The cost of replacing blown fuses is high no matter what the fuse rating.

Any computation of the economic justification of restorer installation on the basis of feeder size or length or on the basis of service improvement alone, without consideration of maintenance savings, must lead to erroneous results.

No one system condition can be labeled standard, and each requires special study, as any operating man knows only too well. That certain system setups exist which do not justify restorer installations is not disputed. In the majority of cases, however, their installation improves not only service continuity but also reduces maintenance costs, thereby justifying their application even if a moderate revision of fusing practice should be necessary in isolated cases.

History of A-C Wave Form, Its Determination and Standardization

Discussion and author's closure of paper 42-152 by Frederick Bedell, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 864–8.

P. W. Shill (British Columbia Electric Railway Company Limited, Vancouver, B. C., Canada): Figure 1 of this discussion is a copy of an old oscillogram which is in the writer's possession. It was taken in the year 1908 at the City and Guilds of London Technical College, Finsbury, London, England, by Doctor Silvanus P. Thompson while demonstrating to his class the phase-angle shift of the current with respect to the voltage of a circuit under conditions of bad power factor.

Strangely enough Doctor Thompson was the author referred to by the author of this paper in reference 4.

The oscillogram, as can be seen by a close examination of Figure 1, was made on an "Ondographe Hospitalier." To the best of the writer's knowledge Hospitalier brought out this instrument in 1898. Quite probably it was the "electric curve tracer" which is referred to in the author's reference 17, to which is assigned also the date 1898.

For the benefit of those not familiar with the instrument, a brief description follows: The chart shown in Figure 1 was fastened around a brass drum. This drum was driven at a constant slow speed by a synchronous motor. The motor also drove a gear train which moved a contact around at another constant slow speed. A second contact was attached to the synchronous motor shaft. It can be seen readily that the two contacts would close the instrument circuit instantaneously, once for each revolution of the motor shaft, but never twice in succession at the same point on the a-c wave. This caused a pen, quite similar to the ones we have now on graphic instruments, to move backward and forward across the chart following the successive impulses received from the current through the advancing contacts. The wave traces have the same type of radial distortion with which we are quite familiar today on some types of graphic instruments; so that the chart was printed with one set of co-ordinate lines drawn radially, as can be seen.

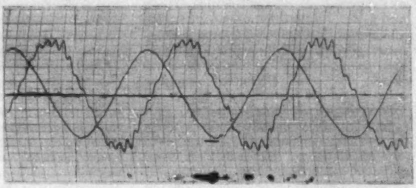


Figure 1. Oscillogram taken in London in 1908 on Ondographe Hospitalier.

This instrument represents an intermediate stage in harmonic analysis, coming as it does between the point-by-point plotting method and the modern reflecting and cathode-ray oscillographs. It was in reality a direct mechanical means of obtaining quickly a trace of the wave form by an automatic point by point record.

In Figure 1 the wave with the very pronounced harmonics is the voltage wave. The almost pure sine wave is the current wave. Time moves from right to left on the chart. These waves were produced from the power supplied to the school by the electricity-supply company operating in that area.

By comparing this voltage wave with one produced by any modern generator, one can readily appreciate the improvement that has been made in generator design, and, therefore, what we owe to the science of harmonic analysis in this field. The year in which the generator which supplied these waves was built is not known to the writer, beyond the fact that it was at some time in the eighteen nineties.

Further, an examination of the general shape of the voltage wave explains why the controversy raged over the so-called "sine-wave assumption." Possibly, when considering the machines in use in the early days, the opponents of the assumption were right. However, one would hardly care to take such a stand today when considering our modern machines. It will also be noticed that if the current wave be corrected for radial distortion it has a more sloping front and a steeper back than a pure sine wave.

Today we are in the habit of considering apparatuses with magnetic cores as producers of harmonics. The current wave was taken from a current passing through an almost pure reactance, as can be seen by the phase angle shift. A comparison of the harmonics appearing in the voltage wave and the current wave would indicate that when the harmonics of a voltage impressed on a circuit exceed a certain magnitude with respect to the fundamental wave, apparatuses with magnetic cores act as chokes or filters for harmonics in the current.

Such wave forms if put out by generators today would certainly cause untold trouble on communication circuits. It so happens that the gentleman who for a time was chief engineer of the telephone company operating in the area which was served by this system was the father of the writer of this discussion. Many a time the father has told his son of the terrible troubles he had with electric-power interference on his system, especially in the days when he was still struggling to try to operate single metallic ground-return telephone circuits.

The writer fully appreciates the great service rendered by the author of the paper to all those seriously interested in the subject of a-c wave form by producing this paper and can only hope that others, with equal knowledge and background in their respective branches of electrical-engineering science as the author has in his, will render the profession equal services by preparing papers of comparable scope and quality. As he says in his concluding sentence, the author has not attempted to tell us the whole story. He has, however, given us references which must prove to be invaluable. Those published from 30 to 50 years ago might otherwise have been in danger of dropping out of sight altogether.

Charles F. Scott (Yale University, New Haven, Conn.): To the significant history which Dr. Bedell recounts he himself has been a notable contributor, as is attested by the 12 references to his own work in the bibliography. To the story of the 1890 period I am able to add a few items relating to the Telluride power transmission and to the determination of wave form. Some of my own work and also that of Lamme and of Mershon are involved.

The wave form of the early commercial alternator with surface winding on a drum armature approximated the sine wave. The first Westinghouse toothed armature (one T-shaped tooth per field pole) produced a wave which caused less transformer-iron loss than that of the smooth armature. I undertook to measure the new wave form—the first such measurement at the Westinghouse factory. I used the contact method but discarded the ordinarily used condenser and ballistic galvanometer and made direct readings by a Cardew (hot-wire) voltmeter. The instrument required normally about a quarter of an ampere for a fair deflection; it took enormously more when there was one contact per revolution of a 12-inch disk, as evidenced by a long soft arc. But the resulting curve was very definite; there was a flat zero for about five per cent of the half cycle and a double peaked maximum with a depressed *V* between the peaks, and the sides were fairly straight lines. The shape of the curve was responsive to the configuration of poles and teeth; thus, as the width of the tooth was greater than the pole, all of the field flux passed into the tooth through a slight angle, and the unchanging flux resulted in a prolonged "flat" zero in the curve. Lamme at once commented that he could change the wave form by reshaping the tooth.

One day Mr. Schmid said to me, "There are two new machines; test them and see what they will do." Then began my real education on synchronous operation; I varied motor field current from normal to zero and then did the same with the generator, observing effects on line voltage and current. I loaded the motor (about 40 horsepower) and made other tests. A few months later, about September 1, 1890, Mr. Schmid said that a 100-horsepower transmission had been sold to L. L. Nunn at Telluride, and we would use two alternators like those made for the transmission from Willamette Falls to Portland, Oreg. My job was to specify the equipment to accompany the two alternators. And without precedent I proceeded to devise a method and apparatus for motor starting and loading, the method of excitation of generator and motor, also to decide upon switches and indicating instruments and prepare instructions for operating a new type of plant. The only justification for some of the complication involved is that it worked. Dr. Bedell quotes P. N. Nunn as saying that the generator and motor were "identical to assure identity of wave form." Possibly, but I never heard of it until Mr. Nunn's explanation 40 years later. The Willamette–Portland and the Telluride transmissions were described by me in the Institute's first paper on Long-Distance Transmission for Light and Power.[1]

R. D. Mershon graduated from Ohio State University in 1890. I attended the commencement, and he told me of his method of measuring wave form. A few months later he developed it at the Westinghouse works. He placed in series with a contact a telephone receiver which responded with vigorous clicks. He introduced a d-c electromotive force which he adjusted until the click was suppressed. The d-c voltage required to just equal the contact voltage was read on a voltmeter. A new brush setting was then made, and the corresponding electromotive force at this point in the alternating wave again read on the voltmeter. This simple and satisfactory method was soon superseded by the oscillograph. Mershon described it in the *Electrical World* (1892, page 425).

Lamme calculated the wave form of the initial Niagara 5,000-horsepower generator and then measured it in a very original way. These he describes in a 12-page paper on "Early Work on First 5,000-Horsepower Alternators," prepared in 1920 at request of E. D. Adams and published in his "Niagara Power."[2] Under "New Method of Design" Lamme relates that I had prepared and showed him an iron filings picture of the magnetic field between the armature and field poles of the machine, made with the aid of a full-sized template. He continues "I believed I could plot the flux conditions by calculation. . . . The plotted field form corresponded to the electromotive force wave for one armature conductor . . . . The wave of other conductors (was the same but) displaced by an amount corresponding to the displacement of the armature conductors themselves . . . . I combined them to get the electromotive force wave of the machine.

"When the machine was set up for test, I arranged to wind a small wire cable around the external cylindrical field frame (about 12 feet in diameter), one end of this cable being attached to a small windlass, which was to be turned at a slow uniform rate to rotate the machine very slowly. The field was magnetized, and a d-c voltmeter was connected across the terminals of the armature winding with a view to taking direct readings at uniform intervals of time during the rotation. If the rotation were slow enough, the voltmeter needle would indicate in succession all the points in the electromotive force wave . . . . The measured wave coincided almost exactly with the predetermined calculation.

"Taking this as a basis, I worked on the method of design during my evenings for the next two or three years . . . . This developed method is the basis of the company's calculation system of today."

Dr. Bedell makes brief reference to William Stanley's pioneer work on the transformer. This is more fully described by Chesney and Scott as part of the Great Barrington demonstration.[3]

REFERENCES

1. LONG-DISTANCE TRANSMISSION FOR LIGHTING AND POWER, **Charles F. Scott.** AIEE TRANSACTIONS, volume 9, 1892, pages 425–42.

2. NIAGARA POWER, **E. C. Adams.** Volume II, page 409.

3. EARLY HISTORY OF THE A-C SYSTEM IN AMERICA, **C. C. Chesney, Charles F. Scott.** AIEE TRANSACTIONS, volume 55, 1936, March section, pages 228–33.

F. Bedell: We are indebted to Mr. Shill for bringing out several interesting points in his discussion. He has emphasized the troubles encountered on early telephone lines because of electric-power interference and has given us a good description of the Hospitalier Ondographe. The curves he has shown well illustrate the great irregularities that existed in the wave forms of early machines, to which attention has been called in the paper. Furthermore, his curves are evidence of the perfection attained in the delineation of wave forms by means of point-to-point curve tracers, which were highly developed and serviceable in their day but were soon outmoded on account of the inconvenience, as well as limited frequency range, of synchronous apparatus.

Irregularities even greater than those shown in these curves were frequently found in early machines. The increase in size of machines, with better design and standardization, has given us wave forms on power systems with remarkably small deviation from a true sine wave, conforming closely to our increasingly rigid standardization rules. One would hardly like to take a stand against the so-called sine assumption today, as Mr. Shill points out.

In the sine-wave assumption, however, it should be kept in mind that it is not assumed necessarily that current and electromotive forces are sine waves. It is assumed that they may be replaced by *equivalent* sine waves having the same rms values, displaced in phase so as to have the same power factor. The vector and circle diagrams based on this assumption have proved their usefulness for fifty years, even with wave forms departing widely from a sine wave. The proof of the pudding is in the eating.

It was early found that certain vector methods (as used, for example, in the once important three-voltmeter and three-ammeter methods for measuring power) were absolutely independent of wave form. It was further found that other methods with which we are all familiar, for example, the determination of performance characteristics by circle diagrams, gave results with engineering accuracy often better in fact than could be obtained by any other method, even though theoretically not free from error.

Such uses of vector and circle diagrams, employed in practically all our engineering projects, are too familiar to need elaboration, but let us take a particular example. In the predetermination of the regulation of a transformer from open-circuit and short-circuit tests, derived by vector methods based on the sine-wave assumption, the regulation is determined *more accurately* than it can be directly measured by commercial instruments. And this, too, in spite of the fact that the primary current of a transformer on open circuit is distorted necessarily, as we well know, by the effects of hysteresis!

An explanation of the fact that irregularities in wave form introduce such small error in vector diagrams based on the sine assumption may be found in the fact that, if fundamental quantities be represented by vectors in a plane, vectors representing harmonics would necessarily be at right angles thereto. This follows from the fact that no harmonic can have an inphase or power component with respect to the fundamental, for currents and voltages of different frequencies represent no power. Harmonics, compared with the fundamentals, are always small and, as the vectors by which they are represented are at right angles to the fundamental plane, their effect is still smaller and merely gives a slight distortion or warping to the plane-vector diagram by an amount too small to have significance. Studies in this direction will be found in reference 12 of the paper. The author has found the representation of alternating quantities by vectors in more than two dimensions useful in some special cases but not generally applicable without difficulty.

Although, as seen above, the validity of the sine-wave assumption and the usefulness of vector and circle diagrams based thereon do not depend upon having perfect or nearly perfect a-c wave forms, our efforts in standardization toward this end will be continued for engineering reasons, such as the effects upon insulation and communication, as discussed in the paper.

Some Air-Blast Circuit-Breaker Installations in Canada

Discussion and authors' closure of paper 42-157 by H. W. Haberl and R. A. Moore, presented at the AIEE Pacific Coast Convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 859–63.

E. W. Knapp (Shawinigan Water and Power Company, Montreal, Que., Canada): It is today a well-established fact that automatic circuit breakers with high-speed and high rupturing-capacity characteristics are required for electric-power systems. The development of air-blast circuit breakers offers considerable promise of adequately meeting these requirements in the future. During the past few years the Shawinigan Water and Power Company has assisted in this development in Canada by utilizing their power system for primary tests during the development period. These tests brought out certain defects and limitations, and, as a result, improvements have been effected. However, it is expected that this equipment will be subject to the usual "growing pains" but will improve with "time and experience." Operating experience to date has also revealed a number of weaknesses, and, in general, the necessary remedial measures have been taken, particularly in those cases where the cause of failure has been established definitely. Normal operating speeds are quite fast, and this feature should prove of considerable value, particularly on power-system networks where transient stability or power-arc damage present problems. It should be noted, however, that although the 69-kv design has been tested to 100 per cent of rated rupturing capacity, the 220-kv design has not been fully tested above 40 per cent of rated rupturing capacity.

Automatic reclosing of power circuits, following transient faults, is playing an important role in power-service improvements. It is quite probable that in the near future, this feature will be considered as an integral part of any system plan, along with relay protections and control. This feature may also be supplemented in special cases with single-phase switching. The air-blast circuit-breaker would appear to be ideally suited to this method of operation, caused by inherently high operating speeds. The authors have attempted to explain briefly a means of adapting the circuit breaker under consideration to automatic reclosing, but I do not fully understand the explanation. It is assumed that "extremely fast reclosing" means 0.35 second or less total time. In this particular design it is not possible to reclose the interrupter contacts while maintaining a high pressure in the arcing chamber. This tends to increase the possibility of restrike across the interrupter contacts, unless the isolator switch is at least partially open before the air blast is stopped. The necessity of operating the isolating switch might result in considerable delay in the automatic reclosing operation, unless the control arrangement is revised. Further explanation of the scheme, together with operating times should prove of interest.

L. B. Chubbuck: For discussion, see item elsewhere on this page.

H. W. Haberl and **R. A. Moore:** The authors of this paper gratefully acknowledge the interest shown by power-company engineers and especially those who have presented written discussions. A number of interesting points have been brought out by the discussers which we believe should be clarified. As most of the discussion is along parallel lines, our closing remarks will be directed toward the discussion as a whole.

Certain tests reported are referred to as "synthetic tests." They are obviously not synthetic tests. Synthetic means artificial. The word "synthetic" has been applied to the method used by large manufacturing companies to test circuit breakers beyond the short-circuit capacity of the available generating system. In the tests reported by the authors, the kilovolt-amperes interrupted by the breaker were, in every case, identically the same kilovolt-amperes which flowed into the fault from the line and generating system. In other words, the system was carrying a load of several hundred thousand kilowatts, just before, during, and just after the short circuit interrupted by the breaker, the conditions being the same as when a breaker clears a fault in service. There was obviously nothing "synthetic" about such tests.

Perhaps the intention was merely to question how much more interrupting capacity the 230-kv breaker has in excess of 1,000,000 kva interrupted in the tests at 230 kv. The author's paper reports other tests on the single element of the 230-kv breaker interrupting over 5,000 amperes at about 60 kv (phase-to-phase across a 60-kv line carrying full load), such interruption being somewhat over 300,000 kva on the single element. The 230-kv breaker has six of these elements in series per pole, making 18 elements in the three-pole breaker. Multiplying 300,000 by 18 gives 5,400,000 kva. The authors consider these tests to prove adequately that the 230-kv breaker has an interrupting capacity in excess of its rating of 2,500,000 kva.

Pistons and cylinders are mentioned by the discussers as possible sources of trouble in air-blast breakers. Pistons and cylinders operated by compressed air were applied to brakes on railway trains many years ago (by the Westinghouse company). Air brakes are very dependable.

One discusser refers to the noise of air-blast breakers and is concerned about its effect on the nerves of operators. A 230-kv air-blast breaker opening a short circuit makes a noise about like the exhaust of a standard steam locomotive starting a heavy train, which does not appear to bother the crew working within 40 feet of the blast. The operator in a station works at a control board usually 100 feet or more away from the breakers and within enclosing walls. He is not at all disturbed.

The discussers refer to high-speed reclosing in the high-voltage classes of breakers described. The 138-kv breaker will reclose in 20 cycles and the 230-kv breaker in 25 cycles. This timing can be decreased if a specific application warrants. However, the authors feel that if the breaker reclosing time was decreased to less than 15 cycles, additional delay in the form of control would need to be added, as the arc products on transmission-line faults are not blown away in less than that time, and on single-phase switching the arc on the de-energized phase has been known to be maintained for an appreciable time by the induced voltage produced from the sound phases.

To obtain successive shots in air-blast breakers it is only necessary to add storage capacity. Most types of the air-blast breakers described have three operations stored in their individual tanks, and with the aid of storage capacity this number of operations can be increased at will. The moving parts of certain of these air-blast breakers in the extreme high-voltage class are very accessible, and contact inspection on the 230-kv air-blast breakers can be made within 20 minutes. Comparing this with a 230-kv oil-breaker contact inspection time which takes hours, we believe the advantages are in favor of the air breaker.

High-voltage air-blast breakers can be safely mounted indoors where a large quantity of stored inflammable material is a potential fire hazard; this indeed is an advantage for the air-blast breakers.

The arcing time of the air-blast breaker is independent of the magnitude or phase angle of the currents involved. This is indeed an advantage over the modern oil breaker where clearing times in the neighborhood of six cycles can be obtained where the current is of sufficient magnitude to have the deionization or the explosion principle applied. However, on small currents of low power factor such as the charging current of transmission lines the air-blast-breaker arcing time is the same as with the heavy currents. The modern oil breaker under charging current conditions has been known to fail to interrupt where the magnitude of the current is less than the amount required for the explosion or deionization principle to apply.

Design and Operation of High-Voltage Axial Air-Blast Circuit Breakers

Discussion and author's closure of paper 42-155 by Armin K. Leuthold, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 869–75.

L. B. Chubbuck (Canadian Westinghouse Company, Ltd., Hamilton, Ont., Canada): As the organization with which the writer is connected manufactures both air-blast and oil breakers, the writer feels free to offer a few comments on both types.

Low-voltage air-blast breakers are definitely free of oil hazard, though the high-voltage breakers usually include oil-immersed current transformers in large fragile porcelains.

While failures have occurred in the past with oil circuit breakers, modern oil breakers fully tested and used within their rating have an excellent record under heavy everyday service. Their arc-rupturing devices are very effective, and all interruption occurs quietly within heavy grounded metal tanks. Modern high-voltage and low-voltage oil breakers are very compact, a 15-kv, 1,000,000-kva metal-clad structure running a little over 36 inch centers.

Air-blast breakers have also had their failures. This type of breaker has short *arcing* time, though any longer time extending until the series disconnecting switch opens would be serious. The *over-all* opening time (the important feature) averages a little less than for commercial oil breakers but can be met by high-speed oil breakers.

The opening of an air-blast breaker is accompanied by considerable noise and flame, rather nerve-racking to an adjacent operator.

Because of limited air supply, an air breaker may be safely operated only two or three times successively. The operating high-pressure piping, valves, relays, and cylinders are rather an involved precision equipment and require high-grade maintenance, particularly for outdoor service. Some of these moving parts are rather inaccessible, enclosed in high porcelains. At Arvida the 154-kv breakers are mounted *indoor*.

We note that, because of lack of suitable high-power laboratories, the authors can provide only "synthetic" test proof of their larger ratings, based on a few system tests. We do not consider such data conclusive. During the development of our air-blast breakers, thousands of tests were made on all parts, and complete poles, to full rating, and overvoltage, in a laboratory of ample capacity.

In conclusion, the authors have given us two very interesting and instructive papers covering the development of this new equipment in Canada.

A. K. Leuthold: High-voltage air-blast circuit breakers can be equipped either with, or without, oil-immersed current transformers, but even in the former case, which is often preferred from the economical standpoint, the oil hazard, compared to an oil circuit breaker, is very small, because no arc interruption has to be carried out under oil.

The low-voltage air-blast breakers are also very compact in design. For example: seven-kilovolt breakers require a space from center to center of approximately 34 inches and 13-kv breakers of approximately 42 inches.

Neither with our outdoor nor our indoor type of air-blast circuit breakers can flames be noticed at the breaker exhaust openings. The noise from an air-blast breaker erected outdoors is not objectionably loud, while the noise from a breaker erected indoors is similar to that of a modern high-speed d-c breaker when interrupting a heavy short circuit.

Breakers equipped with automatic reclosing devices can open, reclose, and open a second time in less than 20 cycles without requiring refilling from the main air-storage tank. After two breaker opening operations, automatic refilling takes place from the main storage tank in approximately four to six seconds, and further switching operations can then be carried out.

Experience has shown that the various pistons, applied for the fully automatic operation of the breaker, operate very satisfactorily, even under very low temperature. On outdoor breakers, this piston assembly is carefully protected against snow and rain and, if desired, can be kept above freezing point by means of small heaters similar to those applied on the motor-control pedestals of outdoor oil circuit breakers.

The arcing chambers of high-voltage breakers are designed with inspection openings or other devices, which enable the movable arcing contacts to be inspected, or even replaced if necessary, in a few minutes. At Arvida, the 150-kv air-blast circuit breakers are installed indoors, because they had to replace two oil circuit breakers previously erected in the same building where two 75,000-kva transformer banks are located. This customer preferred to eliminate the oil hazard by applying two air-blast circuit breakers in place of the previous oil circuit breakers.

The high-voltage air-blast circuit breakers equipped with potential-controlled multiple arc breaks, are tested in our large breaker test plant by breaking the partial interrupting capacity with one arc break only, which enables one to judge what the performance of this breaker will be under full-rated interrupting capacity. A definite conclusion can be drawn from these tests because the voltage distribution between the individual arc breaks is equal on account of the potential control of the arc breaks, which is achieved by means of small capacities.

Through a series of developments, the manufacturer has achieved a very high performance from the high-voltage air-blast circuit breakers which, besides the short interrupting time, enables automatic single- or three-phase high-speed reclosure to be carried out.

This insight into the latest breaker developments, including testing and operating experiences, should induce and encourage power companies to collect experience on their own networks by applying this new type of circuit breaker in order to gain valuable operating data for further improvement in the protection and stability of their high-voltage transmission lines and power-distribution systems.

Design, Manufacture, and Installation of 120-Kv Oil-Filled Cables in Canada

Discussion and authors' closure of paper 42-148 by D. M. Farnham and O. W. Titus, presented at the AIEE Pacific Coast convention, Vancouver, B. C., Canada, September 9–11, 1942, and published in AIEE TRANSACTIONS, 1942, December section, pages 881–8.

A. H. Frampton (The Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): Under the heading "System," section 2, the authors state that cable connections were used between the step-down transformer terminals and the station bus "as providing a substantial saving in steel structure, greater compactness, and better appearance, and much less exposed high-voltage circuit." In the apparently restricted space available at the site of station *B*, the value of the second and fourth points may be granted. However, it is difficult to accept the inference that the saving of the cost of the additional structures required to accommodate open connections to the transformers had a controlling influence in the decision to use cable at this point. The acceptance of this inference is made more difficult by the assumptions that may be drawn from the statements under "System," section 3, that the line route had to be shortened to approximately one third, and the acquisition of expensive right of way avoided in order to justify the main cable section as against the alternative overhead construction.

It would be interesting if the authors could expand on these points. Perhaps they would be prepared to suggest a rule-of-thumb figure as representing the approximate value of right-of-way saving necessary to justify the adoption of cable constuction.

From Figure 2 of the paper it is presumed that this cable system operates in parallel with 60-kv overhead circuits. From the discussion, it is inferred that varying amounts of power are required to be transmitted between stations *A* and *C*, in order to maintain the most efficient loading on certain generating sources. As these parallel paths would obviously have different impedance characteristics, it would appear that the maintenance of a proper subdivision of load between the parallel paths would occasion difficulties. It would be of interest to know whether any special operating or other procedures have been established in this connection.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The authors are to be complimented for the thoroughness of their past investigations of design and installation details and of their present investigations of operating matters. If, as in this case, engineers devote some time and efforts to the study of all details, problems relating to oil-filled cable systems become simple.

Records of duct temperatures, and so forth, as they are planned, should be of considerable value in obtaining the maximum allowable load rating for the cable in the actual installation. As load ratings are most important, it would be of interest to have more detailed information given on the methods being used by the authors and on any special points relating to loading.

According to Table I of the paper, 70 degrees centigrade is considered the maximum allowable copper temperature in accordance with the Association of Edison Illuminating Companies specifications. As indicated in my recent AIEE paper on "Load Ratings of Cable—II,"[1] such cable could be operated safely at higher temperatures. If this is attempted, one, of course, must watch out for limitations, such as reservoir capacity or induced sheath potentials.

The Canadian-developed reservoirs shown in Figure 6 of the paper are of interest. The arrangement may be less expensive than those used in the United States. What material is used for the cell walls?

The design of the conductor with flat strands reduces considerably the free oil space between conductor and paper as compared with conductor with ordinary round strands. Have any test data or calculations

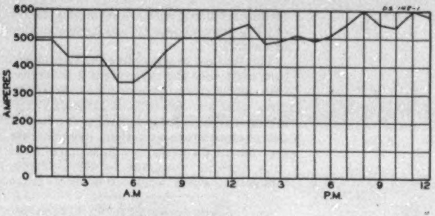


Figure 1. Typical daily load curve for 120-kv cable

Sheath currents not flowing. $T_g = 20$ degrees centigrade

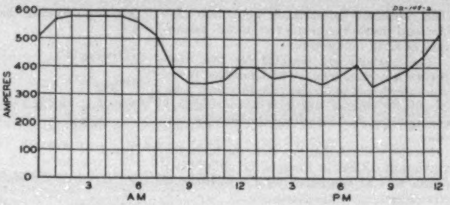


Figure 2. Typical daily load curve for 120-kv cable

Sheath currents flowing. $T_g = 10$ degrees centigrade

been made on the relation of the maximum stresses for these two types of conductors?

The average coefficients of friction of 0.25 to 0.30 for pulling cable into the ducts are less than usually obtained. We have found higher values in pulling cables into precast-concrete round ducts, using a heavy grease on the cable. While the grease is not a very good lubricant, as demonstrated by our tests, it is used mainly to retard or prevent corrosion of the sheath. It seems to me that the main considerations in determining maximum allowable pulling strains are possible harmful effects on the conductor, on the rest of the material inside the sheath, and on the outer surface of the sheath.

In general, the methods and carefulness used on the whole project described in the paper were apparently of such a nature as to permit high allowable loading and still obtain a satisfactory operating record over a period of decades.

REFERENCE

1. LOAD RATINGS OF CABLE—II, Herman Halperin. AIEE TRANSACTIONS, volume 61, 1942, pages 930–42.

D. M. Farnham and **O. W. Titus:** Mr. Frampton feels that the saving of steel alone would not be a controlling factor in the use of cable from the station bus to the transformer. All projects, of course, contain a number of elements of cost. The sum total of all elements is the determinant. In this instance steel was one substantial element. This was in some measure due to the shape of the premises which made it desirable, even necessary, to place the high-voltage outdoor structure along the south side of the property from east to west, using the entire width. It was, therefore, necessary to place the four transformer banks in a row running from east to west and north of the high-voltage structure. The steelwork required to feed these transformers by overhead construction, plus the cost of the overhead construction, was estimated to be higher than the cost of the oil-filled cables. Part of this advantage may have resulted from a reasonable cable-installation cost, since other oil-filled cables and equipment were being installed at the time in the immediate vicinity. Appearance and "dead-front" characteristics might also be appraised as contributing factors if that were necessary.

Our opinion is that for short runs, as encountered in this case, the problem is worth investigating thoroughly before a decision is reached to use either the overhead or underground method.

As underground costs vary differently from those for overhead with difference in load and raw-material costs, we would not care to set up even an approximate rule-of-thumb figure such as Mr. Frampton suggests, particularly at times such as these.

Mr. Frampton's question as to how the present 60-kv ring system is made to carry its share of load when its impedance is so much greater than that of the cable is well taken.

This difficulty was very cheaply and effectively overcome by splitting the high-voltage bus at station *C* and connecting the cable to one pair of overhead lines and the ring to the other pair. These pairs of lines were paralleled approximately 90 miles away.

Table I. Maximum Allowable Current (Amperes)

Cable (Thousands of Circular Mils)	Allowable Copper Temperature (Degrees Centigrade)	Cables in Duct Bank	Sheath Currents		Ground Temperature (Degrees Centigrade)			
			Flowing	Not Flowing	20	15	10	5
300	70	3	X		382	403	422	442
300	70	3		X	412	434	456	476
300	70	6	X		348	368	387	405
300	70	6		X	372	393	413	432
300	80	6	X		380	398	415	431
650	70	3	X		528	558	585	612
650	80	3	X		576	602	627	652
650	70	3		X	635	670	703	736
650	80	3		X	693	724	754	783

Figure 3

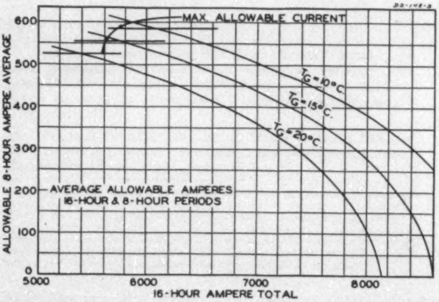
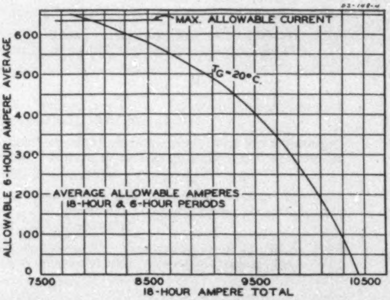


Figure 4



The combined impedances were such that the load divided correctly to take full advantage of both tie circuits.

The authors are grateful for Mr. Halperin's kind remarks. The Canadian reservoirs employed plain steel diaphragms, principally because of wartime difficulty in securing stainless steel within the time permissible.

No tests or calculations were made on the maximum dielectric stresses for the shaped conductors as compared with conventional stranding.

During the development of the pulling eyes, tension tests were made on conductors, paper-wrapped but without the lead sheath. These tests determined both the effectiveness of the pulling eye and the maximum tension on the conductor permissible without permanent deformation. In the field, pulling tensions were kept below these values in all cases. Any added strength caused by the lead sheath was regarded only as added factor of safety.

In replying to the second paragraph of Mr. Halperin's discussion, the listed calculations are samples of those used in establishing the loading schedule of the cables:

Allowable peak load $=I$ at loss factor stated

Loss factor (by design) $=62^1/_2$ per cent $=\dfrac{(I\,\text{avg})^2R}{I^2R}$

Daily basis 100 per cent loss $=I^2\times R\times 24$

$62^1/_2$ per cent loss factor $=$

$$\frac{\text{avg}\,(I^2)\times R\times 24}{I^2\times R\times 24}=0.625$$

$$\text{or}\ \frac{\text{avg}\,(I^2)}{I^2}=0.625$$

or $\text{avg}\,(I^2)=0.625I^2$

For any period of the day
Let

$Ix^2=\text{avg}\,(I^2)$

and

$Ix^2=\text{avg}\,(I^2)$ for the remainder of the 24 hours

Then

$$(Ix^2\times X\ \text{hours})+(Iy^2\times Y\ \text{hours})=0.625I^2\times 24$$

or

$$I^2y=\frac{(0.625\times I^2\times 24)-XIx^2}{Y}$$

$$=0.625\times I^2\times\frac{24}{y}-\frac{x}{y}Ix^2$$

Take X as 18 hours and y as 6 hours then

$$I_6=\sqrt{(0.625\times I^2\times 4-3I_{18}^2)}$$

(a). Sheath currents flowing, $T_G=20$ degrees centigrade. $T_c=70$ degrees centigrade $-I=528$.

(b). Sheath currents not flowing $T_G=20$ degrees centigrade. $T_c=70$ degrees centigrade $-I=635$.

(For allowable maximum loading see Table I of this discussion.)

For (a) $I_6=\sqrt{(0.625\times 528^2\times 4-3I_{18}^2)}=\sqrt{3}(232,320-I_{18}^2)$. For ($b$) $I_6=\sqrt{(0.625\times 635^2\times 4-3I_{18}^2)}=\sqrt{3}(336,021-I_{18}^2)$.

Curves in Figures 1, 2, 3, and 4 of this discussion are made on the basis of these or similar calculations.

This allows cable loadings to a maximum for 18 hours each day, and from these total amperes a load rating can be had for the succeeding six hours, with a resultant loss factor of $62^1/_2$ per cent (see typical loading curves herewith).

This method of scheduling the loading has been used since the cables went into service, with careful attention being paid to temperatures and duct-mouth movements. The idle duct (that is a duct adjacent to a loaded duct) temperature at various locations has been recorded and shows a variation of from $38^1/_2$ degrees centigrade to $40^1/_2$ degrees centigrade, which would indicate a fairly constant copper temperature.

Duct-mouth movements have been recorded both daily and weekly. The daily movements are in the nature of one eighth of an inch or less, while the weekly movement is in the order of three eighths of an inch. Outages or long periods of light loads increase these movements considerably.

The flow of oil to and from reservoirs has been recorded, the daily or even weekly quantity has been approximately one gallon per phase per section, this of course is divided on the two ends. The seasonal variation has so far been only a transfer from cables to reservoirs and has amounted to about 13 gallons per phase per section. This quantity would have to be corrected for the temperature change of the reservoir immersion oil amounting to 0.085 gallon per degree centigrade. The temperature change has been 45 degrees centigrade which is equivalent to 3.8 gallons; thus the actual oil expelled because of seasonal change has been about $9^1/_2$ gallons per phase per section.

Up to the present sufficient power has been handled by using a maximum copper temperature of 70 degrees centigrade. However, if the need arose, there would be little hesitation in increasing this limit to 80 degrees centigrade or even higher. We are now confident that oil demands would not be excessive for the present reservoirs even at extreme temperatures.

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Fuses and Fuse Protection

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Section A

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7. Electricity in Industry, Report of the Inspectors of Factories and Workshops for 1931. *Electrical Review* (London), volume 111, July 1, 1932, page 22.

8. Electricity in Mining; Motors in Use, Accidents Attributable to Power, Exemptions From Regulations, **J. A. B. Horsley.** *Electrician* (London), October 28, 1932, volume 109, page 557.

9. Fifth International Medical Congress for Industrial Accidents and the Prevention and Treatment of Accidents Due to Electricity. *International Labour Review* (London), volume 25, January 1932, pages 102–03.

10. Responsibility for Electrical Accidents. *Electrical Review* (London), volume 111, December 16, 1932, pages 896–7.

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11. Electric Shock and Burns Cause Much Lost Time. *National Safety News*, volume 28, August 1933, page 36.

12. Electrical Accidents, 1932. *Engineer* (London), volume 156, August 25, 1933, page 181.

13. Electricity and Its Safe Use in Everyday Life, **A. C. Cramb.** *Electrician* (London), volume 110, June 2, 1933, pages 699–700; discussion, page 700.

14. Electricity in Factories. *Electrical Review* (London), volume 113, August 11, 1933, page 186.

15. Electricity Not Dangerous. *Electrical World*, volume 102, October 14, 1933, page 510.

16. Hazards of Defective Cords and Fuses, **H. M. Naylor.** *Safety Engineering*, volume 66, November 1933, page 174.

17. Liability to Electric Shock, **Uzac.** *Electrical Review* (London), volume 113, December 1, 1933, page 770.

18. Water-Jet Conductivity. *Electrical Review* (London), volume 113, July 14, 1933, page 48. Conductivity of fire-hose streams.

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19. Electrical Accidents—A Study of Recent Statistics, **J. B. Harris.** *Electrical Review* (London), volume 115, December 7, 1934, page 782.

20. Electrical Injuries: Interpretation of Field Notes—II. **Wills Maclachlan.** *Journal of Industrial Hygiene*, volume 16, 1934, page 52.

21. Where Low Voltages Are Hazardous, **E. W. Gorry.** *National Safety News*, volume 29, March 1934, pages 21–2.

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22. Electric Shocks From Earthed Apparatus, **H. G. Taylor.** *World Power* (London), volume 23, April 1935, pages 170–4. Report F/T82 of the British Electrical and Allied Industries Research Association.

23. Facts About Low-Voltage Fatalities, **A. Maxwell.** Edison Electric Institute *Bulletin*, volume 3, December 1935, page 452.

24. Hazards of Hay Derricks Contacting Electric Lines, **G. I. Drennan.** *Electrical West*, volume 74, June 1935, pages 146–9.

25. Spraying Water on Electrically Charged Conductors (in German), **H. Zaduk.** Verein Deutscher Ingenieure *Zeitschrift* (Berlin), volume 79, November 30, 1935, page 1452.

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26. Electrical Accidents in Factories. *Engineering* (London), volume 142, August 21, 1936, pages 204–05.

27. Electrical Accidents in Industry, 1935. *Electrical Review* (London), volume 119, July 31, 1936, page 142.

28. Electrical Hazards in Industry, **J. V. Peacock.** *Safety Engineering*, volume 72, July 1936, page 17.

29. Electricity in Collieries—Causes of Accidents, **J. A. B. Horsley.** *Electrical Review* (London), volume 119, November 20, 1936, page 708.

30. Static Hazards Met in Solvent Handling, **J. T. Morris.** *Chemical and Metallurgical Engineering*, volume 43, September 1936, pages 484–5.

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31. D-C or A-C? *National Safety News*, volume 35, March 1937, pages 192–3.

32. Electrical Accidents, **W. B. Kouwenhoven.** AIEE *Transactions*, volume 56, 1937, (September section), pages 1077–8.

33. Electrical Accidents (in French), **Uzac.** Société Française des Electriciens *Bulletin* (Malakoff), volume 7, November 1937, pages 1165–76.

34. Electrical Accidents in Mines—Abstract of Report for 1936, **J. A. B. Horsley.** *Engineer* (London), volume 164, September 24, 1937, pages 344–5.

This bibliography on electrical safety was prepared by the AIEE committee on safety and includes a list of the applicable standards, specifications, and safety codes. The items are divided into sections according to subject matter. In sections A, B, D and E the entries are numbered consecutively, subdivided by years beginning with the earliest (1930), and arranged alphabetically by title within each year, the first significant word of the title determining its alphabetical position. In the section on safety standards (C) entries are subdivided according to source. The bibliography was published in pamphlet form by the AIEE during 1942.

35. Electricity in Factories—an Analysis of Accidents and Their Causes, **H. W. Swann.** *Electrical Review* (London), volume 121, October 1, 1937, page 432.

36. Electricity in Factories and Workshops; Chief Inspector's Report for 1936. *Electrical Review* (London), volume 121, July 30, 1937, pages 142–3.

37. High Tension Wires Kill. *Rock Products*, volume 40, August 1937, pages 66–7. Accident in cement plant.

38. Low Voltages Are Not Harmless—Pronounced Hazards May Be Found in Industrial Wiring, **J. Roseveaar.** *National Safety News*, volume 36, July 1937, pages 19–20; (condensed) *Mass Transportation*, volume 33, July 1937, pages 205–06.

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39. Electrical Installation Systems and Their Relation to Personal Safety and Fire Risk, **A. L. Whittenham.** *Electrician* (London), volume 121, December 2, 1938, page 662; *Electrical Review* (London), volume 123, December 2, 1938, page 795.

40. Electrical Accidents—Analysis of 1937 Figures. *Electrician* (London), volume 121, November 11, 1938, page 576.

41. Electrical Accidents and Their Causes, **H. W. Swann.** *Engineering* (London), volume 146, December 16, 1938, page 708.

42. Electricity in Factories—Electrical Accidents and Their Causes for 1937, **H. W. Swann.** *Electrical Review* (London), volume 123, November 11, 1938, page 683.

43. Electricity in the Bathroom. *Safety Engineering*, volume 75, June 1938, pages 33–4.

44. Explosive Gas—Electric Spark. *Safety Engineering*, volume 76, July 1938, page 37. Dangerous practice of using unapproved electric portable extension lamps where there are gases or vapors.

45. Housewife Electrocuted. *Safety Engineering*, volume 76 October 1938, page 73. Defective cord.

46. Look Out for "Hot" Wires. *Rock Products*, volume 41, April 1938, page 49.

1939

47. Accident Prevention; Improvement During 1938. *Electrical Review* (London), volume 125, October 20, 1939, pages 523–4.

48. Electricity in Mines; Annual Report of Electrical Inspector of Mines, **J. A. B. Horsley.** *Electrical Review* (London), volume 125, October 20, 1939, page 520.

49. Electricity Intrigues Me, **J. C. Wilson.** *National Safety News*, volume 39, May 1939, pages 32 and 60. Study of electric shock.

50. Fatal Shock From Earthing Plate. *Electrical Review* (London), volume 124, June 9, 1939, page 846.

51. Is the Spray Treatment of Fruit Trees Dangerous When Done in the Vicinity of Overhead Electric Lines? (in French), Association Suisse des Electriciens *Bulletin* (Zurich), volume 30, February 3, 1939, pages 73–5.

52. Lightning Fatalities. *Electrician* (London), volume 123, September 1, 1939, page 246.

53. 110–120; Danger From Defective Electrical Appliances. *Safety Engineering*, volume 78, November 1939, page 24.

1940

54. Electrical Accidents in Switzerland During 1939 (in French), **F. Sibler.** Association Suisse des Electriciens *Bulletin* (Zurich), volume 31, May 31, 1940, pages 249–54.

55. Liability for Accident; Points Raised by a Linesman's Death. *Electrical Review* (London), volume 127, August 2, 1940, page 87.

1941

56. Electric Fence, **C. F. Dalziel, J. R. Burch.** *Agricultural Engineering*, volume 22, November 1941, pages 399–406.

57. "It Must Have Been a Bad Heart," **G. E. Kimball.** *National Safety News*, volume 43, September 1941, pages 81–3. Electrical injuries of so-called low-voltage type.

Section B

Accident Prevention Methods

1930

1. Comparison of the Likelihood of Electrocution on 115/220 Volt A-C Networks With Grounded and Ungrounded Neutral (in French), **G. Burin des Roziers.** *Revue Générale de l'Electricité* (Paris), volume 27, February 22, 1930, pages 296–9.

2. Prevention of Accidents, **T. C. Gilbert.** *Electrical Review* (London), volume 107, November 7, 1930, pages 772–4; November 14, 1930, pages 811–13; November 21, 1930, pages 858–60.

1931

3. Electrical Safety in 132 Pictures (book, in German), **S. Jellinek.** Leipzig, Germany, 1931. 32 pages.

4. Protecting Temporary Workers in Electric Power Stations. *Pulp and Paper Magazine of Canada*, volume 31, March 5, 1931, page 340.

1932

5. How Toledo Edison Cut Accidents 90 Per Cent, **A. Hoefle.** *Electrical World*, volume 99, May 7, 1932, page 826.

6. Promoting Safety by Engineering Design, **A. L. Colligan.** *Electrical World*, volume 99, March 5, 1932, page 462.

7. Safety Control is Like Production Control. **Electrical World,** volume 99, April 23, 1932, page 736.

8. Safety for the Household. National Bureau of Standards, Circular 397, Washington, D. C., 1932. Chapter 5, "Electrical Hazards," pages 50–73.

1933

9. Demonstrating Electric Safety, **E. H. Eitel.** *National Safety News*, volume 28, July 1933, pages 16–18.

10. "Hold Off" Cardholder for Use Near Live Equipment, **F. W. Sulensky.** *Electrical World*, volume 101, March 4, 1933, page 299.

11. Safety Saw for Tree Limbs, **G. F. Harden.** *Electrical World*, volume 101, March 4, 1933, page 294.

1934

12. Curb-Accident Efforts Effective for Ohio Edison. *Electrical World*, volume 103, April 21, 1934, page 577.

13. Design as a Factor in Electrical Accidents. *Engineering* (London), volume 138, December 31, 1934, page 683.

14. Grounding the Neutral of Star-Connected Three-Phase Low-Voltage Networks (in French), **L. Muller.** *Revue Générale de l'Electricité* (Paris), volume 36, September 8, 1934, pages 325–40.

15. Making Manholes Safer by "Remote" Breaker Control. *Electrical World,* volume 103, June 16, 1934, page 872.

16. Potomac Edison Rebuilds a Wood-Pole Line While Still in Service. *Electrical World,* volume 103, June 16, 1934, page 869.

17. Protection Tests Scheduled. *Electrical World,* volume 103, April 28, 1934, page 627. Rubber gloves and boots tested.

18. Steel-Clad Bank for Public Safety, **J. D. Whitaker,** *Electrical World,* volume 103, September 15, 1934, page 423.

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19. Accident Prevention in Maintenance Work, **A. C. Sachse.** *Electrical World,* volume 105, December 7, 1935, page 2924.

20. Do Lineman Think? **W. W. Palmer.** *Electric Journal,* volume 32, February 1935, pages 66–8; *National Safety News,* volume 31, May 1935, pages 13–14.

21. Lineman Safety Belt of Woven Fabric. *Electrical World,* volume 105, October 26, 1935, page 2583.

22. Repairs 38-Kv Live Line, **H. L. Talbot.** *Electrical World,* volume 105, March 16, 1935, page 583.

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23. Anaesthetic Explosions—Precautionary Measures Necessary With Electrical Apparatus. *Electrician* (London), volume 116, January 17, 1936, page 80.

24. Comparison of Two Methods for Promoting Personal Safety on the Three-Phase Systems; the Insulated and the Grounded Neutral (in French), **L. Castillon.** *Revue Générale de l'Electricité* (Paris), volume 40, July 11, 1936, pages 35–43.

25. Electrocution Through Contact of the Head With a Live Conductor, and an Insulating Helmet for Protection (in French), **A. Dagory.** Société Française des Electriciens *Bulletin* (Malakoff), volume 6, April 1936, pages 433–8.

26. Line Moved "Hot" Quickly at Low Cost, **V. E. Staff.** *Electrical World,* volume 106, September 26, 1936, page 3034.

27. Simple Device Prevents Accidents. *Electrical World,* volume 106, December 5, 1936, page 3796. Door marked to indicate energized equipment.

28. Some Suggestions on the Prevention of Electrical Accidents in Coal Mines, **D. Harrington, C. W. Owings, E. R. Maize.** United States Bureau of Mines. Information Circular 6919, 1936. 14 pages.

29. Static Electricity—Hazards and Safety Measures, **J. M. Myers.** *Power Plant Engineering,* volume 40, October 1936, pages 598–9.

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30. Care and Test Extend Rubber Glove Life, **W. L. Garlington.** *Electrical World,* volume 107, May 22, 1937, page 1810.

31. Fence Controls High-Voltage Test Circuit. *Electrical World,* volume 108, October 23, 1937, page 1398.

32. It's Safer If Grounded, **A. F. Edwards, L. A. Hunt.** *Factory Management and Maintenance,* volume 95, January 1937, pages 71–2.

33. Safety Programs Pay Lasting Dividends, **W. E. Mitchell.** *Electrical World,* volume 107, June 5, 1937, page 2005.

34. Temporary Portable Protection Barriers. *Electrical World,* volume 108, December 4, 1937, page 1894.

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35. Experience With Protective Measures (in German). Verein Deutscher Ingenieure *Zeitschrift* (Berlin), volume 82, April 30, 1938, pages 533–4.

36. Investigations Into Safety Conditions in Wiring of Buildings With Special Reference to Earth Continuity. **A. J. Levy, H. G. John.** South African Institute of Electrical Engineers *Transactions* (Johannesburg), volume 29, September 1, 1938, pages 223–35.

37. Protect Electricians With Multiple Locks. *Electrical World,* volume 109, February 12, 1938, page 582. Locking tongs protect workmen on "dead" circuit.

1939

38. Warning Barrier for Various Conditions. *Electrical World,* volume 109, January 1, 1938, page 60.

39. Finds "Hot Stick" Work Good Practice, **H. E. Clements.** *Electrical World,* volume 111, May 6, 1939, page 1301.

40. Key "Telephoned" With Cyclometer Lock, **J. E. Goodale.** *Electrical World,* volume 112, August 26, 1939, page 620. Safety device used by Consolidated Edison in its distribution system.

41. New Technique in Glove Testing, **R. W. Chadbourn, W. H. Meade.** *Electrical World,* volume 112, July 15, 1939, pages 174–6.

42. Personal Protective Equipment for Electrical Maintenance Men (abstract), **H. W. Arlin.** *Safety Engineering,* volume 77, May 1939, page 36.

43. Respect Low Voltage, **E. W. Beach.** *Safety Engineering,* volume 77, March 1939, pages 21–4.

44. Shocked, **C. L. Keene.** *Safety Engineering,* volume 77, June 1939, pages 6–8; volume 78, July 1939, pages 26, 28. Discussion of electrical injuries and their prevention.

1940

45. Deadly Extension Cords, **G. E. Kimball.** *Safety Engineering,* volume 79, February 1940, pages 9–10.

46. Eleven-Circuit Structure Moved With Lines Hot, **C. T. Malloy.** *Electrical World,* volume 114, November 2, 1940, pages 1322–3.

47. National Electrical Code Handbook—1940, **A. L. Abbott.** McGraw-Hill Book Company, New York, 1940. 589 pages.

48. 1.3 Million Man-Hours of Safety, **H. R. Kurth.** *Electrical World,* volume 114, December 14, 1940, pages 1781–3.

49. Tower Repaired With Line Hot. **Electrical World,** volume 114, December 14, 1940, page 1806.

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50. Control of Kite-Flying Hazards, **G. S. Diehl.** Edison Electric Institute *Bulletin,* volume 9, March 1941, page 105.

51. Demonstrations Hold Attention, **H. G. Pruett, W. A. Keller.** *National Safety News,* volume 44, July 1941, pages 93–4.

52. Double Dead-End Serviced While Hot. *Electrical World,* volume 116, November 1, 1941, page 457.

53. Live-Line Guards for Hot-Line Maintenance, **M. L. Fisher.** *Electrical World,* volume 116, November 15, 1941, page 1573.

54. Live Line Tool Operations Studied. *Electrical World,* volume 116, September 6, 1941, page 748.

55. Precautions Against Electrical Accidents. *Power Plant Engineering,* volume 45, February 1941, pages 80–1.

56. Safeguarding CO_2 Use in Cable Manhole, **J. J. O'Brien.** *Electrical World,* volume 115, March 22, 1941, page 996.

57. Safety Holder for Steel Guy Wire. *Electrical World*, volume 115, February 8, 1941, pages 490, 492.

58. A Safety Program on the Campus, **C. W. Beese.** *National Safety News*, volume 44, December 1941, pages 12–13, 56+.

59. Take No Chances With Low Voltage. *National Safety News*, volume 44, July 1941, page 54.

60. Twelve Messages on Accident Prevention in the Electric Public Utilities. Booklet C-12, Edison Electric Institute, New York.

Section C

Safety Codes and Standards Aimed at the Prevention of Electrical Accidents

(Note: Codes and Standards are subject to constant revision. The following are based on lists issued up to July 1, 1942; latest lists issued by sponsoring organizations should be consulted for complete accuracy at any time.)

National Electrical Safety Code

(The following may be obtained from the American Standards Association, 29 West 39th Street, New York, N. Y., or from the United States Government Printing Office, Washington, D. C.)

National Electrical Safety Code. Published by the Government Printing Office, Washington, D. C., in six separate parts, as follows:

Part I. Safety Rules for the Installation and Maintenance of Electrical Supply Stations. *C*2.1–1941. National Bureau of Standards Handbook 31.

Part II. Safety Rules for the Installation and Maintenance of Electric Supply and Communication Lines. *C*2.2–1941. National Bureau of Standards Handbook 32.

Part III. Safety Rules for the Installation and Maintenance of Electric Utilization Equipment. *C*2.3–1941. National Bureau of Standards Handbook 33. See also National Electrical Code which covers part of same field.

Part IV. Safety Rules for the Operation of Electric Equipment and Lines. *C*2.4–1939. National Bureau of Standards Handbook 34.

Part V. Safety Rules for Radio Installations. *C*2.5–1940. National Bureau of Standards Handbook 35. Applies to radio transmitting and receiving installations, including antennas, counterpoise wires, lead-in conductors, grounding conductors, grounding connections, protective devices, and batteries; does not apply to portable or mobile installations nor to carrier-current coupling equipment.

Part VI. Safety Rules for Electric Fences. National Bureau of Standards Handbook 36. Rules of construction, performance, and installation of fences and controllers.

National Electrical Code

(The following may be obtained from the National Board of Fire Underwriters, 85 John Street, New York, N. Y.)

National Electrical Code. *C*1–1940. National Board of Fire Underwriters Pamphlet 70. Prepared by the electrical committee of the National Fire Protection Association; published by the National Board of Fire Underwriters. Purpose is the safeguarding of persons and property from the hazards arising from the use of electricity within or on public or private buildings and premises, with certain exceptions. Covers accident as well as fire hazard. Revised periodically. See also National Electrical Safety Code.

Underwriters' Laboratories Publications

(The following standard specifications covering electric devices and materials may be obtained from Underwriters' Laboratories, Inc., 207 East Ohio Street, Chicago, Ill.; 161 Sixth Avenue, New York, N. Y.; 500 Sansome Street, San Francisco, Calif. List corrected to February 21, 1942.)

Air-Conditioning and Commercial Refrigerating Equipment. December 1941.

Arc-Welding Equipment, Transformer-Type. September 1937.

Armored Cable. March 1941.

Attachment Plugs and Receptacles. September 1939.

Cabinets and Boxes. February 1941.

Christmas-Tree and Decorative-Lighting Outfits. May 1941.

Circuit-Breakers, Branch-Circuit and Service. June 1940.

Cleats, Knobs, and Tubes (Porcelain). November 1937.

Conduit (Flexible-Steel). July 1938.

Conduit (Rigid-Steel). November 1935.

Cord Sets. October 1937.

Cutout Bases. April 1934.

Electrode Receptacles for Gas-Tube Signs. February 1940.

Fans (Motor-Operated). March 1938.

Fence Controllers (Electric). September 1937.

Fire-Alarm Cables. April 1941.

Fittings (Electrical) for Use in Hazardous Locations. March 1941.

Fixtures (Electric Lighting) and Portable Lamps. September 1941.

Fixtures (Electric Lighting) for Use in Hazardous Locations. August 1934.

Flatirons and Ironing Machines (Electric). June 1940.

Flexible Cord and Fixture Wire. October 1935.

Fuses. February 1941.

Gas-Tube Sign and Oil-Burner Ignition Cable. January 1936.

Heating Appliances (Electric). May 1940.

Heating Pads (Electric). June 1935.

Industrial Control Equipment. July 1938.

Industrial Control Equipment for Use in Hazardous Locations. March 1941.

Lamp-Holders, Edison-Base. March 1941.

Lighting Plants (Electric). September 1921.

Motor-Operated Appliances. April 1937.

Motors and Generators (Electric) for Use in Atmospheres of Combustible Dust, Class II, Group G. June 1941.

Motors and Generators (Electric) for Use in Hazardous Locations, Class I, Group D. Part I, Integral-Horsepower Motors; Part II, Fractional-Horsepower Motors. June 1941.

Nonmetallic-Sheathed Cable. July 1941.

Outlet Boxes and Fittings. November 1941.

Panelboards. March 1941.

Raceways and Fittings (Surface Metal). December 1940.

Raceways and Fittings (Metallic Underfloor). July 1934.

Raceways and Fittings (Nonmetallic Underfloor). August 1934.

Radio-Receiving Appliances, Power-Operated. April 1938.

Ranges (Domestic Electric). December 1940.

Rectifiers. July 1940.

Refrigerating Systems (Absorption-Type). December 1941.

Refrigerating Systems (Unit). December 1941.

Service Cables. April 1937.

Service Equipment. June 1936.

Signs (Electric). March 1940.

Soldering Lugs. January 1930.

Switchboards (Dead-Front). July 1938.

Switches (Enclosed). June 1936.

Switches (Knife). May 1939.

Switches (Snap). August 1941.

Therapeutic Carbon-Arc Lamps. July 1930.

Time-Indicating and Recording Appliances. October 1941.

Transformers (Specialty). June 1940.

Tubing (Flexible Nonmetallic). September 1939.

Wire Connectors (Pressure). May 1939.

Wires and Cables (Rubber-Covered). June 1940.

Wires and Cables (Varnished-Cloth). November 1939.

Wires (Synthetic-Insulated) (Proposed Requirements). May 1940.

Approved Canadian Standards

(The following Standard Specifications may be obtained from the Canadian Engineering Standards Association, National Research Building, Ottawa, Ont. List is corrected to March 31, 1942.)

Canadian Electrical Code, Part I. Essential Requirements and Minimum Standards Governing Electrical Installations for Buildings, Structures, and Premises. (Inside Wiring Rules) *C*22.1–1939.

Canadian Electrical Code, Part II. Specifications for Construction and Test of Electrical Equipment. (Approval Specifications.) *C*22.2. Includes the following Standard Specifications, which are published separately:

Air-Cooled Transformers (Dry Type). *C*22.2 No. 47–1940.

Armoured Cable and Armoured Cord. *C*22.2 No. 51–1941.

Asbestos-Insulated Wires and Cables. *C*22.2 No. 28–1941.

Automatic Motor-Control Devices of Small Capacity. *C*22.2 No. 24–1935.

Auxiliary Gutters, Junction Boxes, and Pull Boxes. *C*22.2 No. 26–1935.

Cabinets and Cutout Boxes. *C*22.2 No. 20–1936.

Cable for Luminous-Tube Signs and for Oil-Burner Ignition Equipment. *C*22.2 No. 17–1935.

Capacitors (Electrical Condensers). *C*22.2 No. 8–1934. Under revision.

Christmas-Tree and Other Decorative-Lighting Outfits. *C*22.2 No. 37–1937.

Cord Sets (second edition). *C*22.2 No. 21–1941.

Cutout Bases. *C*22.2 No. 39–1936.

Definitions and General Requirements (third edition). *C*22.2 No. 0–1941.

Domestic Electric Clothes Washing Machines. *C*22.2 No. 58–1939.

Electric Air-Heaters. *C*22.2 No. 46–1938.

Electric Clocks. *C*22.2 No. 6–1933.

Electric Cranes and Hoists. *C*22.2 No. 33–1936.

Electric Fixtures (second edition). *C*22.2 No. 9–1941.

Electric Floor-Surfacing and Cleaning Machines. *C*22.2 No. 10–1933.

Electric Portable Lighting Devices (Portables) (second edition). *C*22.2 No. 12–1936.

Electric Ranges. *C*22.2 No. 61–1942.

Electric Signs (second edition). *C*22.2 No. 2–1940.

Electrical Appliances for Hair Dressing and Hand-Drying, Etc. *C*22.2 No. 36–1936.

Electrical Equipment for Measuring and Discharge Devices for Flammable Liquids. *C*22.2 No. 22–1935.

Electrical Equipment for Oil-Burning Apparatus. *C*22.2 No. 3–1933.

Electrically Equipped Machine Tools. *C*22.2 No. 73–1941.

Electrically Heated Warming Pads. *C*22.2 No. 15–1937.

Electrically Operated Refrigerating Machines. *C*22.2 No. 32–1936.

Electrode Receptacles for Luminous-Tube Signs. *C*22.2 No. 34–1936.

Enclosed Branch-Circuit Cutouts. *C*22.2 No. 30–1936.

Enclosed Switches. *C*22.2 No. 4–1935.

Enclosures (Other Than Explosion-Proof) for Use in Hazardous Locations. *C*22.2 No. 25–1936.

Extra-Low-Potential Control-Circuit Wire and Cable. *C*22.2 No. 35–1940.

Flexible Cord and Fixture Wire (second edition). *C*22.2 No. 49–1941.

Flexible Steel Conduit. *C*22.2 No. 56–1938.

Flexible Tubing (Nonmetallic). *C*22.2 No. 44–1937.

Fractional-Horsepower Motors (second edition). *C*22.2 No. 11–1942.

Fuses (Both Plug- and Cartridge-Type). *C*22.2 No. 59–1939.

Ground Clamps. *C*22.2 No. 41–1937.

Industrial Control Equipment for Ordinary Locations (second edition). *C*22.2 No. 14–1942.

Insulated Conductors for Power-Operated Radio Devices. *C*22.2 No. 16–1940.

Integral-Horsepower Electric Motors for Other Than Hazardous Locations. *C*22.2 No. 54–1942.

Isolating Switches. *C*22.2 No. 58–1941.

Knife Switches. *C*22.2 No. 50–1938.

Lamp-Holders Having Socket Screw Shells. *C*22.2 No. 43–1937.

Motor-Operated Appliances, Domestic and Commercial (Fractional Horsepower). *C*22.2 No. 68–1942.

Motor-Operated Blowers and Stokers. *C*22.2 No. 20–1935. Under revision; out of print.

Nonmetallic Sheathed Cable. *C*22.2 No. 48–1938.

Outlet Boxes. *C*22.2 No. 18–1934.

Panelboards. *C*22.2 No. 29–1936.

Porcelain Cleats, Knobs, and Tubes. *C*22.2 No. 69–1940.

Portable Electric Displays and Incandescent-Lamp Signs (second edition). *C*22.2 No. 7–1938.

Portable Electric Vacuum Cleaners. *C*22.2 No. 67–1942.

Power-Operated Radio Devices, Inductively Coupled (Transformer) Type (second edition). *C*22.2 No. 1(A)–1940.

Power-Operated Radio Devices, Conductively Coupled (Transformerless) Type (second edition). *C*22.2 No. 1(B)–1941.

Pull-Off Plugs for Electrothermal Appliances. *C*22.2 No. 57–1940.

Receptacles, Plugs, and Similar Wiring Devices (second edition). *C*22.2 No. 42–1942.

Rigid Steel Conduit. *C*22.2 No. 45–1938.

Rubber-Covered Wires and Cables. *C*22.2 No. 38–1938.

Service-Entrance and Branch-Circuit Breakers (second edition). *C*22.2 No. 5–1942.

Service-Entrance Cables. *C*22.2 No. 52–1941.

Snap Switches. *C*22.2 No. 55–1942.

Soldering Lugs. *C*22.2 No. 19–1935.

Specialty Transformers. *C*22.2 66–1942.

Switchboards. *C*22.2 No. 31–1939.

Transformers for Luminous-Tube Signs and Oil-Burner Ignition Equipment. *C*22.2 No. 13–1935.

Wireways and Busways. *C*22.2 No. 27–1936.

Approved American Standards

(The following may be obtained from the American Standards Association, 29 West 39th Street, New York, N. Y. List corrected to July 1, 1942.)

Accident Prevention in Construction, Manual of. *A*10.1–1939. Prepared by accident-prevention committee, Associated General Contractors of America, Inc. Contains some references to temporary electrical installations used in construction work.

Coal-Mine Transportation, Safety Code for. *M*15–1931. Some references to electrical installations.

Compiling Industrial Accident Causes, Recommended Practice for. *Z*16.2–1941. Part 1, Selection of Accident Factors; Part 2, Detailed Classification of Accident Factors.

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Effects of Electric Shock—Influences of Current, Voltage, Frequency

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Section C (See also section B)

Telemeters and Telemetry—Types of Instruments, Systems and Methods

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Section D (See also sections B and H)

Automatic and Remote Controlled Switches and Switchgear

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Section E

Automatic Features of Generating Stations Using Fuels

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Section F

Automatic Boiler and Combustion Control

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Section G

Automatic Hydroelectric Plants

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Section H (See also sections B and D)

Automatic Substations—Descriptions of Individual Installations

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Simplified Calculation of Fault Currents

A simplified procedure for the calculation of short-circuit currents was presented in an earlier report.[1] This procedure, outlined in this article, has been given trial use for over a year and has been found to be generally satisfactory for the purpose intended. Accordingly, it is now recommended for general use by the industry as a simplified method of approximating the magnitude of fault currents.

THE precise determination of short-circuit currents involves a calculation so laborious as to be generally impractical. Thus, some approximation is required, and a degree of judgment must be applied in any method proposed. This method presented here is based upon the determination of an initial value of rms symmetrical current with which multiplying factors are used for application purposes. It is proposed that it be used, where applicable, in place of the methods which involve the use of decrement curves. It is believed that this procedure is sufficiently accurate to serve as a reliable basis for the application of interrupting devices and for a preliminary basis for relay settings.

The recommended multiplying factors take account of generator decrement and the asymmetrical value of current as influenced by the system d-c time constant, and they are adjusted to make allowance for the increase in generator excitation required to maintain normal terminal voltage under load conditions. They neglect, however, such influences as generator voltage regulators and phase-angle differences between generator rotors, which have but little effect within the usual operating time of modern circuit breakers.

A comparison of the current magnitudes determined by the recommended factors with those calculated by more complicated methods after various time intervals is given in Figure 1. The time intervals selected correspond to breaker speeds of 2, 3, 5, and 8 cycles. The fault current and reactances are based on the system kilovolt-amperes. Curves are given for both the total rms current and the a-c component only, the difference between the two being due to the d-c component. The larger the d-c time constant, the more closely is the total rms curve approached, and the smaller the d-c time constant, the more closely is the a-c component curve approached. The time constant of the d-c component for which these curves were made is 0.15 second or nine cycles, which is larger than the values usually encountered in any equipment except generators. The d-c time constant may be expressed generally as equal to $(1/2\pi f)(X/R)$ in seconds, or $(1/2\pi)(X/R)$ in cycles, where X is the 60-cycle reactance and R the d-c resistance to the point of fault.

In this method of determining fault currents, symbols are used with the following significance:

E = leg voltage
X_1 = direct-axis reactance, either transient or subtransient as specified, in ohms per leg
X_2 = negative-sequence reactance
X_0 = zero-sequence reactance
R_0 = zero-sequence resistance

The type of fault (number of phases involved, with or without ground) which will result in the maximum fault current varies with the relative values of the different circuit constants.

The three-phase short-circuit current is E/X_1. The line-to-line short-circuit current is $\sqrt{3}E/(X_1+X_2)$. Since X_2 is usually equal to or greater than X_1, this current seldom exceeds 86 per cent of the three-phase short-circuit current, and, consequently, for line-to-line faults the three-phase fault-current value is generally satisfactory.

The line-to-ground short-circuit current is $3E/(X_1+X_2+X_0)$. Since X_2 is usually approximately equal to X_1 (using the subtransient value for X_1), this expression is often shortened to $3E/(2X_1+X_0)$. In practical cases, regardless of the value X_2, this last expression is satisfactory for determining the current in a double line-to-ground fault.

PROCEDURE

The following gives a résumé of the procedure and the various multiplying factors to be used with currents calculated by the formulas just stated. Tables I and II show what reactance quantity should be used for representing the machines in the positive-sequence network.

Circuit Breakers, Protector Tubes, and Fuses. Determine "highest value of rms symmetrical current for any type of fault" by E/X_1 or $3E/(2X_1+X_0)$, whichever is greater, except that when R_0 is greater than $5X_1$, no consideration need be given to the latter term. Use multiplying factors as given in Table I for specific type of apparatus.

The application of protector tubes requires also the calculation of minimum fault current with minimum connected synchronous capacity and symmetrical current using E/X_1 or $3E/(2X_1+X_0)$, whichever gives the lower value. Special consideration is required where tower-footing resistance can appreciably reduce this line-to-ground fault current. For further details reference may be made to a paper, "Protector Tubes for Power Systems."[2]

Fuses are rated on total current, and, as the time durations involved are very short, their required inter-

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rupting capacity should be based on the total rms current at one-half cycle. Two cases should be considered:

1. In the general case, the current multiplying factor is 1.6, using subtransient reactance for all machines, and including both synchronous- and induction-motor contributions.

2. For fuses rated 15,000 volts or below with interrupting ratings not exceeding 3,000 amperes (except when applied on a generator bus), the resistance introduced by line and transformer is so great that the rms current of a maximum displaced wave in the first half cycle will not exceed 1.2 times the calculated symmetrical current, using subtransient reactance. Application on this basis may be considered safe.

Mechanical and Momentary Ratings. For many purposes, it is necessary to know the maximum possible rms current which can flow in the circuit. This is the current, including both a-c and d-c components, as calculated at one-half cycle. Allowing for over excitation of generators on account of load, the rms value of current as calculated for zero time is about 1.8 times the value calculated by dividing leg voltage by subtransient reactance. As there will always be some decay even in the first half cycle, however, a multiplier of 1.6 gives a safe figure.

In some instances, as when transmission line or cable reactance is an important factor in the limitation of current, the factor 1.6 may be higher than necessary. In such applications at 5,000 volts and below, the ratio of reactance to resistance is sufficiently low so that the d-c component decreases very greatly during the first half cycle. Thus within this voltage range, and except where the bulk of the power is supplied by generators at the point of fault, a standard factor of 1.4 may be used instead of 1.6. A standard factor is particularly desirable, in consideration of the difficulties of obtaining actual resistance figures and the distribution of the d-c component among contributing short-circuit sources.

Table I. Circuit Breakers, Protector Tubes, and Fuses

	Multiplying Factor	Reactance Quantity for Use in X_1		
		Synchronous Generator	Synchronous Motor	Induction Machine
A. Circuit-Breaker Interrupting Capacity				
1. General case				
Eight-cycle or slower breakers ‡	1.0	Subtransient*	Transient	
Five-cycle breaker	1.1			
Three-cycle breaker	1.2			
Two-cycle breaker	1.4			
2. Short circuits fed predominantly by large machines at the point of fault may be increased up to 20 per cent				
3. Air circuit breakers rated 600 volts and less	1.25	Subtransient	Subtransient	Subtransient
B. Mechanical Stresses and Momentary Rating of Circuit Breakers				
1. General case	1.6	Subtransient	Subtransient	Subtransient
2. At 5,000 volts and below, unless current is fed predominantly by directly connected synchronous machines or through reactors	1.4			
C. Protector Tubes				
1. Maximum rating	1.0	Subtransient	Subtransient	Subtransient
2. Minimum rating—use minimum system capacity and minimum of E/X_1 or $3E/(2X_1+X_0)$				
D. Fuses				
1. General case	1.6	Subtransient	Subtransient	Subtransient
2. At 15,000 volts or below with interrupting ratings not exceeding 3,000 amperes	1.2			

‡ Modern breakers are likely to be more effective than their slower predecessors, and, therefore, the application procedure with the older breakers should be more conservative than with modern breakers. Consequently, the factors to be used with older and slower breakers should be the same as for modern eight-cycle breakers.

* This is based on the condition that any hydroelectric generators involved have amortisseur windings. For hydroelectric generators without amortisseur windings, the subtransient reactance should be assumed equal to 75 per cent of the transient reactance for this calculation.

Table II. Preliminary Settings for Relays

	Multiplying Factor	Reactance Quantity for Use in X_1		
		Synchronous Generator	Synchronous Motor	Induction Machine
1. High-speed current actuated	1.0	Subtransient	Subtransient	Subtransient
2. Time overcurrent	1.0	Transient	Transient	

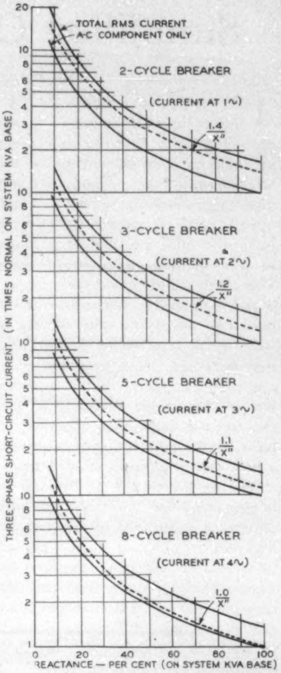

Figure 1. Comparison between decrement curve calculations and calculations by the proposed method

Low-Voltage Systems. Low-voltage air circuit breakers (rated 600 volts or less) are often instantaneous in operation and part contacts during the first half cycle. These breakers, however, are rated on the basis of average current in the three phases, and circuits on which they are installed rarely have X/R ratios exceeding 10. This corresponds to an average rms current during the first cycle equal to 1.23 times the symmetrical current. Such breakers may therefore be applied on the basis of 1.25 times the three-phase initial symmetrical current using subtransient reactance and including both synchronous and induction motors.

In calculating the equivalent system impedances, it should be remembered that at low voltages even small impedance values become of importance, and all elements of the circuit, including current transformers, disconnects, switches, bus runs, and lead wires, should be taken into consideration.

Overcurrent Protective Relays. In approximating the settings of overcurrent relays, the fault currents for two conditions should be determined:

1. The maximum initial symmetrical current for maximum connected synchronous capacity as determined by E/X_1 or $3E/(2X_1+X_0)$, whichever is greater, except that, when R_0 is greater than $5X_1$, no consideration need be given to the latter term.
2. The minimum symmetrical current for minimum connected synchronous capacity as determined by $0.866E/X_1$. In particular situations allowance should be made for remote fault locations and fault resistance.

Ground, distance, balanced, and other types of relays require special consideration.

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1. System Short-Circuit Currents, **W. M. Hanna, H. A. Travers, C. F. Wagner, C. A. Woodrow, W. F. Skeats.** AIEE *Transactions,* volume 60, 1941, September section, pages 877–81.

2. Protector Tubes for Power Systems, **H. A. Peterson, W. J. Rudge, Jr., A. C. Monteith, L. R. Ludwig.** AIEE *Transactions,* volume 59, 1940, May section, pages 282–92.

Report of the Board of Directors

THE BOARD OF DIRECTORS of the American Institute of Electrical Engineers presents herewith to the membership its 58th annual report, for the fiscal year ending April 30, 1942. A general balance sheet showing the condition of the Institute's finances on April 30, 1942, together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in *Electrical Engineering*.

BOARD OF DIRECTORS' MEETINGS

The board of directors held five meetings during the year, four in New York, N. Y., and one in Toronto, Ont.

Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of *Electrical Engineering* devoted to Institute activities.

DEFENSE AND WAR ACTIVITIES

In accordance with a resolution adopted by the board of directors on June 27, 1940, offering the services of the Institute to the President of the United States in connection with national defense problems, the board, committees, Sections, and headquarters staff have co-operated fully at every opportunity.

In addition to the organization of a committee on national defense, and a committee on civil protection, the Institute appointed a representative on the National Technological Civil Protection Committee and five representatives on the Engineers' Defense Board.

Co-operation has been extended in many undertakings, including the National Roster of Scientific and Specialized Personnel, census of engineering construction firms, extension of subcontracting plans, study of supply of and demand for engineers, and others.

President D. C. Prince delivered his address on the subject of postwar planning at meetings of many Sections and other organizations, and it was received with keen interest.

Editor G. Ross Henninger has contributed a substantial amount of his time weekly, since January 23, 1942, to the United States Navy Department, in Washington, in the organization of an editorial and publication review section for the coordination of Navy technical material.

Assistant Editor W. R. MacDonald, Jr., as a first lieutenant in the United States Army Signal Corps Reserve, was called into active service in January 1941 and has since been promoted to the rank of captain. He is responsible for the preparation and distribution of technical material used throughout the Signal Corps.

PRESIDENT'S AND NATIONAL SECRETARY'S VISITS

President Prince and National Secretary H. H. Henline attended the summer and Pacific Coast conventions, winter convention, South West District meeting in St. Louis, Mo., Southern District meeting in New Orleans, La., and the North Eastern District meeting in Schenectady, N. Y. President Prince and National Secretary Henline will attend the summer convention in Chicago, Ill. In May, the president will visit the Cleveland Section, and the national secretary will visit the Rochester Section.

Places Visited by President Prince

Alabama
Alabama Section, Birmingham
Muscle Shoals Section

Arizona
Arizona Section, Phoenix

California
Los Angeles Section
San Diego Section
San Francisco Section

Colorado
Denver Section
Conference on student activities, North Central District (6), Fort Collins

District of Columbia
Washington Section

Georgia
Georgia Section, Atlanta

Kentucky
Louisville Section

Louisiana
Southern District meeting, New Orleans

Maryland
Maryland Section, Baltimore

Massachusetts
Boston Section
Pittsfield Section

Michigan
Michigan Section, Detroit

Missouri
South West District meeting, St. Louis

New York
New York Section
Syracuse Section
North Eastern District meeting, Schenectady

Ohio
Cincinnati Section and University of Cincinnati Branch, joint meeting.

Oklahoma
Tulsa Section

Oregon
Portland Section

Pennsylvania
Erie Section
Lehigh Valley Section, Scranton
Philadelphia Section
Pittsburgh Section, and University of Pittsburgh, Carnegie Institute of Technology, and West Virginia University Branches, joint meeting

Tennessee
East Tennessee Section, Athens
Memphis Section

Texas
Houston Section
New Mexico-West Texas Section, El Paso
North Texas Section, Dallas
South Texas Section, San Antonio

Washington
Seattle Section
Spokane Section

Yellowstone National Park
Pacific Coast convention
Conference on student activities, Districts 8 and 9

Canada
Summer convention, Toronto
Vancouver Section
Engineering Institute of Canada, Montreal

Mexico
Mexico Section, Mexico City

Places Visited by National Secretary Henline

Indiana
South Bend Section
University of Notre Dame Branch, South Bend

Louisiana
Southern District meeting, New Orleans
Southern District executive committee meeting
Southern District conference on student activities

Massachusetts
Worcester Section

Missouri
South West District meeting, St. Louis

Nebraska
Nebraska Section, Omaha

New York
Ithaca Section
North Eastern District meeting, Schenectady

North Carolina
North Carolina Section, Durham

Ohio
Columbus Section

Virginia
Virginia Section, Richmond
University of Virginia Branch, Charlottesville

West Virginia
West Virginia Section, Charleston

Wisconsin
Madison Section

Yellowstone National Park
Pacific Coast convention
Conference on student activities, Districts 8 and 9

Canada
Summer convention, Toronto
Engineering Institute of Canada, Montreal

ANNUAL MEETING

The annual business meeting of the Institute was held on Tuesday morning, June 17, 1941. The annual report of the board of directors for the fiscal year which ended April 30, 1941, was presented in abstract by the national secretary. A report on the finances of the Institute was presented by National Treasurer W. I. Slichter. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1941, was presented, and President-Elect Prince responded to his introduction with a brief address. During this session, there was an address by Doctor Thomas H. Hogg, chairman and chief engineer of the Hydro-Electric Power Commission of Ontario, and the Lamme Medal for 1940 was presented to Doctor Comfort A. Adams, consulting engineer, Edward G. Budd Manufacturing Company, Philadelphia, Pa.

NATIONAL CONVENTIONS

Three national conventions were held during the year, and a brief report on each follows:

Summer Convention. The 57th summer convention was held in Toronto, Ont., June 16–20, 1941. In addition to the annual business meeting, and conference of officers, delegates, and members, there were ten technical sessions, five technical conferences, and one general session, at which Howard Coonley, of the Walworth Company, gave an address entitled "Making Democracy Work." Entertainment features of the convention were an English tea, bridge, dance, banquet, and golf and tennis tournaments. Many inspection trips were held. The registration was 1,203.

Pacific Coast Convention. The 29th Pacific Coast convention was held in Yellowstone National Park, August 27–29, 1941, with a registration of 322. A conference on "After the Emergency, What?" was a feature of the program, which also included three technical sessions, two technical conferences, one general session, two student sessions, and a conference on student activities. Entertainment features included an informal dinner and dancing, boat trip, inspection trips, and ladies' events.

Winter Convention. The 30th winter convention was held in New York, N Y., January 26–30, 1942, with a program including 20 technical sessions, at which more than 70 papers were presented, and 6 technical conferences. At the general session, N. G. Symonds, consultant on co-ordination of conservation orders, War Production Board, gave an address entitled "The Engineer's Contribution to the War Effort," and the Alfred Noble Prize was presented to Robert F. Hays, Jr. At an evening meeting, the Edison Medal was presented to Doctor John B. Whitehead, professor of electrical engineering, The Johns Hopkins University, Baltimore, Md. A smoker, dinner dance, and events for women guests completed the program. The registration was 1,331.

DISTRICT MEETINGS

South West District Meeting. This meeting was held in St. Louis, Mo., October 8–10, 1941, with 530 members and guests attending. Seven technical sessions, one general session, and two technical conferences were held. An address by E. T. Gushee, executive vice-president, Union Electric Company of Missouri, on "Electric Utilities' Part in National Defense" was given at the general session, and postwar problems were discussed at a general luncheon at which Doctor William McClellan, president, Union Electric Company, presided, and President Prince gave his address on postwar planning. Entertainment features consisted of a smoker, banquet and dance, women's bridge, and many inspection trips.

Southern District Meeting. The fourth meeting of this District was held in New Orleans, La., December 3–5, 1941. One general session, two technical sessions, and two student sessions were held at which eight papers were presented, in addition to the annual District conference on student activities. Entertainment consisted of a banquet, dancing, golf, and many inspection trips. The attendance was 384.

North Eastern District Meeting. This meeting, held in Schenectady, N. Y., April 29–May 1, 1942, was of wide interest on account of the quality and diversity of the program. There were one general session, three technical sessions, seven conferences, one student session, a joint luncheon with the Schenectady section of The American Society of Mechanical Engineers, a conference on student activities, a banquet, the Steinmetz memorial lecture, inspection trips, and other features. Attendance was about 500.

SECTIONS

During the year, the Sections committee recommended to Section chairmen that (*a*) plant engineers groups be formed, especially in the industrial centers, (*b*) efforts be made by the Sections to reinterest executives no longer engaged in technical work, (*c*) consideration be given to the desirability of organizing local engineering councils, and having some Section meetings devoted to civic affairs or related nontechnical subjects, (*d*) the Sections send announcements of their meetings to their members serving in the armed forces, (*e*) the Sections encourage their members to take keen interest in the development of plans for return to peacetime conditions, and (*f*) the interest in student guidance work in the Sections be increased.

President Prince gave his address on postwar planning at meetings of many of the Sections, and the keen interest with which it was received produced extensive discussions.

At its January meeting the board of directors voted to assign to Sections all remaining unassigned territory within the United States.

A majority of the Sections included in their programs during the year various phases of war activities.

Table I contains information regarding Sections and Branches and their meetings during the past several years. Detailed information on their activities of the past year may be found in the annual report on Section and Branch activities in the June 1942 issue of *Electrical Engineering*, pages 322–3.

STUDENT BRANCHES

A new Branch was organized at the University of Delaware, bringing the total number to 124.

The committee on Student Branches transmitted to all counselors pamphlet copies of Doctor William E. Wickenden's address "The Second Mile" (*Electrical Engineering*, May 1942, pages 242–7), suggesting that it be brought to the attention of students as an excellent statement of the nature of the engineering profession.

The committee on safety again urged the Branches to hold meetings on safety subjects, and transmitted detailed suggestions regarding such meetings.

The terms of 1,761 enrolled students were expected to expire on April 30, 1942. Of these, 935, or about 53 per cent, applied for admission as Associates.

See references at end of the preceding report on Sections.

General Committees

FINANCE COMMITTEE

In recommending a budget for the current appropriation year beginning October 1, 1941, the finance committee estimated that, in spite of the further loss of overseas revenue and loss from members in the United States enrolled in military service, the income for the current year would be approximately the same as that received during the previous appropriation year ending September 30, 1941, these losses being offset by the continued net increase in the membership of the Institute. So far, this expectation appears to be justified.

Accordingly, the budget was set up on the basis of $337,000, approximately $20,000 more than was expended last year. This has made possible a modest increase in the scope of the Institute activities in many departments including the publication of more technical material, some increases in salary to junior members of the staff and the addition of two people, a more liberal provision for the pension fund, and some increase in provision for travel, particularly for District student conferences. While the budget provides for some increases in expenses in

Table I. Section and Branch Statistics

	For Fiscal Year Ending April 30					
	1937	1938	1939	1940	1941	1942
Sections						
Number of Sections	62	65	67	70	72	72
Number of meetings held	621	624	635	701	703	647
Total attendance	74,950	110,148	85,692	91,949	92,554	78,254
Branches						
Number of Branches	119	120	120	121	123	124
Number of meetings held	1,363	1,334	1,190	1,346	1,163	946
Total attendance	46,121	60,446	53,380	64,972	52,285	37,785

addition to salaries, the budget includes an item of unappropriated funds of about $7,000, which it is anticipated should provide for additional necessary items and also for such further increase in the cost of materials as may take place during the year.

In taking care of the Institute's investment funds, the conservative policy of last year has been continued. About 20 per cent of the funds are invested in common stocks of essential industries and the bulk of the remainder is invested in United States Government bonds. Details of the Institute's investments are shown in Schedule 1 on page 379.

The Institute's healthy financial condition is the result of the policy continued over many years of budgeting expenses on the basis of the anticipated revenue and keeping the expenses within this budget limit. To the accomplishment of this, all parts of the Institute organization, and particularly the staff, have contributed. As a result, there has been built up over the life of the Institute a reserve fund of about $250,000, or about three-fourths of a year's expenses at the current level. In these disturbed times, it is very reassuring to have this modest insurance against any sudden great change in the Institute's income or expenses that might result from the war.

TECHNICAL PROGRAM COMMITTEE

Convention Programs. Three national conventions and three District meetings were held during the year. The total attendance of these six meetings represents a 1.5 per cent decrease as compared with the total attendance of five meetings held during the previous year. The attendance for the present year was unusually good when it is considered that the attendance at three of the meetings of the previous year each established a record over corresponding meetings in respective localities. When compared with the year ending April 30, 1939, in which six meetings were held, the attendance for the past fiscal year represents a 4.0 per cent increase. The 73 technical sessions and conferences at the six meetings were sponsored by 15 different Institute committees. Five of the technical committees held no sessions during the year.

The committee has arranged programs to be of the greatest service to the nation's war effort, in so far as possible. Five sessions and conferences of immediate importance to the war effort were held during the winter convention. These sessions were among the best attended, and in the planning of future programs sessions directly related to problems arising from the war will take precedence.

Civic Affairs Conferences. Several conferences on civic affairs have been held during the year to survey and determine how engineers can render the most effective service in their communities during the war and in the postwar period. In a conference on "After the Emergency, What?" held during the Pacific Coast convention, President Prince presented a plan based on studies made in the General Electric Company with a view to maintaining a high level of production during the peace to follow, and to prevent unemployment. Similar studies of business in various communities, states, and throughout the United States have been encouraged through discussions at subsequent conventions and Section meetings. A conference on "How Can Engineers Render Greater Public Service?" was held during the South West District meeting in St. Louis, Mo. Reports were submitted by 14 local engineering groups or councils on "The Contributions of Local Engineering Councils to Their Communities." Another such conference was held during the winter convention, in which a report of the St. Louis conference was presented by F. A. Cowan, vice-chairman of the technical program committee. "Regional Planning in the State of Pennsylvania" was described by R. H. Smith, deputy secretary, department of commerce, Commonwealth of Pennsylvania. Detailed reports of these two conferences on civic affairs were published in *Electrical Engineering*, March 1942, pages 148–52. Arrangements have been made to hold a similar conference on local engineering councils during the summer convention in Chicago.

Table II. Technical Programs, Last Four Years

	Year Ending April 30			
	1942	1941	1940	1939
Number of national conventions	3	3	2	3
Number of District meetings	3	2	3	3
Registration at national conventions and District meetings	4,274	4,339	3,548	4,100
Number of papers presented	182	195	177	184
Number of papers recommended for *Transactions*	152	167	151	169
Estimated number of pages required for printing papers in *Transactions*	807*	938*	886*	986*
Average length of papers recommended for *Transactions*	5.3	5.72	5.86	5.83
Number of technical sessions	50	46	43	50
Number of technical conferences	23	18	10	12

* Partly estimated.

Restricted Sessions. When subjects on which secrecy is required are discussed, closed committee meetings are held. During the winter convention, a session and conference on air transportation were successfully held with the attendance limited to AIEE members who are United States citizens and a few other strictly accredited individuals.

Conferences. During the war period, especially, technical conferences with a minimum of time-consuming formalities are encouraged. The accompanying tabulation shows that a greater number of conferences has been held during the past year than ever before, bringing their total number to 95 since their origin at the summer convention at Ithaca, N. Y., in 1935. The smaller number of technical papers presented, together with the maintained attendance at conventions, reflect the increasing importance of the conference method in providing up-to-the-minute reports of new developments, and free discussion of controversial questions.

General Sessions. A general session was held at each meeting during the year. Preference has been continued for addresses dealing with the broader problems of the profession, as related to co-operative effort during and after the war period, rather than toward specific technical developments. The general session of the 1942 summer convention will present some of the problems of engineering executives for the consideration of Institute members, through a discussion of "Organization and Management of Large-Scale Engineering Work."

Acknowledgments. The committee gratefully acknowledges the interest and efforts of its members and the work of the chairmen of the technical committees and the members of the headquarters staff.

PUBLICATION COMMITTEE

Electrical Engineering, *Transactions*, and advance pamphlet copies of technical program papers were published during the fiscal year in general on the same basis as for the preceding year.

The program and policy for expediting the publication of AIEE *Transactions* material that was initiated at the summer convention in Swampscott, Mass., in 1940, involved among others three related objectives.

The primary among these objectives was the earlier publication of *Transactions* material. This objective was achieved, effective with the January 1942 issue of *Electrical Engineering*. The first of the 1942 winter convention papers were published in the *Transactions* section of that issue, and the publication of the entire program of winter convention *Transactions* material will be completed with the June 1942 issue of *Electrical Engineering* and the corresponding June "Supplement to *Electrical Engineering—Transactions* Section." This latter supplement will, in contrast to the several previous issues, be down to what is considered normal size—200 pages or less. Also normal with respect to content, it will carry all the published discussions of 1942 winter convention papers but only a very few of the papers themselves—those which for some special reason could not be accommodated in the *Transactions* sections of the previous monthly issues of *Electrical Engineering*.

Requirements of the first objective imposed a second—that the publication of all 1941 papers and discussions and those remaining from 1940 be completed with the 1941 *Transactions* and the corresponding issues of *Electrical Engineering*.

This objective was achieved as originally scheduled, although as expected the program resulted in great enlargement of the June and December 1941 "Supplements" and imposed a delay of several weeks in the completion and distribution of the 1941 *Transactions* volume. These latter manifestations were incidental to the transition in publication procedure, and should not be encountered in the ensuing normal application of the Swampscott program.

A third objective of the Swampscott program was that each annual volume of AIEE *Transactions* should contain all, *and only*, the technical program papers and related discussions presented during the corresponding calendar year and other appropriate record material for that year. The 1942 volume is expected to meet this objective.

The achievement of these desired results imposed many added burdens on the headquarters staff and introduced many transient difficulties into the work of the several committees responsible for the development of technical program material. The publication committee is duly appreciative of the effective co-operation of all parties involved.

World war conditions as of May 1, 1942, have introduced certain problems in the conduct of the Institute's publication service that seem worthy of recording. One of these relates to the matter of censorship-export control of the distribution of the monthly issues of *Electrical Engineering* to members and nonmember subscribers in all foreign countries and territories except Canada. Distribution has been entirely stopped in Asia to Burma, Japan, Netherlands Indies, Philippine and other islands, occupied portions of China, Malayan states, Indo-China, and other enemy-dominated territory; in Europe to all points except Great Britain, Turkey-in-Europe, and Russia. This affects about 230 members and 34 subscribers. Distribution to members and subscribers in Central and South America and the other remaining world points now is being made under export-license control and rigorous limitations imposed by the United States Office of Censorship and the Office of Export Control of the Board of Economic Warfare. Currently this affects about 720 Institute members and 205 subscribers.

MEMBERSHIP COMMITTEE

Membership of the Institute at the end of the fiscal year reached an all-time high record for that date of 18,944 exceeding the former peak year of 1927 by 600 members. This very creditable showing was undoubtedly the result of the hard work and unselfish efforts of the members of the Section membership committees, Branch counselors and members of the national membership committee, comprising approximately 700 Institute members engaged in membership work. This result was made possible to a considerable extent by the splendid response of the members of the Institute at large to the broadcast letter of the chairman, dated October 8, 1941, in which they sent in the names of 989 prospects, many of whom are new members. With the pressure of the war effort and unexpected demands upon the time of membership committee personnel, the amount of effort applied to Institute membership work is another indication of the fine spirit of loyalty which binds members of the Institute together in a national organization of their profession.

Table III shows that the gross increase in membership during the year was 1,863 and that this was reduced to a net increase of 1,058 by membership losses during the year. In the preceding year the gross increase was 1,644, reduced to a net increase of 673 by losses during that year. Analysis of the losses as between these two years is of interest in manifesting effective effort of membership committees in connection with delinquent members.

	Year Ending April 30	
	1941	1942
Resigned	299	211
Dropped	582	484
Total	881	695

Table IV shows the number of applications received from enrolled students and others annually for the past five years. It shows a continuous increase each year. Since applications received during the fiscal year are not all acted upon at the close of the fiscal year, there is no direct check between Tables III and IV. The Branch counselors have co-operated with the committee by writing to graduates and using their influence to procure their applications. The Section membership committees receive from headquarters the cards of graduates residing in their territory and endeavor to interview them with respect to membership.

Table III. Membership Statistics for Fiscal Year Ending April 30, 1942

	Honorary Member	Fellow	Member	Six-Year Associate	Associate	Increase	Total
Membership on April 30, 1941	8	778	4,650	5,890	6,560		17,886
Additions							
New members qualified		3	179	24	1,614	1,820	
Former members reinstated		1	8	10	24	43	
Subtotal		4	187	34	1,638	1,863	
Transfers		37	226	726			
	8	819	5,063	6,650	8,198		20,738
Deductions							
Died	3	22	31	43	11	−110	
Resigned		2	47	93	69	−211	
Dropped		3	45	166	270	−484	
Subtotal	3	27	123	302	350	−805	
Transfers			37	203	749		
	3	27	160	505	1,099		1,794
Membership on April 30, 1942	5	792	4,903	6,145	7,099	1,058	18,944

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending April 30	Students	All Others	Total
1942	971	1,031	2,002
1941	887	1,011	1,898
1940	911	918	1,829
1939	849	872	1,721
1938	739	932	1,671

Table V. Number of Enrolled Students as of April 30

Year	New Applications	Renewals	Total
1942	2,585	3,377	5,962
1941	2,351	3,188	5,539
1940	2,525	2,992	5,517
1939	2,271	2,971	5,242
1938	2,428	2,609	5,037

Table VI. Number of Members in Section Territory Reinstated

August 1, 1941 to April 30, 1942	275
Year beginning August 1, 1940	383
Year beginning August 1, 1939	302
Year beginning August 1, 1938	354
Year beginning August 1, 1937	325

Table VII. Status of Membership Dues as of April 30

Year	Total Membership	Members Fully Paid	
		Number	Per Cent
1942	18,944*	16,595**	87.5
1941	17,886	15,777	88.2
1940	17,213	14,997	87.1
1939	16,605	14,371	86.5
1938	16,078	14,127	87.9

* Of the 18,944 members reported for April 30, 1942, 2,349 were not fully paid. Of this number, 327 names are being carried upon the membership rolls, in an inactive status, in accordance with the resolutions adopted by the Board of Directors in the interest of members in the military service of the United States or allied countries, or members located in such countries unable to remit membership dues at this time. The remainder comprises

1. Members owing dues to April 30, 1941..... 514
2. Members owing dues to April 30, 1942.....1,508

(During the period May 1 to May 20, 1942, 316 members have paid dues to April 30, 1942, reducing the number not fully paid to 1,192.)

** Including 733 Members for Life.

Table V shows data on the number of enrolled students and new applications therefrom for five successive years ending 1942. The improvement in 1942 is especially encouraging.

Tables VI and VIII show additional data on membership which are self-explanatory.

The membership committee continued the organization adopted in the preceding year which provided an advisory subcommittee, comprising committee members within easy reach of New York and thus able to hold more frequent meetings than the whole committee, and a research subcommittee to which were referred committee problems requiring study and research. This organization has been found excellently adapted to the membership committee, and permits of holding but one meeting of the committee as a whole, during the winter convention, when a good attendance is assured.

Table VIII. Record of AIEE Membership

Year	Total May 1	Year	Total May 1	Year	Total May 1
1884	71	1905	3,460	1925	17,319
1885	209	1906	3,870	1926	18,158
1886	250	1907	4,521	1927	18,344
1887	314	1908	5,674	1928	18,265
1889	333	1909	6,400	1929	18,133
1890	427	1910	6,681	1930	18,003
1891	541	1911	7,117	1931	18,334
1892	615	1912	7,459	1932	17,550
1893	673	1913	7,654	1933	17,019
1894	800	1914	7,876	1934	15,230
1895	944	1915	8,054	1935	14,269
1896	1,035	1916	8,202	1936	14,600
1897	1,073	1917	8,710	1937	15,308
1898	1,098	1918	9,282	1938	16,078
1899	1,133	1919	10,352	1939	16,605
1900	1,183	1920	11,345	1940	17,213
1901	1,260	1921	13,215	1941	17,886
1902	1,549	1922	14,263	1942	18,944
1903	2,229	1923	15,298		
1904	3,027	1924	16,455		

The Membership Committee Guide, a pamphlet prepared and distributed in the preceding year, which outlines recommended procedure for the conduct of Section membership committee work, has been found very helpful to Section membership committees.

In June 1941, a letter was written to each of the newly elected Section chairmen suggesting the early appointment of Section membership committees, in order that such committees can get an early start. This letter was well received by Section chairmen, and undoubtedly aided in getting membership work started early during the current committee year. This is important because of the time required to get committees organized and working, and the shortness of the period when most effective work can be done.

The research subcommittee has studied

Table IX. Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Aldrich, W. S.	Associate '92	Dec. 22, 1941	Member	Mar. 1942, p. 167
Anderson, E. F.	Associate '35	Oct. 4, 1941	Associate	Dec. 1941, p. 604
Arnold, B. J.	Associate '92	Jan. 30, 1942	Honorary Member	Mar. 1942, p. 166
Augustinus, P.	Member '27	Nov. 18, 1941	Member	Feb. 1942, p. 96
Babcock, F. H.	Associate '28	Aug. 31, 1941	Associate	Oct. 1941, p. 507
Baker, T. S.	Associate '28	Feb. 27, 1941	Member	May 1941, p. 234
Barker, G. A.	Associate '41	June 18, 1941	Associate	Aug. 1941, p. 411
Barnard, G. H.	Member '26	May 6, 1941	Member	July 1941, p. 356
Belden, J. L.	Associate '36	Oct. 5, 1941	Associate	Mar. 1942, p. 167
Bennett, J. C.	Associate '90	Aug. 2, 1941	Member	Sept. 1941, p. 452
Berg, E. J.	Associate '94	Sept. 9, 1941	Fellow	Oct. 1941, p. 507
Berresford, A. W.	Associate '94	May 30, 1941	Fellow	July 1941, p. 355
Brandenstein, E. W.	Member '37	May 20, 1941	Member	July 1941, p. 356
Budden, D. B.	Member '29	April 19, 1941	Member	July 1941, p. 356
Burbank, E. W.	Associate '25	April 1941	Member	April 1942, p. 213
Canfield, C. E.	Associate '02	July 12, 1941	Member	Sept. 1941, p. 451
Carpenter, H. V.	Associate '03	Nov. 15, 1941	Fellow	Jan. 1942, p. 49
Case, H. M.	Associate '11	Mar. 31, 1941	Fellow	May 1941, p. 234
Clark, F. C.	Associate '12	Oct. 28, 1941	Associate	Feb. 1942, p. 96
Clarke, C. L.	Associate '84	Oct. 9, 1941	Fellow	Nov. 1941, p. 557
Clement, E. E.	Associate '97	Mar. 31, 1941	Fellow	Nov. 1941, p. 557
Combs, R. H.	Associate '13	April 25, 1941	Member	July 1941, p. 356
Conrad, Frank	Associate '02	Dec. 10, 1941	Fellow	Jan. 1942, p. 48
Cooper, D. W.	Associate '40	Sept. 29, 1941	Associate	Mar. 1942, p. 167
Cornell, E. S.	Associate '28	May 14, 1941	Member	July 1941, p. 356
Costello, J. M.	Associate '27	Aug. 15, 1941	Associate	Oct. 1941, p. 507
Crichton, L. N.	Associate '08	Sept. 6, 1941	Member	Oct. 1941, p. 507
Croden, W. T.	Associate '37	Jan. 12, 1942	Associate	Mar. 1942, p. 167
Cunningham, F. J.	Associate '19	April 1941	Associate	June 1941, p. 285
Cunningham, J. H.	Associate '06	Oct. 29, 1941	Associate	Feb. 1942, p. 96
Daugherty, W. W.	Associate '37	Aug. 12, 1941	Associate	Dec. 1941, p. 604
Dillman, C. L.	Associate '36	Apr. 27, 1941	Associate	July 1941, p. 357
Don Carlos, H. C.	Fellow '18	Mar. 29, 1941	Fellow	May 1941, p. 234
Eichberg, F.	Fellow '28	July 30, 1941	Fellow	Sept. 1941, p. 451
Elliott, F. F.	Associate '19	May 8, 1941	Associate	Dec. 1941, p. 604
Emmett, W. L.	Associate '93	Sept. 26, 1941	Honorary Member	Nov. 1941, p. 557
Fraser, W. W.	Associate '21	Mar. 31, 1941	Member	July 1941, p. 356
Gherardi, B.	Associate '95	Aug. 14, 1941	Fellow	Sept. 1941, p. 450
Gerry, M. H.	Associate '93	Dec. 30, 1941	Fellow	Apr. 1942, p. 213
Gibney, E. L.	Member '40	Feb. 26, 1942	Member	Apr. 1942, p. 214
Gladson, W. N.	Associate '98	Oct. 18, 1941	Member	Jan. 1942, p. 49
Guillou, A. V.	Member '27	Dec. 1940	Member	Sept. 1941, p. 451
Hall, C. E.	Associate '09	Feb. 16, 1941	Associate	July 1941, p. 356
Harley, E. A.	Associate '06	July 9, 1941	Associate	Sept. 1941, p. 452
Hehre, F. W.	Associate '13	July 27, 1941	Fellow	Sept. 1941, p. 452
Henshaw, F. V.	Associate '89	Mar. 23, 1941	Member	May 1941, p. 233
Imlay, L. E.	Associate '00	June 9, 1941	Fellow	July 1941, p. 355
Irwin, E.	Associate '12	Nov. 3, 1941	Associate	Jan. 1942, p. 49
Kemp, C. G. R.	Member '34	Nov. 3, 1941	Member	Jan. 1942, p. 49
Koch, P. W.	Associate '28	Sept. 2, 1941	Associate	Mar. 1942, p. 167
Kovediaefl, B. E.	Associate '28	April 1941	Associate	Mar. 1942, p. 167
Lacerte, W. J.	Associate '28	Oct. 12, 1940	Associate	Sept. 1941, p. 452
Lanier, A. C.	Associate '04	Feb. 26, 1942	Fellow	Apr. 1942, p. 213
Lawler, G. S.	Associate '11	Oct. 31, 1941	Member	Dec. 1941, p. 604
Leonarz, E.	Associate '08	July 24, 1941	Fellow	Oct. 1941, p. 507
Lloyd, M. G.	Associate '08	Apr. 26, 1941	Fellow	June 1941, p. 295
Lyle, R. E.	Associate '32	July 20, 1941	Associate	Sept. 1941, p. 452
MacNaughton, A. K.	Associate '21	March 1941	Fellow	May 1941, p. 234
Magnusson, C. E.	Associate '05	July 10, 1941	Fellow	Aug. 1941, p. 411
Mahaney, D. J.	Associate '34	Nov. 28, 1941	Associate	Apr. 1942, p. 214
Marquis, J. B.	Associate '20	June 5, 1941	Associate	Jan. 1942, p. 49
McDougall, S.	Associate '34	Dec. 23, 1941	Associate	Mar. 1942, p. 167
Morganthaler, P. C.	Associate '09	Mar. 29, 1941	Associate	July 1941, p. 356
Morton, G. L.	Associate '08	Apr. 26, 1941	Fellow	June 1941, p. 295
Moser, F. L.	Associate '25	Mar. 25, 1941	Member	May 1941, p. 234
Mulligan, J. F.	Associate '29	Feb. 6, 1941	Member	May 1941, p. 234
Murray, W. S.	Associate '03	Jan. 9, 1942	Fellow	Feb. 1942, p. 96
Murrie, J. L.	Member '41	Aug. 16, 1941	Member	Nov. 1941, p. 557
Nyswander, R. E.	Member '22	Apr. 8, 1941	Member	June 1941, p. 295
Otis, J. P.	Associate '27	Feb. 17, 1941	Associate	Sept. 1941, p. 451
Patterson, A.	Associate '08	Apr. 4, 1941	Member	June 1941, p. 295
Patterson, J. H.	Associate '39	July 19, 1941	Associate	Mar. 1942, p. 167
Petterson, R. H.	Associate '37	Jan. 8, 1942	Associate	Mar. 1942, p. 167
Phelps, J. E. B.	Member '29	April 24, 1941	Member	July 1941, p. 357
Plowman, C. S.	Associate '20	Feb. 7, 1942	Associate	Apr. 1942, p. 213
Poirier, A. E.	Associate '01	Dec. 17, 1941	Associate	Feb. 1942, p. 97
Porter, N. M.	Associate '31	May 29, 1941	Associate	Sept. 1941, p. 452
Purdy, H. T.	Associate '13	Jan. 31, 1940	Member	Sept. 1941, p. 451
Richhart, W. S.	Associate '08	Apr. 9, 1941	Member	June 1941, p. 295
Ripley, L. O.	Associate '05	July 22, 1941	Associate	Sept. 1941, p. 451
Roe, J.	Associate '06	Nov. 24, 1941	Associate	Feb. 1942, p. 97
Rothenstein, O.	Associate '28	Oct. 20, 1941	Associate	Feb. 1942, p. 96
Rugg, H. V.	Associate '07	Sept. 21, 1941	Member	Feb. 1942, p. 97
Sampson, F. D.	Associate '96	Aug. 24, 1941	Member	Oct. 1941, p. 507
Sanville, H. F.	Associate '01	May 24, 1941	Member	July 1941, p. 356
Schaefler, R.	Associate '32	Apr. 18, 1941	Member	June 1941, p. 295

(*Continued on following page*)

Table IX (continued). Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Schalcher, O.	Associate '39	Nov. 3, 1941	Associate	Mar. 1942, p. 167
Schreiber, J. M.	Associate '03	July 18, 1941	Associate	Sept. 1941, p. 451
Schumacher, J. H.	Associate '07	Apr. 1, 1941	Member	May 1941, p. 233
Scott, W. M.	Associate '97	Jan. 19, 1942	Associate	Mar. 1942, p. 167
See, A. B.	Associate '93	Dec. 16, 1941	Associate	Feb. 1942, p. 97
Smith, E. F.	Associate '07	Nov. 13, 1941	Fellow	Feb. 1942, p. 97
Smith, J. C.	Associate '20	July 18, 1941	Associate	Sept. 1941, p. 452
Stebbins, T.	Associate '89	Oct. 14, 1941	Fellow	Nov. 1941, p. 557
Stockbridge, W. A.	Associate '11	Aug. 20, 1941	Associate	Dec. 1941, p. 604
Strike, R. J.	Associate '04	Nov. 2, 1941	Associate	Apr. 1942, p. 214
Swinburne, E. D.	Associate '08	Nov. 12, 1941	Associate	Feb. 1942, p. 97
Torchio, P.	Associate '95	Jan. 14, 1942	Fellow	Feb. 1942, p. 96
Van Gelder, H. M.	Associate '05	Nov. 22, 1941	Member	Feb. 1942, p. 96
Wagner, R. T.	Member '25	Feb. 11, 1942	Member	Apr. 1942, p. 214
Walther, J. T.	Associate '19	Nov. 4, 1941	Member	Jan. 1942, p. 49
Wanamaker, E.	Associate '20	August 1941	Associate	Apr. 1942, p. 214
Ward, H. A.	Associate '35	Recently	Associate	May 1941, p. 234
Whittemore, G. W.	Associate '02	Aug. 22, 1940	Associate	May 1941, p. 234
Yokich, M.	Associate '39	Feb. 12, 1942	Associate	Apr. 1942, p. 214

many problems during the year, the solution of which has contributed to the effectiveness of committee work. One example was its consideration of Doctor Sorensen's suggestion that an effort be made to reinterest members who have developed beyond the "design engineering" stage of their careers and who are now in managerial and executive positions. The result was a letter written to all Institute vice-presidents, District secretaries, and Section chairmen, by the chairman of the Sections committee, soliciting their assistance in a well-defined program for dealing with this problem.

In bringing the advantages of membership to the attention of desirable candidates for admission to the Institute, and in discussions with members about to drop their membership in the Institute, committee members have found that frequently the most effective appeal is that which places emphasis upon the value of the Institute as the national organization of the electrical engineering profession. In relation to present Institute members, and considering the substantial losses each year, it appears that there is opportunity to keep members better informed as to the work and accomplishments of the Institute as a national organization. This problem has also been studied by the research subcommittee and some suggestions have been made for the consideration of the board of directors.

BOARD OF EXAMINERS

The board of examiners held 11 meetings during the past year, averaging about two and one-half hours each, and acted upon 3,662 cases, divided as shown in Table X.

During the past year the board has had the opportunity to put to a practical test the "invitational method" applying to transfer to Fellow grade. This method was recommended by the examiners in December 1940. The first transfers were presented under this procedure in October 1941, and to date 13 cases have been considered, all receiving favorable recommendations. In the opinion of the examiners, all difficulties previously inherent in the Fellow transfer procedure thus have been cleared up.

A survey of the situation with regard to transfer to Member does not under present conditions seem to call for any alteration in the long-established application procedure followed in such cases. The record of the current year shows that of the 246 cases coming before the board only 14 failed to receive a favorable recommendation.

A change in procedure applying to reelections to Member and Fellow was also put into effect during the past year. Up to April 1941, it had been customary to require applicants for re-election, out of the Institute for more than five years, to supply a new set of references. This requirement was waived in favor of a requirement that all re-election cases be posted in *Electrical Engineering* for a period of one month before formal action is taken, in order to provide for the filing of any possible objections based on ethical grounds, as, except for such ethical objections, a man is always considered eligible for re-election to the grade formerly held.

Table X. Applications for Admission and Transfer

Applications for Admission		
Recommended for grade of Associate	1,169	
Re-elected to the grade of Associate	68	
Not recommended	5	
		1,242
Recommended for grade of Member	167	
Re-elected to the grade of Member	17	
Not recommended	24	
		208
Recommended for grade of Fellow	2	
Re-elected to the grade of Fellow	1	
		3
Recommended for Honorary Member	1	
		1
Applications for Transfer		
Recommended for grade of Member	232	
Not recommended	14	
		246
Recommended for grade of Fellow	28	
Not recommended	0	
		28
Students		
Recommended for enrollment as Students		1,934
Total		3,662

HEADQUARTERS COMMITTEE

In accordance with a request from the board of directors, the committee recommended rules governing the use of AIEE headquarters rooms for meeting purposes. The rules were adopted by the board in January.

INSTITUTE POLICY COMMITTEE

The functions of the committees on economic status of the engineer and legislation affecting the engineering profession were consolidated with those of the Institute policy committee, to be carried out by that committee through subcommittees. During the year, the committee reported to the board of directors on several matters which had been referred to it.

COMMITTEE ON PLANNING AND COORDINATION

A meeting of the committee was held at Institute headquarters in New York on January 27, 1942, at which, after careful consideration and thorough discussion of the probable effects of the war upon the summer convention, the members present unanimously adopted the following recommendations to the board of directors:

1. That the 1942 summer convention be held in Chicago as planned.
2. That the summer convention and technical program committees make it a working convention, with the understanding that the resolution on "summer convention policy" adopted by the board of directors on November 12, 1920, be disregarded in this case.
3. That the summer convention and technical program committees be requested to recommend the most desirable length of program.

At this meeting, the committee considered all requests from District and Section officers for national conventions and District meetings in 1943, and recommended to the board of directors the following schedule of meetings:

Winter convention, New York, N. Y., January 25–29

Summer convention, Cleveland, Ohio, June 21–25

Pacific Coast convention, about September 1; Pacific Coast Districts to be requested to give further consideration to the selection of the most desirable location.

District meetings—Tentative approval given to the following, subject to further review by the District committees or by the board of directors if conditions make it desirable:

North Eastern District (1), Providence, R. I., spring

Southern District (4), Virginia

South West District (7), Kansas City, Mo., spring

All of these recommendations were approved by the board of directors on January 29, 1942.

The committee had, previous to the meeting, considered the request of the Vancouver Section that the 1942 Pacific Coast convention be held in Vancouver, B. C., and, having consulted the vice-presidents of the three Districts concerned, recommended to the board of directors that the convention be scheduled in Vancouver for

September 9–11. This recommendation was approved by the board on October 24, 1941.

COMMITTEE ON CODE OF PRINCIPLES OF PROFESSIONAL CONDUCT

The committee still awaits advice from the joint committee on code of ethics sponsored by Engineers' Council for Professional Development, which has been delayed in its work some four months by the illness of its chairman who also is chairman of this committee. It now is hoped to have completed by the joint committee before the summer is over a report, which then can be considered by the AIEE committee. In association with this, the ECPD committee on professional recognition is endeavoring to formulate a statement of the principles which underlie a code of ethics and which give to ethics a fundamental influence on a profession.

The chairman of the AIEE committee while still ill received a written charge of unethical practices made against an engineer by another engineer, and this will shortly be circulated for consideration by all members of the committee on code of principles of professional conduct.

COMMITTEE ON CONSTITUTION AND BYLAWS

The work of this committee has been limited to a few routine changes in the bylaws. There has been no change in the constitution.

Sections 65, 74, and 83 were modified or deleted in keeping with the action of the board of directors, taken at its meeting of August 8, 1941, to consolidate the work of the committee on economic status of the engineer and the committee on legislation affecting the engineering profession into that of the Institute policy committee.

Section 92 was modified as to date only to make it consistent with the present rules of the John Fritz Medal board of award.

COMMITTEE ON RESEARCH

The committee is now in its second year as a general committee of the Institute rather than a technical committee. As in the preceding year, the principal activity has been that of studying the program of the Engineering Foundation research projects sponsored by the Institute.

One of these, research on the stability of impregnated-paper insulation, project 66, which has been under way for some years at the Johns Hopkins University under the immediate direction of Doctor J. B. Whitehead, will be terminated for the time being at the end of the current year. This termination does not mean that all study desirable regarding this question has been completed, but it does mean that the work is well under way with an indication as to how future work should be done by those who are particularly interested in this problem as it applies to the business of manufacturing apparatus using impregnated-paper insulation. Also, since this research does not have direct bearing upon any war activity, it is believed that it would not be wise to continue it at present.

Another research, on insulating oils and cable saturants, project 74, under way at the Massachusetts Institute of Technology under the immediate supervision of Professor J. C. Balsbaugh, is somewhat newer than project 66 and is now operating on what might be called a production basis. It is recommended that this project be continued at least another year.

The Institute also sponsors jointly with the American Welding Society an extensive welding research program of the Engineering Foundation. This program is so actively supervised by the American Welding Society that the Institute has not undertaken a careful review of it. This does not indicate any lack of interest in electric welding, a science at present so vital to many of our war production activities.

In order that the committee on research may keep in close touch with the technical committees of the Institute and be of service to them when their work indicates that those committees should provide some research program, a research officer, so to speak, has been appointed in each of the technical committees. It is intended that this officer shall be the contact representative between his committee and the committee on research.

Perhaps the one acute problem facing us just now is the production of jewels for meter bearings. This is not definitely an electrical-engineering problem, but jewels for meter bearings are of such great importance to the whole electrical industry that any suggestion leading to the development of a supply of them will be of value.

Should the national situation remind any Institute member of things the committee on research should do, information regarding those recommendations will be welcome and the committee will do its best to turn all promising suggestions into active programs.

COMMITTEE ON SAFETY

The committee has continued its effort to encourage the Student Branches to hold meetings on safety. A circular letter was sent to each Student Branch again at the beginning of the autumn term urging them to hold meetings on safety and suggesting methods of arranging for interesting discussions. Another bulletin was sent at a later date informing them how to borrow two instructive motion-picture films for presentation at meetings on safety. There has been co-operation with the National Safety Council which has conducted a campaign on safety among the colleges.

A survey of the papers published in recent years on electrical safety was undertaken with the result that the committee has in preparation a bibliography of papers and articles on the subject that have been published during the past ten years.

At the suggestion of the committee, there was published in the September 1941 issue of *Electrical Engineering* a paper by G. S. Lawler, entitled "Fires of Electrical Origin in Factories," which had been presented previously at a meeting of the Lynn Section. In this paper, Mr. Lawler emphasized the importance of proper maintenance of electrical apparatus and equipment in prevention of fires and accidents. The committee believes that present war conditions make it more important than ever that electrical installations be maintained in good order.

The committee plans to continue active attention to these and other problems now before it.

Standards

STANDARDS COMMITTEE

While the past AIEE standards year has not been marked by any radically new developments, there was presented to the committee for action a greater-than-normal number of projects. This increase in activity, a continuation of last year's accelerated program, can of course be traced to wartime industrial conditions. The various standards co-ordinating committees and the technical committees of the Institute were equally responsible as the agencies for carrying out this growing program.

Eight projects were approved by the standards committee for publication, as follows:

Standards for Wet Tests, Revised Standards for Measurement of Test Voltages in Dielectric Tests, Revised Standards for Railway Motors, Rating of Electrical Apparatus for Short-Time and Intermittent Duty, Fuses Above 600 Volts, Capacitance Potential Devices, Test Code for D-C Machines, and Test Code for Single-Phase Motors.

The following projects are in various stages of committee development:

Guiding Principles for Selection of Reference and Limiting Values for Standards, Standards for Neutral Grounding Devices, Revised Standard for A-C Power Circuit Breakers, Revised Standard for Lightning Arresters, Revised Report on Standards for Mercury-Arc Rectifiers, Test Code for Impulse Measurements.

The American Standard "Definitions of Electrical Terms," defining over 4,000 electrical and allied terms, is now in the hands of the printer. In furtherance of the desire of the American Standards Association Sectional Committee on definitions to obtain the widest possible distribution for this publication, the AIEE directors authorized a minimum sales price of $1 per copy. This price was made possible only through a quantity production order for 25,000 copies. A national sales campaign is planned, which it is hoped will place not only the entire first edition, but will also require a quantity reprint. Individuals and organizations active in the electrical and allied fields are to be asked to co-operate. It is the belief of those who have given much time and thought to the definitions development that only through placement in the hands of all interested individuals can there be obtained a recognition of the real value inherent in the glossary and a justification for the immense number of man-hours spent in its development.

Two other matters of interest also came to a head during the year: the standards manual, and the standards numbering system. The manual was approved in final revised form and it is hoped will prove of great service in explaining the intricacies of standardization procedure and a valuable guide in future activities.

A study of the numbering systems used

by AIEE and ASA showed that it is impractical to select any system that will eliminate all inconsistencies. It also became evident that most of the present difficulties will eventually disappear.

The activities of Institute representatives on ASA sectional committees were continued, and in some cases considerably accelerated because of the war effort. Replacement of representatives was found necessary in a number of cases, largely because of occupational shifts. The organization of a new sectional committee on electric fences is now under way. All international standardization activity as carried on by the International Electrotechnical Commission is at a practical standstill as a result of the war. However, it is planned to push standardization as it affects South and Central America. One step in this direction is the proposed translation of Standards into Spanish and Portuguese.

UNITED STATES NATIONAL COMMITTEE OF THE INTERNATIONAL ELECTROTECHNICAL COMMISSION

The work of the International Electrotechnical Commission, because of the present international situation, has been practically suspended. The central office is being maintained, however, and continues to receive mail at its old address in London, although its valuable records have been moved to a place of safety "for the duration." The United States National Committee of the IEC also is keeping its organization intact and will continue to meet annually so that, as soon as circumstances permit, it will be possible to resume activities.

The USNC at its annual meeting in December 1941 re-elected its officers: E. C. Crittenden, president; L. F. Adams and H. S. Osborne, vice-presidents; Doctor C. H. Sharp, honorary president.

At its annual meeting, the USNC gave further consideration to the desirability of assisting in developing standardization work among the countries of North and South America. As a result of this consideration, the USNC decided to discharge its committee on the subject since the committee's exploration had shown that various other organizations were planning to undertake co-operative work with South American countries and that it seemed best for the USNC to abandon its efforts and to assist where it could the work of other agencies, particularly that of the ASA.

Technical Committees

COMMITTEE ON AUTOMATIC STATIONS

The bibliography on automatic stations covering the period 1932–1940 has been extended to cover 1941, plus a few additional references for 1940, and is now in the hands of the publication committee.

The committee prepared a questionnaire on a-c automatic reclosing equipment applying to stub feeders only. A subcommittee has distributed copies throughout the various Districts, including Canada, and the material collected from this survey will be included in a future report.

A questionnaire on pilot conductors has been received by the relay subcommittee of the committee on protective devices, with which the questionnaire was jointly issued. That subcommittee has issued a preliminary report pertaining to that section of the survey in which it is interested. It is expected that in the near future certain portions of the collected material will be turned over to the committee on automatic stations for classifying the data in which it is interested.

COMMITTEE ON EDUCATION

At the 1941 summer convention, the subcommittee on student guidance reported on its work during the past year. From a questionnaire sent to all the Institute Sections 59 replies were received, of which 20 indicated that those Sections had been carrying on some form of high-school student guidance during the previous year. Of the other 39, 32 stated that they were interested in this kind of work and would welcome suggestions as to how to proceed.

The subcommittee on student guidance was continued for the present year, and an attempt has been made to help those Sections which are interested in carrying on guidance work for the benefit of high-school seniors. The results of this work have not yet come in.

The committee held an informal meeting at the time of the South West District meeting in October 1941, and decided to sponsor an educational session at the winter convention the following January. Three papers were secured and approved for presentation at this convention—one of them for publication in the *Transactions*. The presentation of the papers brought out much interesting discussion.

During the 1942 winter convention, the committee held its annual meeting and discussed the problems of education in the light of the present war emergency. Any move toward the shortening of the academic time spent by students in courses in electrical engineering and any abbreviation of the net content of courses were deplored. The speeding-up of graduation by the elimination of vacations was approved. A resolution to this effect was transmitted to the board of directors, and was approved at the next meeting of the board.

After this meeting, the difficulty of carrying on inspection trips for the benefit of engineering students during the war emergency was discussed and the possibility of showing motion pictures of industrial plants instead was mentioned. A subcommittee on inspection trips was appointed to look into the matter of accumulating information about films that might be secured.

The committee decided to sponsor a discussion session on education at the 1942 summer convention in Chicago, Ill., but to ask for no formal papers on account of the difficulty of getting them written during the war emergency.

For the North Eastern District meeting, the committee approved one paper on the subject of vocational education.

COMMITTEE ON ELECTRICAL MACHINERY

The committee held two meetings during the year, one at the summer convention in Toronto in June 1941, and one at the winter convention in New York in January 1942, at which technical sessions at conventions were discussed and the work of the committee and its subcommittees was reviewed. Plans were made for the 1942 summer convention, to sponsor one symposium on emergency overloading of apparatus and one on mercury-arc rectifiers and their application.

The subcommittee on synchronous machinery has progressed in its work on the revision of the Test Code on Synchronous Machinery and is planning on having the revision prepared and ready for review by June 1942.

The transformer subcommittee continued its co-operation with the ASA committee in the review of the revised ASA Standards on transformers. Papers have been presented and are being prepared for presentation on overloading of transformers for emergency conditions.

The subcommittee on test codes for single-phase motors completed its work on the test code which was submitted to the standards committee. It was issued by the standards committee in November 1941 as the AIEE Proposed Test Code for Single-Phase Motors.

The Test Code for D-C Machines, submitted last year to the standards Committee, was issued by that committee in July 1941.

The subcommittee on rectifiers has, among other things, been studying definitions and a paper was prepared for presentation at the North Eastern District meeting with a view toward unifying terminology. This is of some importance as common practice has not been uniform in terms and meanings in all cases.

The subcommittee on insulation resistance has started work with the object of producing a standard on methods of measuring insulation resistance and a guide for the interpretation of the results obtained.

The committee sponsored one session at the summer convention in 1941 at which five papers on miscellaneous subjects were presented. At the Pacific Coast convention, one paper approved by the committee was presented. At the South West District meeting, one session was sponsored jointly by the committees on electrical machinery and power generation, and several papers on motors were presented. One session on power and machinery and a conference on mercury-arc rectifiers were arranged for the North Eastern District meeting.

COMMITTEE ON ELECTROCHEMISTRY AND ELECTROMETALLURGY

As a result of the pressure of work incident to the war there has been little committee activity during the year. At the meeting held on January 26, 1942, the progress report of the subcommittee on voltage transients in arc-furnace circuits was discussed, and it was voted to continue the study being carried on by the subcommittee. It was requested that any instances of severe transients or short circuits on arc-furnace circuits be reported to N. R. Stansel, subcommittee chairman.

The need for standardization of transformers for electric-arc-furnace operation was also presented. On motion duly approved, President Prince was requested to appoint a joint subcommittee consisting of representatives of the committee on electrochemistry and electrometallurgy and the transformer section of the committee on electrical machinery to consider the standardization of electric-arc-furnace transformers.

The rapidly increasing demand for electric power by electrothermal and electrochemical plants was discussed. It was pointed out that within the next two to three years these operations will require an additional 20 billion kilowatt-hours per year in the production of aluminum, magnesium, chlorine, stainless steel, and similar war materials.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

Two major committee projects were completed. The report on "Telemetering, Supervisory Control, and Associated Circuits," prepared by a joint subcommittee of the committees on automatic stations and on instruments and measurements has been issued in pamphlet form. This encyclopedic report classifies and tabulates the characteristics of the various types of telemetering equipment and should form a useful basis of any future standardization. The report on "Progress in the Art of Metering Electric Energy" prepared by the subcommittee on watt-hour meters was published in serial form in *Electrical Engineering*, beginning with the September 1941 issue and concluding with the December issue. The report is now available in pamphlet form also. It constitutes a complete and authoritative history of the development of the d-c and induction types of watt-hour meters, and includes also a short history of the development of the demand meter.

Technical sessions on instruments and measurements were held at the 1941 summer convention in Toronto and at the 1942 winter convention, at which five, and four papers, respectively, were presented. The committee also sponsored a group of papers stimulated by its subcommittee on dielectric measurements in the field and presented at the session on insulation testing at the 1941 summer convention. The pressure of military work and the confidential nature of new developments probably will seriously curtail the output of technical papers in the immediate future.

In view of the necessity for conserving our energies, a questionnaire was circulated early in the year to stimulate a critical examination of the various standardization activities of the committee. At the fall meeting, it was decided to leave certain lines of work in abeyance and to concentrate on the more pressing problems.

The subcommittee on the revision of AIEE Standard No. 4 has prepared an addition to the table of sphere-gap voltages for 6.25- and 12.5-centimeter spheres which will permit their use up to spacings equal to their diameters. This has been approved by the committee on instruments and measurements and by the standards committee. This subcommittee which is identical in personnel with the corresponding ASA Sectional Committee, remains in readiness for any future standardization in this field.

The subcommittee on definitions has been active in revising certain definitions for the ASA "Dictionary" C42 and in securing agreement between these definitions and those used in the "Code for Electricity Meters" and the "Meterman's Handbook."

The subcommittee on current transformers has participated in the revision of section 4 of ASA Standard C57 for Transformers, Regulators, and Reactors, in the light of the year's experience with this "Proposed American Standard."

A new subcommittee was appointed to prepare a master code for temperature measurements and has made very good progress.

More recently another subcommittee has been formed to study the report on standards for electrical recording instruments, AIEE No. 40, and to revise it, if necessary, in order that it may be advanced to full standard status.

The committee continues to be represented by liaison members on several other technical committees of the Institute.

COMMITTEE ON MARINE TRANSPORTATION

The activities of this committee have been very greatly curtailed because of war conditions and pressure of work upon all members engaged in shipbuilding and associated industries.

A meeting was held on December 12, 1941. Several changes in Standard No. 45 were approved by the committee, and an addendum issued for the changes approved since the issue of the addenda dated March 20, 1941. The committee has endeavored to keep this Standard up to date to conform to the best practice in shipbuilding. War conditions and scarcity of various materials have necessitated deviation from materials specified therein. The committee decided that such temporary changes should not be incorporated in Standard No. 45. This matter is one that the various manufacturers can best handle to suit their own particular cases.

The subcommittee on wire and cables has been active, and has done considerable work on suggested revisions proposed by the Insulated Power Cable Engineers Association. The purpose of the proposed revisions is to bring the specification for wire and cables in Standard No. 45 more nearly in conformity with the IPCEA standards.

A special subcommittee was appointed to confer with the American Bureau of Shipping on matters relating to the general scope and purpose of Standard No. 45. This matter will require considerable study by the subcommittee before it can be presented to the main committee for consideration. It is doubtful if final action can be taken this year, and it will probably be carried over into the next fiscal year.

COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION

The committee is now completing the second year of its activities under the new organization with four main subcommittees as follows: substations, transmission, distribution, and general systems. Each subcommittee functions through working groups, the number of which depends on the subject problems coming within its scope.

During the past year, the main committee's activities have been confined largely to sponsoring technical sessions at conventions and reviewing technical papers. The committee sponsored three complete sessions at the winter convention, and likewise is planning three technical sessions at the 1942 summer convention. At the winter convention, one session organized by the committee was devoted to the broad and timely subject, "Distribution Systems in Wartime." Another session was devoted to lightning. One session wholly on cables, and another on lightning, are planned for the summer convention. Other papers sponsored by the committee cover subjects associated directly with the transmission and distribution art, while some cover fields of interest to other committees and are therefore under joint sponsorship.

The proposed revision of Standard No. 41, Insulator Tests, issued in March 1941, is being worked on at the present time with the expectation that it will soon be offered for approval as an AIEE standard. So few comments have so far been received that it is believed this standard can be offered for approval shortly.

The activities of the main committee have been considerably restricted on account of the war conditions; and, as there have not been in evidence problems of immediate concern or importance apparently requiring attention of the committee, the calling of frequent meetings has not seemed justified. Likewise, the work of the subcommittees and working groups has been considerably restricted on account of present wartime conditions. A number of subjects of interest are being considered by these groups, but sufficient progress has not yet been made to report on now.

As many members of the committee are closely associated with problems arising from United States war activities, there has not appeared justification for making heavy demands on the time of these men for committee work which is not mandatory.

COMMITTEE ON PRODUCTION AND APPLICATION OF LIGHT

The committee has continued to keep members of the Institute informed on the latest developments in illuminating engineering, principally through the sponsorship of conferences at District meetings and national conventions and by obtaining articles from qualified authors on illumination subjects for publication in *Electrical Engineering*.

All of these activities have been intensified this year, and plans are now under way to extend them further, especially in those lighting fields which are vital to national defense. A complete report on a conference on civilian-defense lighting held during the 1942 winter convention appeared in *Electrical Engineering* for March 1942, pages 155–6.

A conference on lighting for production is now under consideration for the summer convention in June 1942. Prominent speakers from Canada and the United States will be asked to prepare discussions on this subject which is vital to the defense of each country.

In addition to reviewing all papers on the subject of lighting which are presented for consideration by the technical program committee, and selecting timely lighting subjects and encouraging competent authors to prepare articles for *Electrical Engineering*, this committee also sponsors in *Electrical Engineering* brief summaries and photographs of notable lighting installations.

COMMITTEE ON PROTECTIVE DEVICES

The committee held three meetings during the year and probably will hold another at the 1942 summer convention. Attendance was good, and the discussions dealt mainly with new and revised standards, apparatus insulation levels, temperatures for various types of switchgear, subcommittee reports on subjects under consideration, and papers proposed for technical sessions.

In keeping with the preparedness and war effort, the committee has attempted to bring to completion as rapidly as possible work on the various Standards under its jurisdiction. This appeared very desirable and necessary in order that production during the war period can be based on Standards which reflect proper requirements and present practice. Two new Standards (Nos. 31 and 32) were prepared, approved, and transmitted to the standards committee with recommendation for approval and publication as AIEE Standards. Four Standards (Nos. 19, 22, 27, and 28), which had been issued the previous year for trial use, were completely reviewed and recommended revisions approved. Two other Standards (Nos. 24 and 25) were reviewed. One was recommended to be left unchanged for the time being and work on revision of the other has not been completed.

The major work of the committee was divided among subcommittees: on circuit breakers, switches, and fuses; fault-current limiting devices; lightning arresters; and relays.

The reports of the subcommittees indicate that very substantial progress has been made, especially when it is realized that all members have had unusually large demands on their time for war purposes in connection with their regular work. Consequently, their participation in committee activities, while active and co-operative, have been under somewhat of a handicap.

The following three reports covering a decade of progress in the art were completed and published in *Electrical Engineering:* "Ten Years of Progress in Circuit Interrupters" November 1941; "Ten Years of Progress in Relaying," December 1941; "Ten Years of Progress in Lightning Protection," April 1942.

The committee also has been responsible for the preparation of two extensive bibliographies in its field. One, covering relay literature for the period 1927–39, was prepared by the relay subcommittee and issued as a special Institute publication in July 1941. The second, a comprehensive bibliography on circuit-interrupting devices, including circuit breakers, enclosed switchgear, air switches, bus bars, and fuses, and covering the period 1928–40, is currently in process of publication.

The committee sponsored 13 papers at the 1941 summer convention, 7 at the Pacific Coast convention, 2 at the South West District meeting, one at the Southern District meeting, 19 at the 1942 winter convention, and 1 at the North Eastern District meeting.

The future work of the committee may logically follow along lines of endeavor similar to those of the past. In view of the industry needs, every effort should be made to complete at an early date the proposed standard on distribution-type protector tubes and the revision of Standard No. 25, Fuses Above 600 Volts, as well as the technical report dealing with the subject of selection of neutral-grounding-device impedance.

Awards

COMMITTEE ON AWARD OF INSTITUTE PRIZES

Four national and 11 District prizes were awarded for papers presented during the calendar year 1941 and for the student papers presented during the academic year ending June 30, 1941.

A large number of eligible papers, which were of good quality, were considered, and in determining the awards for the national best paper prizes and the initial paper prize the committee had the benefit of the gradings and recommendations of the technical committees which reviewed the papers. In addition to the four papers awarded national prizes, eight other papers were given honorable mention.

The changes made in the rules on award of "National and District Prizes," issued in the new edition of January 1941, have been in effect for more than an entire year, and have been found to be working satisfactorily.

EDISON MEDAL

The Edison Medal, which is awarded by a committee composed of 24 members of the Institute, was awarded for 1941 to Doctor John Boswell Whitehead, "for his contributions to the field of electrical engineering, his pioneering and development in the field of dielectric research, and his achievements in the advancement of engineering education," and was presented on January 28, 1942, during the winter convention. The medal may be awarded annually for "meritorious achievement in electrical science, electrical engineering, or the electrical arts."

LAMME MEDAL

The Lamme Medal committee awarded the medal for 1941 to Forrest E. Ricketts, vice-president, Consolidated Gas Electric Light and Power Company, Baltimore, Md., "for his contribution to the high reliability of power-supply systems, especially in the design of apparatus for selective relaying and circuit reclosure." Arrangements are being made for the presentation of the medal at the summer convention in Chicago, Ill., June 22–26, 1942. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery."

JOHN FRITZ MEDAL

The John Fritz Medal board of award, composed of representatives of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE, awarded the 38th medal (for 1942) to Everett Lee DeGolyer, consulting petroleum engineer, Dallas, Tex., and a past president of AIME, for "pioneer work in the application of geophysical exploration to the search for oil fields."

HOOVER MEDAL

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The fifth medal, for 1941, was awarded to D. R. Yarnall, cofounder and chief engineer, Yarnall-Waring Company, Chestnut Hill, Pa., a member of ASME and a past president of United Engineering Trustees.

ALFRED NOBLE PRIZE

This prize, established in 1929, consists of a certificate and a cash award from the income from a fund contributed by engineers and others to perpetuate the name and achievements of Alfred Noble, past president of ASCE, and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1940–41 was presented to R. F. Hays, Jr. (A'36), Sperry Gyroscope Company, Garden City, N. Y., for his paper "Development of the Glow Switch" (published in AIEE *Transactions*, volume 60, 1941, May section, pages 223–6).

WASHINGTON AWARD

The Washington Award for 1942 was bestowed upon William Lamont Abbott (F'13) retired chief operating engineer, Commonwealth Edison Company, Chicago, Ill., and a past president of both ASME and WSE, "for advancing the standards of the engineering profession for service to higher education for aiding combustion research," and was presented to him at a dinner on February 26, 1942. This award may be made annually to an engineer by the commission of award composed of nine representatives of the Western Society of Engineers and two each of ASCE, AIME, ASME, and AIEE.

CHARLES LE GEYT FORTESCUE FELLOWSHIP COMMITTEE

The committee met in March to decide upon the award of the fellowship for the year 1942–43. In view of the very small number of candidates, with a further possibility that any one of them might be called for some form of service with the government, it was decided that no fellowship would be awarded this year. This action is the same as was taken last year for similar reasons.

Joint Activities

UNITED ENGINEERING TRUSTEES, INC.

This organization is the corporate body which holds title in the name of the four Founder Societies to their joint physical properties, namely, the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation. It operates and manages the Engineering Societies Building, and administers certain joint activities of the four Founder Societies.

The building is fully occupied by the Founder Societies and associates. During the year, certain changes were made to improve the services to members of the societies and others, notably improvement in the illumination of the library by the installation of fluorescent lamps, changes in elevator doors on several floors, reconstruction of one floor to provide a greater number of offices, and the change of the electricity supply system from direct to alternating current.

An abstract of the annual report of the corporation for the year which ended September 30, 1941, appeared on pages 56–57 of *Electrical Engineering* for January 1942.

ENGINEERING FOUNDATION

The Engineering Foundation is a joint organization of the national societies of civil, mining and metallurgical, mechanical, and electrical engineers established for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind." It assists in a wide range of research projects of interest to science, engineering, industry, and the general public, each project being under the sponsorship of one of the Founder Societies.

Under the sponsorship of the Institute, the Foundation has continued to support the research on stability of impregnated-paper insulation, at the Johns Hopkins University, the research on insulating oils and cable saturants, at the Massachusetts Institute of Technology, and the welding research, the latter under joint sponsorship with the American Welding Society.

A comprehensive abstract of the annual report of the Engineering Foundation for the year which ended September 30, 1941, appeared on pages 57–59 of *Electrical Engineering* for January 1942.

ENGINEERING SOCIETIES LIBRARY

The Engineering Societies Library, which was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, and the preparation of a composite card catalog, probably constitutes the best collection of its type in the United States.

On September 30, 1940, the library had 152,263 volumes, 7,755 maps, and 4,492 searches. Books and pamphlets totaling 3,021 were received during the year ending at that time. Current issues of 1,124 periodicals were received. About 20,000 entries were added to the index of periodicals, which now contains over 260,000 cards.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail, and others. The library board has been considering means by which members might be more adequately informed regarding the services available.

An abstract of the annual report of the library appeared on pages 59–60 of *Electrical Engineering*, January 1942.

EMPLOYMENT SERVICE

Operating as an incorporated non-profit-making organization, the Engineering Societies Personnel Service, Inc., assists members of the Founder Societies desiring to secure new positions, and conversely assists employers searching for engineers. Nonmembers may receive such assistance when positions available cannot be filled by members.

Offices are operated in New York, N. Y., Detroit, Mich., Chicago, Ill., and San Francisco, Calif., with the co-operation of the Engineering Society of Detroit in that city, the Western Society of Engineers in Chicago, and the Engineers Club of San Francisco in that city.

The service is supported by the joint contributions of the societies and the individuals who secure positions. An analysis of registration and placement records as reported to the national societies is given in table XI.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

This Council, organized in 1932 to engage in activities leading toward the enhancement of the professional status of the engineer, includes three representatives of each of the eight participating organizations, which are the national societies of civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, the National Council of State Boards of Engineering Examiners, and the Engineering Institute of Canada. Its principal activities have been carried on by four committees: student selection and guidance, engineering schools, professional training, and professional recognition.

Including the actions on engineering curricula taken on October 30, 1941, the record is:

Total curricula submitted, including reinspections	896
Accredited	460
Accredited provisionally	105
Not accredited	167
Reinspections with no change in status	132
Action pending	6

A comprehensive report on the annual meeting held on October 30, 1941, and a complete list of accredited curricula appeared on pages 606–10 of *Electrical Engineering* for December 1941.

JOINT CONFERENCE COMMITTEE

Upon recommendation of the committee on planning and co-ordination, the board of directors authorized the president and national secretary to join with like officers of the other Founder Societies in an informal conference committee to meet whenever deemed advisable to consider joint efforts in national defense and other matters. The committee met in Indianapolis, Ind., Washington, D. C., Montreal, Que., and Roanoke, Va.

In accordance with its recommendations, the societies organized a joint committee on postwar planning and a joint committee on

Table XI. Analysis of Employment Service

	Men Registered					Men Placed				
Month	New York	Chicago	San Francisco	Detroit	Total	New York	Chicago	San Francisco	Detroit	Total
1941										
May	154	67	86	23	330	61	21	30	12	124
June	193	100	68	79	440	52	20	24	22	118
July	151	84	61	60	356	63	31	26	15	135
August	126	94	76	56	352	41	17	45	15	118
September	144	66	50	43	303	38	15	32	12	97
October	156	84	65	34	339	37	22	16	10	85
November	139	66	46	42	293	37	15	32	10	94
December	115	49	41	28	233	45	18	27	11	101
1942										
January	160	65	58	32	315	41	20	40	12	113
February	148	51	50	37	286	31	29	19	14	93
March	139	60	67	32	298	43	26	34	16	119
April	138	44	49	26	257	49	29	23	10	111
Total	1,763	830	717	492	3,802	538	263	348	159	1,308

inter-American engineering co-operation, It considered various other matters of interest to members of the several societies.

REPRESENTATIVES

In addition to the many divisions of its work represented by its own general and technical committees, the Institute participates in a wide range of activities of interest and importance to engineers and others through its representation upon about 30 joint committees and national organizations.

A list of representatives was published in the September 1941 issue of *Electrical Engineering* and in the 1942 Year Book.

Appreciation

On behalf of the membership, the board of directors expresses its sincere appreciation of the many important contributions to Institute activities made by the national committees and the District, Section, and Branch officers. Despite the extreme difficulties encountered during the past year, and consequent severe restrictions of time available for engineering society activities, enthusiasm for and constructive accomplishment in Institute work have continued at a high level. The continuing keen interest among the members in general has contributed substantially to the accomplishments of the Institute. The board thanks all for their interest and unselfish co-operation.

Respectfully submitted for the board of directors.

H. H. HENLINE
May 22, 1942 *National Secretary*

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value, April 30, 1942

Schedule 1

	Principal Amount of Bonds or Number of Shares of Stock	Restricted Funds: Reserve Capital Fund	Restricted Funds: International Electrical Congress Library Fund	Restricted Funds: Lamme Medal Fund	Restricted Funds: Total Restricted Funds	Property Fund (Equipment Replacements)
Railroad Bonds:						
Alleghany Corporation 20 year collateral trust convertible 5%, due 1949	$15,000.00	$ 10,627.50			$ 10,627.50	
Baltimore & Ohio Railroad Company 6% refunding and general mortgage series C, due 1995 (stamped)	12,000.00	8,940.00		$4,330.00	13,270.00	
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00	1,105.00			1,105.00	
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974 (certificates of deposit)	10,000.00*	9,818.75*			9,818.75*	
New York Central Railroad Company 5% refunding and improvement mortgage series C, due 2013	6,000.00	5,742.50			5,742.50	
Northern Pacific Railway Company 6% refunding and improvement mortgage series B, due 2047	10,000.00	10,962.50			10,962.50	
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00*	5,497.50*			5,497.50*	
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00*	7,225.00*			7,225.00*	
Total railroad bonds		$ 59,918.75		$4,330.00	$ 64,248.75	
Public Utility Bonds:						
New York & Queens Electric Light & Power Company 3½% first and consolidated mortgage, due 1965	10,000.00	$ 11,000.00			$ 11,000.00	
Bond and Real Estate Mortgage:						
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.) 4%, due 1944	14,152.05*	$ 13,208.32*			$ 13,208.32*	$943.73*
United States Government Bonds:						
Treasury Savings bonds series D, due July 1, 1949	10,000.00	$ 7,644.00			$ 7,644.00	
Treasury Savings bonds series D, due January 1, 1950	10,000.00	7,500.00			7,500.00	
Treasury bonds 2%, due 1950/48	77,000.00	78,130.94			78,130.94	
Treasury bonds 2½% due 1972/67	6,300.00	1,100.00	$5,200.00		6,300.00	
Defense bonds series F, due June 1, 1953	34,000.00	25,160.00			25,160.00	
Total United States Government bonds		$119,534.94	$5,200.00		$124,734.94	
Capital Stocks:						
American Can Company	60 shares	$ 4,988.40			$ 4,988.40	
American Telephone & Telegraph Company	30 shares	4,897.95			4,897.95	
Commonwealth Edison Company	200 shares	7,580.68			7,580.68	
Eastman Kodak Company	35 shares	4,768.23			4,768.23	
E. I. du Pont de Nemours Company	40 shares	6,398.20			6,398.20	
General Electric Company	130 shares	4,463.80			4,463.80	
International Harvester Company	100 shares	5,030.50			5,030.50	
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 shares*	2,094.15*			2,094.15*	
Standard Oil Company of New Jersey	110 shares	4,791.78			4,791.78	
Union Carbide & Carbon Corporation	70 shares	4,858.35			4,858.35	
Total capital stocks		$ 49,872.04			$ 49,872.04	
Total		$253,534.05	$5,200.00	$4,330.00	$263,064.05	$943.73
*Less reserve in full for the securities designated *, considered to be of doubtful value		$ 37,843.72			$ 37,843.72	$943.73
Total Securities, Less Reserve		$215,690.33	$5,200.00	$4,330.00	$225,220.33	

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

22 EAST 40TH STREET
NEW YORK

May 15, 1942

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have made an examination of your balance sheet as of April 30, 1942, and of your recorded cash receipts and disbursements for the year ended that date. Our examination consisted of a review of the system of internal control and of the accounting procedures of the Institute and examination or tests of its accounting records and other supporting evidence by methods and to the extent we deemed appropriate. We present the following:

Balance Sheet, April 30, 1942 (Exhibit A).
Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value (Schedule 1).
Statement of Recorded Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1942 (Exhibit B).
Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1942 (Exhibit C).

In our opinion, the accompanying Exhibit A fairly presents your financial condition at April 30, 1942, and the accompanying Exhibits B and C fairly present your recorded cash receipts and your disbursements of funds, as indicated, for the year ended that date.

Yours truly,

(Signed) HASKINS & SELLS

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet, April 30, 1942

Exhibit A

ASSETS		
Property Fund Assets:		
One-fourth interest in physical properties of United Engineering Trustees, Inc.:		
Land, buildings, and equipment (less depreciation and renewal reserve)	$385,367.94	
Funded depreciation and renewal reserve	113,080.54	
Total	$498,448.48	
Equipment:		
Library—volumes and fixtures	37,296.37	
Office furniture and fixtures (less reserve for depreciation, $24,973.05)	9,764.34	
Works of art, etc.	3,001.35	
Investment, less reserve in full $943.73—Schedule 1		
Total property fund assets		$548,510.54
Restricted Fund Assets:		
Securities—at cost, less reserve, $37,843.72 (quoted market value, $203,652.82), Schedule 1	$225,220.33	
Cash (Exhibit C)	63,751.26	
Accrued interest receivable	36.25	
Total restricted fund assets		289,007.84
Operating Fund Assets:		
Cash (Exhibit B)	$ 16,633.48	
Accounts receivable:		
Members—for dues (less reserve, $8,000.00)	10,861.98	
Advertisers	1,424.03	
Miscellaneous	4,158.44	
Accrued interest receivable	1,812.04	
Inventories:		
Transactions, etc.	1,277.25	
Text and cover paper	6,492.70	
Work in process (May issue of *Electrical Engineering*, etc.)	6,513.99	
Badges	1,345.97	
Total operating fund assets		50,519.88
Total		$888,038.26

LIABILITIES		
Property Fund Reserves		$548,510.54
Restricted Fund Reserves:		
Reserve Capital fund	$251,211.20	
Pension fund	18,000.00	
Life Membership fund	8,805.91	
International Electrical Congress of St. Louis Library fund	5,358.05	
Lamme Medal fund	4,556.83	
Mailloux fund	1,075.85	
Total restricted fund reserves		289,007.84
Operating Fund Reserves, Liabilities, Etc.:		
Accounts payable	$ 13,113.38	
Deferred income:		
Dues received in advance	4,886.98	
Entrance fees and dues advanced by applicants for memberships	659.60	
Deferred credits for other unallocated receipts	487.55	
Subscriptions for *Transactions* received in advance	45.16	
Reserve for prepaid subscriptions for *Electrical Engineering*	5,066.74	
Operating fund reserves	26,260.47	
Total operating fund reserves, liabilities, etc.		50,519.88
Total		$888,038.26

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Recorded Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1942

Exhibit B

Cash on Deposit With the National City Bank of New York, May 1, 1941		$ 41,617.90
Receipts:		
Dues (including $96,060.00 allocated to *Electrical Engineering* subscriptions)	$212,948.30	
Advertising	59,619.75	
Transactions subscriptions	6,933.62	
Electrical Engineering subscriptions	9,183.16	
Miscellaneous publications (preprints, Standards, *Transactions* index, etc.)	20,114.89	
Students' fees	13,389.00	
Entrance fees	9,420.85	
Membership badges	2,577.19	
Transfer fees	1,663.04	
Interest and dividends on investments of Restricted Capital fund	7,822.83	
Miscellaneous	16.95	
Total receipts		343,689.58
Total		$385,307.48
Disbursements:		
Publications expense:		
Electrical Engineering	$ 95,951.25	
Transactions	19,805.15	
Year Book	6,988.69	
Miscellaneous publications (preprints, Standards, *Transactions* supplement, etc.)	14,566.26	
Institute meetings	13,759.99	
Institute Sections	37,694.33	
Institute Branches	3,446.66	
Edison Medal committee	188.10	
Finance committee	771.23	
Headquarters committee	193.21	
Membership committee	8,828.86	
Pension fund committee	250.00	
Standards committee (including $1,132.43 for "Definitions of Electrical Terms")	9,867.82	
Technical committees	647.96	
Forward	$212,959.51	$385,307.48
Total (forward)		$385,307.48
Disbursements (forward)	$212,959.51	
Traveling expenses:		
Geographical Districts:		
Executive committees	3,863.21	
Vice-presidents	823.38	
Branch counselors and chairmen	10,844.76	
President's appropriation	1,700.78	
Board of directors	6,516.84	
National nominating committee	1,121.49	
Administrative expenses	50,953.79	
Geographical Districts:		
Initial paper prizes	75.00	
Branch paper prizes	340.50	
Institute prizes, national	215.00	
American Co-ordination Committee on Corrosion	25.00	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	10,984.81	
Library assessment	10,182.56	
Engineering Societies Personnel Service, Inc.	1,210.10	
Engineers' Council for Professional Development	1,275.00	
Engineers Defense Board	166.67	
Engineering Foundation Projects:		
Insulating oils and cable saturants	250.00	
Welding research	250.00	
Research on impregnated paper insulation	250.00	
National Fire Protection Association—Dues	60.00	
Membership badges	2,643.40	
Legal services	250.00	
Transfers:		
To Pension fund	8,000.00	
To Reserve Capital fund	40,160.00	
To Life Membership fund	2,000.00	
Miscellaneous	52.20	
Total disbursements		368,674.00
Cash on Deposit With the National City Bank of New York, April 30, 1942		$ 16,633.48

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1942

Exhibit C

	Restricted Funds							
	Reserve Capital Fund	**Life Membership , Fund**	**International Electrical Congress of St. Louis Library Fund**	**Lamme Medal Fund**	**Mailloux Fund**	**Pension Fund**	**Total Restricted Funds**	**Property Fund (Equipment Replacements)**
Cash on Deposit With the National City Bank of New York and Various Savings Banks, May 1, 1941	$27,095.95	$3,473.34	$5,289.37	$ 24.00	$1,059.87	$10,000.00	$ 46,942.53	$2,738.98
Receipts:								
Interest on bonds and dividends on stock		$ 144.44	$ 52.43	$624.00			$ 820.87	
Interest on bank balance		53.20			$ 15.98		69.18	
Proceeds from sale of securities	$14,103.83	3,134.93					17,238.76	
Transfer from operating fund	40,160.00	2,000.00				$ 8,000.00	50,160.00	
Miscellaneous								$ 0.19
Total receipts	$54,263.83	$5,332.57	$ 52.43	$624.00	$ 15.98	$ 8,000.00	$ 68,288.81	$ 0.19
Total	$81,359.78	$8,805.91	$5,341.80	$648.00	$1,075.85	$18,000.00	$115,231.34	$2,739.17
Disbursements:								
Purchase of securities	$45,838.91		$5,200.00				$ 51,038.91	
Equipment replacements, alterations, and repairs								$2,739.17
Gold and bronze replicas of Lamme medal and certificate				$229.30			229.30	
Transfer to operating fund—reimbursement of prior years' expenses				211.87			211.87	
Total disbursements	$45,838.91		$5,200.00	$441.17			$ 51,480.08	$2,739.17
Balance on Deposit With the National City Bank of New York and Various Savings Banks, April 30, 1942	$35,520.87	$8,805.91	$ 141.80	$206.83	$1,075.85	$18,000.00	$ 63,751.26	

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H. R. Summerhayes
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.M. Towner
J. D. Wood

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J. E. Clem, Fault Current Limiting Devices
I. W. Gross, Lightning Arresters
E. L. Harder, Relays
R. T. Henry, Circuit Breakers, Switches, and Fuses

Therapeutics, Applications of Electricity to

C. V. Aggers, *chairman*, Westinghouse Electric and Manufacturing Company, X-Ray Division, 2519 Wilkins Avenue, Baltimore, Md.

Lloyd L. Call
W. D. Coolidge
Roy Kegerreis
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Power Generation

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Fraser Jeffrey
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C. W. Mayott
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C. R. Reid
M. J. Steinberg
H. D. Taylor
Robert Treat

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R. P. Crippen, Emergency Measures to Increase Output of Generating Systems

C. M. Laffoon, Emergency Measures to Increase Output of Generating Equipment

M. J. Steinberg, Joint AIEE-ASME Committee on Specifications for Prime Mover Speed Governors

Power Transmission and Distribution

Philip Sporn, *chairman*, American Gas and Electric Service Corporation, 30 Church Street, New York, N. Y.

H. E. Wulfing, *vice-chairman*, 72 W. Adams Street, Chicago, Ill.

I. W. Gross, *secretary*, American Gas & Electric Service Corporation, 30 Church St., New York, N. Y.

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J. J. Torok
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L. R. Gaty, Stations
W. W. Lewis, Transmissions
C. F. Wagner, General Systems

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V. M. Montsinger
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E. L. Moreland
E. B. Paxton

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H. B. Gear
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Parker and Aaron
20 Exchange Place, New York, N. Y

Student Branches of the Institute

Name and Location	District	Counselor (Member of Faculty)	Chairman	Secretary
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Alabama Polytechnic Inst., Auburn, Ala.	4	W. W. Hill	J. W. Barr	W. H. Keith
Alabama, Univ. of, University, Ala.	4	W. J. Miller	J. Edmonds	R. N. Whitehurst
Alberta, Univ. of, Edmonton, Alberta, Can.	10	E. G. Cullwick	H. T. Stevenson	T. Cardwell
Arizona, Univ. of, Tucson, Ariz.	8	J. C. Clark	K. C. Andrus	H. Jacobs
Arkansas, Univ. of, Fayetteville, Ark.	7	A. S. Brown	T. O. Stanfield	V. D. Hawkins
British Columbia, Univ. of, Vancouver, Can.	10	W. B. Coulthard	B. Elliott	J. Gray
Brooklyn, Poly. Inst. of, Brooklyn, N. Y.	3	A. B. Giordano		
(Day Division)			W. Becker	J. Rutledge
(Evening Division)			E. Kappauf	L. L. Lipschultz
Brown Univ., Providence, R. I.	1	F. N. Tompkins	P. C. Armor	E. F. Weitz, Jr.
Bucknell Univ., Lewisburg, Pa.	2	J. B. Miller	N. Parkin	J. Raudenbush
Calif. Inst. of Tech., Pasadena, Calif.	8	F. W. Maxstadt	E. Atkins	W. Snyder
Calif., Univ. of, Berkeley, Calif.	8	C. F. Dalziel	J. Jondrow	R. Wing
Carnegie Inst. of Tech., Pittsburgh, Pa.	2	G. R. Patterson	Wm. Shafer	D. Lyon
Case School of Applied Science, Cleveland, Ohio	2	P. L. Hoover	B. Hyman	G. M. Kastner
Catholic Univ. of America, Washington, D. C.	2	T. J. MacKavanagh	J. Laird	Wm. C. Brady
Cincinnati, Univ. of, Cincinnati, Ohio	2	L. R. Culver	C. Sheridan	M. Knapp
Clarkson College of Tech., Potsdam, N. Y.	1	A. R. Powers	F. Brown	W. Parnell
Clemson Agri. College, Clemson, S. C.	4	F. T. Tingley	E. G. Orahood	R. L. Buchanan
Colorado State Col. of A. & M. Arts, Fort Collins, Colo.	6	F. B. Beatty	J. Bittner	E. Nitschke
Colorado, Univ. of, Boulder, Colo.	6	H. B. Palmer	C. P. Spaulding	W. J. Stroh
Columbia Univ., New York, N. Y.	3	J. R. Ragazzini	E. Buyer	C. P. Thomas
Connecticut, Univ. of, Storrs, Conn.	1	G. S. Timoshenko	L. Doerr	W. Prelle
Cooper Union, New York, N. Y.	3	E. E. Shelton		
(Day Division)			E. I. Herszkorn	H. Grad
(Evening Division)			S. Rogat	J. H. Mulligan, Jr.
Cornell Univ., Ithaca, N. Y.	1	R. W. Ager	C. Seeger	D. B. Wheeler
Delaware, Univ. of, Newark, Del.	2	M. G. Young	Wm. Schuster	C. Perry
Denver, Univ. of, Denver, Colo.	6	A. Benson	J. Loudon	G. Harvey
Detroit, Univ. of, Detroit, Mich.	5	H. O. Warner	B. J. Chartier	J. F. Jordon
Drexel Inst. of Tech., Philadelphia, Pa.	2	E. O. Lange	G. C. Mergner	I. L. Auerbach
Duke Univ., Durham, N. C.	4	O. J. Meier, Jr.	P. C. Sherertz	S. C. Clark
Florida, Univ. of, Gainesville, Fla.	4	E. F. Smith	M. Adams	B. Martin
George Washington Univ., Washington, D. C.	2	A. G. Ennis	F. Holcomb	E. D. Schreiner
Georgia School of Tech., Atlanta, Ga.	4	H. B. Duling	J. E. VanNess	J. M. Walker
Harvard Univ., Cambridge, Mass.	1	J. D. Cobine		A. R. Eckels
Idaho, Univ. of, Moscow, Idaho	9	R. H. Hull	K. Andrew	R. Haegele
Illinois Inst. of Tech., Chicago, Ill.	5	E. H. Freeman	J. McDonald	I. Kaluzna
Illinois, Univ. of, Urbana, Ill.	5	E. A. Reid	V. A. Rydbeck	J. E. Hinchcliffe
Iowa State College, Ames, Iowa	5	B. S. Willis	D. Kuester	F. Schatz
Iowa, Univ. of, Iowa City, Iowa	5	H. R. Reed	W. C. Morrison	J. S. Norton
Johns Hopkins Univ., Baltimore, Md.	2	F. Hamburger, Jr.	C. B. Boenning	J. C. Burdette
Kansas State College, Manhattan, Kan.	7	J. E. Ward, Jr.	R. Slinkman	Wm. Dague
Kansas, Univ. of, Lawrence, Kan.	7	G. A. Richardson	G. Johnson	B. Petree
Kentucky, Univ. of, Lexington, Ky.	4	E. A. Bureau	B. E. Rector	B. L. Patton
Lafayette College, Easton, Pa.	2	F. W. Smith	R. Wright	R. Crellin
Lehigh Univ., Bethlehem, Pa.	2	J. L. Beaver	F. H. Bower	C. S. Bennett
Louisiana State Univ., Baton Rouge, La.	4	A. K. Ramsey	A. Switzer	C. Mathews
Louisville, Univ. of, Louisville, Ky.	4	S. T. Fife	R. P. Owen	E. L. Witten
Maine, Univ. of, Orono, Me.	1	N. E. Wilson	R. D. Moulton	D. E. Davis
Manhattan College, New York, N. Y.	3	H. J. Lockwood	T. Madigan	H. Eicher
Marquette Univ., Milwaukee, Wis.	5	E. W. Kane	R. F. Loos	L. Flynn
Maryland, Univ. of, College Park, Md.	2	L. J. Hodgins	A. Deming	G. Reynolds
Mass. Inst. of Tech., Cambridge, Mass.	1	H. B. Dwight	L. D. Schwartz	J. L. Hummer
Michigan Col. of Min. & Tech., Houghton, Mich.	5	C. Russell	C. C. Avery	R. E. Sherman
Michigan State Col., E. Lansing, Mich.	5	M. M. Cory	W. C. Buwalda	R. G. Parkhurst
Michigan, Univ. of, Ann Arbor, Mich.	5	J. S. Gault	R. Ehrlich	J. M. Dufl
Milwaukee Sch. of Engg., Milwaukee, Wis.	5	E. L. Wiedner	L. J. Karaus	M. E. Lahti
Minnesota, Univ. of, Minneapolis, Minn.	5	J. H. Kuhlmann	R. Engquist	T. Moorhouse
Mississippi State Col., State College, Miss.	4	H. C. Simrall	P. W. Griffin	K. E. Marshall
Missouri Sch. of Mines & Met., Rolla, Mo.	7	J. S. Johnson	G. Skitek	R. Frame
Missouri, Univ. of, Columbia, Mo.	7	D. Waidelich	R. S. Morrison	E. Vredenburgh
Montana State College, Bozeman, Mont.	9	G. Fiedler	D. Kampschror	R. Auclair
Nebraska, Univ. of, Lincoln, Neb.	6	L. A. Bingham	Q. L. Bonness	F. L. Martinson
Nevada, Univ. of, Reno, Nevada	8	S. G. Palmer	J. Goetz	I. L. Carter
Newark Col. of Engg., Newark, N. J.	3	F. A. Russell	A. W. Earl	E. E. Reed
New Hampshire, Univ. of, Durham, N. H.	1	W. B. Nulsen	R. Foley	R. Deming
New Mexico State Col., State College, N. M.	7	M. A. Thomas	M. Kuester	L. Gardenhire
New Mexico, Univ. of, Albuquerque, N. M.	7	H. L. Jones	L. Williams	N. Maguire
New York, Col. of the City of, New York	3	Harry Baum		
(Day Division)			E. Piller	S. Chaika
(Evening Division)			D. Wolfand	R. Chaber
New York Univ., New York	3	P. C. Cromwell		
(Day Division)			D. Beck	E. Eaton
(Evening Division)			A. Ennist	A. Johanson
North Carolina State Col., Raleigh, N. C.	4	L. M. Keever	W. C. Ranes	R. S. Kelly
North Dakota State Col., Fargo, N. D.	6	H. S. Rush	F. J. Palmer	H. Neverman
North Dakota, Univ. of, Grand Forks, N. D.	6	C. W. Rook	R. Gustafson	C. E. Groves
Northeastern Univ., Boston, Mass.	1	R. G. Porter		
(Division A)			J. R. Whitford	T. F. Mahoney
(Division B)			J. L. Paine	R. E. Minkwitz
Northwestern Univ., Evanston, Ill.	5	R. W. Jones	H. Schultz	A. Hayes
Norwich Univ., Northfield, Vt.	1	D. E. Howes	J. S. Allen	W. Flint
Notre Dame, Univ. of, Notre Dame, Ind.	5	J. A. Northcott	G. A. Haninger	E. P. Cleary

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Ohio State Univ., Columbus, Ohio	2	S. O. Evans	W. H. Stolmack	R. B. Livensparger
Ohio Univ., Athens, Ohio	2	W. M. Young	T. Raymond	Wm. Swinehart
Oklahoma A. & M. Col., Stillwater, Okla	7	A. Naeter	H. H. McCowen	J. O. Grantham
Oklahoma, Univ. of, Norman, Okla	7	C. L. Farrar	A. J. Torre	M. C. Walker
Oregon State Col., Corvallis, Ore	9	F. O. McMillan	L. E. Chaffin	D. Halfhill
Pennsylvania State Col., State College, Pa	2	P. X. Rice	G. Gimber	G. Dixon
Pennsylvania, Univ. of, Philadelphia, Pa	2	S. R. Warren, Jr.	C. J. Fowler	C. H. Colehower
Pittsburgh, Univ. of, Pittsburgh, Pa	2	R. C. Gorham	J. F. Orloff	D. F. Shankle
Porto Rico, Univ. of, Mayaguez, P. R.	3	G. F. Anton	R. A. Preston	J. E. Gomez
Pratt Institute, Brooklyn, N. Y.	3	D. H. Wright	W. C. Morch	D. J. Gagne
Princeton Univ., Princeton, N. J.	2	W. C. Johnson	D. S. Jamison	R. D. Veghte
Purdue Univ., Lafayette, Ind	5	J. H. Karr	J. T. Youngblobd	B. L. Parsons
Rensselaer Poly. Inst., Troy, N. Y.	1	E. D. Broadwell	J. P. VanDuyne	W. F. Croft
Rhode Island State Col., Kingston, R. I.	1	W. J. Mowbray	G. Jacobs	R. Paige
Rice Institute, Houston, Texas	7	C. F. Wischmeyer	W. F. Wohlt	E. F. Zagst
Rose Poly. Inst., Terre Haute, Ind	5	C. C. Knipmeyer	R. E. Miller	D. E. Criss
Rutgers Univ., New Brunswick, N. J.	3	J. L. Potter	R. Hennessy	W. Vandergrift
Santa Clara, Univ. of, Santa Clara, Calif	8	W. J. Warren	J. O. Beaumont	J. J. Malneritch
South Carolina, Univ. of, Columbia, S. C.	4	W. M. Bauer	F. Smith	W. G. Mikell
South Dakota State Col., Brookings, S. D.	6	Wm. H. Gamble	E. C. Edwards	G. A. Johnson
So. Dakota State School of Mines, Rapid City, S. D.	6	E. E. Clark	E. Bender	J. O. Johnson
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Southern Methodist Univ., Dallas, Texas	7	H. F. Huflman	E. H. Flath, Jr.	W. Baldwin
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Stevens Inst. of Tech., Hoboken, N. J.	3	Wm. L. Sullivan		
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Texas Technological Col., Lubbock, Texas	7	C. V. Bullen	C. Ingram, Jr.	H. C. Shuler
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Union Col., Schenectady, N. Y.	1	H. W. Bibber	G. Weed	D. Bock
Utah, Univ. of, Salt Lake City, Utah	9	L. D. Harris	R. H. Lee	R. A. Callister
Vermont, Univ. of, Burlington, Vt	1	R. O. Buchanan	M. L. Riggs	P. Waterman
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Virginia Poly. Inst., Blacksburg, Va	4	Claudius Lee	W. E. Haynes	J. T. Hanna
Virginia, Univ. of, University, Va	4	J. S. Miller, Jr.	A. L. Campbell	P. B. Peyton, Jr.
Washington, State Col. of, Pullman, Wash	9	H. F. Lickey	D. Ammerman	J. Johnson
Washington, Univ. of, Seattle, Wash	9	R. E. Lindblom	D. D. Donley	R. O. Petrich
Washington Univ., St. Louis, Mo	7	D. A. Fischer	S. Kaisel	J. L. Glaser
West Virginia Univ., Morgantown, W. Va	2	A. H. Forman	J. C. Amos	G. J. Batlas
Wisconsin, Univ. of, Madison, Wis	5	J. R. Price	E. H. Dickenson	A. Lytle
Worcester Poly. Inst., Worcester, Mass	1	V. Siegfried	W. Tunnicliffe	R. Boyce
Wyoming, Univ. of, Laramie, Wyo	6	G. H. Sechrist	R. Wilkes	S. Watt
Yale Univ., New Haven, Conn	1	A. G. Conrad	R. L. Ungvary	J. M. Rescoe

Total Branches..........124

Geographical District Executive Committees

District	Chairman (Vice-President, AIEE)	Secretary (District Secretary)	Chairman, District Committee on Student Activities
1 North Eastern	K. B. McEachron, General Electric Company, 100 Woodlawn Ave., Pittsfield, Mass.	R. G. Lorraine, General Electric Co., 1 River Road, Schenectady, N. Y.	A. G. Conrad, Yale University, New Haven, Conn.
2 Middle Eastern	C. A. Powel, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.	J. E. Treweek, Pennsylvania Power and Light Company, 117 E. Broad St., Hazleton, Pa.	R. C. Gorham, University of Pittsburgh, Pittsburgh, Pa.
3 New York City	C. R. Jones, Westinghouse Electric and Manufacturing Company, 40 Wall Street, New York, N. Y.	Roy L. Webb, Consolidated Edison Co. of New York, Inc., 4 Irving Place, New York, N. Y.	D. H. Wright, Pratt Institute, Brooklyn, N. Y.
4 Southern	J. Elmer Housley, Aluminum Co. of America, Alcoa, Tenn.	A. S. Hoefflin, Louisville Gas and Electric Co., 311 W. Chestnut St., Louisville, Ky.	E. A. Bureau, University of Kentucky, Lexington, Ky.
5 Great Lakes	A. G. Dewars, Northern States Power Company, 15 S. 5th St., Minneapolis, Minn.	N. C. Pearcy, Public Utility Engineering and Service Corporation, 231 South La Salle Street, Chicago, Ill.	H. O. Warner, University of Detroit, Detroit, Mich.
6 North Central	Arthur L. Jones, General Electric Co., P. O. Box 2331, Denver, Colo.	Murray G. Graff, General Electric Co., 650 17th St., Denver, Colo.	E. E. Clark, South Dakota State School of Mines, Rapid City, S. D.
7 South West	E. T. Mahood, Southwestern Bell Telephone Co., 11th and Oak Streets, Kansas City, Mo.	R. G. Kloeffler, Kansas State College, Manhattan, Kans.	W. F. Gray, Texas Technological College, Lubbock, Tex.
8 Pacific	Walter C. Smith, General Electric Co., 823 Russ Bldg., San Francisco, Calif.	E. M. Wright, Pacific Gas and Electric Co., 245 Market St., San Francisco, Calif.	F. W. Maxstadt, California Institute of Technology, Pasadena, Calif.
9 North West	E. W. Schilling, Montana State College, Bozeman, Mont.	Herold E. Murdock, Montana Power Company, 18 East Main Street, Bozeman, Mont.	R. E. Lindblom, University of Washington, Seattle, Wash.
10 Canada	C. A. Price, Canadian Westinghouse Co., Ltd., Sanford Ave. N., Hamilton, Ont.	John M. Somerville, Canadian Porcelain Co., Ltd., Box 428, Hamilton, Ont.	

NOTE: Each District executive committee includes also the chairmen and secretaries of all Sections within the District, and the District vice-chairman of the national membership committee.

Sections of the Institute

Name	District	When Organized	Membership Aug. 1, 1942	Chairman	Secretary	Secretary's Address
Akron	2	Aug. 12, '20	94	K. F. Sibila	A. G. Seifried	B. F. Goodrich Co., 500 S. Main St., Akron, Ohio
Alabama	4	May 22, '29	44	G. W. McCracken	M. V. Zimmerman	Commonwealth and Southern Corp., Birmingham, Ala.
Arizona	8	Mar. 22, '41	37	G. F. Maughmer	E. A. Gissel	Central Arizona Lt. & Pwr. Co., Phoenix, Ariz.
Boston	1	Feb. 13, '03	516	R. G. Porter	W. I. Middleton	Simplex Wire & Cable Co., Cambridge, Mass.
Central Indiana	5	Jan. 12, '12	133	J. R. Pies	C. R. Swenson	240 North Meridian St., Indianapolis, Ind.
Chicago	5	1893	836	J. C. Woods	E. C. Minteer	Minteer & Josler, 327 So. LaSalle St., Chicago, Ill.
Cincinnati	2	June 30, '20	214	E. F. Nuezel	F. J. Breen, Jr.	3821 Delmar Ave., Cheviot, Ohio
Cleveland	2	Sept. 27, '07	376	C. A. Harrington	D. Ramadanoff	The National Carbon Co., W. 73rd St. & N.Y.C.R.R., Cleveland, Ohio
Columbus	2	Mar. 17, '22	95	S. A. Martin	S. O. Evans	Ohio State University, Columbus, Ohio
Connecticut	1	Apr. 16, '21	333	A. G. Conrad	C. D. Hewitt	227 Church St., New Haven, Conn.
Denver	6	May 18, '15	180	H. B. Palmer	Hubert Sharp	Mountain States Tel. & Tel. Co., P. O. Box 960, Denver, Colo.
East Tennessee	4	Sept. 2, '36	145	R. A. Hopkins	B. H. McCoin	City of Knoxville Electric Dept., Knoxville, Tenn.
Erie	2	Jan. 11, '18	65	C. M. Davis	W. D. Bearce	4121 Sassafras St., Erie, Pa.
Florida	4	Jan. 28, '31	111	C. F. Titus	Joseph Weil	University of Florida, Gainesville, Fla.
Fort Wayne	5	Aug. 14, '08	93	S. D. Summers	W. W. Brooks	General Electric Co., Fort Wayne, Ind.
Georgia	4	Jan. 14, '04	119	J. M. Flanigen	Carl Evans	Electrical South, 1020 Grant Bldg., Atlanta, Ga.
Houston	7	Aug. 7, '28	133	P. H. Underwood	S. C. Commander	2615 Fannin St., Houston, Texas
Iowa	5	June 25, '29	84	L. A. Ware	W. B. Boast	Iowa State College, Ames, Iowa
Ithaca	1	Oct. 15, '02	53	H. G. Smith	A. B. Credle	Cornell University, Franklin Hall, Ithaca, N. Y.
Kansas City	7	Apr. 14, '16	137	C. G. Roush	C. M. Lytle	P. O. Box 679, Kansas City, Mo.
Lehigh Valley	2	Apr. 16, '21	188	E. R. Beers	L. Z. Ludorf	Penn. Power and Light Co., 135 N. Washington St., Wilkes-Barre, Pa.
Los Angeles	8	May 19, '08	540	T. M. Blakeslee	P. L. Johnson	Box 5300, Metropolitan Station, Los Angeles, Calif.
Louisville	4	Oct. 15, '26	73	M. M. Hughes	L. G. Weiser	Westinghouse Electric & Mfg. Co., 1618 Heyburn Bldg., Louisville, Ky.
Lynn	1	Aug. 22, '11	186	J. H. Goss	M. S. Wilson	General Electric Co., Factory 2nd L., West Lynn, Mass.
Madison	5	Jan. 8, '09	73	R. R. Knoff	E. J. Van Duesen	2702 Sommers Ave., Madison Wis.
Mansfield	2	Mar. 6, '39	71	H. O. Whiteley	J. M. Robinson	North Electric Mfg. Co., Galion, Ohio
Maryland	2	Dec. 16, '04	322	H. J. Casey	B. Van Ness	1503 Lexington Bldg., Baltimore, Md.
Memphis	4	May 23, '30	56	J. P. Argo	Herman Leightman	Memphis Light, Gas and Water Div., 179 Madison Ave., Memphis, Tenn.
Mexico	3	June 29, '22	53	G. Robles	J. Rosas	Mexican Lt. & Pwr. Co., Ltd., Gante No. 20, Mexico, D.F. Mexico
Michigan	5	Jan. 13, '11	418	M. B. Stout	J. W. Bishop	134 Englewood Ave , Detroit, Mich.
Milwaukee	5	Feb. 11, '10	313	F. W. Bush	T. B. Jochem	Cutler-Hammer, Inc., 315 N. 12th St., Milwaukee, Wis.
Minnesota	5	Apr. 7, '02	98	Wm. Endicott	C. W. Lethert	Northern States Power Co., 15 So. 5th St., Minneapolis, Minn.
Montana	9	June 24, '31	45	D. K. Brake	C. R. Davis	Montana Power Co., Butte, Montana
Muscle Shoals	4	Feb. 18, '38	33	R. C. Fuller	S. J. Wolfenson	T.V.A., Project W3, Wilson Dam, Ala.
Nebraska	6	Jan. 21, '25	59	J. M. Gibb	I. M. Ellestad	Northwestern Bell Telephone Co., Omaha, Nebr.
New Mexico-West Texas	7	Mar. 7, '40	43	F. A. Decker	L. R. Hammond	Phelps Dodge Corp., El Paso, Texas
New Orleans	4	Dec. 8, '33	133	J. V. Hay	F. E. Johnson	317 Baronne St., New Orleans, La.
New York	3	Dec. 10, '19	3,447	C. C. Whipple	M. D. Hooven	Public Service Electric & Gas Co., 80 Park Pl., Newark, N. J.
Niagara Frontier	1	Feb. 10, '25	198	J. F. Oehler	E. R. Paige	Buffalo Niagara Electric Corp., Buffalo, N. Y.
North Carolina	4	Mar. 21, '29	107	E. E. Kilburn	A. L. Humphrey	Tide Water Power Co., Warsaw, N. C.
North Texas	7	May 18, '28	170	T. D. Thomas	H. G. Mathewson	Southwestern Bell Telephone Co., Dallas, Texas
Oklahoma City	7	Feb. 16, '22	104	Ralph Randall	C. L. Jobe	Oklahoma Gas & Elec. Co., Oklahoma City, Okla.
Philadelphia	2	Feb. 18, '03	809	G. W. Bower	A. C. Muir	1138 Commercial Trust Bldg., Philadelphia, Pa.
Pittsburgh	2	Oct. 13, '02	662	B. M. Jones	G. W. Penney	Westinghouse Electric & Mfg. Co., E. Pittsburgh, Pa.
Pittsfield	1	Mar. 25, '04	194	C. W. Germeck	W. S. Fielding	General Electric Co., 100 Woodlawn Ave., Pittsfield, Mass.
Portland	9	May 18, '09	196	C. E. Canada	A. O. Mangold	Northwestern Electric Co., 920 S.W. Sixth Ave., Portland, Ore.
Providence	1	Mar. 12, '20	100	D. W. Alden	G. E. Andrews	40 Bridgham St., Providence, R. I.
Rochester	1	Oct. 9, '14	112	R. A. Whitford	J. H. Rogers	262 Albemarle St., Rochester, N. Y.
St. Louis	7	Jan. 14, '03	361	I. T. Monseth	O. T. Farry	Wagner Electric Corp., 6400 Plymouth Ave., St. Louis, Mo.
San Diego	8	Jan. 18, '39	54	B. B. Gravitt	W. J. Kenline	3491 Cooper St., San Diego, Calif.
San Francisco	8	Dec. 23, '04	549	B. L. Robertson	G. C. Tenney	68 Post St., San Francisco, Calif.
Saskatchewan	10	Oct. 14, '25	8	M. L. Haynes	J. R. Young	1945 Scarth St., Regina, Sask, Can.
Schenectady	1	Jan. 26, '03	559	T. M. Linville	P. H. Light	General Electric Co., Central Station Dept., Schenectady, N. Y.
Seattle	9	Jan. 19, '04	230	L. D. Snow	H. M. Gustafson	General Elec. Co., 1290 Dexter Horton Bldg., Seattle, Wash.
Sharon	2	Dec. 11, '25	153	W. L. Teague	H. S. Gates	1583 McDowell St., Sharon, Pa.
South Bend	5	Feb. 26, '41	58	J. A. Northcott, Jr.	H. E. Ellithorn	417 Parkovash Ave., South Bend, Indiana
South Carolina	4	Mar. 2, '40	53	F. W. Chapman	W. H. Kendrick	South Carolina Electric & Gas Co., Columbia, S. C.
South Texas	7	May 23, '30	53	W. F. Pinckert	G. E. Schmitt	Lower Colorado River Authority, Austin, Texas
Spokane	9	Feb. 14, '13	82	H. J. Reeves	R. C. Kelly	Washington Water Power Co., 825 W. Trent Ave., Spokane, Wash.
Springfield	1	June 29, '22	52	Isaiah Creaser	L. P. Kongsted	American Bosch Corp., Springfield, Mass.
Syracuse	1	Aug. 12, '20	119	S. C. Osborne	M. H. Pratt	Central N. Y. Power Corp., 300 Erie Blvd., W., Syracuse, N. Y.
Toledo	2	June 3, '07	89	W. C. Champe	W. M. Campbell	2145 Central Grove Ave., Toledo, Ohio
Toronto	10	Sept. 30, '03	396	D. W. Callander	T. C. D. Churchill	76 Adelaide St., West, Toronto, Ont., Canada
Tulsa	7	Oct. 1, '37	87	O. A. Haas	H. A. Norberg	Nelson Electric Mfg. Co., 219 N. Detroit, Tulsa, Okla.
Urbana	5	Nov. 25, '02	78	M. A. Faucett	W. E. Miller	University of Illinois, Urbana, Ill.
Utah	9	Mar. 9, '17	64	Wm. N. Grooms	J. A. McDonald	General Electric Co., P. O. Box 779, Salt Lake City, Utah
Vancouver	10	Aug. 22, '11	95	T. Ingledow	F. V. Knight	1796 W. 15th Ave., Vancouver, B. C., Can.
Virginia	4	May 19, '22	150	E. S. Fitz	W. A. Murray	Virginia Poly. Inst., Blacksburg, Va.
Washington	2	Apr. 9, '03	600	G. R. Wilhelm	F. S. Black	Potomac Electric Power Co., 10th and E Sts., N.W., Washington, D. C.
West Virginia	2	Apr. 9, '40	39	E. D. Knight	R. C. Hoffman	Appalachian Electric Power Co., Charleston, W. Va.
Wichita	7	Sept. 16, '37	57	J. F. Huff	R. F. Dice	Kansas Gas and Electric Co., Wichita, Kans.
Worcester	1	Feb. 18, '20	63	W. W. Locke, Jr.	T. R. Holton	American Steel & Wire Co., Worcester, Mass.
			16,650			
Total Sections	72					